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UMI®
THERMAL MANAGEMENT AND PREDICTION OF HEAT CHECKING 
IN H-13 DIE-CASTING DIES

DISSERTATION

Presented in Partial Fulfillment of the Requirements for

the Degree Doctor of Philosophy in the Graduate

School of The Ohio State University

By

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ABSTRACT

The theoretical aspects of the die-casting process have long been neglected while considerable efforts have been devoted to development of powerful hardware and complex measuring equipment, and improvement of die and casting materials by the die-casting industry and academia. The lack of insight into the theory of the process has resulted in many failures during processing. These failures have been caused by wrongly chosen process parameters and inappropriate die designs. Failures have lead to an increasing interest in optimizing the die design and the process conditions. An effective way of achieving this is to use numerical calculations to simulate the process conditions avoiding a prolonged and costly trial and error stage prior to actual production. Computer simulations are capable of producing vital information for filling and solidification analyses and for determining the conditions present within the dies during operation.

Working conditions of die cavity surfaces are the major factors influencing die service life. The service life of a die-casting die is crucial, since its cost is high. The quality of castings and the scrap rate is also determined by the conditions of the die cavity surfaces. The reasons mentioned above make the study of one of the most common failure modes, thermal fatigue, of dies essential. Thermal fatigue is a product of rapid and cyclic thermal loads as well as the die geometry and is accelerated by other damage mechanisms. The initial phase of the thermal fatigue starts with formation of micro-crack networks referred as heat checking. Heat checking results in deterioration of die cavity surfaces. The issue of thermal management of dies also needs to be studied to determine the thermal fields and consequent structural (stress and strain fields) state within the dies.
Thermal management covers the means of internal cooling with cooling lines, outside cooling of die cavity surfaces with spray, natural convection and radiation to the environment, and the heat transfer at the die/insert interfaces of the die/casting system.

The objective of this study was to study thermal and structural issues, to develop an analytic approach to diagnose areas susceptible to heat checking, and to estimate the number of shots to the onset of heat checking. This will lead to determination of ways of running dies to keep the heat checking problem under control. Preliminary efforts included collecting thermal process, die material property and fatigue data through a literature survey. Spray tests were also conducted to determine heat transfer coefficients during lubricant spray. This was followed by the thermal and structural analyses of a 1-D simple die/casting system. The objective of the first activity was to identify the impact of factors such as thermal process parameters, physical properties of the die material, and the simple geometric elements on the quasi-steady state thermal fields. Results of 1-D thermal study helped understanding the thermal fields through a very detailed data and eliminating some factors as dominant players in the course to thermal fatigue phenomenon.

The second activity was a further analysis of associating the previously mentioned parameters with the structural (stress/strain) state within tooling. Structural analysis identified the most effective parameters to reduce the structural abuse on the dies.

The research efforts were concluded with a compound study of thermal, structural and fatigue analyses of an actual die-casting application with a major heat-checking problem. The thermal and structural analyses yielded reasonable and acceptable results, while the fatigue calculations overestimated the number of cycles known to produce heat checking. The discrepancy between the actual cycles to failure and the estimations was caused by the simplifications of the die geometry, lack in the existing fatigue models and appropriate (high temperature low frequency) fatigue data for H-13. On the contrary, the structural and fatigue analyses were successful in the diagnostics of the critical tooling areas which are prone to heat checking. This final research activity also included issues
such as tempering, cyclic loading, elevated temperature material properties, temperature cycling and, influence of mechanical loading on the structural state within tooling.
Dedicated to my wife, Aleea, to my daughter Selin and to my parents, Celal and Suzan Sirinterlikci for all their love and support.
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CHAPTER 1

INTRODUCTION

1.1 Introduction and Problem Statement

It is necessary to reduce production costs in today's market environment. In the die-casting process, reducing the lead-time of the process and tooling design, and minimizing the trial-error stage of production can help achieve this goal. It is also necessary to prolong the die service life and prevent catastrophic die failures since the die cost is one of the major contributors to the overall cost of the process.

Die-casting dies are subjected to high mechanical and thermal loads, which can cause catastrophic or delayed die failure due to accumulated damage. Most failures develop slowly and may be predicted. However, sudden catastrophic failures after a few cycles can cause large economic losses due to destroyed tooling, expensive down times, and disturbance of delivery schedules. To predict and avoid the die failures and their consequences, it is essential to understand the thermal management of dies, the interactions among the thermal and stress/strain fields, material properties, and their influence on die life. If the impacts of these factors on die life are well known and can be utilized during the design stage of dies and casting process, prolonged die service life can be achieved. A more reliable prediction of die life will also enable more accurate estimates of actual die and production costs, and will reduce undesired machine down times and will help in achieving a better production management during production activities.
Thermal fatigue is one of the most common problems encountered in the die-casting process. It is a result of cyclic, rapid, non-uniform heating and cooling of dies. It is also accelerated by mechanical loading and other damage mechanisms such as erosion and corrosion. Thermal fatigue is one of the major causes of poor quality castings and die failures. The initial phase of the thermal fatigue starts with the formation of micro-crack networks referred as heat checking. Heat checking results in deterioration of die cavity surfaces. Further damage due to the heat checking will lead to sticking and defective castings and subsequent die damage and failure.

If an unconstrained object is slowly and uniformly heated or cooled, it expands or contracts proportionally to its coefficient of thermal expansion and the temperature change encountered in relation to a reference temperature. However, if the body is constrained during the heating and cooling process, it will develop stresses and strains since it cannot reach its unconstrained dimensions. Also, if a body doesn’t load and release through the same stress-strain path, repeated or cyclic heating and cooling might result in accumulation of inelastic deformation. If the time varying stress within the body is tensile, accumulation may lead to thermal fatigue. Even an unconstrained body may develop large thermal stresses if exposed to non-uniform or rapid heating and cooling. Objects having sudden geometric and compositional variations also include internal constraints caused by temperature distributions within them. In the case of rapid surface temperature changes of a solid body, the temperature of near surface layers increase and decrease rapidly while the rest of the body cannot respond fast enough to the changes. Differential expansions and contractions within the body provide internal constraints while various layers expand and contract by different amounts. Each region is constrained by its neighbors. The constraints induce a stress-strain field within the body [Knoerr, 1992].

Thermally induced stresses are not the only factors involved in the thermal fatigue phenomenon. Elevated temperature die material properties, mechanical loads from filling and locking, residual stresses and cavity surface conditions also affect the material’s response to heat checking and thermal fatigue. Tempering, decarburization, phase or
structure changes due to exposure to elevated temperatures accelerate the thermal fatigue. Damage mechanisms such as corrosion and erosion are the other factors which may contribute to the thermal fatigue damage as shown in Figure 1.1.

Existing thermal fatigue studies are focused on different areas such as fabrication of tools, material issues including material response of tool steels to thermal fatigue conditions. Little has been done in the areas of thermal process, thermally induced structural state and its contribution to heat checking. The literature does contain several studies, most of which are recent, focusing on determination of thermal fatigue damage. The objective of this dissertation is to study the conditions leading to the thermal fatigue phenomenon through numerical analysis tools. Furthermore, by combining existing and new data on heat transfer in die-casting dies and die material properties with Finite Element Analysis modeling, a method for predicting the onset of heat checking in H-13 dies can be developed. A case study will be the method of testing this hypothesis.

1.2 Research Approach

The research approach and the methods utilized are briefly presented below. As stated previously, the goal of this dissertation is to study the thermal fatigue and heat-checking phenomena to develop an effective approach to determine the onset of heat checking. To achieve this goal the steps below were followed.

- Study of thermal management, thermal fatigue and heat checking concepts
- Collection of thermal process, die material property (mechanical and thermophysical) and thermal and mechanical fatigue data
- Thermal study of die-casting dies through 1-D BINORM analysis
- Thermal-Structural study of die-casting dies through 2-D ABAQUS modeling
- A case study: thermal/structural and fatigue analyses of an actual die-casting application with a major heat checking problem.
Figure 1.1: Cause and effect diagram for heat checking and thermal fatigue in die-casting die
The first step of the dissertation was to study thermal management, thermal fatigue and heat checking concepts. This was followed by data gathering to achieve a reliable and accurate numerical study. A literature survey was conducted. This survey helped in collecting thermal process and fatigue data as well as mechanical and thermophysical properties of a commonly used die material, H-13 tool steel for die cast aluminum components. Spray tests were also conducted to obtain the heat transfer coefficient during lubricant spray under die-casting conditions.

After the completion of data gathering, a 1-D thermal study of die/casting system was conducted. BINORM, a thermal analysis code based on bi-normal solidification, was utilized for this portion of the study. The main objective of this study was to identify the impact of factors such as thermal process parameters, physical properties of the die
material and simple geometric elements such as die and casting thickness on the thermal fields. This study also provided as a sensitivity analysis where the variation in data from literature and spray tests was utilized in analyzing thermal fields.

Further analysis of dies was achieved through associating some of the previously mentioned factors which influence the temperature profile and the thermal gradients with the structural state within tooling. This step was conducted employing the temperature profiles obtained through previous 1-D thermal analysis and imposing them along the die cavity/casting interface in 2-D thermal FEM (finite element) models. Thermal finite element analysis was conducted through ABAQUS, and this was followed by a sequentially coupled structural analysis with the same software.

The case study was the major effort understanding the thermal and structural conditions within an actual die-casting tooling with a major heat checking problem. For thermal analysis, two different FEM programs, PROCAST and ABAQUS were used. Procast was used to model the thermal field in the die and ABAQUS was used to model the stress and strain fields in the die, consistent with the modeled thermal field. Once the structural analysis results were obtained, they were utilized in the fatigue analysis. The final step of the case study, the fatigue analysis, included the study of tempering and elevated temperatures on the die material and also fatigue properties, correlations between the material properties and the onset of heat-checking for further improvements of the current fatigue models. Influences of cyclic and monotonic loading on mechanical properties of the die material were also discussed. A final chapter of conclusions and the future work is given.

1.3 Outline of the Dissertation

The outline of this dissertation is summarized in this section of this introductory chapter, Chapter 1. Chapter 2 includes a wide range of information and data produced in the thermal management and fatigue areas. Thermal management and heat transfer issues
in the die/casting system are the focus of the first section of Chapter 2. Basic tools and methods of thermal management of die-casting dies are included within the chapter as well as a wide range of thermal process and heat transfer data. The second section of Chapter 2 covers the die failure modes and especially heat checking and thermal fatigue mechanisms in great details. A literature review consisting of 3 groups of research papers are presented in the third section of Chapter 2. The literature survey includes resources concerning thermal/stress and fatigue analyses, fabrication of tool steels (H-13 and similar martensitic alloy steels) and dies, thermal fatigue testing of materials. Impacts of factors such as thermal process parameters, physical properties of the H-13 tool steel and geometric factors of the die/casting system on the thermal state of dies were studied and presented through 1-D thermal models in the first section of Chapter 3. Chapter 3 in its second section also includes the impact of the same factors, which were mentioned in the first section, on the structural state within the tooling. Chapter 4 covers the effort of establishing an effective thermal fatigue approach through numerical analysis. The chapter also includes the current available thermal fatigue data and the needs for numerical determination of onset of heat checking. An actual case with a major heat-checking problem was studied and the outcome is presented in this chapter. A final chapter of conclusions and the future work are presented in Chapter 5.
CHAPTER 2

LITERATURE REVIEW

2.1 Thermal Management and Heat Transfer in the Die/Casting System

2.1.1 Thermal Management of Dies

The die-casting process is one of the most commonly used manufacturing processes and is still technologically advancing. The process is being used to cast larger and far more complex parts. The nature of the process is that the molten metal is injected into a cavity under high pressure. The cavity is filled within hundredths of a second with the molten metal. Once the melt has solidified and cooled, the casting is ejected from the die and the die surface is sprayed for cooling and deposition of a lubricant layer. Dies are closed a few seconds after spray stage is completed. The closed dies are then ready for receiving a new shot [Hattel et al, 1993].

Management of the heat content during processing not only affects the stress-strain state within the dies but also the part quality and scrap rates. Thermal cycling is controlled by the cycle time (the production rate). It is a major factor determining the service life of dies.

Thermal management of dies cannot be realized without investigating the heat input from the casting into the dies and studying the heat flow patterns with the heat removal means as shown in Figure 2.1.
Figure 2.1: Thermal management of dies (a) dies are open (b) dies are closed [Sully, 1988]
The thermal cycle consists of a sudden thermal heat input into the dies from castings, heat dissipation from castings during solidification and cooling, an open die stage, and spray cooling of die cavity surfaces. Figure 2.2 shows a die cavity surface temperature profile for a 30 second aluminum die casting cycle in H-13 dies.

![Figure 2.2: Representative die cavity surface temperature history for an aluminum alloy die-casting cycle](image)

Two regions within the dies are established with the thermal cycling as shown in Figure 2.3. The first region is the transient thermal layer which may continue up to one-inch depth from the die cavity surface. Within this layer temperatures fluctuate depending
upon their distances to the die cavity surface. Points, which are closer to the die cavity surface, will be exposed to greater fluctuations. The second layer behind the first layer experiences little change in temperatures and thermal gradients [Osborne, 1995].

The depth of the transient layer is determined by such factors as geometry of the cavity surface details, the heat input, the casting cycle time, the heat transfer coefficient at the die/casting interface, and the thermal diffusivity of the die material. The second layer, with almost constant thermal gradient, is mainly affected by the casting cycle time and the thermal conductivity of the die material [Booth et al, 1981]. The thermal gradient for different sections of the die will differ since it is a function of geometry and the thermal conditions. The thermal gradient indicates the rate of heat removal from each section. It can be estimated using the tools mentioned below.

Figure 2.3: Transient heat flow [Barone et al, 1993]
There are several methods available for analyzing the heat input and consequent thermal removal requirements of die casting dies. The tools used could vary within a wide range, which would include ones such as analytical methods, simple hand calculations based on analytical methods, numerical investigations through finite element and finite difference analyses.

The most common and basic tool used is the overall heat balance. The heat content for the alloy between the injection and ejection is calculated. This is the amount of heat which must be removed from the casting and consequently from the dies. After the determination of heat input into the dies, the proportion of heat removal by different mechanisms is determined. The preheating unit capacity can also be obtained through the overall heat balance method. The preheating process is aimed to heat the tooling to higher temperatures than the room temperature prior to actual production to shorten the time of reaching the quasi-steady state and to reduce the stress levels during this warm up stage before reaching the quasi-steady state. There have been other methods, which are purely based on geometric considerations. The geometric shapes and die detail segments are utilized within the thermal models to estimate the heat flow within the dies and at the die/casting and die/waterline interfaces [Herman, 1996]. With the employment of computers the design of dies and the means of heat removal are more accurate than they have been in the past. In today’s die casting engineering, finite-element and finite-difference methods are commonly used to solve the heat transfer problem within complex dies/casting systems.

The heat input into the dies is removed by,

- Spray cooling of die cavity surfaces
- Cooling passages – waterlines and fountains
- Heat radiation and natural/forced heat convection from the open die surfaces to the surroundings
- Conduction to the die-casting machine platens
The heat flow within the dies can be influenced by heat dams by forcing it towards a desired direction to prevent congestions and by using oil or electric heating units to balance the thermal state at certain sections of dies to prevent unwanted rapidly cooled areas. Heating units are also utilized to preheat the dies to desired initial temperatures.

Previous work has been focused on the amount of heat introduced to the dies and the amount removed by different heat removal means. Most of the heat input comes from the release of the latent heat of solidification or fusion. Figure 2.4 shows the percent estimates of heat input sources and contribution of different heat removal mechanisms. The major portion of the heat removal is accomplished by heat conduction through the die halves by means of internal cooling [Bednareck et al, 1991].

The cooling passages (lines) are the major internal cooling tools. Water is the most commonly used fluid in cooling passages. The factors affecting the heat removal of cooling line are:

- Location of cooling lines (proximity to the die cavity surface)
- Size of cooling lines
  - Diameter
  - Length
- Flow rate and type
  - High velocity (turbulent) flow
- Number of rows used
  - Single row
  - Multiple rows
- Heat transfer coefficient at the cooling line
  - Heat transfer coefficient determined by thermo-physical properties of cooling fluid
  - Scale formation within the cooling line
Cooling lines are heat exchangers which remove the heat input continuously throughout the process. The location and the size of the cooling lines are important when designing the dies to avoid congested heat flow paths and insufficient heat removal. Cooling lines are also one of the major factors determining the quality of the castings and the production rate.

Figure 2.4: Heat input and dissipation in aluminum die-casting [Bednareck et al, 1991]
Turbulent flow with higher fluid flow velocities has a better cooling performance by increasing the cooling line interface heat transfer coefficient. Turbulent flow can be obtained by twisted baffle inserts, coil springs or artificial ribs within the cooling passages [Incropera et al, 1990]. Nelson and Tobin conducted extensive studies on waterlines. Tobin concluded that as flow rates in some cooling lines decreased, the other lines compensated the reduced performance of heat removal. Lower water inlet temperatures gave better cooling performance [Tobin, 1994]. Reduced flow rates increased the cooling line wall temperature. However, the wall temperature was found to be independent of water temperature.

Scale formation to a thickness of 0.005" caused a 38.5% reduction in heat transfer coefficient [Nelson, 1968]. Scale is the undesired solid content of the cooling water and is deposited on the wall of the cooling line during the cooling process. To prevent the scale formation ethylene glycol is often added to the cooling water. Heat removal rates also fall as the percent ethylene glycol increased. Water-ethylene glycol mixtures with higher dynamic viscosity consequently result in a decrease in the interface heat transfer coefficient. The location of the waterlines was found to have no significance for the transient cooling response [Hsieh, 1989]. Other studies on multiple lines indicated that the waterlines that are spaced far apart have less cooling effect [Groeneveld et al, 1975] and closer waterlines to die cavity surface with larger diameters will serve better for controlling die cavity surface temperatures [Kaiser et al, 1972]. The heat removed by the cooling lines can be determined by the expressions mentioned above and through the general conduction and cooling laws.

In general practice, cooling lines are the last feature to be located within the dies and the contribution of cooling lines to heat checking of die cavity surfaces is less when compared to the spray cooling. Although the cooling line’s effect on heat checking of die cavity surfaces is less significant compared to spray cooling, some studies reported the presence of thermal fatigue cracks at the die/waterline interfaces [Prystay et al, 1996].
Spray also plays a crucial role in cooling. It provides rapid cooling for the die cavity surface, which is hot after the completion of ejection stage, and helps in reducing the cycle time. The spray causes large temperature gradients near the die cavity surface. The heat flow is reversed during filling causing a reversal in the stress state. If temperature history below the die cavity surface is recorded, a steep drop in the temperatures are encountered especially at the very beginning of the spray. Temperature drop continues throughout the spray with a decaying rate. As shown in Figure 2.6, after the completion of spray, die temperatures rise almost up to the point when the spray was initiated.

Figure 2.5: Heat flow paths, locating the waterlines and heat dams (W.L.: waterlines, A-X indicating different areas and flow paths) [Herman, 1996]
Figure 2.6: Thermal response of substrate to spray [Lee et al., 1991]
Spraying also allows lubricant deposition onto the cavity surfaces. The lubricant layer will work as a heat regulator and separator between the molten metal and the die cavity surface during filling and solidification and a release agent during ejection of castings [Schmidt, 1981]. The factors and parameters affecting spray cooling performance and lubricant deposition are:

- **Spray parameters**
  - Spray distance
  - Spray angle
  - Spray pressure/flow rate
  - Spray volume (duration)
  - Dilution ratio (for emulsion spray)
- **Spray program**
  - Continuous spray
  - Pulsating spray
- **Spray practice**
  - Emulsion (Lubricant diluted with water)
  - Water + lubricant + air spray
- **Thermal-physical properties of lubricants**
  - Surface tension relation to die surface
  - Viscosity
  - Density
  - Vapor pressure as a function of temperature
  - Latent heat of vaporization
  - Specific heat
  - Thermal conductivity
  - Solid fraction and the average size of the solid particles
Bishop studied the effect of varying spray fluid temperature and spray pressure with a die block at an initial temperature of 260°C. The spray fluid temperature was found to have no significant effect on the heat removal. The initial mode of heat transfer during spraying was transitional boiling mixing the nucleate and the film boiling phenomena. The nucleate boiling regime was determined to start within the range of 166-216 °C of die block surface temperature [Bishop et al, 1990]. Chabbra extended the work of Bishop, Miller and Altan [Chabbra et al, 1991] by studying spray parameters such as pressure, flow rate, angle, distance and program. The outcome of his study revealed the following:

- A spray program with a combination of high and low pressure provided the optimal heat transfer per unit volume coolant.
- There is a critical flow rate beyond which any increase did not raise the heat removal rate.
- The spray angle within the range of 0-30 degrees to the normal of the die plate had no effect on heat removal rates.
- Closer spray distances exhibit better cooling performances.

Cooling effects of lubricants were studied through experimental and numerical investigations. The effects of initial die cavity surface temperature and spray fluid density (spray volume per-unit surface area per-unit time as defined by Lee) were the focus of the studies conducted by several researchers [Lee et al, 1991][Graff et al, 1993]. Figure 2.7 indicates water spray heat transfer coefficients reported by various studies. Figure 2.8 shows the effects of initial die surface temperature and liquid flux density on spray heat transfer coefficients [Lee et al, 1991].

In the past few years, there have been numerous other studies on spray cooling and lubricant deposition performance. Estimations of the spray's contribution to the heat removal process varied within the range of 5-30% [Bednareck et al, 1991][Brennan, 1994].
Mobley and Lucas developed a method to study the lubricant deposition thickness under various spray and initial die temperature conditions [Lucas, 1995]. Several other studies were focused on the lubricant chemistry [Fraser et al, 1997], application of lubricants and the effects of friction [Aoyama et al, 1991]. In one study, ejection forces were measured by placing strain gauges on the ejector pins [Tosa et al, 1972]. Properties such as surface tension, viscosity, density, and latent heat of vaporization, specific heat, thermal conductivity and solid fraction have also been studied. A study of the performance of lubricants by the Ohio State University is underway and is directed by Brevick and Mobley [Osborne et al, 1997].

![Figure 2.7: Heat transfer coefficients as a function of initial surface temperature and water spray flux density [Lee et al, 1991]](image-url)
Figure 2.8: Spray heat transfer coefficients as a function of initial surface temperature and liquid flux density [Lee et al, 1991]
The caster can control the spray method and cooling line design. A small amount of radiation and convection to the surroundings occurs while the dies are open. There has not been too much attention given to these two mechanisms since they are less significant compared to spray and internal cooling. Another reason these mechanisms were not in focus is that there is no direct control over radiation that can be established once the die material, environment and die process conditions are determined. Additional devices such as fans are used to increase heat removal from open die surfaces. Without the employment of such devices as fans, convection phenomenon cannot be controlled. The natural convection will be driving mechanism with the absence of such devices.

2.1.2 Heat Transfer at the Interfaces

A die casting cycle cannot be characterized by a single heat transfer coefficient at the die cavity surface (die/casting interface) since it consists of different stages. These stages were mentioned in the early sections of this chapter.

The presently available data for heat transfer coefficients at the die/casting interface, especially for the filling, solidification and the cooling stages vary within a wide range. The differences can be explained by the differences in geometry of the castings/dies, the interface and casting process conditions, and the methods to measure and analyze the data.

Table 2.1 includes the literature review conducted by Papai and Mobley [Papai et al, 1993]. Reported values vary within the range of 3.3 to 87 KW/m²K. The smallest value is still greater than that of gravity casting. For small spin casting thickness values of 45-85 μm of Ni-20%Al with a substrate surface velocity of 11.8-16 meters per second heat transfer coefficients of 120-255 kW/m²K were obtained by Vincent [Vincent et al, 1987]. The figures reported for the spin casting process are greater than the highest values experienced in the die-casting process.
<table>
<thead>
<tr>
<th>Casting Material</th>
<th>Casting Thickness</th>
<th>Mold Material</th>
<th>Surface Coatings</th>
<th>$h$ (kW/m$^2$K)</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>A380</td>
<td>19.64 mm</td>
<td>Low carbon steel</td>
<td>76 μm hydrocarbon</td>
<td>7.0 in runner</td>
<td>[Lindsey, et al, 1968]</td>
</tr>
<tr>
<td>A380</td>
<td>1.5-7 mm</td>
<td>H-13</td>
<td>-</td>
<td>9.0 in runner</td>
<td>[Lindsey, et al, 1968]</td>
</tr>
<tr>
<td>99.9 Al</td>
<td>50 mm diameter</td>
<td>Steel</td>
<td>20-40 mm graphite</td>
<td>20 at 30 MPa</td>
<td>[Nishida, et al, 1976]</td>
</tr>
<tr>
<td>A380</td>
<td>1.59 mm</td>
<td>H-13</td>
<td>None or water/ Hydrocarbon</td>
<td>79-87</td>
<td>[Hong et al, 1979]</td>
</tr>
<tr>
<td>Al-Si</td>
<td>-</td>
<td>Steel</td>
<td>-</td>
<td>50 at 38 MPa</td>
<td>[Davies, 1980]</td>
</tr>
<tr>
<td>Al</td>
<td>85 mm</td>
<td>Steel</td>
<td>-</td>
<td>3.3</td>
<td>[Prates et al, 1972]</td>
</tr>
</tbody>
</table>

Table 2.1: Experimental interface heat transfer coefficients for aluminum casting in steel dies

<table>
<thead>
<tr>
<th>Casting Material</th>
<th>Mold Material</th>
<th>$h$ (kW/m$^2$K)</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al</td>
<td>H-13</td>
<td>10</td>
<td>[Brennan, 1994]</td>
</tr>
<tr>
<td>Al</td>
<td>H-13</td>
<td>10</td>
<td>[Barone et al, 1993]</td>
</tr>
<tr>
<td>Al</td>
<td>H-13</td>
<td>20.8</td>
<td>[Volz et al, 1995]</td>
</tr>
</tbody>
</table>

Table 2.2: Other reported interface heat transfer coefficients for aluminum casting in steel dies
In their study of casting A380 in H-13 dies, Papai and Mobley concluded that the die casting coefficients observed at the die/casting interface were about 80 kW/m² K for the rough surfaced die with initial alloy temperature of 650 °C. The smooth surfaced dies had half the values for the same furnace temperature. The higher temperature alloy caused greater heat transfer coefficients. Using time-averaged values was also found to be a good assumption to analyze a casting cycle [Papai et al, 1993]. Table 2.3 shows the data obtained by Papai and Mobley.

<table>
<thead>
<tr>
<th>Surface Condition and Melt Temp. (°C)</th>
<th>Lubricant</th>
<th>Pressure</th>
<th>h (kW/m² K) Inside Surface</th>
<th>h (kW/m² K) Outside Surface</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rough, 650 Light Low</td>
<td></td>
<td>Low</td>
<td>81.63</td>
<td>45.23</td>
</tr>
<tr>
<td>Rough, 650 Light High</td>
<td></td>
<td>High</td>
<td>93.02</td>
<td>60.68</td>
</tr>
<tr>
<td>Rough, 650 Heavy High</td>
<td></td>
<td>High</td>
<td>84.75</td>
<td>78.66</td>
</tr>
<tr>
<td>Rough, 650 Heavy Low</td>
<td></td>
<td>Low</td>
<td>91.51</td>
<td>82.16</td>
</tr>
<tr>
<td>Rough, 650 Light Low</td>
<td>Heavy</td>
<td>Low</td>
<td>73.04</td>
<td>76.08</td>
</tr>
<tr>
<td>Smooth, 650 Light Low</td>
<td></td>
<td>Low</td>
<td>34.80</td>
<td>35.09</td>
</tr>
<tr>
<td>Smooth, 650 Light High</td>
<td></td>
<td>High</td>
<td>33.10</td>
<td>48.48</td>
</tr>
<tr>
<td>Smooth, 650 Heavy High</td>
<td></td>
<td>High</td>
<td>34.53</td>
<td>37.28</td>
</tr>
<tr>
<td>Smooth, 650 Heavy Low</td>
<td>Heavy</td>
<td>Low</td>
<td>39.97</td>
<td>34.48</td>
</tr>
<tr>
<td>Smooth, 650 Light Low</td>
<td></td>
<td>Low</td>
<td>39.97</td>
<td>37.61</td>
</tr>
</tbody>
</table>

Table 2.3: Heat transfer coefficients for die casting of A380 aluminum in H-13 die casting dies (650 °C furnace temperature) (Inside and outside surfaces represent the locations relative to the inner core)[Papai et al, 1993]

The effect of pressure and the state of the metal plays an important role in heat transfer during cavity filling, solidification and cooling stages. Some studies focused on these factors. Table 2.4 and 2.5 shows the data taken from these studies. The figures used by Takach [Takach, 1997] also exhibit similar results in terms of effect of the state of the material when compared with a study by Nelson [Nelson, 1968]. Nelson reports heat transfer coefficients such as 64.8 kW/m² K for liquid Magnesium 6.4 AZ91B DME #2
steel contact. 63.5 kW/m² K was reported during freezing of casting. The heat transfer coefficient figure reduced to 19.2 kW/m²K for frozen casting/die surface interaction.

<table>
<thead>
<tr>
<th>State of the metal</th>
<th>h (kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Liquid metal</td>
<td>61.97</td>
</tr>
<tr>
<td>Mushy metal</td>
<td>60.57</td>
</tr>
<tr>
<td>Solid metal</td>
<td>18.51</td>
</tr>
</tbody>
</table>

Table 2.4: Effect of the state of Al casting on heat transfer in H-13 dies [Takach, 1997]

<table>
<thead>
<tr>
<th>Pressure (MPa)</th>
<th>h (kW/m² K)</th>
<th>State</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>3.4</td>
<td>Liquid</td>
</tr>
<tr>
<td>91</td>
<td>34.0</td>
<td>Liquid</td>
</tr>
<tr>
<td>196</td>
<td>52.5</td>
<td>Liquid</td>
</tr>
<tr>
<td>0</td>
<td>0.84</td>
<td>Solid</td>
</tr>
<tr>
<td>91</td>
<td>31.0</td>
<td>Solid</td>
</tr>
</tbody>
</table>

Table 2.5: Effect of the state and cavity pressure of the Al casting in H-13 dies [Sekhar et al, 1979]

Nishida and Matsubara also studied the effect of the pressure on the die/casting heat transfer coefficients. The aluminum casting experiments pointed the following results regarding pressure effects as shown in Table 2.6.
Table 2.6: Impact of cavity pressure of the Al casting in H13 dies [Nishida et al, 1976]

<table>
<thead>
<tr>
<th>Pressure (MPa)</th>
<th>h(kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>4.2</td>
</tr>
<tr>
<td>65</td>
<td>42</td>
</tr>
</tbody>
</table>

Table 2.7 gives information about the ranges of spray heat transfer coefficients. Depending upon the spray process conditions and the lubricant chemistry spray heat transfer coefficients may vary as indicated in the following tables.

<table>
<thead>
<tr>
<th>(150°C) Spray Flux (m³/s m²)</th>
<th>h (kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0002</td>
<td>1.2</td>
</tr>
<tr>
<td>0.0012</td>
<td>2.5</td>
</tr>
<tr>
<td>0.0024</td>
<td>4.0</td>
</tr>
<tr>
<td>0.0048</td>
<td>7.2</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>(250°C) Spray Flux (m³/s m²)</th>
<th>h (kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0002</td>
<td>2.5</td>
</tr>
<tr>
<td>0.0012</td>
<td>3.8</td>
</tr>
<tr>
<td>0.0024</td>
<td>6.0</td>
</tr>
<tr>
<td>0.0048</td>
<td>10.0</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>(350°C) Spray Flux (m³/s m²)</th>
<th>h (kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0002</td>
<td>1.5</td>
</tr>
<tr>
<td>0.0012</td>
<td>3.2</td>
</tr>
<tr>
<td>0.0024</td>
<td>5.4</td>
</tr>
<tr>
<td>0.0048</td>
<td>9.8</td>
</tr>
</tbody>
</table>

Table 2.7: Spray heat transfer coefficients as a function of spray flux - The number at the first column in parenthesis indicates the initial test plate surface temperatures [Lee et al, 1991]
Table 2.8: Reported ranges for spray heat transfer coefficients (under various conditions)

<table>
<thead>
<tr>
<th>h (kW/m² K)</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5 - 2.1</td>
<td>[Brennan, 1994]</td>
</tr>
<tr>
<td>0.2</td>
<td>[Volz et al, 1995]</td>
</tr>
</tbody>
</table>

Table 2.9: Spray heat transfer coefficients as a function of spray fluid temperature (other spray process conditions were identical) [Bishop et al, 1990]

<table>
<thead>
<tr>
<th>Spray Water Temperature (°C)</th>
<th>h (kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>80</td>
<td>30.5</td>
</tr>
<tr>
<td>52</td>
<td>20.3</td>
</tr>
<tr>
<td>24</td>
<td>14.2</td>
</tr>
</tbody>
</table>

In the literature, there are limited data on the heat transfer from the open die surfaces to the surrounding. Heat transfer from dies to surroundings occurs in two ways, convection and radiation. To simulate the effect of the both factors, a total heat transfer coefficient can be used. Some figures for heat transfer coefficients from die surface-ambient air are shown in Table 2.10. In a personal interview at the Material Science and Engineering Department of the Ohio State University, Dr. Carroll Mobley reported air heat transfer coefficients about 0.04 kW/m²K.
Table 2.10: Die/air interface heat transfer coefficients

<table>
<thead>
<tr>
<th>$h$ (kW/m$^2$ K)</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.03</td>
<td>[Brennan, 1994]</td>
</tr>
<tr>
<td>0.02</td>
<td>[Volz et al, 1995]</td>
</tr>
<tr>
<td>0.10</td>
<td>[Barone et al, 1993]</td>
</tr>
</tbody>
</table>

Cooling line heat transfer coefficients are given in Table 2.11. More information about cooling lines can be found in the future section (2.3) of this chapter.

Table 2.11: Cooling line heat transfer coefficients (a: only the flow rate was reported)

<table>
<thead>
<tr>
<th>$h$ (kW/m$^2$ K)</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>4.6</td>
<td>[Volz et al, 1995]$^a$</td>
</tr>
<tr>
<td>5.9</td>
<td>[Brennan, 1994]</td>
</tr>
<tr>
<td>10</td>
<td>[Barone et al, 1993]</td>
</tr>
</tbody>
</table>

Figures for heat transfer coefficients at the interfaces of any two tools are given in Table 2.12. Reported values also included the thermal interaction at the parting lines as seen in the same table.

Table 2.12: Die/die interface heat transfer coefficients (a: The first figure is the heat transfer coefficient for parting surface while the second represents the heat transfer along the die insert/block interfaces, b: The first figure is the heat transfer coefficient at interface of die halves while the second figure represents the heat transfer after clamping)

<table>
<thead>
<tr>
<th>$h$(kW/m$^2$ K)</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>5</td>
<td>[Brennan, 1994]</td>
</tr>
<tr>
<td>1-5</td>
<td>[Barone et al, 1993]$^a$</td>
</tr>
<tr>
<td>5-10</td>
<td>[Volz et al, 1995]$^b$</td>
</tr>
</tbody>
</table>
2.1.3 Process Variables, Conditions or Phenomena That Control Heat Transfer Coefficient

To calculate the thermal fields within the dies and castings rely on the boundary conditions and the heat transfer coefficients at all interfaces. These factors are dependent on time and casting conditions present within the die cavities.

Most existing thermal studies have focused on the prediction of the heat transfer coefficients of fluids, which are in contact with solid surfaces. There have been a few studies conducted on understanding the heat transfer between solidifying fluid-solid surface and interaction during solid-solid surface contact. These issues are important for modeling of casting solidification and cooling.

The heat transfer condition at the die/casting interface are determined by the following factors during filling, solidification and cooling stages:

- Melt (mushy liquid/solid metal mix) cavity pressure or contact pressure
- Gas or air presence at/or near the interface
- Liquid metal temperature
- Die surface conditions
  - temperature
  - surface roughness
  - hardness
- Lubricant deposition (presence of interfacial fluids or solids) at the die/casting interface
- Location in the die/casting system (relative location to the cooling passages, gates, cores or die halves)
In the case of two solid surfaces, which are in contact, the actual area of contact is less than the nominal area of the contact since only the asperities of each surface can actually touch the high points of the other. While thermal radiation occurs across the non-contacting portion of the interface, heat conduction will occur through the contact points. If high enough contact pressure values which are greater than a critical value \[\text{[Greenwood et al, 1966]}\),

\[
\frac{A_t}{A_n} = \frac{p}{H \ast c}
\]

(2.1)

Where:
- \(A_t\): True area of contact
- \(A_n\): Nominal area of contact
- \(p\): Contact pressure
- \(H\): Surface hardness of the softer material
- \(c\): Conversion factor between the hardness and strength (tensile or yield strength)

(corresponding stress value for the hardness of the softer material applied normal to the interface), exist the true area of contact can be assumed as equal to the nominal contact area. As the true contact area approaches the nominal contact area, the thermal resistance at the interface due to physical contact becomes negligible. Extremely high temperatures increase the sensitivity of the surface to the contact pressure meaning that at elevated temperatures smaller pressure values may be enough to increase the contact and subsequent heat transfer coefficients \[\text{[Greenwood et al, 1966]}\).

In the contact of two solid planar surfaces, the heat flow away from the interface is in direction of the common normal. Near the interface, heat flow becomes 3-D since the contact at the interface is not a perfect plane, which is parallel to the interface. The temperature distributions for each surface would vary along the interface due to the irregularities on the contacting surfaces. The two different heat paths as shown in Figure
2.9. As seen from the figure, Path A does not have a discontinuity in its temperature profile, while Path B does. Heat conduction will occur through the contact areas whereas thermal radiation occurs through gaps. Heat conduction through interstitial fluids (such as lubricants) will also be encountered. Ho and Pehlke stated that it is possible to define two imaginary surface temperatures, $T_1$ and $T_2$ based on the extrapolations from the both sides of the contact interface [Ho et al, 1984]. A heat transfer coefficient can be defined assuming a spatially averaged heat flux $q$, through the contact interface.

Figure 2.9: Temperatures at the interface region of two contacting surfaces [Ho et al, 1984].
The approximation of a single heat flux through the interface is based on the assumption that the interface layer has reached a quasi-steady state. If the transient state is still present at the interface, the rate of heat entering and leaving the interfacial zone would be different. With the presence of small enough interfacial zone at the die-casting conditions, it is logical to assume a quasi-steady state.

Geometric features and thermal conductivity of each contacting body determine the contact heat transfer coefficient. Clausing presumed that the contact was made through circular and evenly distributed points [Clausing, 1965]. He calculated the heat transfer coefficient in vacuum,

\[ h = \frac{q}{T_2 - T_1} \quad (2.2) \]

The interstitial fluid-like lubricant presence at the die/casting interface creates another passage for heat conduction. The heat convection through the gap does not occur since the gap at the interface does not allow the movement of the fluid. Total heat transfer

\[ h_c = \frac{2k \sum c_j}{A_n \Psi} \quad (2.3) \]

where \( k \): average conductivity of the two contacting materials \( (1/k = \frac{(k_1+k_2)}{k_1k_2})/2) \)

\( c_j \): contact spot size for a spot \( j \)

\( \Psi \): a function of spot spacing (determined by \( c/b \) ratio)
though the interface will be the sum of the heat conduction at the contact points and the heat conduction through the lubricant layer. The coefficient of conduction through interfacial fluid is defined by,

$$ h_i = \frac{k_i}{d} $$

(2.4)

where $k_i$: thermal conductivity of the interfacial fluid

$d$: average gap between the contacting surfaces

The contacting surfaces are not only exposed to the lubricants, but also there could be an oxide film at the interface. The oxide film will alter the resistance to heat flow. The film coefficient for oxide layer, $h_{f}$, can be described as similar to the interfacial fluid's thermal conductivity. Since the asperity contact and interfacial fluid layer contribute to the heat flow in a cumulative way, and the oxide layer works against the heat flow, the total contact heat transfer coefficient can be described as follows,

$$ h_t = \left( \frac{1}{h_c + h_i} + \frac{1}{h_f} \right)^{-1} $$

(2.5)

where $h_t$ is the overall contact heat transfer coefficient.

The non-coated molds start the casting cycle with a higher heat transfer coefficient than the coated ones. Later in the casting cycle coated molds will have higher heat transfer coefficients. The surface roughness of the coating determined by the coating particle size also affects the heat transfer coefficient. If the particle size of the coating is small, the coating thickness has a more pronounced affect alone [Nishida et al, 1976]. Papai and Mobley used thermal probes with different surface qualities to investigate the
effect of the surface roughness on the heat transfer coefficients [Papai et al, 1993].

Nishida and Matsubara's pressurized solidification test results indicated that the heat transfer coefficient increased dramatically as the load applied, stayed at a high value (depending upon the load) and dropped as the air gap forms. Heat transfer coefficient histories during different casting cycles with different punch loads are shown in Figure 2.10.

![Heat transfer history for various punch loads](image)

Figure 2.10: Heat transfer history for various punch loads [Nishida et al, 1976]

Figure 2.11 indicates that the peak value of the heat transfer coefficient was proportional to the square root of the pressure. Since the diameter of the casting was much greater than the actual castings, the applicability of these results was assumed not to be feasible by Nishida [Nishida et al, 1976].
Figure 2.11: Maximum value of heat transfer coefficient as a function of derived pressure from experiments [Nishida et al, 1976]

The investigation of the contact of liquid mercury-solid surface with spherical asperities revealed that small pressure was adequate for complete wetting [Timsit, 1982]. The effect of the presence of cavity gas on heat transfer between a non-solidifying liquid metal and a solid substrate was investigated by Yovanovich [Yovanovich, 1970]. While assuming that cavity gas was resistant to heat flow and in the absence of insulating layers, he found that the die/casting heat transfer coefficient is directly proportional to the applied pressure. This is because, as the pressure is increased, the volume of the cavity gas must decrease thus increasing the contact between the tooling and casting surfaces.

If the substrate is hot and the heat transfer rate is slow enough, liquid metal may remain at the interface for a sufficient time period for the metal to wet the cavity surfaces. In this case a large heat transfer coefficient and some amount of welding (soldering) is encountered. If clean die cavity surfaces are well below the melting temperature, the melt can be under-cooled at the asperities. Nucleation occurs at those points and the nuclei can grow laterally to prevent the melt from wetting the die cavity surface as shown in Figure
2.12. Prates and Biloni measured the interface heat transfer coefficient for predendritic nuclei formation at the asperities. The heat transfer coefficient was determined to be proportional to the square of the number of surface nuclei [Prates et al, 1972]. Ho and Pehlke at first observed a skin of solidified metal formation at the interface [Ho et al, 1984]. They concluded that later on the casting cycle depending on the thermal conditions and geometry of the interface either asperity contact or air gap formation might occur. While the casting cools, it shrinks. The shrinkage creates stresses that separate the casting from the cavity surface. Hooke’s Law can estimate these stress values:

\[ \sigma = E \alpha \Delta T_{\text{casting}} \]  

(2.6)

where \(\sigma\): interfacial shear stress

\(E\): Young’s modulus of the casting
α: coefficient of thermal expansion for the casting

ΔT_{casting}: change in casting temperature

While castings shrink away from the dies, they will shrink towards the cores. Air gaps may form at the die/casting interface, reducing the heat flow. This concept was mostly investigated in permanent molding studies. It needs to be studied in more details in high-pressure die-casting. If the pressure history within the cavity is known, after freezing, presence of low enough pressures may make permanent - high pressure die-casting analogy possible. There is also a critical temperature at which the casting may stick at the interface. Maringer experimentally studied and defined this critical temperature [Maringer, 1988].

\[ T_{\text{critical}} = T_{\text{melt}} - \Delta T_{\text{cavity}} \]  \hspace{1cm} (2.7)

where \( T_{\text{critical}} \): critical cavity surface temperature

\( \Delta T_{\text{cavity}} \): rise in temperature of cavity surface

\( T_{\text{melt}} \): melting point of casting

\( \Delta T_{\text{cavity}} \) is determined by the interface heat transfer coefficient. High values of the heat transfer coefficient will increase the change in the surface temperature.

The mechanisms of heat convection and thermal radiation during open die stage are explained below: Some heat escapes from the dies through thermal radiation while extremely hot die surfaces are exposed to the air. Thermal radiation is electromagnetic waves similar to the light waves emitted from light sources. Radiation only occurs from the die surfaces when they are exposed to the air during open die stage. The Stephan-Boltzman law defines the amount of heat radiated:

\[ Q_r = r \cdot A \cdot (T_i^4 - T_a^4) \]  \hspace{1cm} (2.8)
where $Q_r$: Rate of heat transfer through thermal radiation

$r$: Radiation constant for die casting die surfaces ($r = \varepsilon \sigma$ (Stephan-Boltzmann constant))

$A$: Area of radiating surface

$T_i$: Die cavity surface temperature

$T_a$: Ambient temperature to which thermal radiation occurs

When air flows near open die cavity surfaces, it will absorb heat from the hot surface and carry it away. In other words, the hot surface is cooled by air. The transfer of heat from the hot dies to the moving air is called natural convection. Figure 2.13 shows the concept by rising air, which is at some low temperature $T_a$, flowing over a surface that is assumed to be at a constant but a higher temperature $T_s$.

![Figure 2.13: When air flows over a hot surface, heat is transferred (through heavy arrows) from surface to the air through convection [Herman, 1996]](image-url)
In the die casting process, it is not uncommon to blow air over the die surfaces. In most cases however, the air adjacent to the die will get hot and rise creating some movement of the air over the surface due to natural heat convection. The rate of heat transfer from the surface to the ambient air can be estimated by using the Newton’s Cooling Law:

\[ Q_c = h \cdot A \cdot (T_s - T_a) \]  \hspace{1cm} (2.9)

where \( Q_c \): rate of heat transfer through heat convection  
\( h \): coefficient of convective heat transfer  
\( A \): surface area  
\( T_s \): die cavity surface temperature  
\( T_a \): ambient temperature to which heat convection occurs

The heat transfer coefficient \( h \), depends on the size and position of surface. The \( h \) values can be calculated using following empirical relations [Herman, 1996].

<table>
<thead>
<tr>
<th>Position of the Surface</th>
<th>Formulation for Spatially Averaged Heat Transfer Coefficient (( h ): W/m(^2)K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Vertical surface less than 0.6 meter high (Laminar flow)</td>
<td>( 1.42 \cdot (\Delta T/L)^{0.25} )</td>
</tr>
<tr>
<td>Vertical surface over 0.6 meter high (Turbulent flow)</td>
<td>( 1.31 \cdot (\Delta T)^{0.33} )</td>
</tr>
<tr>
<td>Horizontal surface facing up</td>
<td>( 1.32 \cdot (\Delta T/L)^{0.25} )</td>
</tr>
<tr>
<td>Horizontal surface facing down</td>
<td>( 0.59 \cdot (\Delta T/L)^{0.25} )</td>
</tr>
</tbody>
</table>

Table 2.13: Die/air heat transfer coefficients (\( \Delta T \): the temperature difference between the surface and the ambient (in °C), \( L \): the length of the surface (in meters)) [Holman, 1986]
As previously mentioned, spray cooling performance is affected by many factors. In a die-casting cycle it is necessary to apply a lubricant between each shot. The lubricant is frequently applied to the surface by spraying. The lubricant has several important functions. It acts as a release agent to ensure the separation of casting from the die. The protective layer of lubricant also prevents soldering (welding) of casting to the die cavity during filling and solidification. The lubricant spray also affects the die surface temperature during operation.

Die-casting dies operate within the temperature range of 350–600 °F (176.7 - 315.6 °C). In this temperature range, the nature of the interaction between spray droplets and die surfaces is complex. For the spray to effectively cool the die faces and the lubricant to be deposited the droplets must first contact the die surface and the water content within the lubricant has to boil away. Sometimes to facilitate cooling, water is sprayed on the die surface prior to the lubricant spray. At high temperatures, a blanket of steam is built up on the die surface and the droplets cannot contact with the die surface. The influence of this phenomenon can be seen in the graph of heat flux versus surface temperature between a heated block and a spray shown in Figure 2.14. In section I of the graph, the surface is relatively cool and little boiling occurs during arrival of spray droplets at the surface, so heat is removed primarily through convection. In section II, the surface is hot enough to initiate boiling of water on contact and removes more heat through this mechanism. This is called nucleate boiling. In section III, the heat transfer is decreasing because the layer of steam is beginning to form on the block surface. This section is referred to as transition boiling. Section IV of the graph shows the film boiling region wherein the steam layer is fully developed on the heated surface separating the spray droplets from the surface.

The temperature at which the droplets begin to make contact with the die surface, called the Leidenfrost point or the wetting temperature, is affected by many variables. If the die surface has not been cooled to the Leidenfrost temperature, no lubricant will be deposited. To reduce the surface temperature to the wetting point, most spray applications will be initiated with water spray prior to the lubricant spray.
Figure 2.14: Typical heat flux values versus surface temperature curve showing the four different regimes (I: convective cooling, II: nucleate boiling, III: transition boiling, IV: film boiling) of a water spray [Mizikar, 1970]

The empirical relations for cooling lines are given in the following section. The empirical heat transfer coefficient for turbulent flow of water with Reynolds numbers greater than 10,000 was given by Herman [Herman, 1976] as,

\[
h = 0.023 \left( \frac{k}{D} \right) \left( \frac{VD \rho}{\mu} \right)^{0.8} \left( \frac{\mu c_p}{k} \right)^{0.4}
\]

(2.10)

where  
- \( h \): interface heat transfer coefficient (W/m²K)  
- \( k \): thermal conductivity of the water at the water temperature (W/m K)  
- \( D \): (hydraulic) diameter of the cooling line (m)
V: velocity of the water (m/s)
ρ: density of the water (kg/m^3)
μ: dynamic viscosity of the water at the water temperature (kg/m s)
c_p: specific heat of the water at the water temperature (J/kg K)

The second parenthesis on the left side of Equation 2.10 represents the Reynolds number. The third parenthesis is the dimensionless Prandtl number. Reynolds number characterizes the type of flow. High Reynolds numbers represent the dominance of shear forces over viscous (friction) forces and consequent turbulent flow. Prandtl number represents the interaction between the velocity and the temperature profiles [Pitts et al, 1977]. Hsieh also presents relations for determining convective heat transfer coefficients for flow regimes with Reynolds numbers less than 2,100, and within the range of 2,100-10,000 [Hsieh, 1989]. More empirical relations for different types of flow, analysis and comparison of different parameters and cooling fluid can be found in a recent work completed at the Ohio State University by Ambitabh Nath [Nath, 1999].

2.1.4 Procedures For Experimental Determination of Heat Transfer Coefficients For the Aluminum Die Casting Process

The experimental set ups used to measure heat transfer coefficients varied in a wide range for gravity and pressurized filling processes. The late stages of the die-casting process with lower pressure levels can be associated with the gravity filling tests in the search of determining the heat transfer coefficients.

The experimental set-ups have included following configurations to measure the heat transfer within the castings and heat transfer coefficients at the die/casting interface during filling, solidification and cooling,
• Top chill tests
• Bottom chill tests
• Side chill tests
• Cylindrical mold tests
• Pressurized filling tests
• Instrumented permanent mold testing
• Instrumented die casting studies

Several studies focused on the solidification of castings that are insulated on all sides except top or bottom. A high conductivity chill was found to be forming at the bottom or at the top chill interface. Prates, Fissolo and Biloni measured the solidified thickness in time for 85 mm thick castings of pure metals on clean and coated bottom chills [Prates et al, 1972]. They compared their results with a formula of solidification thickness considering the heat transfer coefficient and other parameters to obtain a single heat transfer coefficient for each experiment. Adams established the following formula [Adams, 1958].

\[
m = \left( \frac{h(T_m - T_d)}{\rho_c \Delta H_f a} \right) t - \frac{h}{2k_c} m^2
\]  \hspace{1cm} (2.11)

where  
\begin{align*}
h & : \text{heat transfer coefficient at die/casting interface} \\
T_m & : \text{surface temperature of casting} \\
T_d & : \text{surface temperature of die} \\
m & : \text{solidified thickness of casting} \\
t & : \text{elapsed time} \\
\Delta H_f & : \text{heat of fusion of metal} \\
k_c & : \text{thermal conductivity of metal} \\
\rho_c & : \text{density of metal}
\end{align*}

43
Figure 2.15: Schematic of the various configurations used measuring the heat transfer in casting [Papai et al, 1993]
and \( a \) is described as below as a function of specific heat of casting, surface temperature of casting and heat of fusion of casting metal,

\[
a = \frac{1}{2} + \sqrt{1 + \frac{(c_p)_v(T_m - T_s)}{3\Delta H_f}}
\]  

(Bamberger et al, 1987). Bamberger, Weiss and Stupel cast 60 mm thick Al-Si alloy castings on steel chills, and measured the temperature in the chills. If the chill is above the casting, the solidified portion of the casting starts to pull away from the chill surface forming an air gap. The temperatures within the dies, castings and the relative motions of the surfaces for air gaps were measured. [Ho et al, 1983]

Some other studies focused on the thermal analysis of a casting solidification in the horizontal direction. Horizontal solidification was accomplished by using either side chills located in the insulated molds or all metal permanent molds [Jeyarajan et al, 1976]. Cylindrical molds were also used in the experimental studies. Air gap formation was also encountered in the experimental studies with cylindrical molds [Sciama, 1968]. The heat transfer rate and the elapsed time before air gap formation were determined using the temperature measurements from the molds. The heat transfer results were determined by the amount of mold heating and expansion. Ohno, Minami and Kanaya [Ohno et al, 1981] investigated the solidification of aluminum in a wide variety of cylindrical molds. Thermocouples were placed in the casting and the mold. The interfacial heat transfer coefficients were determined as a function of time by comparison of simulated and experimental temperature values. When the aluminum and copper molds are used with low superheat testing, the heat transfer coefficient rapidly increased, and then quickly settled to a certain value. But for the lower conductivity materials such as stainless steel with lower superheat testing and for all the experiments with high superheat content, the heat transfer coefficient slowly increased and then decreased.
Srinivasan and Seshadri studied vertical plate, vertical square, vertical rectangular and cylindrical castings [Srinivasan et al, 1970]. Gozlan and Bamberger used H-19 steel molds with the mold thickness of 20 mm to cast aluminum bronze castings of 15 mm thick (Cu-10 Al- 4Fe). Thermocouples were mounted in the mold at various distances from the casting. The castings were made with various initial mold temperatures. Some tests were run with uncoated molds while the other mold surfaces were coated with 10 and 100 μm colloidal graphite [Gozlan et al, 1987]. Isaac, Reddy, and Sharma measured the air gap formation with an LVDT, and measured temperatures for casting aluminum in a cast iron mold with cavity size 280 mm long, 70 mm tall and 70 mm thick. The air gap formation at the center and the corner of the molds was measured. The volume ratios of 1-2 and, 3 were used and the coating thickness of 50, 100, 150 and 200 micrometer were used at the mold surfaces [Isaac et al, 1985]. Nishida, Isotani and Matsubara used pure aluminum in steel molds with a variety of casting and mold wall thickness values. The temperatures were measured both in the casting and in the mold. The heat transfer coefficient history was determined. [Nishida et al, 1976]. Heat transfer to internal chilling elements through dip tests was also studied [Sun, 1970]. While the surface of the chill was assumed to be at the (dip) bath temperature, temperature history at the half radius of the chill was measured and recorded to estimate the thermal gradients, heat fluxes and consequent heat transfer coefficients. Other attempts to understand the heat transfer at the interface included, chill block melt spinning, planar flow casting and, various other processes [Vincent et al, 1987].

The actual high-pressure die-casting process is different from the previously mentioned processes. Only the outcome of pressurized and moving substrate tests can be used to understand the effect of pressure and flow on the interface heat transfer coefficients. The high pressure will enforce contact of the metal with the die. The heat transfer coefficients at die/casting interface are quite high compared to ones from the sand or permanent molding processes. Nishida and Matsubara developed an apparatus where a cylinder of aluminum was subjected to high pressures while solidifying radially [Nishida
et al, 1976]. Hong, Beckman and Mehrabian cast A380 in a H-13 die cavity with a size of 100 x 63.5 x 1.59 mm. The stationary die had thermocouples at nominally 0 to 6.35 mm from the die cavity interface. Investigations were carried out for the pressures of 87.5 and 175 MPa, two melt temperatures (687 and 605 °C), two gate velocities and with or without water based hydrocarbon lubricant [Hong et al, 1979]. Hatamura cast a long M shaped casting, (5 x 14 x 630 mm) with the thermocouples located 0.4 and 0.8 mm from the casting surface in some ejector pins. The study also included the pressure measurements from sensors embedded in the ejector pins. A384 alloy was cast at 65.75 MPa with an oil lubricant. The measurements were taken at several locations, at the runner, and at several distances from the runner [Hatamura et al, 1989]. Papai and Mobley experimented with a H-13 tooling made for die casting A380 transmission cases. The impact of two pressure levels, two die cavity lubrication conditions (heavy lubrication with 4 second spray, light with 2 second spray), two different superheat levels, different die surface roughness values on the heat transfer coefficient were investigated embedding various probes and thermocouples within the cavity. The heat transfer coefficients were calculated using measured thermal gradients. The experimental thermal fields matched the simulated thermal fields by adjusting the interface heat transfer coefficients [Papai et al, 1993].

Several techniques used to obtain the heat transfer coefficients for solidification experiments are summarized below.

- Comparison of measured thickness solidified as a function of time with predictions using numerical methods for different values of heat transfer coefficients.
- Comparison of measured die/casting temperatures with numerical simulation results using various values of heat transfer coefficients.
- In chill block melt spinning, planar flow casting and, splat quenching, the temperatures can be observed photographically. The release time for such processes can give information about the removal of latent heat, heat flux and heat transfer coefficients.
Some methods were based on known relations for some alloys such as relating the size of micro structural features such as dendrite arm spacing or cell size to either the time to solidify or the quench rate. This enables the investigator to compare the micro structurally determined quench rates with those predicted by models to predict the heat transfer coefficient.

Experimental procedures to measure the temperature fields within the dies for filling, solidification and cooling stages of the die-casting process should be based following factors,

- Critical areas such as locations (on the both sides in ejector and cover die half) near and away from the gate, and near the cores have to be investigated.
- Flat areas should be a proper choice for inserting flat-ended thermal probes.
- Fast responding, reliable thermocouples with a large enough output (voltage) range should be chosen.
- Closest thermocouple to the surface should be located at the surface (or as close as possible to avoid the time lag for readings and the numerical analysis for surface temperature extrapolations through inverse modeling).
- Multi-thermocouple probes should be used for direct measurement of the thermal gradient and determination of the consequent heat flux values. Also in some critical areas at which the temperature history may be important but the probe cannot be placed due to the geometric and other constraints, a single thermocouple can be used.
- All boundaries of testing or actual tooling should also closely be monitored to help developing a numerical model for analyzing the thermal problem.

Papai and Mobley developed a multi-thermocouple thermal probe as shown in Figure 2.16. It consisted of three major components listed below [Papai et al, 1993] for the investigation of contact heat transfer coefficients in die casting A380 aluminum alloy in H-13 dies. Similar probes were also used for measurement of cooling performance.
Their probe consisted of:

- Thermocouples
- Probe tips (to which thermocouple beads are welded)
- Probe tip carriers

![Diagram of probe design](image)

Figure 2.16: Multi-thermocouple thermal probe design [Papai et al, 1993]

Probes have to be made from the same material as the die component in which they are placed. The thermocouple beads have to be attached within the probe carrier to prevent any interference of other mediums such as air, and during exposure to the high temperature they should stay at their original location for reliable measurements. Type K
(chromel-alumel) thermocouples are usually preferred since they exhibit a larger voltage output when exposed to elevated temperatures than the other thermocouple types. They are resistant to atmospheric conditions at elevated temperatures. Their response time is also fast.

Thermocouples should be calibrated with several known temperature values covering the die-casting temperature ranges.

Temperatures measured by the thermocouples do not exactly reflect the actual temperature values at the measurement locations. The actual - measured temperature relation is described by the following equation,

\[ T_{\text{actual}} = T_{\text{indicated}} + \tau \left( \frac{dT_{\text{indicated}}}{dt} \right) \]  \hspace{1cm} (2.13)

where \( \tau \): response time
\( t \): time

The probe carrier and the probe have to be press fit to prevent sliding of carrier. The measurement is a hard task especially if you are dealing with hot and uneasy medium like molten or mushy metal. Thermal probes with multiple thermocouples can be used for measuring the thermal gradients during spray cooling, radiative and natural convection stages. From the thermal gradient, heat flux values can be driven near the surface. Convective heat transfer coefficients can also be calculated for spray cooling and for radiation and natural convective cooling. Since the radiation and natural convection occur simultaneously, both of their effects can be gathered in a combined heat transfer coefficient.

If the heat flux or temperature histories at the boundaries of an object are known in time, then the temperature field within the object can be determined. Like many dynamic heat transfer situations, the physical situation at the surface may affect the accuracy of the measurements. In testing of spray cooling, the surface heat flux and temperature histories of a test plate or die may be determined from temperature
measurements at one or more interior locations [Beck, 1985]. This is called an inverse heat conduction problem since it involves estimating an unknown boundary condition from a known temperature history of one or multiple interior points. Many methods have been developed concerning inverse thermal problems. A method developed by Beck has gained the most acceptance. The Fourier equation governs the thermal conduction within the plate. A pseudo-boundary condition is assumed asserting that the temperature histories of the multiple interior points are known from the thermocouple measurements as shown in Figure 2.17. With the proper assumptions of the boundary condition at the back of the plate (die) and an initial condition, the analysis can be conducted. The method guesses a value of surface heat flux and then performs finite difference analysis to calculate the temperature field based on the assumed surface heat flux. The assumed temperature field is then compared with the measured temperature histories. The difference between the measured and the assumed temperature values are minimized

Figure 2.17: Geometry of the heat plate model with multiple thermocouples (q: heat flux out of the plate, E1, E2, E3: thermocouple locations) [Bishop et al, 1990]
using sum of squares equation and the assumed heat flux values are updated until there is a close enough match between the two temperature data. Once the heat flux is known, heat transfer coefficients can be found using the Newton’s Cooling Law, Equation 2.9. Some other methods split the body into two regions (see Figure 2.18), one in between the known boundary condition and the interior temperature measurement location, the other from the thermocouple location to the unknown boundary. The former can be solved with

\[ q(t) = ? \]

\[ T = Y(t) - X_i \]

\[ \frac{q(t)}{T} = \frac{?}{?} \]

\[ \begin{align*}
q(t) & = ? \\
Y(t) & = \text{Inverse} \\
q(t) & = ? \\
x_1 & = \text{Direct}
\end{align*} \]

![Figure 2.18: Subdivision of a single interior sensor inverse heat conduction problem into inverse and direct problems (x₁: sensor location, q(t): time varying heat flux, y(t) time varying measured quantity) [Beck, 1985]](image_url)

direct thermal analysis through finite differences. The latter can be solved using the inverse heat conduction analysis. If there is only one thermocouple present within the body, the thermal gradient cannot be directly measured. It can only be estimated after the temperature fields in both areas are determined. Surface heat flux values can later be obtained through the Newton’s Cooling Law. Beck’s method is based on a time marching type of algorithm. The calculations start with the initial condition and then, the heat flux history at the subsequent time steps are determined. In space marching algorithms, the calculations start on the boundary between direct and inverse regions and are then continued for subsequent grid points within the inverse region towards the unknown boundary. There are also other algorithms such as the one developed by Raynaud and Bransier, which are based on an averaging technique that uses both the space and time marching methods [Kurpisz, 1995].

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Recently there has been an interest in thermal imaging of die cavity surfaces. Thermograph measurements may not correspond to the actual values unless they are well calibrated. To obtain realistic data, problems with reflections, atmospheric absorption and spatial variations in surface emissivity must be handled [Prystay et al, 1996]. Thermographic devices could only be used for open die stage temperature measurements.

2.2 Failure of Dies, Heat Checking and Thermal Fatigue

2.2.1 Die Failure

Failure of dies results from the interaction between the loading conditions during the process, and the resistance of the die material to withstand the stresses and strains induced by the loading conditions.

In this chapter, the process and the loading conditions of die-casting dies are described, and resulting failure types are explained. The effects of various parameters involved in die-casting are also presented.

Die-casting dies are exposed to a combination of cyclic rapid thermal and mechanical loading. To improve the product quality and to prolong the die service life, understanding the die load and resulting deflections, stress-strain and temperature distributions is crucial. A die-casting cycle with its corresponding loads is illustrated in Figure 2.19.

The rapid, cyclic heating and cooling of dies at the cavity surfaces induce thermal stress and strains within the substratum. During rapid temperature changes on the surface of a solid, the temperature of the substrate increases and decreases rapidly while the temperature of the rest of the body cannot respond fast enough to the changes. Difference in expansions and contractions caused by uneven temperature distributions induce in internal constraints. Various layers expand and contract by different amounts. Each layer is constrained by the neighboring ones. Complex die cavity surface geometry is also
another factor, which creates congestion of heat flow paths, subsequent uneven temperature fields and stress concentrations [Knoerr et al, 1992]. Figure 2.20 shows the structural state of dies during heating and cooling of die-cavity surfaces [Noesen et al, 1966].

Figure 2.19: Stages and corresponding loads to die casting process [Dedhia, 1997]
Figure 2.20: Temperature, stress and strain distributions in die-casting dies during heating and cooling of die cavity surfaces [Noesen et al, 1966]
Cavity pressures during filling, intensification and solidification stages of the process and tie-bar loading are the only sources of mechanical loading. The clamping force is the reaction to keep the die halves together and to prevent flashing during injection of metal into the cavity. Figure 2.21 indicates the pressure history of injection and intensification for the die casting process. The pressure at the end of injection is nearly uniform within the cavity. After the injection, the pressures can vary within casting. Figure 2.22 indicates the pressure history at four locations along a serpentine M shaped casting application.

The combination of thermal and mechanical loading and boundary conditions determine the actual stress distribution within the dies. Depending upon the direction and the magnitude of the thermal stresses, the combined stress distribution can be less or greater than the stresses induced by mechanical loading.

The thermal and the mechanical loading is very much dependent upon the die geometry, the metal flow in the die cavity, thermal conditions at the interfaces and within the dies, the material properties and the casting equipment. These parameters and resulting loads determine the type of failure encountered during the casting operation.

Major modes of die failure described by Draper [Draper, 1970] are,

- Heat Checking (Thermal fatigue)
- Wash-out (Erosion)
- Corrosion
- Gross cracking
Figure 2.21: Pressure profile during die-casting process [Sully, 1988]

Figure 2.22: Pressure history inside a die cavity [Hatamura et al, 1989]
Erosion is a gradual wearing away of the material of the die cavity surface at specific locations due to the impingement of metal during injection. Cores, die surfaces and gate areas are susceptible to erosive wear. Pitting is a selective mode of corrosion. It is a removal of one or more micro-constituents by the dynamic stream caused by the pressure penetration of the molten metal. Corrosion is a deterioration of material at the die cavity surface by oxidation and chemical interaction with the molten metal [Askeland, 1994]. Gross-cracking or premature failure is a sudden and catastrophic failure of any die system components. It is generally observed as isolated cracks with or without any relation to thermal fatigue. Cracks are random cracks and presumed to be related to the manufacturing and treatment processes for making die components. The die failure is caused by combination of different modes depending upon the conditions under which dies operate and the way they were fabricated [Knoerr et al. 1992].

2.2.2 Heat Checking and Fatigue Failure

Fatigue failure is a general term given to sudden and catastrophic separation of a machine component into two or more pieces or loss of function as a result of the cyclic and rapid loads or stress concentrations over a period of time. Failure occurs by the initiation and propagation of cracks until they become unstable [Collins, 1993]. The loads and deformations that cause failure may sometimes be well below the static failure levels. When loads and deformations are of magnitudes such that less than $10^4$ cycles are required to produce failure, the phenomenon is called low cycle fatigue. When more than $10^5$ cycles are required to produce failure, the phenomenon is called high-cycle fatigue. According to some researchers, the limit separating the low and the high-cycle fatigue regime is accepted as 50,000 cycles. The dividing limit is not well defined. Some other definitions set the limit at 10,000 [Collins, 1991]. Some resources set different limits for different materials. Peak stresses for the low cycle fatigue are above the yield strength, and the strains induced have an obvious plastic component. In contrast to low-cycle fatigue, high-cycle fatigue strains are limited within the nominal elastic region.
Differentiation between high and low-cycle fatigue would be most rational when based on elastic and plastic strain measures.

Cyclic or repeated loading is of concern because it initiates the formation of internal and surface micro-cracks that are propagated under normal die-casting process conditions. Finally, a crack size is reached that reduces the remaining cross-sectional area to a point where the structure or the portion of it, can no longer handle the loading. Increasing cyclic stress levels can cause earlier crack initiation and subsequent die failure. Otherwise, failure times prolonged and a low enough stress level can be reached and maintained where cracks do not initiate and cycling does not result in failure.

It is generally accepted that the three stages of fatigue are crack initiation (nucleation), crack propagation and failure. Many studies have been devoted to understanding these mechanisms associated with fatigue phenomenon [Askeland, 1994].

2.2.2.1 Heat Checking & Onset of Heat Checking/Crack Initiation (Nucleation) in Die-Casting

Following are a collection of definitions and information on heat checking and onset of heat checking described by die-casting researchers. Information is presented in the form of quotes.

- "The limiting property of these materials (such as AISI H13 steel) is "heat checking", a condition in which network cracks are formed from stresses brought about by the constraint of the subsurface material during heating and cooling. There are many variables, which affect severity of heat checking. These include the maximum and minimum temperatures experienced by the die in a casting cycle, the temperature difference between the maximum and minimum temperatures, the frequency of heating and cooling, the heating and cooling rates during cycling, the die material and its processing etc." [Hochanadel, 1996]

- "It is generally agreed that one of the principal causes of termination of die life is heat checking, which occurs through a process of crack initiation and propagation from thermal fatigue induced on a die surface. Thermal fatigue is caused by temperature gradients at the die surface, which are present during rapid temperature changes as molten metal is injected into the die. As a result of these changes, strain is
produced which is related to the coefficient of expansion of material. Heat checking occurs when temperature-induced stresses exceed the strength of the material, producing cracks in the most sensitive areas of the die. The cracks propagate as further castings are made, and a network of cracks forms which gradually spreads and deepens." [Howes, 1969]

![Diagram showing temperature and number of cycles to form cracks](image)

*Figure 2.23: Howes indicating critical/initial crack length of 0.020 inches (0.508 mm) for H-13 thermal fatigue test specimens [Howes, 1969]*

"Evaluation of thermal fatigue is based on the number of cycles to (1) initiate a crack or (2) produce a crack depth of, say 2 mm. The resistance may also be based on the rate at which crack propagates per cycle...Extrapolation of crack depth back to the point of zero crack depth given an indication of the number of cycles necessary to initiate the crack." [Howes, 1969]

"Crack initiation usually occurs at stress concentrations from a structure’s geometry, machining irregularities and surface imperfections. Initiation in pieces with no stress raising surface flaws is caused by cyclic slip of favorably oriented surface grains. The resulting surface intrusions occur in the direction of maximum shear stress and will grow until a length sufficient for growth as a Mode I fatigue crack is reached. This critical crack length varies between 4 and 250 (2.5 mm) microns for various metals, with the critical crack depth of alloy steels in the range of 5 microns. This depth may also be regarded as the maximum depth that a surface flaw may attain before it acts as a crack initiation point."[Graham, 1978]
Figure 2.24: Growth of chronologically successive cracks in one specimen [Howes, 1969]

- Uddeholm's heat-checking standard charts are presented in Figure 2.25. The explanation for the standards is as follows (Total heat checking rating is the sum of the points obtained from both scales):
  - Scale A: Pictures with increasing sizes of network cracks
  - Scale B: Rating of the largest crack present
Figure 2.25: Uddeholm heat checking standards (Ratings 2, 6 and 9 for both Scales A-B with magnification factor of 2X)[Malm et al, 1979]
Benedyk, Moracz and Wallace included the following information regarding evaluation of heat checking performance of certain materials including H-13 tool steel. [Benedyk et al, 1970]

"The average maximum crack length on the four sides of the fatigue specimen and the sum of the number of cracks times the square of their length are plotted versus the number of thermal fatigue cycles for various types of H-13 steels." The data for both average maximum crack length and \( \Sigma n_d^2 \) (summation of the squares of the crack lengths) indicate the best thermal fatigue resistance is obtained. "The number of cycles required for the initiation of cracks increased steadily with the austenitizing temperature, as indicated extrapolating the lines in Figure 2.26 to zero maximum average crack length"

![Figure 2.26: Determining onset of heat checking through crack propagation data [Benedyk et al, 1970]](image-url)
Moracz describes a test and evaluation procedure [Moracz, 1969]: "After an appropriate number of cycles of the type described previously, the specimen was removed from the testing apparatus and lubricant oxide film that formed during testing was removed by polishing the edges with 400 grade emery paper followed by 600 grade emery paper. The number of cycles tested before the specimen was removed varied from 2,000 to 5,000 depending on the resistance of the metal to thermal fatigue cracking. A section of edge, which is three inches long and equi­distant from each end was examined for cracks. Cracks at the edge were measured at 100X by using an optical assembly of a Leitz micro-hardness unit or a toolmakers microscope. Cracks were recorded in 50-micron ranges (0-50, 50-100 microns, etc.) and then averaged. Cracks as small as 25 microns could be observed." "The craze patterns including the large cracks can be related to a parameter that considers the total crack area. For this evaluation, it was assumed that the summation of the squares of the crack lengths within three-inch length examined \( \sum n d^2 \) accounts qualitatively for the increase in craze pattern area, which occurs in thermal fatigue testing. For this parameter, \( n \) signifies the number of cracks of a particular size, and \( d \) signifies the average crack length for that particular size after a specified number of cycles." [Moracz, 1969]

"At 21,000 shots, heat checking was observed by the naked eye." "The variation of crack density with shots measured on the cracks longer than 0.2 mm in length." Iwanaga defined crack density as number of cracks per unit length. More information on Iwanaga's study can be found in the literature review within section 2.3.1. [Iwanaga, 1997]

"Manson defines the initial crack length to be 0.003 inch (0.076 mm), which is enough to be detected. The number of cycles for a crack to develop from 0 to 0.003 inch is referred as \( N_i \)." [Nyamekye et al, 1998]

**2.2.2.2 Fatigue Failure**

Fatigue life expresses the ability of a material to withstand a given cyclic exposure and is measured in terms of cycles. In other words, fatigue life under certain thermal and mechanical conditions and interaction with an environment has significance only when associated with a specific exposure cycle. Fatigue conditions are characterized by the following factors,
In a strain driven loading situation, the simplest fatigue cycle to which an element may be subjected is a zero-mean sinusoidal or triangular history of constant amplitude with fixed frequency applied for a specified number of cycles. Such a strain-time history, known as a completely reversed cyclic strain, is shown in Figure 2.27. Using the Figure 2.27 we can define several useful terminology,

\[ \varepsilon_{\text{max}}: \text{maximum strain} \]
\[ \varepsilon_{m}: \text{mean strain} = (\varepsilon_{\text{max}} + \varepsilon_{\text{min}})/2 \]
\[ \varepsilon_{\text{min}}: \text{minimum strain} \]
\[ \varepsilon_a: \text{alternating strain amplitude} = (\varepsilon_{\text{max}} - \varepsilon_{\text{min}})/2 \]
\[ \Delta \varepsilon: \text{strain range} = (\varepsilon_{\text{max}} - \varepsilon_{\text{min}}) \]
\[ \mathbf{R}: \text{strain ratio} = \varepsilon_{\text{min}}/\varepsilon_{\text{max}} \]
\[ \mathbf{A}: \text{amplitude ratio} = \varepsilon_a/\varepsilon_m = (1-R)/(1+R) \]
Any of the two quantities above, but the combination of \( \varepsilon_a \) and \( \Delta \varepsilon \) or the combination of \( A \) and \( R \) are sufficient to describe the strain-time history. A second major type of strain-time history is the non-zero mean spectrum. This pattern is similar to the completely reversed case except that mean strain is either tensile or compressive. The nonzero mean case may be thought of as a static strain equal in magnitude to the mean \( \varepsilon_m \), with a superposed completely reversed cyclic strain of amplitude \( \varepsilon_a \) [Conway et al, 1991]

The two modes of fatigue phenomenon; low and high-cycle fatigue can be experienced by the die caster, depending upon the die design and process conditions.

### 2.2.2.3 Low-Cycle Fatigue

During the last two decades progress has been made in understanding the low cycle fatigue phenomenon. Many parameters affecting this fatigue regime have been investigated. These parameters are related to the stress-strain interaction, loading conditions, material properties and the external environment. Experience has shown, also one might intuitively conclude that the more severe the stress concentration in the die, the shorter the die life will be.
During operation under severe circumstances, two issues listed below arise,

- The very first transient cycle may create an unacceptably large plastic deformation, which may be enough to cause failure or excessive distortion.
- Low cycle fatigue

Low-cycle thermal fatigue occurs when a structure is unable to establish a residual stress state in the initial cycle that will allow subsequent cycles to have elastic response only. When there is such a residual stress state, the structure is said to have shaken down and low cycle fatigue is not a problem. Otherwise the conditions might lead to low-cycle fatigue or thermal ratcheting (whereby there is a net change in dimension of the structure with each cycle). A structure with rachetting damage becomes unable to perform its design function [ABAQUS, 1997].

In cyclic problems, the temperatures during the hot part of the cycle are often high enough to induce creep in the material. The creep range for metals usually begins at about 2/3 of the metals' melting temperature on the absolute scale. Depending upon the maximum temperature level experienced, low-cycle thermal fatigue may involve not only repeated cycles of plasticity but also creep. However, in this case creep is not anticipated due to high melting temperatures of tool steels. The amount of permitted inelastic deformation per cycle cannot be large, since the structure must last for certain number of cycles.

Typical stress amplitude versus number of cycles to failure (S-N) curve is shown in Figure 2.28. In the range from 1-10^2 cycles the fatigue strength is nearly constant and close to the strength of the material. In this plastic deformation region, the behavior of the material is much better described as a function of the cyclic strain amplitude than the stress amplitude. At a certain stress level, some material's S-N curve becomes parallel to the x-axis. This stress value is called fatigue (endurance) limit. The behavior of the material is characterized by a stress-strain hysteresis loop as shown in Figure 2.29 and typically non-linear and dependent of the processing history of the material.
Figure 2.28: A typical S-N curve with fatigue (endurance) limit for several wrought steels [Bannantine et al, 1990]

Low-cycle mechanical fatigue behavior of a material can be determined in a test standardized by ASTM (ASTM E-399). Several test specimen geometries can be used. The recommended three-point bend specimen is a single edge-cracked beam having an initial normalized crack. The standard compact test specimen is pin-loaded in tension. It also has an initial crack. Both specimens are shown in Figure 2.30.
Figure 2.29: A typical hysteresis loop [Knoerr et al, 1992]

Figure 2.30: Test specimens for crack growth tests [Knoerr et al, 1992]
During the fatigue tests, when the specimen is loaded with a cyclic stress or strain amplitude of constant magnitude, the material may behave in different ways listed as follows. The cyclic softening and hardening behaviors are shown in Figure 2.31.

* Cyclically stable behavior
* Cyclically hardening behavior
* Cyclically softening behavior
* Complex cyclic behavior

It has become general practice to keep the strain amplitude constant to investigate low cycle fatigue behavior. Most test methods utilize the local strain concept. The basic idea behind this concept is the point that the local fatigue response of the material at the critical point (that is the site of the crack initiation) is similar to the fatigue response of a small specimen subjected to the same cyclic loading conditions [Collins, 1993]. The cyclic strains can be determined by testing or by finite element modeling. For modeling, it is necessary to have both monotonic and cyclic material properties.

Non-zero mean stress has a primary influence on the service lives greater than a critical number of life cycles called the transition life. Transition life is defined in the upcoming sections. During fatigue tests, it was observed that a non-zero mean stress will shorten the service life of a structure. The difference between a compressive mean stress and a tensile mean stress is, that in the compressive mean stress region failure is insensitive to the magnitude of the stress, whereas in the tensile mean stress region failure is very sensitive to it. On the contrary, the influence of non-zero mean strain is important at design lives less than the transition life. Results of fatigue tests have indicated that a tensile mean strain has the same influence as a compressive mean strain of same magnitude [Knoerr et al, 1992].
2.2.2.4 High cycle fatigue

Repeated stressing can produce fracture even with the stress amplitude held within the nominal elastic response range of the material.

Yield criteria for materials are stress-based, meaning that yielding occurs when the equivalent stress within a body is greater than the yield strength of the material. Strains greater than 0.2% are usually associated with plastic material behavior. Examining the von Mises equivalent stress reveals if a body is undergoing purely elastic or elastic-plastic deformations at high strain levels. If there is no plastic deformation during all but the very first cycle, it is an indication of high-cycle fatigue. Conway and Sjodahl describe a criterion to differentiate the low and high-cycle fatigue; a

Figure 2.31: Some cyclic material behavior [Knoerr et al, 1992]
distinguishing feature of the low-cycle fatigue is that the peak stresses are above the yield strength, and hence the strains induced usually have a plastic component, which is large enough to be noticed. On the contrary high-cycle fatigue behavior strains are within the elastic region [Conway et al, 1991]

2.2.2.5 The nature of fatigue, crack initiation and propagation

The fatigue failure consists of three phases. A crack initiation phase occurs first, followed by a crack propagation phase and final unstable rapid crack growth phase leading to failure. In this section, phases of the fatigue mechanism will be addressed.

Although much effort has been spent on the crack initiation phase, it is still very difficult to predict the number of cycles to initiation. It however is very crucial to understand the conditions under which a crack initiates, because the life of a cyclically loaded structural component can be determined only when the three phases are evaluated individually and successfully.

Experimental studies have indicated that the crack initiation life must be related to the geometry of the structure, the cyclic loading conditions, the tensile properties and the environmental effects [Zheng, 1986]. Crack initiation and sub critical crack propagation are localized phenomena that depend upon the stress-field intensity. In other words, stress concentration has the main influence on the crack initiation. Consequently the geometry of notches and the radii of cavity surfaces are the parameters that also affect the crack initiation. Data obtained by testing a single edge notched specimen have shown that cracks do not initiate when the notch geometry and the nominal-stress fluctuations results in a magnitude of parameter $\Delta K_p(r)^{1/2}$ that is less than a given critical value of the material. $r$ is the radius at the notch tip and $\Delta K_{p}$ is the induced maximum stress-intensity fluctuation caused by the cyclic loading. Stress intensity factors $K_{p}$ represent the simultaneous effects of both geometry and loading conditions over the stress fields. Some investigators observed that the crack initiation life could be related to yield strength of steels [Barsom et al, 1987].
The stress intensity factor $K$ is described in the following equation,

$$K = \sigma(\sqrt{af(g)})$$

(2.14)

where $f(g)$ is a parameter that depends on the size of the crack $a$, and the specimen geometry [Barsom et al, 1987]. The most successful approach for predicting the crack initiation is the local strain concept. The fatigue response of a small specimen loaded with constant cyclic strain amplitude can be related to the number of cycles to crack initiation. The relationship between the local strain state and crack development is identified from the observations of surface conditions [Fash et al, 1988].

To determine the life of a structural element that contain cracks, some other approaches have been developed. To evaluate the crack growth phase, tests were designed in which the specimen has an initial crack and is then cyclically loaded with constant stress amplitude. The incremental increase of the crack length is measured and the corresponding numbers of cycles are recorded. The data are then recorded on a crack length versus number of load cycles curve. The number of cycles to failure will decrease with a larger initial crack size and higher constant stress amplitude.

Different crack displacement modes are described by subscripts I, II, & III. The most commonly investigated mode is the Mode I, the crack opening mode. It occurs when the crack faces moving apart from each other in the same plane under tensile loading. Mode II or the sliding mode consists of the crack faces sliding relative to one another in a direction normal to the leading edge of the crack. Mode III, or the tearing mode involves relative sliding of the crack faces parallel to the leading of the crack edge. Mode I is initiated by tension loading while the other two are caused by shear loading in different directions [Dowling, 1993].
In the crack propagation phase the crack will grow from the initial flaw size to final crack size. Since the nature of the crack propagation process has been studied many times, many models have been proposed for predicting the crack grow rate, \( \frac{da}{dN} \). The governing laws are of the form,

\[
\frac{da}{dN} = f(\Delta \sigma, a, C)
\] (2.15)

where \( \Delta \sigma \): alternating stress range

C: material constant

a: initial crack size

N: number of cycles

Equation 2.15 indicates that the crack growth rate depends on the amplitude of the alternating stress range and the initial crack length. These are the same parameters, which influence the stress intensity factor \( K \). Thus, the crack growth rate can also be related to the stress-intensity factor. Crack propagation data obtained from experimental studies have supported the above relationship.
Experimental studies also indicated that the fatigue crack propagation phase of many metals could be divided into three regions. Figure 2.33 shows a log-log plot of the crack growth rate $da/dN$ versus the amplitude of the stress-intensity factor $\Delta K_i$. The stress-intensity factor $K_i$ is defined by the equation in case of Mode I displacement behavior and failure resistance. $\Delta K_i$ is determined by a cyclic stress range $\Delta\sigma$. Region I is the crack initiation and early propagation phase. Region II indicates the stable crack growth. Region III corresponds to the transition into the unstable phase of rapid crack growth. For most materials, Region II is governed by the following relationship,

$$\frac{da}{dN} = C \cdot \Delta K_i^n$$  \hspace{1cm} (2.16)

where $\Delta K_i$: alternating stress-intensity factor

$C$: material's resistance

$n$: material specific exponent (which describes the slope of the curve in that region)

$a$: crack length

$N$: number of cycles

Compared to Region II, which has a linear log-log correlation, Region III has a non-linear and higher crack grow rate. Region I has the lowest propagation rate. Equation 2.16 shows the importance of the stress intensity factor in controlling the fatigue crack propagation. The final phase of the fracture is characterized by the linear elastic stress-intensity factor. If the stress-intensity factor at the crack tip reaches a critical value $K_c$, the crack propagation switches from slow crack propagation to unstable crack propagation. The critical value of $K_c$ is known also as fracture toughness and represents the material's resistance against failure. The fracture toughness depends on the particular material, temperature, and the loading conditions.
Figure 2.33: Crack growth behavior [Hertzberg, 1983]

There is no complete agreement on the details of the initiation and propagation mechanisms. Some of the explanations offered by the researchers are presented below [Collins, 1993]:

Fatigue crack nuclei assumed to be formed through the movements of dislocations that produce fine slip bands at the crystal structures. The application of a static stress produces slip steps at the surface that are approximately $10^{-4}$ to $10^{-5}$ cm in height. These slip bands are characterized as coarse slip bands. Under repeated loading it is more common to observe fine slip bands of about $10^{-7}$ cm in height in the regions where the fatigue cracks were initiated. The slip bands introduce surface grooves and ridges as shown in Figure 2.34 because of reversed slip on adjacent slip planes due to load reversal. These grooves and ridges may either be sharply saw-toothed or smoothly rounded corrugations. If many planes slip, the resulting structures are shallow and undulating. If only a few closely spaced planes are active, sharply defined patterns form. Clearly defined
Figure 2.34: Nucleation and propagation mechanism [Collins, 1993]

Figure 2.35: Intrusions and extrusions on a pre-polished surface [Hertzberg, 1983]
extrusions also form as a result of reversed slip. Detectable slip band cracks are always found at these extrusions, which may be intrusions caused by the reversed slip. Once formed, these intrusions grow in depth by the reversed slip process. They may well be the major portion of the fatigue life of the metal.

Another proposed theory for the initiation of fatigue nuclei is based on an observation that many dislocation loops are produced by cyclic stresses. The interaction of these loops produces many vacancies in lattice structure that condense at the operative slip planes to form stable holes. Certain holes have been found along slip bands and grain boundaries, but the mechanism of their formation has not been clearly understood.

The extension of fatigue nucleus by reversed slip takes places along the slip planes closely aligned with the direction of maximum shearing stress. While the crack continues to grow along the active slip plane, no change in mechanism of growth has been encountered. This type of crack has been referred as Stage I crack growth. Stage I crack seems to be driven by low applied stresses and conditions leading to very slow crack growth. If stress cycles, notches or conditions that result in high ratio of tensile stresses to shear stress components exist, then Stage I crack growth may be replaced by Stage II crack growth.

Stage II crack growth is governed not only by the local shearing stress but also by the maximum principal normal stress in the neighborhood of the crack tip. Consequently, the crack tip deviates from its slip path to propagate in a direction roughly perpendicular of the maximum normal stress. The fracture surface during Stage II growth is characterized by striations and beach marks that can be related in their density and width to the applied stress level. The surface produced during the Stage II growth is relatively smooth.

Ultimately the crack length reaches a critical dimension and very few additional cycles may cause complete failure. The final failure region will typically show evidence of deformation produced prior to final separation.
2.2.2.6 Multi-axial fatigue

The fatigue process is much complicated if the state stresses and strains are multi-axial. Three different concepts describing the fatigue life under multi-axial conditions have been developed.

The first one is a concept that the three-dimensional stress-strain state is reduced to an equivalent one-dimensional model. This concept gained acceptance in high-cycle fatigue life regimes. The equivalent quantity is frequently obtained by the yield criteria such as Tresca and von Mises.

The second approach is the concept of plastic work or energy method. The concept is based on an idea that the material's resistance to fatigue is measured and evaluated by the amount of work or energy required to cause failure.

The third approach is the critical plane approach. In Stage I Mode II or Mode III type loading causes crack growth. For Stage I the plane of maximum shear lies at 45 degrees to the surface. The crystallographic growth in this stage is based on slip processes and related to the applied shear stress. Mode I loading is associated with Stage II crack growth. Stage II is a continuum crack growth in a plane perpendicular to the maximum principal stress.

2.2.2.7 Fatigue and Stress Analysis in Die-Casting Dies

In today's dynamic engineering world, majority of engineering problems involve components subjected to cyclic, fluctuating and rapid loads. Such loading induces time varying stresses and may often result in fatigue failure of structures. It is also difficult to detect progressive changes in the material properties during exposure to fatigue. The damage done during cyclic exposure is also cumulative, and generally unrecoverable.

Fatigue analysis is a life prediction method that enables designers to evaluate the service life of a structure during the design phase. Necessary modifications can then be made at the early stage of the tool and process design, when the costs for modifications are relatively low.
To estimate the service life of a structure, it is necessary to determine the number of cycles to failure, which is the sum of the number of the cycles required for crack initiation, $N_i$, and the number of cycles in which the crack will grow to a critical size, $N_f$.

\[ N = N_i + N_f \] (2.17)

Different crack initiation and propagation techniques are used for life prediction. The fatigue analysis procedure for estimating the crack initiation portion of a structure’s life is shown in Figure 2.36. The procedure consists of two operations,

![Fatigue Analysis Procedure Diagram](image)

Figure 2.36: A fatigue analysis procedure [Knoerr et al, 1992]
- Structural analysis computing the stresses and strains at the critical locations of the dies when data are given for loading history and material properties.

- Damage analysis consists of damage assessment and accumulation. Damage assessment uses the stresses and the strains determined for critical areas to compute damage per cycle loading which later can be accumulated. When the damage sum reaches a critical value (the failure criterion), the number of cycles at that location of the die is the predicted fatigue life.

For a steady state analysis, the loading of each cycle is assumed to be identical. Thus, damage analysis and accumulation can be performed in one step, as the amount of damage per cycle is the same. A fatigue resistance curve showing the relationship of the strain or stress amplitude in the critical die area to the number of cycles, a strain or stress-life curve, would directly yield the fatigue life of the die to crack initiation. The damage analysis should be performed differently for the crack initiation and crack propagation phases. The strain or stress amplitude for the damage analysis is determined by an elastic-plastic stress analysis. For the service life estimate from the initiation to the ultimate failure, fracture mechanics approaches must be taken.

Today's designer is increasingly challenged by the realization of dies with higher performance, higher production rates, resistance to higher temperatures, higher reliability and longer life at a reasonable cost and a fabrication time. To successfully achieve these design objectives, one must face such issues as stated by Collins [Collins, 1993]:

- Calculations of life are generally less accurate and less dependable than those of strength calculations.
- Fatigue characteristics of a material are difficult to be deduced from mechanical properties and must be measured directly.
- Results of different but identical fatigue tests may differ requiring a statistical interpretation.
• Materials and design configurations must often be selected to provide slow crack propagation and, if possible, detection of cracks before they become dangerous.

Some of the macroscopic and microscopic effects and basic data requiring consideration in designing under fatigue phenomenon must include the impact of,

• A simple, completely reversed alternating stress on the mechanical properties of materials
• A steady stress with a superposed alternating component (the effects of cyclic stresses with a nonzero mean)
• Frequency of loading
• Multi-axial alternating stresses
• Stress gradients and residual stresses, such as imposed by manufacturing processes employed for fabrication
• Stress raisers, such geometric details as convex and concave surfaces
• Surface finish, including the effects of machining, electroplating and coating
• Fatigue behavior of materials at elevated temperatures
• Size of structural element
• Accumulating cycles at various stress levels and the permanence of the effect
• Variation in fatigue properties to be expected for a certain material
• Environmental factors
• Interaction between fatigue and other modes of failures [Collins, 1993]
• Tempering
• Phase Change
• Decarburization (loss of carbon)
2.2.2.8 Life Estimations and Method of Universal Slopes

Generally the fatigue stress and strains are measured or estimated under stable hysteresis conditions. The two most important two factors determining the material behavior are cyclic stress and strain range. Cyclic stress and strain range can be related to fatigue life through empirical models depending upon the type of the fatigue occurrence within the structures. A number of empirical correlations relating strain amplitude and fatigue life cycles to failure, \( N_f \), have been developed.

For high cycle fatigue, the elastic strain range \( \Delta \varepsilon_{el} \) is correlated to \( N_f \) through the following equation,

\[
\Delta \varepsilon_{el} = \frac{\sigma_f}{E} (2N_f)^{-b}
\]

where \( E \) is the Young's modulus

\( \sigma_f \) and \( b \) are empirical constants

The factor \( \sigma_f \) is related or close to material's uni-axial tensile strength meaning that the strongest material will exhibit best high-cycle fatigue response. This is also consistent with the idea that a significant portion of high-cycle fatigue life is occupied with the nucleation of cracks since nucleation of cracks are driven by the local plastic deformation, especially on the materials surface. Increases in the surface strength can delay formation of cracks. Surface shot peening and specialized surface treatment methods such as carburizing, nitriding and surface martensite formation are used to increase the surface yield and tensile strength and consequently to improve the high cycle fatigue behavior. The exponent \( b \) is related to the material's cyclic work hardening coefficient, \( n \). It is determined through the empirical Equation 2.19 [Hertzberg, 1983],

83
\[ \frac{\Delta \sigma}{2} = K' \left( \frac{\Delta \varepsilon}{2} \right)^{n'} \]  \hspace{1cm} (2.19)

The coefficient \( b \) varies with \( n' \) as,

\[ b = \frac{n'}{(1 + 5n')} \]  \hspace{1cm} (2.20)

Since larger \( b \) values produce more negative slope in the \( \Delta \varepsilon/2 - N_f \) plot, it can be deduced that a material having a high cyclic work-hardening coefficient is not as useful for high-cycle fatigue performance compared to a lower \( n' \) value. The factor \( n' \) has less impact on the high-cycle fatigue response compared to \( \sigma_f' \). Proper use of the relationship between \( b \) and \( n' \) requires knowledge of cyclical hardening response of a material. With the absence of such data, monotonic hardening coefficient \( n \) can be used to estimate initial \( b \) values.

Empirical relations between plastic strain amplitude and the lives to failure for low-cycle fatigue have also been developed. One expression is,

\[ \frac{\Delta \varepsilon_{pl}}{2} = \varepsilon_{f}' (N_f)^{-c} \]  \hspace{1cm} (2.21)

where \( c \) is typically 0.5-0.7 and correlates with \( n' \) through \( c = \frac{1}{(1 + 5n')} \)

\( \varepsilon_{f}' \) is close to the tensile ductility which is equal to \( \ln \left( \frac{1}{1-R_A} \right) \) where \( R_A \) is the reduction in area

Thus, for low cycle fatigue it is desirable to have a material that exhibits both good ductility and high work hardening to obtain increased life values.

To correlate the strain amplitude with cycles to failure, it is reasonable to assume that the following relationship can be used for the transition life as shown in Figure 2.37.
\[
\frac{\Delta \varepsilon}{2} = \frac{\Delta \varepsilon_{pl}}{2} + \frac{\Delta \varepsilon_{el}}{2} = \varepsilon_f^c (N_f)^{-c} + \frac{\sigma_f}{E} (N_f)^{-b} \tag{2.22}
\]

where \(\Delta \varepsilon\) is total strain range.

The relationship 2.22 is plotted in Figure 2.37. For the lives less than \(10^4\) total strain range is almost equal to plastic strain range, while for the lives greater than \(10^4\), total strain range can be approximated by elastic strain range. Experiments have indicated that the cyclic life is better related to the total strain amplitude especially at the longer life end of the low cycle range. The intersection of the elastic and the plastic region determines the transition life.

High ductility is desired for low cycle applications since fatigue cracks are nucleated in the early stages of the structure's life. Since a material that has a sufficient work hardening ability and good malleability inhibits slow crack growth, these properties are required for low-cycle fatigue response [Hertzberg, 1983].

Manson relates cyclic total strain range \(\Delta \varepsilon\) and number of cycles to failure \(N_f\) [Conway et al, 1991] with the following equation,

\[
\Delta \varepsilon = M (N_f)^c + \frac{G}{E} (N_f)^\gamma \tag{2.23}
\]

where \(E\) is the Young's Modulus

and \(M, G, z,\) and \(\gamma\) are material properties.
Upon investigation of many materials, Manson related the factors $M$ and $G$ to the material properties that could be determined with a simple tensile test,

$$ M = D^{0.6} \quad (2.24) $$

$$ G = 3.5\sigma_u \quad (2.25) $$

where $D = -\ln (1-R_A)$ is the logarithmic ductility

$\sigma_u$ is the ultimate tensile strength

Furthermore Manson presumed that the exponents $z$ and $\gamma$ could be approximated as -0.6 and -0.12, respectively regardless of material. Combining the equations leads to,
The first term represents the plastic strain amplitude whereas the second term is the elastic strain range.

\[
\frac{\Delta \varepsilon}{2} = \frac{\Delta \varepsilon_{pl}}{2} + \frac{\Delta \varepsilon_{el}}{2} = D^{0.6}(N_f)^{-0.6} + 3.5 \frac{\sigma_u}{E}(N_f)^{-0.12} \tag{2.26}
\]

Since the material independent components \( z \) and \( \gamma \) are the slopes of curves of the relations in log-log scale, the method is called universal slopes [Samuels et al, 1975][Conway et al, 1991]. The plastic strain range best approximates low-cycle fatigue life while high-cycle life can be related to elastic strain range.

Morrow suggested that the mean stress effect could be taken into account by modifying the elastic term in the basic universal slopes equation, Equation 2.22 [Bannantine et al, 1990]. This yields the following:

\[
\frac{\Delta \varepsilon}{2} = \frac{\Delta \varepsilon_{pl}}{2} + \frac{\Delta \varepsilon_{el}}{2} = \varepsilon_f(N_f)^{-c} + \frac{\sigma_f - \sigma_0}{E}(N_f)^{-b} \tag{2.29}
\]

where \( \sigma_0 \) is the mean stress.

For the steels with hardness levels which are below 500 BHN (Brinell Hardness Number) the true fracture strength \( (\sigma_f) \) can represent the cyclic strength \( (\sigma_{\varepsilon'}) \) with the following relation:

\[
\sigma_f = \sigma_u + 345 \text{ MPa} \tag{2.30}
\]

For the hardness levels below 200 BHN \( \sigma_f \) is assumed to be 1033 MPa [Landgraf, 1996].
2.3 Previous Studies in Thermal Stress and Fatigue

In this chapter the reviewed literature is categorized in 2 major groups. The first group contains the papers concerning the thermal-stress and fatigue analysis. Papers covering the topics such as steel and die fabrication, heat treatment of die-casting dies, microstructure effects on material mechanical and fatigue properties and testing of die materials constitute the second group.

2.3.1 Thermal-Stress and Fatigue Analysis within Die-Casting Dies

The paper by Suzuki, Isihara and Miyachi described the experiments performed to initiate heat checking by applying repeated thermal shocks to a specimen whose residual stresses were measured by X-ray analysis prior to the application of the shock. The initiation of heat checking and propagation of cracks can be predicted by measurement of residual stresses. The thickness layer of the compressive residual stress was acknowledged to be very important in preventing heat checking in addition to the recognized influence of the magnitude of residual stresses [Suzuki et al, 1972].

A recent paper by Iwanaga expanded Suzuki, Ishara and Miyachi’s study. The author studied the relationship between the initiation and propagation of heat checking, and the surface residual stresses on an actual H-13 production die. The heat checking observed in detail by Scanning Electron Microscopy (SEM) and the surface residual stresses were measured by X-Ray diffraction. Detailed observations of surface cracking and measurement of residual stresses were done repeatedly about more than 10 times for each prescribed shot on the cover and ejector halves of the production die as shown in
Table 2.14: Process conditions and die properties [Iwanaga, 1997]

<table>
<thead>
<tr>
<th>Die Material</th>
<th>AISI H13</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hardness</td>
<td>HRC 43-46</td>
</tr>
<tr>
<td>Surface Roughness</td>
<td>$R_a=1.0-1.5\mu m$</td>
</tr>
<tr>
<td>Die Casting Machine</td>
<td>4.9MN</td>
</tr>
<tr>
<td>Die Dimension</td>
<td>240 x 300 x 50mm$^3$</td>
</tr>
<tr>
<td>Preheating of Die</td>
<td>Preheating by Steam</td>
</tr>
<tr>
<td>Cycle Time</td>
<td>50 sec.</td>
</tr>
<tr>
<td>Al alloy</td>
<td>AA 383.0 (ADC 12)</td>
</tr>
<tr>
<td>Temp. of Molten Al</td>
<td>640 C</td>
</tr>
</tbody>
</table>

Figure 2.38. Table 2.14 shows the process conditions and die material properties used in testing. Crack density was defined as number of cracks per unit length as given in Equation 2.31,

$$r = \frac{n}{\sqrt{\frac{s}{m}}}$$  \hspace{1cm} (2.31)

where $r$: crack density (number/mm)

- $n$: crack numbers crossing vertical or horizontal segments
- $s$: total length of segments
- $m$: magnification of photo
Figure 2.38: Schematic die drawings [Iwanaga, 1997]

Figure 2.39: Crack density [Iwanaga, 1997]
The study revealed important results as summarized below [Iwanaga, 1997],

- Early stage cracks had three types of different origins; pits and wavy patterns, machining scratches and defects such as indentations and strain concentrated parts of the die surface.

- The surface residual stress, which was compressive before the process, became immediately tensile and saturated in the early stage of the die-casting operation.

Figure 2.40: Origins of cracks; pits, indentations and scratches (Magnification ratios: 50X, 50X and 500X from left to right) [Iwanaga, 1997]

- On the gate side of the die cavity, in which the saturated tensile stress was large, micro-cracks initiated in the saturation period. On the opposite side apart from the gate, in which the saturated tensile residual stress was small, the residual stress did not decrease and micro-crack did not initiate until the end of the service life.
In the later stage of crack propagation on gate side of the die cavity, while heat checking was observed by the naked eye, the tensile residual stress dropped to zero.

Iwanaga also studied the residual stress profile and the relation between the crack density and residual stresses. The information regarding these is as shown in Figures 2.42 and 2.43.
Figure 2.42: Residual stress distribution in substratum of die surface [Iwanaga, 1997]

Figure 2.43: Relation between crack density and residual stresses in the ejector die [Iwanaga, 1997]
Hattel and Hansen presented an analytical method for predicting the normal stresses in a die surface, which is exposed to thermal loading. Potentially, this model may be used for the die-casting process. Several expressions for the normal stresses, which are a function of the mechanical and thermal properties, were developed for two different die-casting cases with and without the presence of any coating and/or lubricant layer at the die/casting interface. The assumptions made by the authors were,

- The mold was allowed to expand in x direction since melt was thought not to have any mechanical resistance. \((\sigma_{xx}=0)\)
- Model was assumed to be symmetrical; the normal stresses in y and z directions would be equal. \((\sigma_{yy}=\sigma_{zz} \text{ and } \epsilon_{yy}=\epsilon_{zz} \text{ is also constant in space but vary in time})\)
- Since the casting is often thin compared with the die halves and the inserts are assumed to be well constrained by the rest of the dies, and the die wall does not bend. \((\sigma_{xy}=\sigma_{xz}=\sigma_{yz}=0 \text{ and } \epsilon_{xy}=\epsilon_{xz}=\epsilon_{yz}=0)\)

![Figure 2.44: 2-D domain of the melt (casting) and mold](Hattel et al, 1994)
Hattel and Hanson derived and evaluated the expressions with an emphasis of the effect of the heat transfer coefficient at the die/casting interface [Hattel et al, 1994]. Applying Hooke’s generalized law,

\[
\sigma_{yy} = \frac{E\nu}{(1 + \nu)(1 - \nu)} \left[ \left( 1 + \frac{\alpha\Delta T}{1 - \nu} \right) - \frac{2\nu\epsilon_{yy}}{1 - \nu} \right] + \left[ \epsilon_{yy} - (1 + \nu)\alpha\Delta T \right]
\]  

(2.32)

where \( E \): elastic modulus

\( \nu \): Poission's ratio

normal stresses at the surface can be obtained. After some reductions, the relations becomes,

\[
\sigma_{yy} = \frac{E(\epsilon_{yy} - \alpha\Delta T)}{1 - \nu}
\]  

(2.33)

Utilizing the equilibrium in \( y \) direction and assuming symmetry yields,

\[
\int_{0}^{L} \sigma_{yy} \, dx = 0
\]  

(2.34)

Rearranging with substitution of the value of \( \sigma_{yy} \) from the relation 2.33 into 2.34 and assuming that \( \epsilon_{yy} \) and \( \alpha \) are constant, results in the following relation,

\[
\epsilon_{yy} = \frac{\alpha}{L} \int_{0}^{L} \Delta T \, dx
\]  

(2.35)

\[
Q = \int_{0}^{L} \rho c_p \Delta T \, dx
\]  

(2.36)
Q is the total energy per unit area as seen in relation 2.36. The equation 2.35 may be re-written as,

\[ \varepsilon_{yy} = \frac{\alpha Q}{\rho c_p L} \]  

(2.37)

Assuming that the heat flow is uni-directional, Q may be expressed as the heat flux per unit area, q, integrated over the time period,

\[ Q = \int_0^t q \, dt = \int_0^t k(T_s - T_i) \, dt \]  

(2.38)

where \( \kappa \) is the thermal diffusivity of the die material (\( \kappa = k/\rho c_p \))

\( T_s \) is the surface temperature

\( T_i \) is the initial temperature of the die wall

Equation 2.38 is only valid if the thermal resistance between the casting and the die surface is zero, meaning that the heat transfer coefficient between the two is infinitely large. Another point for the model above is that the constant initial temperature assumption is only true for the first casting cycle. Substituting Q from 2.38 into 2.37 gives the \( \varepsilon_{yy} \) as a function of material’s thermal diffusivity and conductivity, time and the thermal conditions of the die surface. Introducing the dimensionless Fourier number (\( Fo = \kappa t/L^2 \)), substituting \( \varepsilon_{yy} \) of 2.37 into 2.33 and rearranging, the following stress relation can be obtained.

\[ \sigma_{yy} = \frac{\alpha E(T_s - T_i)}{(1 - \nu)} \left[ 2 \left( \frac{Fo}{\pi} \right)^{1/2} - 1 \right] \]  

(2.39)

At the beginning of the cycle \( t \) is equal to 0 (\( Fo=0 \)). The stress value at the die surface is shown in the following relation.
\[ \sigma_{yy} = -\frac{\alpha E(T, -T)}{(1 - \nu)} \] \hspace{1cm} (2.40)

Figure 2.45 shows the stress state at the surface as a function of the dimensionless Fourier number.

If the \( \sigma_{yy} \) values are normalized by the initial stress value, the plot in Figure 2.46 can be drawn.

To model the case with coating or lubricant layer a thermal resistance at the casting/die interface is used. The convective heat transfer coefficient \( h \), the dimensionless Biot number \( (=hL/k) \) and the constant \( a (=hk^{1/2}/k) \) were introduced with the second model.

Figure 2.45: \( \sigma_{yy} \) stress history throughout the casting cycle (\( \sigma_{yy} \) in MPa) [Hattel et al, 1994]
If the thermal model for a semi-infinite body with a convective heat transfer boundary condition at the casting/die interface is solved, the following temperature distribution is obtained,

\[
\frac{T - T_i}{T_m - T_i} = 1 - \text{erf} \zeta - \exp \left[ \frac{hx}{k} + \frac{h^2 \kappa}{k^2} \right] \left[ 1 - \text{erf} \left( \zeta + \frac{h(\kappa)}{k} \right) \right] \tag{2.41}
\]

where \( \zeta = \frac{x}{(4\alpha t)^{1/2}} \)

\( T_m \) is the temperature of the melt

\( T_i \) is the initial temperature of the die wall

Since the focus is the die cavity surface \((x=0)\), the Equation 2.41 can be rearranged by placing the constant \( a \), into the relation,
\[ T_m - T_s = (T_m - T_i) \exp[a^2 t] \text{erfc}(at^{1/2}) \]  

(2.42)

where \( T_s \) is the surface temperature

and 2.42 can be obtained. Applying Newton’s Cooling Law mentioned in Equation 2.43,

\[ Q = hA(T_m - T_s) = \int_0^t \frac{q}{A} dt \]  

(2.43)

Q can be obtained as follows,

\[ Q = h(T_m - T_i) \left[ \frac{\exp[a^2 t] \text{erfc}(at^{1/2})]}{a^2} - 1 \right] + \left[ \frac{2}{a} \left( \frac{t}{\pi} \right)^{1/2} \right] \]  

(2.44)

Figure 2.47 shows the time history of \( Q \) for different convective heat transfer coefficients. Also \( Q \) can be non-dimensionalized by dividing 2.44 by \( h(T_m - T_i) \). Finally \( \varepsilon_{yy} \) and \( \sigma_{yy} \) can be estimated with the employment of Biot and Fourier numbers,

\[ \varepsilon_{yy} = a(T_m - T_i) \left[ \frac{\exp[a^2 t] \text{erfc}(at^{1/2})]}{Bi} - 1 \right] + 2 \left( \frac{\Fo}{\pi} \right)^{1/2} \]  

(2.45)

\[ \sigma_{yy} = \frac{E\alpha(T_m - T_i)}{1 - n} \left[ \exp[Bi^2 \Fo \text{erfc}(BiFo^{1/2})] - 1 \right] \left[ 1 + \frac{1}{Bi} \right] + 2 \left( \frac{\Fo}{\pi} \right)^{1/2} \]  

(2.46)

Figure 2.48 shows the \( \sigma_{yy} \) stress history as a function of the dimensionless Fourier number for different Biot numbers.
Figure 2.47: $Q$ versus $t$ and $h$ for different Biot numbers [Hattel et al, 1994]

Figure 2.48: $\sigma_{yy}$ as a function of the Fourier number for different Biot numbers [Hattel et al, 1994]
Results showed that the maximum compressive stress occurred at the beginning of the cycle for the case without the presence of coating. The interface thermal resistance assumption delayed and reduced the maximum stress at the surface. The paper also described a reduction ratio to define the difference for the two cases [Hattel et al, 1994].

Hattel, Hansen & Anderson also presented a control volume based FDM (Finite Difference Methods) technique for solving thermally induced stress and strains in die-casting. The governing equations are the general Navier equations for the non-constant material properties. The equations are based on equilibrium equations where the stresses are stated by the strains through Hooke's Law. The strains are expressed by linear displacements. Expression in tensor notation is as follows;

\[
\left[ \frac{E}{(1+\nu)} \left( \frac{u_{i,j} + u_{j,i}}{2} + \frac{\nu}{(1-2\nu)} \delta_{ij} \mu_{k,k} \right) - \delta_{ij} \left( \frac{E\alpha\Delta T}{1-2\nu} \right) \right]_{ij} = 0 \tag{2.47}
\]

where \( u_{i,j} \) is the component of the linear displacement vector (in i axis relative to j axis)
\( \alpha \): coefficient of linear thermal expansion
\( E \): Young's modulus
\( \nu \): Poisson's ratio
\( \delta_{ij} \): Kronecker delta (1 if \( i=j \) and 0 if \( j \) is not equal to \( i \))

The 3-D thermal-elastic equations were solved iteratively on a staggered grid using a line-by-line solver. The method was extended to handle non-uniform meshes, non-constant material data and multi-domain problems. The code was also linked to the software MAGMASOFT to obtain interaction with and to utilize its graphic abilities. As an example, die-casting of an aluminum console for a photocopy machine was simulated on a mesh structure of 120,000 nodes, and the stress-strain distribution was obtained both in the casting and in the dies [Hattel et al, 1993].
Rosbrook and Shivpuri investigated the structural and thermal response of a die-casting die through test specimens and studied the relationship between the responses and thermal fatigue [Rosbrook, 1992]. A finite element model was developed for analyzing Wallace’s thermal fatigue test specimens [Benedyk et al, 1970]. Computer simulation of Wallace’s dunk test specimens was used to determine the temperature and stress-strain distributions within a two-dimensional plane-strain cross-section of the H-13 test sample. Estimated temperatures were compared with Wallace’s temperature measurements.

Figure 2.49: FEM model of the test specimen [Rosbrook, 1992]
Figure 2.50: Boundary conditions: 3 internal cooling, 2 & 1 symmetry, 4 exposed to die-casting cycle shown in the next figure [Rosbrook, 1992]

<table>
<thead>
<tr>
<th>temporal region</th>
<th>heat flow direction</th>
<th>heat flow rate</th>
</tr>
</thead>
<tbody>
<tr>
<td>I: heating, $t_1$ (injection and solidification)</td>
<td>into die</td>
<td>high</td>
</tr>
<tr>
<td>II: convection cooling, $t_2$ (dies open)</td>
<td>out of die</td>
<td>low</td>
</tr>
<tr>
<td>III: spray cooling, $t_3$ (lubrication application)</td>
<td>out of die</td>
<td>high</td>
</tr>
</tbody>
</table>

Figure 2.51: Modeled casting cycle [Rosbrook, 1992]
Initial simulations were conducted with the convective heat transfer coefficients suggested by Mobley [Papai et al, 1991] for aluminum die-casting with H-13 dies. The heat transfer coefficients were later modified so that experimental and calculated temperature fields would be compatible. The test piece indicated quasi-steady state after three cycles. Since a perfectly plastic material behavior was assumed, stress-strain cycling reaches a stable hysteresis regime as soon as quasi-steady state thermal behavior is attained. Cyclic stresses and strains were measured during stable hysteresis response. The method of universal slopes was used to relate the calculated cyclic strain ranges to the number of cycles necessary for fatigue crack initiation. Predicted life cycles were found to be compatible with Wallace's results. Different test conditions, which had not been investigated by Wallace, were also examined. Rosbrook predicted the total number of fatigue cycles at which cracking initiates, and compared it with the extrapolated value in Benedyk and Wallace's dunk test. The results indicated that the fatigue was high-cycle fatigue. Even though the strain range was 0.00906 ($\varepsilon_{\text{max}} - \varepsilon_{\text{min}} = 0.0102 - 0.00114$), the cyclic stresses were far below the yield stress. According to the results, the following empirical relationship was accurate in predicting crack initiation for the elastic fatigue regime [Rosbrook, 1992],

$$\Delta \varepsilon_{el} = \frac{3.5\sigma_u (N_f)^{-0.12}}{E}$$

(2.48)

where $\Delta \varepsilon_{el}$: range of elastic strain ($\varepsilon_{\text{max}} - \varepsilon_{\text{min}}$)

$\sigma_u$: ultimate tensile strength

E: Young's Modulus

$N_f$: number of cycles to failure
Samuels and Draper conducted an analysis of thermally induced stresses in Anviloy die-casting dies used in steel casting. To understand the effect of stresses induced by repeated cycles, an uncoupled quasi-static, thermo-elastic model transforming the thermal output from one dimensional heat transfer model into stress state was used. Samuels and Draper stated that their thermo-elastic model to describe the stress state was adequate. In the paper, the authors mentioned that materials, which do not have high density, would be prone to shear fracture, since the highly localized stresses developing between voids cannot be relieved by localized plastic flow. The yield criterion for ductile materials was developed as a function of the temperature:

$$\sigma_{ys}(T) = \sigma_0 - BT$$  \hspace{1cm} (2.49)

where \(B\) is assumed to be a known constant

- \(\sigma_{ys}\) is the shear yield strength
- \(\sigma_0\) is the shear yield strength at temperature \(T_0\)

By subtracting the maximum shear stress at a given temperature from the yield strength value at the same temperature, it can be deduced if yielding has occurred.

![Heat transfer model with finite differences](image)

Figure 2.52: Heat transfer model with finite differences [Samuels et al, 1975]
Figure 2.53: Thermal-stress model [Samuels et al, 1975]
Figure 2.54: Temperature field with time for a 0.5 in thick steel casting/die model (each curve shows the distance from the casting/die interface in inches) [Samuels et al, 1975]
Output from the stress model was then used to illustrate the impact of such variables as the die operating temperature and the physical properties of the die material on the stress levels. All assumptions and limitations of thermal stress modeling techniques were described. Draper and Samuels also discussed the general nature of the thermal stresses and the interaction between the stresses and low-cycle fatigue in an earlier study [Draper et al, 1972].
Weaver and Sanders established a computer model which is shown in Figure 2.56 to estimate the temperature gradients and thermal stresses within die-casting dies [Weaver et al, 1977]. The model employed an Explicit FD (Finite Difference) Scheme to numerically solve the governing differential equations describing the heat transfer. The model would estimate the temperature gradient throughout the casting, the ceramic insert and the die block body. It would also predict thermal stresses within the insert as shown in Figure 2.57. The model was useful for analyzing the effects of film coefficients on heat transfer, heat of fusion emitted during solidification and the effects of metal flow conditions on thermal conditions. The model predicted almost identical temperature fields compared with Samuels' previous studies. The difference was thought to be of the assumptions made concerning the initial temperature of the casting/die interface.

Noesen and Williams dealt with the properties of die materials and cast metals, which contribute to prolong die life relative to the thermal fatigue. Calculations were made to facilitate quantitative comparisons. [Noesen et al, 1966].

Figure 2.56: Geometry segments used in the study [Weaver et al, 1977]
Malm and Norstrom presented a material-oriented model for thermal fatigue and thermal-structural analyses and they discussed the connection of the model with tool steels in hot working applications. The model, which is based on ordinary fatigue laws, includes a simple stress-strain analysis. From the analysis, expressions were generated which relate crack initiation and growth to crucial material and process variables. Thermal fatigue maps and the utilization possibilities were also discussed. Any attempt to describe thermal fatigue in terms of ordinary fatigue processes should be based on an analysis of the plastic and elastic strains caused by the thermal cycling. The assumptions during modeling made by Malm and Norstrom were:

Figure 2.57: Stress fields at the time step of 0.1, 1.0 and 5.0 seconds [Weaver et al, 1977]
• Plane-stress conditions prevail in case a body is fixed in one plane and allowed to expand in a direction perpendicular to that plane.

• The material properties are isotropic and the material behaves in an ideal elastic-plastic way with no strain hardening and Bauschinger effect.

• Equilibrium assumed to be reached at the top and the bottom temperature of each cycle.

The authors considered a thermal cycle bounded by the lower temperature \( T_1 \) and the higher temperature \( T_2 \). The thermal extension in each direction is,

$$ \varepsilon_{th} = \alpha(T_2 - T_1) \quad (2.50) $$

where \( \alpha \) is the mean value of the coefficient of thermal expansion. According to the assumptions during modeling, the body is in a fixed plane and the thermal strain has to be compensated by the elastic and plastic strains.

$$ \varepsilon_{th} + \varepsilon_{el} + \varepsilon_{pl} = 0 \quad (2.51) $$

Figure 2.58 shows the stresses and strains induced by thermal cycling. There exist two elastic strain values; one at the top temperature and one at the bottom temperature of the cycle. Using Hooke's Law for plane stress,

$$ \varepsilon_{el} = \frac{(1 - \nu)\sigma_1}{E_1} \quad (2.52) $$

can be written for \( T_1 \). Equation 2.52 can also be written for \( T_2 \).
As seen from Figure 2.58, the initial point of the cycle is stress free. With the first heating step between the temperatures $T_1$ to $T_2$ (the points 0 to B) the thermal stress intersects with the temperature dependent yield strength at the point A and follows the yield strength curve to the point B. Plastic strain of the very first heating step is obtained by using the previous equations, and is shown in Equation 2.53.

\[
\varepsilon_{p0} = \alpha(T_2 - T_1) - \frac{(1-\nu_2)\sigma_2}{E_2}
\]  

\text{(2.53)}
Cooling from $T_2$ to $T_1$, the stress-strain curve follows the path from B to D and gives the following expression for the cyclic plastic strain,

\[
\varepsilon_p = \alpha(T_2 - T_1) - \frac{(1-2\nu)\sigma_2}{E_2} - \frac{(1-2\nu)\sigma_1}{E_1}
\]  

(2.54)

During second heating, the curve follows the path D to B through E with the identical plastic strain defined at the previous step. The following cycles will follow the path in the order of points, B, C, D, E, B and induce identical stress-strain state. The model ignored the first cycle.

Low-cycle fatigue is defined as the plastic strain in each cycle exceeds the elastic strain. Utilizing equations 2.52 and 2.53, the following condition for low-cycle fatigue regime can be written,

\[
\alpha(T_2 - T_1) > 2\frac{(1-\nu_2)\sigma_2}{E_2} + 2\frac{(1-\nu_1)\sigma_1}{E_1}
\]  

(2.55)

In the low-cycle regime, the life of crack initiation is usually determined by the Coffin-Manson Law,

\[
N_f^n \varepsilon_p = C\varepsilon_f
\]  

(2.56)

Where $N_f$ is the number of cycles to crack initiation

- $\varepsilon_p$ is the plastic strain range for each cycle
- $\varepsilon_f$ is the true deformation to fracture
- $n$ is a constant (0<n<1)
- $C$ is a constant (0<n<1)
If the $\varepsilon_p$ from 2.54 is substituted in 2.56 and the equation is rearranged the following was obtained,

$$N_f = (\varepsilon_f)/\left[ \alpha(T_2 - T_1) - \frac{(1-2\nu)\sigma_2}{E_2} - \frac{(1-2\nu)\sigma_1}{E_1} \right]^{1/n}$$

(2.57)

Since the fracture phenomenon mostly occurs under tensile stress conditions, it is logical to use lower temperature deformation.

The plastic strain range determines crack propagation in low-cycle fatigue regime. The following crack growth law has gained acceptance;

$$\frac{da}{dN} = \rho a \varepsilon_p^q$$

(2.58)

where $a$ is the initial crack length

$N$ is the number of cycles

$\rho$ and $q$ are positive constants

The relation for plastic strain $\varepsilon_p$, from 2.54 can be substituted into the Equation 2.58 to obtain another relation, which includes loading conditions, material properties and crack geometry.

As opposed to low-cycle fatigue, high-cycle fatigue regime is dominated by the elastic strain agreeing with the following inequality,

$$\alpha(T_2 - T_1) < -2\frac{(1-2\nu)\sigma_2}{E_2} + 2\frac{(1-2\nu)\sigma_1}{E_1}$$

(2.59)

Malm and Norstrom identified three different cases. In the first case, cyclic plastic strain exists if the constraints 2.59 and 2.60 are both satisfied (elastic strain greater than plastic
strain and $\varepsilon_p > 0$),

$$\alpha (T_2 - T_1) > \frac{(1 - 2\nu)\sigma_2}{E_2} + \frac{(1 - 2\nu)\sigma_1}{E_1}$$

(2.60)

In the high-cycle regime, the number of cycles to crack initiation can be obtained from Basquin's Law,

$$N_f^m \varepsilon_{el} = B$$

(2.61)

Where $N_f$ is the number of cycles to crack initiation

$\varepsilon_{el}$ is the elastic strain range for each cycle

$m$ is a constant ($0 < m < 1$)

$B$ is a positive constant

The thermal elastic strain range includes two different elastic deformations with elastic modulus values at temperatures $T_1$ and $T_2$. The paper suggests the following expression to be used to relate the two,

$$E\varepsilon_{el} = E_1 \varepsilon_1 + E_2 \varepsilon_2$$

(2.62)

High-cycle fatigue case can be modeled merging the equations 2.52, 2.61 and 2.62. The following relation was obtained,

$$N_f = \left[ B \frac{(1 - \nu_1)\sigma_1}{E_1} - \frac{(1 - \nu_2)\sigma_2}{E_2} \right]^{1/m}$$

(2.63)

A Paris type of relation such as the following can present the crack propagation rate for the high cycle fatigue,
\[
\frac{da}{dN} = A \alpha r^{\gamma} \frac{\sigma_1^0}{\sigma^2}
\] (2.64)

where \( \sigma_1 \) is the peak value of the applied tensile stress

\( \sigma \) is the yield strength

\( r \) is a constant \((2 < r < 4)\)

\( A \) is a positive constant

In the second case there is no cyclic plastic strain. Only cyclic tensile stress exists exhibiting plastic strain at the initial cycle \((\varepsilon_p = 0, \varepsilon_{p0} > 0)\). If the following condition is satisfied, purely elastic behavior will be present.

\[
\frac{(1 - \nu_1)\sigma_1}{E_1} + \frac{(1 - \nu_2)\sigma_2}{E_2} \geq \alpha (T_2 - T_1) \geq \frac{(1 - \nu_2)\sigma_2}{E_2}
\] (2.65)

The elastic strain at the temperature \( T_1 \) is,

\[
\varepsilon_{el} = \alpha (T_2 - T_1) - \frac{(1 - \nu_2)\sigma_2}{E_2}
\] (2.66)

From Hooke's Law for plane stress, the peak tensile stress at the same temperature will be,

\[
\sigma_1 = \frac{E_1 \alpha (T_2 - T_1)}{(1 - \nu_1)} - \frac{E_1 (1 - \nu_2)\sigma_2}{(1 - \nu_1) E_2 (1 - \nu_1)}
\] (2.67)

Equations 2.66 and 2.67 can now be used in the relations 2.63 and 2.64 to determine the initiation and propagation of cracks.
The third case is when not even the very first cycle indicates any sign of plastic deformation, $\varepsilon_{p0}=0$. The condition for this case is,

$$\frac{(1-\nu_2)}{E_2} \sigma_2 \geq \alpha(T_2 - T_1)$$

(2.68)

and the elastic strain value at the extreme points of the cycle are $\varepsilon_{e2} = \alpha(T_2 - T_1)$ and $\varepsilon_{e1} = 0$. The stress cycle in this case is a looping between the zero stress of $T_1$ and a maximum compressive stress at the temperature $T_2$. The theoretical maximum stress is expressed as,

$$\sigma_2 = \frac{E_2 \alpha(T_2 - T_1)}{(1-\nu_2)}$$

(2.69)

Since there is no tensile stresses are involved within the cycling, this case theoretically should not cause any fatigue damage.

The theoretical analysis conducted by Malm and Norstrom is very intriguing even though it requires experimental verification to validate the issues. The authors stated that the proper case should first be recognized before the use of the theoretical models since each case has a unique behavior. The paper has the material properties necessary for the calculations. For the experimental condition chosen by one of Malm's previous studies from 1977, the data estimated satisfied the low-cycle fatigue behavior. To calculate the life cycles to crack initiation, Equation 2.56 was used with a constant $c$ value of 0.1 and $n$ value of 0.5. The comparison of the experimental and the estimated data is shown in Table 2.15. There is other compatible experimental data available by other studies which were mentioned in the paper. The paper also studied Benedyk, Moracz and Wallace's experimental data [Benedyk et al, 1970]. Their paper also includes all the material property data required for quantitative calculations. Benedyk's study was focused on the high-cycle fatigue problem and can be modeled with Basquin's Law. The data from
<table>
<thead>
<tr>
<th>Steel grade</th>
<th>$T_2, , ^\circ$C</th>
<th>$N_F$ Experiments</th>
<th>$N_F$ Calculated</th>
</tr>
</thead>
<tbody>
<tr>
<td>UHB ORVAR 2 (AISI H-13)</td>
<td>650</td>
<td>~300</td>
<td>267</td>
</tr>
<tr>
<td></td>
<td>700</td>
<td>200-250</td>
<td>166</td>
</tr>
<tr>
<td></td>
<td>750</td>
<td>100-150</td>
<td>127</td>
</tr>
<tr>
<td>UHB QRO 45</td>
<td>650</td>
<td>~300</td>
<td>304</td>
</tr>
<tr>
<td></td>
<td>700</td>
<td>~200</td>
<td>182</td>
</tr>
<tr>
<td></td>
<td>750</td>
<td>~150</td>
<td>133</td>
</tr>
</tbody>
</table>

Table 2.15: Comparison of the experimental and estimated values [Malm et al, 1979]

Benedyk's paper did not agree with the high cycle fatigue conditions proposed by Malm and his colleagues. The paper stated that since it was too difficult to test crack propagation it was not possible to verify validity of the model. The paper explained the difference with the data available at that time, lacking the constant in the governing laws, not carefully obtained $da/dN$ values and the experimental results included the influence of softening and oxidation of cracks. [Malm et al, 1979].

Kim and Ruhlandt investigated the thermally induced stresses within die casting dies. The authors applied a 2-D FEM (Finite Element Analysis) to establish the model of stress fields. The main focus of the study was to understand the effect of gating design in thermally induced stresses. Two different runner system designs were investigated, straight and convergent. Figure 2.59 shows the coupled heat transfer-structural model geometry for both cases. The paper indicated that the die life can be extended by proper use of preheat, maintaining the lubricant layer and avoiding certain abrupt geometric details to prevent excessive stress concentrations. [Kim et al, 1985].

Schindler proposed that there is a significant correlation between the surface temperature of the die steel during the operation and the intensity of the heat-checking network. The time-temperature combination at die surface temperatures above $0.5 \, T_{\text{melting}}$
(550 °C for H-13) has the greatest influence on softening of the die surface where plastic strain in each cycle exceeds the elastic strain [Schindler et al, 1977].

![Diagram showing runner design](image)

Figure 2.59: Thermal models for runner design [Kim et al, 1985]

In a more recent study, a computer model was constructed by Nyamekye, Wei and Martinez to predict and improve the mold life for the permanent molding process [Nyamekye et al, 1998]. The study included a CAD/CAE computer model for heat transfer analysis and thermal stress analysis. As a final step, the thermal fatigue life prediction has been successfully established in the right order of figures for the permanent molding process. The fatigue crack initiation was realized through Morrow’s equation as shown below:

\[
2N_f = \left[ \frac{\sigma_s}{\sigma_f - \sigma_m} \right]^{1/b}
\]

(2.70)

where \(2N_f\): fatigue life in reversals
\(\sigma_f\): true fracture strength
\(b\): fatigue strength (Basquin exponent)
The following conclusions were drawn:

- A procedure, combining computer modeling and experimental approaches, was developed that permits the thermal conductivity of a permanent mold coating to be estimated under conditions for which the coating is in contact with molten metal. Using the inverse heat module of PROCAST and the measured temperatures within the insulating coating material, the thermal conductivity of the coating material can be estimated as a function of temperature providing a chance to characterize the thermal conductivity of the different coating materials for the permanent molding process.

- The thermal cycles and the cyclic thermal stresses within the mold were modeled. The maximum temperature difference and the maximum thermal stress were consistently found at the tip of the insert, suggesting crack initiation would begin at this region.

- Using the computer module developed by the authors and the thermal stress results, the number of cycles until the crack appears at the insert’s tip was predicted. The predicted location is exactly the same as the location from the test runs. Depending upon the value of the thermal expansion coefficient used, the number of cycles for the first crack appear at that region varied from zero to several thousand cycles. Thus, an accurate knowledge of the thermal and mechanical properties of the mold is needed to assure accurate prediction of the number of cycles to crack initiation.
In a very recent study by Nagasawa [Nagasawa, 1999], fatigue crack initiation of the die-casting dies was studied. The paper focused on different grades of H-13 test dies with V-shaped grooves. With the addition of constants into the fatigue life equation used, the actual and predicted die lives indicated a match. There is only limited information published regarding this study and is in Japanese.

2.3.2 Making, Heat Treating Die-Casting Dies, Microstructure Effects on Material and Fatigue Properties

There have been numerous studies regarding the manufacturing processes used for making steels and fabricating dies, and the effects of microstructure. Most of the available papers are mentioned in this chapter. Some other related topics such as die-casting practice [Young, 1979][Young, 1968, 2], coatings and damage mechanisms [Draper et al, 1972][Shivpuri et al, 1991][Chu et al, 1993] have also been investigated. The studies on the related topics are not included within this section.

2.3.2.1 Making of Steel, Fabrication of Dies and the Impact on Mechanical Properties

Hamaker and Yates investigated the maraging steels, hot work die steels and thermal-mechanical processing techniques. The special material characteristic required for die applications included resistance to thermal fatigue, thermal expansion, thermal conductivity, hardness, resistance to deformation in service, toughness to gross cracking, resistance to erosive and washing action of the die casting alloy, weldability and dimensional stability. The study discussed the requirements and the improvements for the materials [Hamaker et al, 1966].
Msndon provided information regarding the effect of lubricant layer, including the insulating role of the lubricants, to minimize thermal gradients and subsequent thermal fatigue. The resistance to softening was determined as one of the critical factors. As the die softens, tensile strength drops, however life is reduced in proportion to the 8th power of the loss of tensile strength. Thus if the tensile strength was reduced by a factor of 2, life may be reduced by a factor of 250 [Msndon, 1972].

Bertolo and Wallace determined tensile, toughness, and thermal fatigue properties of H-13 and 300 maraging steel, the two die materials used by the aluminum die casting industry. The effect of several factors on the mechanical properties was evaluated including the austenitizing temperatures and hardness levels of the H-13 material and aging temperatures of the 300-maraging alloys. The toughness values from three tests were compared: plane strain fracture toughness $K_{IC}$, conventional Charpy V-notch impact toughness and instrumental pre-cracked Charpy impact toughness [Bertolo et al, 1976].

Norstrom investigated the performance of hot work tool steels. He studied the basic properties, such as temper resistance, hot yield strength, ductility and toughness, as well as thermal fatigue and thermal shock resistance. Eight commercial martensitic hot work steels were tested. It was acknowledged that the material properties varied considerably between different steel types, depending on the differences in alloy composition. The resistance to thermal fatigue or heat checking was favored by high hot yield strength, high temper resistance and good ductility. On the other hand, the resistance to thermal shock and premature failure requires good yield toughness and ductility. A proper hot-work steel for high temperature applications should possess high hot yield strength, high temper resistance and good ductility and toughness [Norstrom, 1982].

Gaven and Norstrom investigated the newly developed tool steel, UHB QRO 80. Thermal fatigue resistance, resistance to softening and thermal shock behavior was compared to those of H-10, 11-13, 21 and Cromo-N [Gaven et al, 1983]. Johansson, Jonsson and Worbye also investigated another newly developed die steel QRO 80M in 1985. Physical and mechanical properties of the steel were discussed and related to the alloying content and tempered carbides with special attention given to the influence on
thermal fatigue resistance. Measurements of fracture toughness, creep resistance, and high temperature impact toughness were also presented and compared to those of H-13 [Johansson et al, 1985].

Schmidt investigated the effects of austenitizing temperature and quench practice on micro-structure, austenitic grain size, carbide solving behavior, as-quenched and tempered hardness capability, room temperature and 800 °F Charpy V-notch impact resistance and room temperature $K_{IC}$ fracture toughness of H-13 tool steel [Schmidt, 1987].

Cocks summarized the work performed by Case Western University. Grain boundary precipitation was recognized to reduce impact strength. Since cooling rate affects the tendency to develop these carbides, an effort was made to develop a quench calculator that could be used to predict the cooling rate of various sections of tooling. Cooling rates over 105 °F/min (41 °C/min) were thought to produce a martensitic structure with less grain-boundary carbide and impact strengths of 18 to 21 ft-lb in a Charpy V-notch test. Slow cooling rates produced a pearlitic structure with significant quantities of grain boundary carbide and associated impact strengths of 3 to 6 ft-lb [Cocks, 1988].

Nostrom, Johansson, Ohrberg examined the thermal fatigue and thermal shock behavior of martensitic hot work steels. These properties were found to be different between different steel grades, and the results were presented in terms of fundamental mechanical properties [Norstrom et al, 1981].

Kogler, Breitler and Schindler covered the properties which die casting industry requires and how they correspond with characteristics indicated by the various H-13 types. The selection of proper quality of H-13 die material was discussed for high volume applications requiring extended die lives [Kogler et al, 1989].

Schmidt also investigated the relation of domestic and import H-13 tool steel chemistry, microstructure and banding severity and mechanical properties with testing random lots of premium steels [Schmidt, 1989].
Norstrom studied the importance of ductility and toughness in hot-work die steels and proposed proper test procedures. It has been recognized that these properties are important to die-casting die life. The paper focused on the importance of ductility and toughness of die steels with respect to die failure mechanisms such as heat checking and gross cracking. Test methods for toughness properties were also discussed and a proper test procedure for hot-work die steels was proposed [Norstrom, 1989].

Becker, Fuchs, Haberling and Rasche studied the microstructure and technological properties of hot work tool steels for die-casting. To specify the shape, and to dimension of dies, the die maker should require a reliable database on properties of materials. To correctly assess such data it is necessary to know the relationship between the material properties and the composition. The paper attempted to address these correlations [Becker et al, 1989].

Wallace, Roberts and Hakulinen dealt with the influence of cooling rate on the microstructure and toughness of premium H-13 die steels. Various rates of cooling from austenitizing temperature have an impact on the toughness and on the structure of premium grade H-13 steel [Wallace et al, 1989].

Berger developed a method to measure volume percentage of primary carbides in premium H-13 tool steel without computerized equipment using standard optical microscopy techniques. The quantitative primary carbide data was plotted against Charpy-V notch impact toughness of oil-hardened specimens. A good correlation was determined [Berger, 1989].

Gehricke worked on the development of new maraging steels with higher resistance to thermal fatigue. Maraging steels were developed some 30 years ago for heavy-duty applications because of their toughness and temperature resistance. Conventional grades were limited to relative low maraging temperatures such as 930 °F. The aim was to develop a new alloy allowing higher maraging temperatures [Gehricke, 1993].
Park and Brevick analyzed the mechanical failures in H-13 shot sleeves. The study included the effects of flaws on fracture mechanisms of shot sleeves and discussed issues related with fatigue crack growth in a shot sleeve as the process continues [Park et al, 1995].

Wallace and Schwam worked on the improvement of die life. Die life is a function of material, heat treatment, surface treatment and the thermal conditions within the dies prior to the operation. Premium grade H-13 was chosen as a die material. Desired die material properties include a high fatigue resistance and a high resistance to gross cracking. A thermal fatigue test was developed to determine the susceptibility to heat checking. A Charpy V-notch testing was used to measure relative resistance to gross cracking. The heat treatment process was controlled to obtained a hardness of 46 Rockwell C. The tempering temperature used during heat treatment affects resistance to gross cracking and growth of non-metallic inclusions. Dies preheated to 300 °F were found to be less susceptible to gross cracking than those not pre-heated. Surface treatments such as nitriding and carbonitriding, peening, and lubricating were shown to increase the die life. Electro-discharge machining of the dies allows the dies to be machined after being hardened by heat treatment. According to the paper, the die must be heat-treated by austenitizing in a vacuum furnace, cooled by very high pressure nitrogen gas and tempered twice [Wallace et al, 1995]

Roche, Beaton, Klarenfjord investigated the mechanical properties such as toughness, ductility and distortion levels achieved through the vacuum heat treatment of large dies. The outcome was used providing a comparison basis for deciding the most efficient balance between die making costs and die life [Roche et al, 1997].

2.3.2.2 Making of Steel, Fabrication of Dies and the Effects on Thermal Fatigue

Fletcher and Youngkin studied the different types of vacuum degassing and vacuum remelting processes. Comparisons were made as the various metallurgical improvements that were obtained with these methods. Data showed the improvement in
elongation and reduction of area as a result of vacuum remelting. The thermal fatigue resistance of H-13 as a result of vacuum arc remelting and the influence in metal cleanliness were increased [Fletcher et al, 1968].

Young provided a processing procedure for stress relieving, heat treatment practice. One of the causes of the early failure was found to be decarburization of the surface during heat treatment. A decarburized-surface hardness of 35 Rockwell C resulted in the loss of thermal fatigue resistance. Also poor grinding practice was recognized as a source for residual stresses. His study covered the other sources of stresses and the methods avoiding the stress problem [Young, 1968,1].

Kasak and Steven investigated the microstructural considerations in heat checking of die steels. The effect of austenite grain size of H-13 was investigated. Large grain size variations were induced by special heat-treating techniques to simulate the austenite grain size as the only significant variable. The results of thermal fatigue tests showed no significant effect of grain size on the heat checking behavior. Thermal fatigue tests with different initial hardness levels (strength values) supported the idea that the initial strength level has not a negligible impact. Lower initial hardness levels for H-13 caused earlier and more accelerated deterioration of steel surface by heat checking. Also, sulfurizing test results indicated that the sulfur is not detrimental [Kasak et al, 1970].

Graham and Wallace studied the mechanisms of crack initiation for H-13 steel hardened from temperatures 1850 °F and 2200 °F and 300-maraging steel aged at temperatures from 950 °F and 1050 °F degrees. Metallographic examination showed that thermal fatigue cracks initiate in surface flaws, primarily from corrosion pits. Even though austenitizing temperatures of 1050 °F results in improved fatigue resistance, the upper practical temperature was found to be 1950 °F [Graham et al, 1974].

Schindler, Klumberg and Stuhl used SEM (Scanning Electron Microscope) for investigating the thermal fatigue characteristics. Fracture in a solid occurs along preferred planes, so it can yield information about the internal structure. The interior surface section of a thermal fatigue fracture investigated seemed like it was caused by low cycle-fatigue. Micro-segregations have a direct influence not only on a microscopic scale,
but they can also affect the macro-properties of steels. The distribution of carbon resulting from the separation of alloying elements Cr, Mo and V in H-13 steel was investigated by electron beam microanalysis. The microprobe measurements indicated that the alloying elements and the carbon concentration varied in the same direction. Heat treatment of dies and hot tensile strength were also discussed. It was concluded that all factors which result in an increased hot yield strength, micro-homogeneity, cleanliness of steel, and proper heat treatment improve fatigue strength and thermal fatigue resistance [Schindler et al, 1977].

Sullivan presented some basic considerations regarding the heat checking problem, then discussed the selection of die materials from the available ones in early 80's for die casting [Sullivan, 1981].

Danzer, Schindler and Krainer studied the creep behavior of AISI H-10 and H-13 steels with regard to die life in die casting of brass and aluminum. According to the authors creep rather than fatigue causes heat checking on tool surfaces. The die life was predicted by linear accumulation of creep damage and was plotted as a function of maximum surface temperature and preheating temperature of the die. According to the paper, it could be shown that the bainitic rather than martensitic heat-treated steels have better high temperature strength and better creep properties. Therefore, the service life can be improved using bainitic tool steels [Danzer et al, 1983,2].

Danzer, Sturm, Schindler and Zleppnig classified the thermal fatigue cracks according to their appearances. Thermal fatigue cracks are classified in three groups; heat checks, stress cracks and splinterings. The influence of the steel grade on the formation and propagation of these cracks was investigated. According to the paper, high creep resistance of die material can delay the formation of heat checks. High toughness was also found to be a delaying factor for the crack propagation [Danzer et al, 1983,1].

Breitler and Schindler studied the importance of heat treatment of H-13 for die casting dies. The paper states that knowledge of the advantages of premium quality H-13, combined with the application of all controllable factors can subsequently increase the die life and lead to a cost reduction of the castings [Breitler, 1985].
Du and Wallace's study enveloped the effect of sulfur content on the thermal fatigue behavior and dynamic fracture toughness of H-13 at five different sulfur levels. Sulfur levels of 0.0030% and 0.0028% were found to have no significance on thermal fatigue resistance, while 0.075% sulfur lowered the fatigue resistance significantly [Du et al, 1987].

A study by Schwam, Aikin, and Wallace was focused on the controlled heat treatment in vacuum furnace and distortion of die casting dies of H-13 steel. A common way for fabricating H-13 dies for aluminum die castings is to machine the die close to final dimensions in the annealed condition and then heat treat the die in a vacuum furnace. The heat treatment process consists of austenitizing in a vacuum furnace, followed by quenching with nitrogen gas and double tempering. High cooling rates are essential to obtain good mechanical properties, but also result in die distortion, residual stresses, and die fractures. The objective of the study was to determine the quenching conditions required to attain good thermal fatigue resistance and toughness at least within the first one half inch of the die face. The objective was pursued by controlling the cooling rate of that face from the austenitizing temperature to keep the minimum rate at 50 °F/min over the 1750 to 550 °F temperature range. The project examined the rates of cooling for various depths obtained within the dies. The die distortion issue was also considered, as well as the properties of residual stresses [Schwam et al, 1995].

Hochanadel, Edwards, Maguire and Baldwin investigated the effect of microstructure on thermal fatigue resistance of investment cast and wrought AISI H-13 hot work die steel. Variable thickness plate investment castings of AISI H-13 die steel were poured and characterized in the as-cast and heat-treated conditions. The study included light microscopy and mechanical testing. Wrought samples of standard and premium grade H-13 were heat treated and characterized in a similar way for comparison. Microstructure differences were observed in the as-cast samples poured to different section thickness values. Dendritic cell size and carbide morphology were the most important microstructure differences observed. After a full heat treatment, microstructural differences between the wrought and cast materials were independent of shell
thickness. The mechanical properties of the cast and the heat-treated material proved to be similar to the standard heat-treated wrought material. A thermal fatigue-testing unit was designed and built to correlate the heat-checking tendency of AISI H-13 steel to its processing and micro-structural condition. Surface hardness decreased significantly with thermal cycling, and the heat checking was noticed after 50 cycles. Thermal softening and thermal fatigue susceptibility were quantified and discussed relative to the microstructure conditions created by processing and heat treatment. It was found that the premium grade wrought H-13 steel provided the best overall resistance to heat checking [Hochanadel, 1995].

Gao, Reid, and Jahedi studied the effect of austenitizing temperature and tempering sequence on the thermal fatigue resistance of H-13 die steel. Cylindrical specimens were austenitized at four different temperature levels, followed by a triple temper to achieve a hardness value within the range of 46-48 HRc. Single, double, triple and quadruple temper and cryogenic treatment with single temper to 46-48 HRc, were also used to treat specimens, austenitized at 1030 °C. Thermal fatigue tests were carried out on a new thermal fatigue test rig utilizing induction heating and water spray cooling. Increased austenitizing temperature results in coarsening of grain structure and increased as-quenched hardness. Cryogenic treatment after quenching to room temperature causes a complete transformation of austenite to martensite. The fatigue test results showed that the specimens austenitized at 1030 °C have a better thermal fatigue resistance than those treated at 980 °C, 1080 °C and 1130 °C. For specimens austenitized at 1030 °C, the results indicate that the triple temper, quadruple temper and cryogenic treatment improve the thermal fatigue resistance as compared to single and double temper [Gao et al, 1997].

2.3.2.3 Die Casting and Thermal Fatigue Testing

Coffin, Wesley and Schenectady designed an apparatus for studying the effects of cyclic thermal stresses and ductile materials. In cases where high rates of heat transfer exist, severe thermal stresses can be developed in structures. These stresses can be
relieved by plastic flow and they are not regarded as serious when steady state conditions reached. Under the impact of thermal cyclic behavior, cyclic thermal stresses are developed. Depending on the frequency of the thermal cycles and the severity of the thermal stress, fatigue failures can occur. Because of low thermal conductivity and high thermal expansion 18% Chromium-8% Nickel stainless steels are particularly prone to this damage mode. The paper described an apparatus built to obtain information about this particular material’s resistance to thermal fatigue under certain conditions and to investigate the issues in thermal fatigue problem [Coffin et al, 1954,1]. Another study by Coffin and Schenectady reported the results of the previous study of cyclic strain fatigue failure caused by thermally induced stresses. A cyclic temperature history was imposed on a thin tubular test specimen subjected to complete longitudinal constraint. The effect of thermal stress cycling on strain hardening and life cycles to failure for a fixed mean temperature, the effect of degree and kind of previous cold work on strain hardening and life cycles to failure, the effect of mean temperature on thermal stress cycling, the effect of time period of the cycle on life cycles to-failure and the effect of prior strain cycling on stress-strain characteristics were discussed [Coffin et al, 1954,2].

Dunn conducted Wallace’s dunk tests. Vacuum-remelted H-13 was shown far better performance than air-melted H-13. Also the study included the effect of hardness on fatigue behavior of dies. H-13 steel with a hardness level of 48 Rockwell C scale exhibited the best fatigue resistance [Dunn, 1969].

Benedyk, Moracz and Wallace conducted an investigation to evaluate the thermal fatigue behavior of die materials used in dies and cores in aluminum die-casting. A small-scale thermal fatigue test, which simulated the thermal cycle of the die-casting process in the most severe conditions during aluminum die-casting, was utilized. The test used internally water-cooled long rectangular specimens that were cycled between air and a molten aluminum alloy bath. The influences of processing conditions, geometry and material properties, including austenizing temperature, hardness, surface carburization, fillet radius of the billet, location in billet, cast structure, vacuum re-melting and resulfurization, on the thermal fatigue resistance of H-13 steel were determined.
Specimens also included a variety of other alloys, such as Waspaloy, TZM and Cu-Be alloy. Data obtained covered both the initiation and propagation of cracks. The cracks in H-13 steel were found to be both intra- and intergranular and filled with an iron, chromium oxide layer. Extensive crack patterns were developed on selected steels to demonstrate that a linear relation exists between the crack growth and number of cycles, permitting the evaluation of the crack resistance based on the short crack length. Thermal fatigue resistance increased as the austenizing temperature was raised from 1750 °F to 2050 °F. Maximum thermal fatigue resistance obtained at a hardness of about 48 Rockwell C in the 45-52 HRc range. Steel close to the center of the forged billet behaved poorer in thermal fatigue than steel close to the surface. Thermal fatigue cracks of resulfurized H-13 occur with fewer cycles and propagate faster when oriented in a transverse compared to a longitudinal direction. A bar of vacuum-remelted H-13 steel subjected to small forging reduction was inferior to standard H-13, but when comparable forging reduction employed on another bar, the vacuum remelted steel exhibited superior thermal fatigue resistance. Residual stresses, decarburization and carburization were detrimental to the thermal fatigue performance. Also, cast and resulfurized steels did not perform as well as the standard H-13 steel [Benedyk et al, 1970].

Sabharwal and Wolf developed an experimental set-up to study the thermal fatigue phenomenon. The method was established to create the thermal conditions experienced by die steel in aluminum die-casting by intermittent heating of a small portion of the face of a cylindrical test piece while the main body is kept at a lower temperature by means of a copper jig. Pressurized air was used as a cooling medium during the cooling part of the cycle. The thermal fatigue resistance was determined on the basis of number of cycles before crack initiation, on propagation frequency and severity of cracking. Photographic investigation was conducted to understand the material behavior during testing [Sabharwal et al, 1970].

Malm and Tidlund presented a series of experiments regarding heat checking and erosion. The tests included the effects of die casting process parameters, such as melt temperature, composition, die-melt contact temperature and cooling rate, as well as
material properties like steel grade, heat treatment and surface treatment [Malm et al, 1979].

Engberg and Larsson studied the elevated temperature low cycle and thermo-mechanical fatigue properties of AISI H-13 hot work tool steel. The thermal fatigue of the martensitic hot work tool steel, AISI H-13 is complicated by structural degradation, which in turn is influenced by plastic deformation. The material properties are dependent upon plastic deformation, temperature, and time. Isothermal low cycle fatigue testing was performed at 500 and 600 °C and also to a limited amount at 80, 300, and 750° C. Most of the tests were performed on the specimens with 47 HRc value. At 600 °C, no difference in life was observed between the different hardness values. The cyclic stress-strain curves evaluated at half-lives were almost identical. Softening is believed to cause similarity, while the initial differences will only influence the behavior during a small fraction of the life after which all material differences will have vanished. Softening is due to particle coarsening and decrease of initially very high dislocation density. The temperature dependence of the solubility limit for the carbides and the activation of self diffusion were reflected by the data. The model predicts that a low solid solubility and a high volume fraction of carbides will give rise to a low rate softening as will a high strain rate and a low temperature. Plastic deformation was found to increase particle coarsening significantly, but the extent was independent of the magnitude of the plastic strain within the limits investigated. Thermo-mechanical fatigue testing was performed with a maximum temperature of 600 °C in the cycle. The minimum temperature was varied within the range of 80 to 400 °C. In comparison to the isothermal data, life was reduced when Tmin was less than or equal to 200 °C, but remained constant when a Tmin of 400° C was used. Softening increased with increasing minimum temperature. The same hardness variants as described above were also tested within the temperature range of 200-600 °C. No significant differences in life were observed, although the cyclic stress-strain curves (maximum tensile stress at half life) differed. The Ostergen method for life prediction fit the data reasonably if the plastic work is normalized with the cube of the shear modulus [Engberg et al, 1988].
John, Hartman and Gallagher studied the thermal-mechanical loading induced crack growth. The paper described the development of an experimental technique to verify the thermal-stress-intensity factor generated by the temperature field around the cracks. Thin plate specimens of AISI-SAE 1095 steel were used for heat transfer and thermal-mechanical fracture tests. Rapid thermal loading was achieved using high intensity focused infrared spot heaters. These heaters were also used to generate controlled temperature rates for heat transfer verification rates. The experimental results indicated that thermal loads could generate stress-intensity factors large enough to induce crack growth. The proposed thermal-stress-intensity factors appear to have the same effect as conventional mechanical-stress-intensity factors with respect to fracture [John et al, 1992].
CHAPTER 3

RESEARCH METHOD AND RESULTS

3.1 Experimental Determination of Spray Heat Transfer Coefficients

This study was originated by the idea of evaluating the cooling performance of die-casting lubricants used in spray cooling of die cavity surfaces. For the experiments, a testing apparatus built by a major US automotive manufacturer was used. The apparatus called LTA (Lubricant Testing Apparatus) included a 1-inch round test plate with four thermocouples embedded within. All four type-K thermocouples were 0.010 inch below the surface. The temperature readings from the thermocouples were assumed to be the surface temperature while zero heat flux assumption was made at the back of the test plate. Finite difference method was used to solve the 1-D thermal field in the plate. The surface thermal gradients and the heat flux values were estimated using the thermal fields. Once the surface heat fluxes were determined, they were used to estimate the heat transfer coefficient through Newton’s Cooling Law:

\[
q = h (T_s - T_a) \tag{3.1}
\]

where \(q\) is the heat flux (KW/m\(^2\))

\(T_s\) is the plate surface temperature (°C)

\(T_a\) is the spray fluid temperature (°C)

\(h\) is the spray heat transfer coefficient (W/m\(^2\) K)
The tests included various lubricants with various spray conditions and the lubricant dilution ratios reported by the collaborators. Some of the results are given in the following table. The Table 3.1 indicates cooling performance of commercial lubricant and double distilled water. Double distilled water was used in the tests as a reference spray fluid. Using double distilled water also prevented any undesired solid residue on the test plate surface. The heat transfer coefficient obtained fell within the spray heat transfer coefficients reported by the other studies such as the one presented in Figure 2.7 by Lee [Lee et al, 1991 ] and in other fields mentioned in the literature survey of this dissertation.

<table>
<thead>
<tr>
<th>Water tests 14.5 inches, 60-45 psig</th>
<th>Initial T (°C)</th>
<th>h (kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>205</td>
<td>5.2</td>
<td></td>
</tr>
<tr>
<td>261</td>
<td>5.4</td>
<td></td>
</tr>
<tr>
<td>317</td>
<td>3.6</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Lubricant, 60:1 14.5 inches, 60-45 psig</th>
<th>Initial T (°C)</th>
<th>h (kW/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>205</td>
<td>3.3</td>
<td></td>
</tr>
<tr>
<td>262</td>
<td>3.4</td>
<td></td>
</tr>
<tr>
<td>317</td>
<td>2.6</td>
<td></td>
</tr>
</tbody>
</table>

Table 3.1: Water and lubricant cooling performance tests (Spray distance is 14.5 inches and the air and lubricant pressures 60 and 45 psig respectively, flow rate 0.275 lpm (liters per minute as measured by the flow meter))

3.2 1-Dimensional Thermal Analysis

The main objective of this study is to identify the effect of factors such as thermal process parameters, physical properties of die materials and simple geometric details in determining thermal fields within dies. This section of the research also served as sensitivity analysis where the variation in data from literature and spray tests was utilized in analyzing temperature and thermal gradient histories throughout the process. Further
analysis of associating the previously mentioned factors and the structural state within tooling was conducted employing the temperature profiles obtained through 1-D thermal analysis and imposing them along the die cavity/casting interface in 2-D ABAQUS models. Further analysis including structural analysis is covered in the third section of Chapter 3. This study was inspired by a previous BINORM study. Venakatasamy conducted a sensitivity analysis using BINORM. However, the focus of his previous study was different. The study included the effect of various input parameters on the initial die surface temperature and the casting freezing time [Venkatasamy, 1996].

BINORM was used in this study for 1-D thermal analysis. It is a code written in FORTRAN for one-dimensional thermal analysis. It was developed by Jonathan Papai at the Ohio State University under the supervision of Dr. Carroll Mobley. The code describes the binormal solidification of a casting under one-dimensional heat flow. It estimates the thermal field history within the casting and the dies. Figure 3.1 shows the BINORM model for the thermal analysis.

The BINORM input set requires parameters such as casting and die material properties, die and casting wall thickness, initial die and melt temperature (liquid superheat), ambient and coolant temperature, heat transfer coefficients at the interfaces of the die/casting system (such as die/casting, die/coolant (waterline), die/air), die open and close times, spray delay time and its duration. The code can address many major thermal issues associated with the die/casting system and the casting cycle. Some of the more specific data include initial, minimum and maximum die surface temperatures, casting ejection temperature, heat flux to the dies during dwell stage, thermal gradients at the initial, minimum and maximum surface temperatures and solidification time of casting.
The casting cycle is simplified and converted to the following four steps as shown in Figure 3.1:

- Metal contact with the dies for the dwell period,
- Die exposure to the ambient after the casting is ejected,
- Spray cooling of die surfaces,
- Die exposure to the ambient after spray, before the very next cycle.
Based on the input data, BINORM assigns an initial temperature heat content and thermal conductivity to each element of the die and casting system, the thermal resistance at the die/casting, die/coolant and die/air interfaces. A temperature difference and a thermal resistance between each adjacent element are then calculated. Through thermal resistance and temperature difference, heat flux values into and out of each element are determined. The heat fluxes into and out of each element are summed for a net heat flux of an element. The heat flux is then multiplied by the time step to calculate the heat flow into each element after each time step. This heat flow is added to the heat content of the element before the next time step for obtaining the new heat content. The temperature and thermal conductivity of each element are updated based on the new heat content. This procedure is repeated over the entire cycle, with the output of each cycle serving as the input for the next cycle, for the specified number of casting cycles [Venkatasamy, 1996].

The die material used in the thermal analysis was H-13 tool steel and the casting material was A380 aluminum alloy. The compositions of these two materials can be found in Appendix F. The factors considered in this preliminary research activity can be identified in three groups.

- Physical properties of the die material
- Simple casting and die geometry
- Die casting thermal process parameters and consequent thermal conditions

The thermo-physical properties of the die material which were considered in this study are shown in the following table. Detailed data on these properties can be found in the coming pages.
Thermo-physical properties

<table>
<thead>
<tr>
<th>Property</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal conductivity – ( k )</td>
</tr>
<tr>
<td>Density - ( \rho )</td>
</tr>
<tr>
<td>Specific heat capacity - ( c_p )</td>
</tr>
</tbody>
</table>

Table 3.2: Thermo-Physical properties

Geometry effects were studied by using various casting and die thickness values. The results of this study will be presented in the second portion of the 1-D thermal analysis. The die thickness values actually correspond to the cooling line locations in BINORM. Default die thickness was 0.05 meter and the casting wall thickness was assumed to be 0.004 meter for all the simulations in the first section of the 1-D thermal analysis. A reference set of BINORM input parameters are given in the appendices. A second reference set of input parameters for thick wall castings are also presented in Appendix G.

The process parameters covered in the thermal analysis are shown in Table 3.3. Die cavity surface temperature and other thermal field history were obtained from the BINORM simulations. Preliminary analyses with 30, 50 and 100 casting cycles showed that the dies reach quasi-steady state at around 30 cycles. Using 30 cycles also enabled production of more data points dividing each step into more time steps. The results are presented in the following section.

3.2.1 Effects of Die Thermo-Physical Properties

The effects of thermal conductivity, density and specific heat capacity on the die thermal fields are discussed in this chapter. The effects of variation in thermal conductivity values were investigated using the figures obtained from the literature review. These thermal conductivity values are shown in Table 3.3. During the literature survey, the thermal conductivity data from the material property databases of the PROCAST and MAGMASOFT software indicated a more complicated relation with temperature rather than linearity. Since BINORM allows only a linear correlation of
Thermal process parameters

<table>
<thead>
<tr>
<th>thermal process parameters</th>
<th>value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial superheat of casting (^{a})</td>
<td>24.56 + 0.0036 T</td>
</tr>
<tr>
<td>Initial die temperature</td>
<td>24.56</td>
</tr>
<tr>
<td>Heat transfer coefficients at the die/casting interface throughout the stages of the process</td>
<td>28.45</td>
</tr>
<tr>
<td>• Open die stage</td>
<td>32.25</td>
</tr>
<tr>
<td>• Casting presence within the cavity</td>
<td></td>
</tr>
<tr>
<td>• Spray cooling of die surfaces</td>
<td></td>
</tr>
<tr>
<td>Heat transfer coefficients at the cooling line</td>
<td></td>
</tr>
<tr>
<td>Air gap formation parameters</td>
<td></td>
</tr>
<tr>
<td>• Casting surface temperature at which air gap forms</td>
<td></td>
</tr>
<tr>
<td>• Air gap heat transfer coefficients</td>
<td></td>
</tr>
<tr>
<td>Spray process conditions</td>
<td></td>
</tr>
<tr>
<td>• Die surface temperature at the initiation of the spray</td>
<td></td>
</tr>
<tr>
<td>• Spray duration</td>
<td></td>
</tr>
<tr>
<td>• Spray fluid temperature</td>
<td></td>
</tr>
</tbody>
</table>

Table 3.3: Thermal process parameters (a: different values used for different wall thickness values)

<table>
<thead>
<tr>
<th>k (W/m K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>24.56 + 0.0036 T</td>
</tr>
<tr>
<td>24.56</td>
</tr>
<tr>
<td>28.45</td>
</tr>
<tr>
<td>32.25</td>
</tr>
</tbody>
</table>

Table 3.4: Thermal conductivity values of H-13 (T: Temperature in °C)

thermal conductivity and temperature, only the values shown in Table 3.4 were used. The values were chosen from the extremes reported in the literature to emphasize the impact of thermal conductivity on die thermal field.
Results are shown in Figure 3.2. The linear function and the constant values, which carry values close to the data produced by the function, produced almost identical temperature profiles. The more pronounced differences in thermal conductivity values were reflected in the temperature profiles. Higher thermal conductivity values produced lower temperature values and temperature ranges for the thermal cycle. The initial and the maximum temperature of the cycle, the dwell curve and the open die stages were somewhat affected by the changes in thermal conductivity values. While the initial point experienced the most impact, the minimum temperature of the cycle was not affected by the variation in the data, since the spray conditions did minimize the effect of the variation.
The density figures for H-13 tool steel reported in the literature do not show much variation. The values used for this section are shown in Table 3.4.

<table>
<thead>
<tr>
<th>ρ (kg/m³)</th>
</tr>
</thead>
<tbody>
<tr>
<td>7700</td>
</tr>
<tr>
<td>7600</td>
</tr>
<tr>
<td>7800</td>
</tr>
</tbody>
</table>

Table 3.5: Density values of H-13

The effect of density found to be negligible as expected. Even though the effect was small, higher density values increased the die surface temperatures since they increased the heat storage capability (heat capacity) of the die material. The effect is clear especially during dwell and open die stages. Greater density values yielded lower peak points due to the decrease in the material's thermal diffusivity.

![Figure 3.3: Influence of density (ρ): kg/m³ on surface temperatures](image)

Figure 3.3: Influence of density (ρ): kg/m³ on surface temperatures
The specific heat capacity values obtained from the literature survey are within the range of 460.2 - 603.2 J/kg K. The values from that range were used to understand the effect of specific heat capacity of H-13 on the die thermal field.

<table>
<thead>
<tr>
<th>$c_p$ (J/kg K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>460.2</td>
</tr>
<tr>
<td>543.9</td>
</tr>
<tr>
<td>603.2</td>
</tr>
</tbody>
</table>

Table 3.6: Specific heat capacity values (T: Temperature in °C)

Figure 3.4: Influence of specific heat capacity ($c_p$: J/kg K) on surface temperatures
As anticipated, the higher specific heat capacity values for the die material, the higher the die temperatures are since the heat storage capability of the die material increases with the increasing specific heat capacity values. The effect is evident during dwell and open die stages and is similar to the effect of density. The difference between a linear function and a close constant value did not affect the temperature profile. Greater specific heat capacity values also yielded lower peak points. This was also caused by the reduction in thermal diffusivity of the die material.

3.2.2 Effect of Die Geometry

This section of the study was limited by BINORM's 1-D modeling ability. The only factors studied within this section are the die and the casting thickness. The die thickness in BINORM is actually the identifier of the cooling line location. Thus, the die thickness was used to simulate the effect of the cooling line location on die thermal fields and especially on surface temperature history.

Various die thickness values were used to understand the effect of the die thickness. The values used in this study are shown in Table 3.7. Along with the die thickness, the effect of the 1-D finite element size was also investigated.

<table>
<thead>
<tr>
<th>Die Thickness (m)</th>
<th>Number of Elements</th>
<th>Element Size (m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0263</td>
<td>10</td>
<td>0.00263</td>
</tr>
<tr>
<td>0.0394</td>
<td>15</td>
<td>0.00263</td>
</tr>
<tr>
<td>0.0500</td>
<td>19</td>
<td>0.00263</td>
</tr>
<tr>
<td>0.1000</td>
<td>19</td>
<td>0.00526</td>
</tr>
</tbody>
</table>

Table 3.7: Die thickness values
Results indicated that the closer the cooling line to the cavity surface (thinner dies), the smaller the die surface temperature. The initial point of the thermal cycle showed the most reaction to the variation in die thickness values. The minimum temperature of the thermal cycle was also influenced by the geometry. The maximum point of the cycle reacted as well and it was a lower level response compared with those of the other two points. The effect of the die thickness was more than any other factor investigated. While the element size of 0.00263 meter was found to create accurate results, 0.00526 meter thick elements yielded inaccurate results, especially during the spray cooling stage. This was thought to be caused by the stability issue due to high finite element size – time step ratio. However, the oscillations were not seen in the thermal shock. Elements smaller than 0.00263 meter could not be employed due to the limitation on the data size in BINORM without any modifications of the code. BINORM was not

![Graph showing the influence of die thickness on surface temperatures](image)

Figure 3.5: Influence of die thickness (dt in meter) on surface temperatures
modified since 0.00263 meter thick elements are believed to produce accurate results. Maximum number of elements used was 19.

3.2.3 Effect of Die Casting Process and Thermal Conditions

Various die casting process parameters and subsequent thermal conditions are the focus of this study.

The effect of initial die temperature was studied with a uniform temperature distribution assumption at the initiation of the casting process. The temperatures used are listed in Table 3.8.

<table>
<thead>
<tr>
<th>Die Initial Temperatures (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>100</td>
</tr>
<tr>
<td>125</td>
</tr>
<tr>
<td>150</td>
</tr>
<tr>
<td>200</td>
</tr>
</tbody>
</table>

Table 3.8: Initial Die Temperature

Within 100-200 °C range, the effect of initial die temperatures on the temperature history of the die cavity was found to be negligible. Different initial temperature values with the same casting-die geometry should only affect the number of cycles to quasi-steady state.
Figure 3.6: Influence of initial die temperature (T: °C) on surface temperatures

The effect of cooling line heat transfer coefficients was studied using the values from Table 3.9. These values were also obtained from different sources.

<table>
<thead>
<tr>
<th>Cooling Line Heat Transfer Coefficient (W/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.256</td>
</tr>
<tr>
<td>3.349</td>
</tr>
<tr>
<td>4.802</td>
</tr>
<tr>
<td>6.006</td>
</tr>
</tbody>
</table>

Table 3.9: Cooling line heat transfer coefficient
Results indicated that with a very low cooling line heat transfer coefficient such as 1,256 W/m² K, dies run at considerably higher temperatures as opposed to runs with higher cooling line heat transfer coefficients. The impact of cooling line heat transfer coefficient is more significant at the initial and minimum temperatures of the thermal cycle and during the dwell and open die periods. When the heat transfer coefficient is increased from 1,256 to 3,349 W/m² K, a drastic change in the die surface temperature profile was observed. The heat transfer coefficient values above 3,349 W/m² K did not make any major contribution to the cooling performance of the cooling line. The maximum temperature of the thermal cycle was not affected by the cooling line heat transfer coefficients since the filling of the cavity happens momentarily and the cooling line cannot respond to remove heat too fast to have an impact on the spike during this very rapid heat input.

Figure 3.7: Influence of cooling line heat transfer coefficient (h: W/m² K) on surface temperatures
Effect of the air (die/air) heat transfer coefficients for the open die period was obtained using the values from Table 3.10.

<table>
<thead>
<tr>
<th>Die/Air Interface Heat Transfer Coefficient (W/m² K)</th>
<th>42</th>
<th>126</th>
<th>335</th>
<th>837</th>
</tr>
</thead>
</table>

Table 3.10: Die/air interface heat transfer coefficient

The values in Table 3.10 were obtained from the literature. There is a contradiction between the air heat transfer coefficient values reported by Herman [Herman, 1996] and Mobley during a personal communication. Higher values reported by Herman may have been explained by forced convection phenomenon if there were fans used while obtaining these figures. However, Herman does not report any means of forced heat convection. Very low heat transfer coefficients were successful in preventing surface cooling during the second open die stage after spray cooling. Increasing heat transfer coefficients reduced the surface temperature profile during the post spray open die stage. Within the range of 42 to 837 W/m² K, temperature changes of over 100 °C were observed from the temperature profiles. The curves were not parallel to each other during the dwell and consequent open die stages. At very low spray initiation temperature caused by high air-cooling, the spray's effect was minimized. The initial point of the cycle was the most affected critical point of all. The minimum temperature of the cycle indicated more impact compared to the peak point of the thermal cycle since it comes after the most two influenced stages, the dwell and consequent open die stages.
Figure 3.8: Influence of air heat transfer coefficient (h: W/m² K) on surface temperatures

Effect of the die/casting interface heat transfer coefficients was studied using the values from Table 3.11.

<table>
<thead>
<tr>
<th>Die/Casting Interface Heat Transfer Coefficient (W/m² K)</th>
<th>41,868</th>
<th>10,010</th>
<th>20,021</th>
<th>50,053</th>
<th>80,311</th>
</tr>
</thead>
</table>

Table 3.11: Die/casting interface heat transfer coefficient
Results indicated that increasing die/casting heat transfer coefficients mainly affected the peak, more explicitly the maximum temperature, and the shape of the spike during filling. Higher heat transfer coefficients were translated to higher peak temperatures. The die/casting interface heat transfer coefficient values were varied over a large range, from 10,010 to 80,311 W/m² K. The impact of the variation was minimal in the open die stage of the process. The effect of the heat transfer coefficients, which are greater than 50,053 W/m² K, on the peak was not significant due to limited heat content within casting. The temperature values after the open die stage were almost identical no matter what the heat transfer coefficient is, yielding identical minimum temperatures of the thermal cycle.

Figure 3.9: Influence of die/casting heat transfer coefficient (h: W/m² K) on surface temperatures
Three different simulations were conducted with different air gap formation temperatures (casting surface temperature at which air gap forms) and air gap heat transfer coefficients. The values can be seen in the following table.

<table>
<thead>
<tr>
<th>Air Gap Formation (@ Casting Surface Temperature °C)</th>
<th>Air Gap Heat Transfer Coefficient (W/m²K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>10</td>
<td>4,187</td>
</tr>
<tr>
<td>400</td>
<td>4,187</td>
</tr>
<tr>
<td>400</td>
<td>12,502</td>
</tr>
</tbody>
</table>

Table 3.12: Air gap interface conditions and heat transfer coefficient

10 °C casting surface temperature exhibited no air gap formation since the casting was ejected well over 10 °C. When the 1st and 2nd cases from Table 3.12 are compared, it can be concluded that air gap formation reduces the die surface temperature during dwell since it blocks the heat flow from casting to the die surface. The change in coefficient of air-gap heat transfer offered little difference between the two runs with air-gap formation starting at 400 °C. A slight difference was observed during dwell stage between 2nd and 3rd cases shown in Table 3.12. The minimum, maximum and initial points of the thermal cycle were not affected.
Spray fluid temperature was varied within the range of 10-35 °C as shown in Table 3.13. The impact of spray fluid temperature was found to be negligible. The minimum and initial points indicated very little reaction to the changes in spray fluid temperatures.

<table>
<thead>
<tr>
<th>Spray Fluid Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
</tr>
<tr>
<td>15</td>
</tr>
<tr>
<td>35</td>
</tr>
<tr>
<td>10</td>
</tr>
</tbody>
</table>

Table 3.13: Spray fluid temperature
The effect of spray heat transfer coefficient is important in terms of removing bulk amount of heat rapidly from the die surfaces. The effect of heat transfer coefficient was investigated using the values from the literature survey and the tests conducted for a joint General Motors - Ohio State University project. The spray heat transfer coefficients utilized in this study are given in Table 3.14.
Figure 3.12: Influence of spray fluid temperature (T: °C) on surface temperatures

Table 3.14: Spray heat transfer coefficient (T: Temperature in °C)

| Spray Heat Transfer Coefficient (W/m² K) |  
|-----------------------------------------|---|
| 1000.6 + 0.0419 T                       |
| 2001.3 + 0.0419 T                       |
| 3001.9 + 0.0419 T                       |
| 4002.5 + 0.0419 T                       |
| 5024.2 + 0.0419 T                       |
| 12560.4 + 0.0419 T                      |

Analysis showed that the greater the spray heat transfer coefficient, the steeper the temperature fall was during the application of the spray. There was a significant difference in the initial die temperatures of the thermal cycle, even though peak temperatures were similar and did not show as much difference as that in the minimum.
Spray heat transfer coefficient also affected the thermal response during the dwell and open die period of the process.

Table 3.15: Cooling line fluid temperature

<table>
<thead>
<tr>
<th>Cooling Line Fluid Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
</tr>
<tr>
<td>10</td>
</tr>
<tr>
<td>15</td>
</tr>
<tr>
<td>35</td>
</tr>
</tbody>
</table>
Values from Table 3.15 were used to study the impact of cooling line on the die thermal fields. Higher cooling line temperatures were converted to higher die surface temperatures. Although most points of the thermal cycle were affected besides the peak and temperature profiles were translated downward in a parallel way, major changes were not encountered.

![Figure 3.14](image)

Figure 3.14: Influence of cooling line fluid temperature ($T$: °C) on surface temperatures

The impact of spray initiation point was also included in this preliminary section of the research. This study was conducted using the values from Table 3.16.
Table 3.16: Spray initiation and end time

<table>
<thead>
<tr>
<th>Spray initiation (sec) After die opening</th>
<th>Spray end time (sec) After die opening</th>
<th>Spray duration (sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>6.0</td>
<td>7.5</td>
<td>1.5</td>
</tr>
<tr>
<td>5.0</td>
<td>5.5</td>
<td>1.5</td>
</tr>
<tr>
<td>4.0</td>
<td>5.5</td>
<td>1.5</td>
</tr>
<tr>
<td>7.0</td>
<td>8.5</td>
<td>1.5</td>
</tr>
</tbody>
</table>

Figure 3.15: Influence of spray times on surface temperatures

Since the die surface temperature at the initiation of spray did not vary much, no major impact on the minimum point can be seen. The minimum point of the thermal cycle was only shifted rightward in the thermal profile.
3.2.4 Sensitivity Analysis

Upon the completion of 1-D thermal parametric analysis, a simple sensitivity analysis was conducted to quantify the impact of variation in data for thermal system parameters on the system's critical factors. These factors were the initial, the minimum and the maximum temperatures of the thermal cycle and the thermal gradients at the die surface for these three points. A reference set of parameters was used for each die/casting configuration. Parameters were modified one by one for the parametric study and the sensitivity analysis. The reference input parameter sets for 0.004 and 0.010 m castings can be found in Appendix G.

Sensitivity analysis was carried out using two groups of castings; thin and thick walls. Each group included 3 different wall thickness values. While 0.002, 0.004 and 0.006 m thick castings made up the thin wall group with the relatively thin walls, thick wall castings included 0.01, 0.012 and 0.014 m thick castings. The outcome of the initial section indicated very little impact of air gap formation on the 3 critical points. Thus, air gap heat transfer coefficient and air gap temperatures were not included in this portion of the study.

3.2.4.1 Physical Properties of the Die Material

Thermal conductivity varied within the range of 24.56 - 32.25 W/m K. This corresponds to a range of 31% difference.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>11</td>
<td>9</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>159</td>
<td>52</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>27</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>16,510</td>
<td>11</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>4</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>3,871</td>
<td>41</td>
</tr>
<tr>
<td>0.002</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>17</td>
<td>8</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>269</td>
<td>50</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>22</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>7,870</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>5</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>6,510</td>
<td>39</td>
</tr>
<tr>
<td>0.004</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>19</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>343</td>
<td>47</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>18</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>6,370</td>
<td>13</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
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<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>8,310</td>
<td>37</td>
</tr>
</tbody>
</table>

Table 3.17: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in thermal conductivity
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>17</td>
<td>9</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>263</td>
<td>51</td>
</tr>
<tr>
<td>0.010</td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>19</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>1,561</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>4,715</td>
<td>38</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>18</td>
<td>9</td>
</tr>
<tr>
<td>0.012</td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>297</td>
<td>51</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>18</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>8,320</td>
<td>21</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>5,320</td>
<td>37</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>19</td>
<td>8</td>
</tr>
<tr>
<td>0.014</td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>325</td>
<td>49</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>18</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>7,720</td>
<td>23</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>5,840</td>
<td>36</td>
</tr>
</tbody>
</table>

Table 3.18: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in thermal conductivity.
The thin wall castings group had little variation due to increasing wall thickness values. Most influence was observed on the thermal gradient values since thermal gradient or the heat flow is controlled by thermal conductivity. The initial temperature had the most influence among the temperature points. This can be explained by the heat recovery stage. Heat flow during this stage is determined again by thermal conductivity. The minimum point of the cycle is not affected. The maximum point experienced very little impact since it is determined by the thermal shock. The thick wall castings group had a different behavior when compared with the thin castings. While initial point was as sensitive as that of thin wall castings, the maximum and minimum points became less sensitive to thermal conductivity.

Density varied within the range of 7,600 - 7,800 kg/cm³. This corresponds to a difference of 3%. No major variation was observed as expected since density data from literature did not vary. Further data such as density-temperature relation on density can be found in the appendices of this dissertation within the material database.

Specific heat varied within the range of 460.2 - 603.2 J/kg K. This corresponds to 31% difference. The changes in the specific heat values did not reflect any major changes in temperature values. More impact was determined in thermal gradients. No significant variation within the thick and thin castings group was observed. Both the initial point and the minimum point are affected at the same level by the changes. The maximum point did not show any major impact for both thin and thick wall castings. The thick wall castings had shown slightly more sensitivity to specific heat capacity compared with the thin walls.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.002</td>
<td>$T_{ini}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>4</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>970</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>157</td>
<td>2</td>
</tr>
<tr>
<td>0.004</td>
<td>$T_{ini}$</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>6.</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>789</td>
<td>1</td>
</tr>
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<td></td>
<td>$T_{min}$</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>271</td>
<td>2</td>
</tr>
<tr>
<td>0.006</td>
<td>$T_{ini}$</td>
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<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>9</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
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<td>$dT/dx_{max}$</td>
<td>500</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>358</td>
<td>2</td>
</tr>
</tbody>
</table>

Table 3.19: Ranges for surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in density.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>6</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>683</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>158</td>
<td>1</td>
</tr>
<tr>
<td>0.010</td>
<td>$T_{ini}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
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<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
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<td>0</td>
</tr>
<tr>
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<td>$dT/dx_{max}$</td>
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</tr>
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<td>$T_{min}$</td>
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<td>1</td>
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<tr>
<td></td>
<td>$dT/dx_{min}$</td>
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<td>1</td>
</tr>
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<td>$T_{ini}$</td>
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<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>7</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
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<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>381</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>201</td>
<td>1</td>
</tr>
<tr>
<td>0.014</td>
<td>$T_{ini}$</td>
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<td>1</td>
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<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
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<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>381</td>
<td>1</td>
</tr>
<tr>
<td></td>
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<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>201</td>
<td>1</td>
</tr>
</tbody>
</table>

Table 3.20: Ranges for surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in density
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>0.002</td>
<td>$T_{ini}$</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>49</td>
<td>19</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>17</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>12,020</td>
<td>9</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
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<td>4</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
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<td>18</td>
</tr>
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<td>0.004</td>
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<td>5</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
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<td>18</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
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<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>10,380</td>
<td>14</td>
</tr>
<tr>
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<td>$T_{min}$</td>
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<td>4</td>
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<tr>
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<td>$dT/dx_{min}$</td>
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<td>0.006</td>
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<td></td>
<td>$dT/dx_{ini}$</td>
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<td>18</td>
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<td>$T_{max}$</td>
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<tr>
<td></td>
<td>$dT/dx_{max}$</td>
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<td>14</td>
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<tr>
<td></td>
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<td>5</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>3,425</td>
<td>17</td>
</tr>
</tbody>
</table>

Table 3.21: Ranges for surface temperature and thermal gradients (Units: °C for temperatures, and °C/cm for temperature gradients) for thin wall castings due to changes in specific heat capacity
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
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<td>6.</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
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<td>21</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>10</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>7,540</td>
<td>16</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>8</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>2,179</td>
<td>21</td>
</tr>
<tr>
<td>0.010</td>
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<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>12</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>104</td>
<td>21</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>9</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>5,780</td>
<td>16</td>
</tr>
<tr>
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<td>$T_{min}$</td>
<td>9</td>
<td>7</td>
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<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>2,507</td>
<td>20</td>
</tr>
<tr>
<td>0.012</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>13</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>115</td>
<td>21</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>7</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>4,273</td>
<td>15</td>
</tr>
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<td></td>
<td>$T_{min}$</td>
<td>9</td>
<td>7</td>
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<tr>
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<td>$dT/dx_{min}$</td>
<td>2,805</td>
<td>20</td>
</tr>
<tr>
<td>0.014</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 3.22: Ranges for surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in specific heat capacity
3.2.4.2 Die Geometry (Thickness)

Die thickness varied within the range of 0.02632 – 0.10 m. This corresponded 279 \% difference. Two element sizes were also used. The initial point exhibited the maximum impact. The minimum point was also affected by the changes in the die thickness almost at the same level as the initial temperature. The maximum point is not very sensitive to the die thickness. Very little variation was also observed with thin wall castings group. Thicker castings were not as sensitive as thin castings. They also did not indicate any response to the increasing wall thickness values.

3.2.4.3 Effect of Die Casting Process and Thermal Conditions

Initial die temperature varied within the range of 100-200 °C. This corresponds to a difference of 100%. With the changes in the initial die temperature, no variation was found for both thin and thick wall castings. The cooling line heat transfer coefficients were varied over the range of 1,256 – 6,006 W/m² K corresponding to 378 % difference. While the initial and minimum points for the thin wall castings was sensitive to the changes in cooling line heat transfer coefficients, the maximum point was not affected significantly. The initial point is the most sensitive of the three. Wall thickness variation within each thin and thick castings group did not yield any major changes in the critical temperatures and thermal gradients. Critical points of the thick wall castings exhibited greater sensitivity compared with the thin wall castings. The outcome makes sense since the maximum point of the thermal cycle is determined by the thermal shock during the injection stage of the process and cooling line is a continuously working tool. Especially it is dominant before and after the spray step determining the die surface temperature before the spray, consequently the minimum and the initial points of the thermal cycle.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range ($^\circ$C or $^\circ$C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.002</td>
<td>$T_{ini}$</td>
<td>56</td>
<td>38</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>187</td>
<td>83</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>12</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>68,700</td>
<td>46</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>32</td>
<td>29</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>5,060</td>
<td>67</td>
</tr>
<tr>
<td>0.004</td>
<td>$T_{ini}$</td>
<td>87</td>
<td>37</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>281</td>
<td>68</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>36</td>
<td>8</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>25,430</td>
<td>30</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>49</td>
<td>29</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>6,960</td>
<td>50</td>
</tr>
<tr>
<td>0.006</td>
<td>$T_{ini}$</td>
<td>105</td>
<td>35</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>334</td>
<td>59</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>38</td>
<td>8</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>24,850</td>
<td>43</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>60</td>
<td>28</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>7,680</td>
<td>40</td>
</tr>
</tbody>
</table>

Table 3.23: Ranges for surface temperature and thermal gradients (Units: $^\circ$C for temperatures, and $^\circ$C/m for temperature gradients) for thin wall castings due to changes in die thickness.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>17</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>67</td>
<td>15</td>
</tr>
<tr>
<td>0.010</td>
<td>$T_{max}$</td>
<td>6</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>3,720</td>
<td>8</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>8</td>
<td>8</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>1,213</td>
<td>10</td>
</tr>
<tr>
<td>0.012</td>
<td>$T_{ini}$</td>
<td>19</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>74</td>
<td>14</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>6</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>3,680</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>9</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>1,337</td>
<td>10</td>
</tr>
<tr>
<td>0.014</td>
<td>$T_{ini}$</td>
<td>20</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>80</td>
<td>13</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>6</td>
<td>1</td>
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<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>3,290</td>
<td>11</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>10</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>1,434</td>
<td>9</td>
</tr>
</tbody>
</table>

Table 3.24: Ranges for surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in die thickness.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.002</td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
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<td>0</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
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<td>0</td>
</tr>
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</tr>
<tr>
<td>0.004</td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>0</td>
<td>0</td>
</tr>
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<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>157</td>
<td>0</td>
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<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
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<td>0</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
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<td>0</td>
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</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
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<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>11</td>
<td>0</td>
</tr>
</tbody>
</table>

Table 3.25: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in die initial temperature.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
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<td></td>
<td>$T_{ini}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>0.010</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>0.012</td>
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<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>0.014</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$T_{ini}$</td>
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<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>0</td>
<td>0</td>
</tr>
</tbody>
</table>

Table 3.26: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/cm for temperature gradients) for thick wall castings due to changes in die initial temperature
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>21</td>
<td>18</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>89</td>
<td>41</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>11</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>7,280</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>13</td>
<td>14</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>1,927</td>
<td>25</td>
</tr>
<tr>
<td>0.002</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>35</td>
<td>19</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>149</td>
<td>37</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>14</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>10,300</td>
<td>12</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>22</td>
<td>15</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>3,140</td>
<td>22</td>
</tr>
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<td></td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>44</td>
<td>18</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>189</td>
<td>35</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>15</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>11,740</td>
<td>21</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>27</td>
<td>14</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>3,879</td>
<td>20</td>
</tr>
</tbody>
</table>

Table 3.27: Ranges for die surface temperature and thermal gradients (Units: C for temperatures, and C/m for temperature gradients) for thin wall castings due to changes in cooling line heat transfer coefficient
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_{\text{ini}}$</td>
<td>38</td>
<td>22</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{ini}}$</td>
<td>162</td>
<td>46</td>
</tr>
<tr>
<td>0.010</td>
<td>$T_{\text{max}}$</td>
<td>13</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{max}}$</td>
<td>8,360</td>
<td>16</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{min}}$</td>
<td>18</td>
<td>17</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{min}}$</td>
<td>2,802</td>
<td>29</td>
</tr>
<tr>
<td>0.012</td>
<td>$T_{\text{ini}}$</td>
<td>42</td>
<td>22</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{ini}}$</td>
<td>181</td>
<td>44</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{max}}$</td>
<td>14</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{max}}$</td>
<td>8,160</td>
<td>20</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{min}}$</td>
<td>21</td>
<td>17</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{min}}$</td>
<td>3,115</td>
<td>27</td>
</tr>
<tr>
<td>0.014</td>
<td>$T_{\text{ini}}$</td>
<td>46</td>
<td>22</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{ini}}$</td>
<td>197</td>
<td>42</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{max}}$</td>
<td>14</td>
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<td>$dT/dx_{\text{max}}$</td>
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<td>16</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{min}}$</td>
<td>3,369</td>
<td>26</td>
</tr>
</tbody>
</table>

Table 3.28: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in cooling line heat transfer coefficient.
Air heat transfer coefficients varied within the range of 42 - 837 W/m²K. This range corresponds to 1,900% difference in air heat transfer coefficients. The initial temperature of the thermal cycle has the most sensitivity to the air heat transfer coefficient. This can be explained with the thermal recovery of the die surface. After the end of the spray step, cooler surface layers are heated back by the warmer layers away from the surface. The heat recovery event and the initial point of the thermal cycle are controlled by the die/air and cooling line heat transfer coefficient. The minimum point has less sensitivity than the initial point but it is also affected since the temperature at which spray is initiated is also controlled by the heat transfer to ambient and heat removal by the cooling lines. The maximum point also showed some sensitivity. The temperature range for the maximum point of the thermal cycle was around 6% and the thermal gradient varied within 10-20% range. There was no major variation among the values within the thin castings group. Critical points of the thick wall castings found to be more sensitive to air heat transfer coefficient. This can be explained with the thick castings having more heat content compared with thin walls. The variation in the maximum and minimum points were reduced to its half compared with the thin wall castings. The initial point for thick walls was still very sensitive.

Die/casting heat transfer coefficient varied within the range of 10,010 - 80,311 W/m²K corresponding to 702% change. The minimum and the initial points were not affected for thin wall castings. Impact on the maximum point dissipated with increasing wall thickness values. The gradient did not observe any major variation. For the thick castings, the initial and minimum point seem to have some impact on them while the maximum point is not sensitive to the changes in heat transfer coefficients in contrast to that of the thin walls.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_{\text{ini}}$</td>
<td>48</td>
<td>39</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{ini}}$</td>
<td>1,126</td>
<td>1,223</td>
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<tr>
<td></td>
<td>$T_{\text{max}}$</td>
<td>24</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{max}}$</td>
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<td></td>
<td>$T_{\text{min}}$</td>
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<tr>
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<td>$T_{\text{ini}}$</td>
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<td>41</td>
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<td></td>
<td>$dT/dx_{\text{ini}}$</td>
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<td>1,294</td>
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<td>$T_{\text{max}}$</td>
<td>31</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{max}}$</td>
<td>1,802</td>
<td>20</td>
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<tr>
<td></td>
<td>$T_{\text{min}}$</td>
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<td>29</td>
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<td>0.004</td>
<td>$T_{\text{ini}}$</td>
<td>107</td>
<td>41</td>
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<td>$dT/dx_{\text{ini}}$</td>
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<td>1,352</td>
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<td>$T_{\text{max}}$</td>
<td>34</td>
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<td></td>
<td>$dT/dx_{\text{max}}$</td>
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<td></td>
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<tr>
<td></td>
<td>$dT/dx_{\text{min}}$</td>
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<td>47</td>
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Table 3.29: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in die/air heat transfer coefficient

175
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.010</td>
<td>( T_{\text{ini}} )</td>
<td>58</td>
<td>33</td>
</tr>
<tr>
<td></td>
<td>( dT/dx_{\text{ini}} )</td>
<td>2,095</td>
<td>1,218</td>
</tr>
<tr>
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<td>( T_{\text{max}} )</td>
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<td>3</td>
</tr>
<tr>
<td></td>
<td>( dT/dx_{\text{max}} )</td>
<td>10,340</td>
<td>20</td>
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<tr>
<td></td>
<td>( T_{\text{min}} )</td>
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<td>15</td>
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<td></td>
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<td>23</td>
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<td>0.012</td>
<td>( T_{\text{ini}} )</td>
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<td>33</td>
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<td>( dT/dx_{\text{ini}} )</td>
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<td>1,233</td>
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<tr>
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<td>( T_{\text{max}} )</td>
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<td>3</td>
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<td>9,570</td>
<td>23</td>
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<td>( T_{\text{min}} )</td>
<td>18</td>
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<td>( dT/dx_{\text{min}} )</td>
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<td>22</td>
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<td>( T_{\text{ini}} )</td>
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<td>33</td>
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<td></td>
<td>( dT/dx_{\text{ini}} )</td>
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<td>1,250</td>
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<tr>
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<td>( T_{\text{max}} )</td>
<td>16</td>
<td>3</td>
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<td></td>
<td>( dT/dx_{\text{max}} )</td>
<td>8,250</td>
<td>25</td>
</tr>
<tr>
<td></td>
<td>( T_{\text{min}} )</td>
<td>20</td>
<td>15</td>
</tr>
<tr>
<td></td>
<td>( dT/dx_{\text{min}} )</td>
<td>3,012</td>
<td>22</td>
</tr>
</tbody>
</table>

Table 3.30: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in die/air heat transfer coefficient
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
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<td></td>
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<td>0</td>
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<td>$dT/dx_{ini}$</td>
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</tr>
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<td>$T_{max}$</td>
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<td>32</td>
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<td>67</td>
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<td>$T_{ini}$</td>
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<td>0</td>
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<td>$dT/dx_{ini}$</td>
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<td>0</td>
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<td>$T_{max}$</td>
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<td>20</td>
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<td>$dT/dx_{max}$</td>
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<td>59</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>0</td>
<td>0</td>
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<td></td>
<td>$dT/dx_{min}$</td>
<td>9</td>
<td>0</td>
</tr>
<tr>
<td>0.004</td>
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<td>2</td>
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<td>$T_{max}$</td>
<td>70</td>
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<td></td>
<td>$dT/dx_{min}$</td>
<td>165</td>
<td>0</td>
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</table>

Table 3.31: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in die/casting heat transfer coefficient
### Table 3.32: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in die/casting heat transfer coefficient

<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.010</td>
<td>$T_{ini}$</td>
<td>17</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>67</td>
<td>15</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
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<td>1</td>
</tr>
<tr>
<td></td>
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<td>8</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>1,213</td>
<td>10</td>
</tr>
<tr>
<td>0.012</td>
<td>$T_{ini}$</td>
<td>19</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>74</td>
<td>14</td>
</tr>
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<td></td>
<td>$T_{max}$</td>
<td>6</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>3,680</td>
<td>10</td>
</tr>
<tr>
<td></td>
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<td>7</td>
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<tr>
<td></td>
<td>$dT/dx_{min}$</td>
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<td>10</td>
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<td>$dT/dx_{ini}$</td>
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<tr>
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<td>$T_{max}$</td>
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<td>$dT/dx_{max}$</td>
<td>3,290</td>
<td>11</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>10</td>
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<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>1,434</td>
<td>9</td>
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</table>
Spray fluid temperature varied within the range of 10-35 °C. This corresponds to 250% difference in spray fluid temperature values. The spray fluid temperature did not affect the maximum point of the thermal cycle. The minimum point was the most affected of all critical points. However, the range of the minimum point dissipated rapidly from 14% to 7% with increasing wall thickness (increasing heat content) values within thin castings group. The initial point was somewhat varied within 2 – 5% range. Spray is the determining factor for the minimum point as expected. Thicker wall castings exhibited the same behavior and the range of the critical points decreased. Variation due to increasing wall thickness values within the thick castings group was less compared with the thin wall castings. Temperature of the minimum point of the thick wall castings showed slightly more sensitivity, while the initial and maximum points were less sensitive.

Spray heat transfer coefficient varied within the range of 1,000 – 12,581 W/m²K. This corresponds to 1,155% change. The spray heat transfer coefficient 1,000 W/m²K was found to be very ineffective for 0.002 m castings due to very little heat content. The surface temperature was very low for this spray condition, preventing surface from cooling. The maximum point of the thermal cycle was not affected much (about 5%) by the changes in spray heat transfer coefficients since it is determined mostly by the thermal shock. The minimum and initial points showed greater sensitivity to the changes. The minimum point was the more affected of the two critical points of the thermal cycle. Both of the points were expected to be controlled by spray cooling of the surfaces. There was also no variation within the thin castings group. The thick castings exhibited more sensitivity at the all three critical points. There was also no variation within the thick castings group either.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.002</td>
<td>$T_{ini}$</td>
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<td>4</td>
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<tr>
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<td>$dT/dx_{ini}$</td>
<td>16</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>2</td>
<td>1</td>
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<td></td>
<td>$dT/dx_{max}$</td>
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<td>1</td>
</tr>
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<td>$T_{min}$</td>
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<td>$dT/dx_{min}$</td>
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<td></td>
<td>$dT/dx_{ini}$</td>
<td>14</td>
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</tr>
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<td>$T_{max}$</td>
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<td>2</td>
</tr>
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<td>$T_{max}$</td>
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<td>0</td>
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<tr>
<td></td>
<td>$dT/dx_{max}$</td>
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</tr>
<tr>
<td></td>
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<td>$dT/dx_{min}$</td>
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<td>8</td>
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</table>

Table 3.33: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in spray fluid temperature.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
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<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>(T_{\text{ini}})</td>
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<td>4</td>
</tr>
<tr>
<td></td>
<td>(dT/dx_{\text{ini}})</td>
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<td>4</td>
</tr>
<tr>
<td></td>
<td>(T_{\text{max}})</td>
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<td>0</td>
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<td>(dT/dx_{\text{max}})</td>
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<td>14</td>
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<td>(dT/dx_{\text{min}})</td>
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<td></td>
<td>(T_{\text{ini}})</td>
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<td>3</td>
</tr>
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<td></td>
<td>(dT/dx_{\text{ini}})</td>
<td>18</td>
<td>3</td>
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<td>3</td>
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<td>(dT/dx_{\text{ini}})</td>
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<td>(T_{\text{max}})</td>
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<td>(T_{\text{min}})</td>
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</table>

Table 3.34: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in spray fluid temperature
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
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<td>23</td>
</tr>
<tr>
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<td>32</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>15</td>
<td>3</td>
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<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>10,590</td>
<td>7</td>
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<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>65</td>
<td>52</td>
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<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>6,680</td>
<td>121</td>
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<td>24</td>
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<tr>
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<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
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<td>29</td>
</tr>
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<td>4</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>9,700</td>
<td>12</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>114</td>
<td>57</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>12,530</td>
<td>129</td>
</tr>
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<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
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<td>23</td>
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<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>169</td>
<td>26</td>
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<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>21</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>10,810</td>
<td>21</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>151</td>
<td>58</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>17,480</td>
<td>136</td>
</tr>
</tbody>
</table>

Table 3.35: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in spay heat transfer coefficient
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>65</td>
<td>31</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>183</td>
<td>41</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>21</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>13,130</td>
<td>27</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>145</td>
<td>69</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>8,900</td>
<td>171</td>
</tr>
<tr>
<td>0.010</td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>74</td>
<td>31</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>206</td>
<td>40</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>21</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>9,380</td>
<td>25</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>166</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>10,400</td>
<td>174</td>
</tr>
<tr>
<td>0.012</td>
<td>T&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>81</td>
<td>31</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;ini&lt;/sub&gt;</td>
<td>225</td>
<td>38</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;max&lt;/sub&gt;</td>
<td>23</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;max&lt;/sub&gt;</td>
<td>5,578</td>
<td>19</td>
</tr>
<tr>
<td></td>
<td>T&lt;sub&gt;min&lt;/sub&gt;</td>
<td>185</td>
<td>71</td>
</tr>
<tr>
<td></td>
<td>dT/dx&lt;sub&gt;min&lt;/sub&gt;</td>
<td>11,800</td>
<td>177</td>
</tr>
</tbody>
</table>

Table 3.36: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in spray heat transfer coefficient.
Cooling line water temperature varied within the range of 10-35 °C. This range corresponds to a difference of 250%.

There was very little influence on the maximum point (about 1%). Both the minimum point and initial point of the thermal cycle showed some impact. The influence can be explained with very little heat input/presence within tooling. Thicker castings were less sensitive compared with the thin wall castings. A little variation within each group was also detected.

<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Spray initiation (sec) After die opening</th>
<th>Spray end time (sec) After die opening</th>
<th>Spray duration (sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.002</td>
<td>6.0</td>
<td>7.5</td>
<td>1.5</td>
</tr>
<tr>
<td>0.004</td>
<td>5.0</td>
<td>6.5</td>
<td>1.5</td>
</tr>
<tr>
<td>0.006</td>
<td>4.0</td>
<td>5.5</td>
<td>1.5</td>
</tr>
<tr>
<td>0.010</td>
<td>6.0</td>
<td>11.0</td>
<td>5.0</td>
</tr>
<tr>
<td>0.012</td>
<td>5.0</td>
<td>10.0</td>
<td>5.0</td>
</tr>
<tr>
<td>0.014</td>
<td>7.0</td>
<td>12.0</td>
<td>5.0</td>
</tr>
</tbody>
</table>

Table 3.37: Spray initiation and end times

The impact of spray initiation times on the thermal state of the dies was studied varying the spray start (delay) times between 4 to 7 seconds after die opening. Even though the thin wall castings seemed to be little more sensitive to spray times compared with thick wall castings, changing spray initiation point of the thermal cycle did not affect the temperature of the three critical die surface temperatures and the temperature gradients for both the thin and thick wall groups. The thin wall castings are affected more since the heat content is less compared with thick wall group. The maximum range encountered did not exceed 4% difference. Very little variation was also observed within for both thin and thick castings group.
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.002</td>
<td>$T_{\text{ini}}$</td>
<td>13</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{ini}}$</td>
<td>55</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{max}}$</td>
<td>5</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{max}}$</td>
<td>3,859</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{min}}$</td>
<td>8</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{min}}$</td>
<td>1,157</td>
<td>7</td>
</tr>
<tr>
<td>0.004</td>
<td>$T_{\text{ini}}$</td>
<td>13</td>
<td>12</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{ini}}$</td>
<td>57</td>
<td>21</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{max}}$</td>
<td>7</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{max}}$</td>
<td>4,751</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{min}}$</td>
<td>8</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{min}}$</td>
<td>1,213</td>
<td>14</td>
</tr>
<tr>
<td>0.006</td>
<td>$T_{\text{ini}}$</td>
<td>11</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{ini}}$</td>
<td>49</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{max}}$</td>
<td>4</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{max}}$</td>
<td>2,878</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$T_{\text{min}}$</td>
<td>7</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{\text{min}}$</td>
<td>1,008</td>
<td>5</td>
</tr>
</tbody>
</table>

Table 3.38: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in cooling line fluid temperature
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_{ini}$</td>
<td>12</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>54</td>
<td>11</td>
</tr>
<tr>
<td>0.010</td>
<td>$T_{max}$</td>
<td>4</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>2,707</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>6</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>951</td>
<td>8</td>
</tr>
<tr>
<td>0.012</td>
<td>$T_{ini}$</td>
<td>12</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>53</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>3</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>2,268</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>6</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>918</td>
<td>7</td>
</tr>
<tr>
<td>0.014</td>
<td>$T_{ini}$</td>
<td>12</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>52</td>
<td>8</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>3</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>1,883</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>6</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>887</td>
<td>6</td>
</tr>
</tbody>
</table>

Table 3.39: Ranges for surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to change in cooling line fluid temperature
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( \text{T}_\text{ini} )</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{ini} )</td>
<td>7</td>
<td>2</td>
</tr>
<tr>
<td>0.002</td>
<td>( \text{T}_\text{max} )</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{max} )</td>
<td>370</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>( \text{T}_\text{min} )</td>
<td>2</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{min} )</td>
<td>337</td>
<td>4</td>
</tr>
<tr>
<td>0.004</td>
<td>( \text{T}_\text{ini} )</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{ini} )</td>
<td>12</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>( \text{T}_\text{max} )</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{max} )</td>
<td>491</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>( \text{T}_\text{min} )</td>
<td>5</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{min} )</td>
<td>618</td>
<td>4</td>
</tr>
<tr>
<td>0.006</td>
<td>( \text{T}_\text{ini} )</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{ini} )</td>
<td>16</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>( \text{T}_\text{max} )</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{max} )</td>
<td>563</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>( \text{T}_\text{min} )</td>
<td>6</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>( \text{d}T/\text{d}x_\text{min} )</td>
<td>855</td>
<td>4</td>
</tr>
</tbody>
</table>

Table 3.40: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thin wall castings due to changes in spray initiation and end times (duration)
<table>
<thead>
<tr>
<th>Casting wall thickness (m)</th>
<th>Parameter</th>
<th>Range (°C or °C/m)</th>
<th>% Difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.010</td>
<td>$T_{ini}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>5</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>199</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>277</td>
<td>2</td>
</tr>
<tr>
<td>0.012</td>
<td>$T_{ini}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>6</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>142</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>326</td>
<td>2</td>
</tr>
<tr>
<td>0.014</td>
<td>$T_{ini}$</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{ini}$</td>
<td>7</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>$T_{max}$</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{max}$</td>
<td>107</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>$T_{min}$</td>
<td>3</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>$dT/dx_{min}$</td>
<td>372</td>
<td>2</td>
</tr>
</tbody>
</table>

Table 3.41: Ranges for die surface temperature and thermal gradients (Units: °C for temperatures, and °C/m for temperature gradients) for thick wall castings due to changes in spray initiation and end times (duration)
3.2.4.4 Results of Sensitivity Analysis

Results of the analysis can be summarized through a sensitivity matrix. This matrix was prepared using a grading scale to determine which parameters were sensitive to the changes in them influencing the critical temperatures and the thermal gradients. At first, a different scale for each parameter was used to create the sensitivity matrix. Each scale was based on the relation: change in the response - change in the parameter. Since there are changes as small as 2 - 3% in some parameters while other parameters exposed to changes as much as 1,900% and the responses, especially for the critical temperatures, reflected such changes as 40% maximum, it was decided to use a universal scale which is only based on the changes in response values of the thermal system. The scale is an absolute scale and it uses the following criterion for grading the changes experienced by the critical factors of the thermal system:

<table>
<thead>
<tr>
<th>% Change</th>
<th>Grade/Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>0 to 5</td>
<td>- / Not Sensitive</td>
</tr>
<tr>
<td>5 to 10</td>
<td>Y - / Somewhat sensitive</td>
</tr>
<tr>
<td>10+</td>
<td>Y / Sensitive</td>
</tr>
</tbody>
</table>

Table 3.42: An absolute grading scale for sensitivity analysis (Originated from the idea that the changes in critical temperature points may not cause changes over 100%)

The outcome of this grading scale is shown in Table 3.43 and 3.44. The results were in agreement with some of the other grading scales used before this universal scale. However, only the outcome of the universal scale is reported in this chapter. Information in the parenthesis is the exceptions (wall thickness values in cm and their grades are reported)
<table>
<thead>
<tr>
<th>Factor/Parameter</th>
<th>Initial Temperature</th>
<th>Initial Gradient</th>
<th>Maximum Temperature</th>
<th>Maximum Gradient</th>
<th>Minimum Temperature</th>
<th>Minimum Gradient</th>
</tr>
</thead>
<tbody>
<tr>
<td>Die Thermal Conductivity</td>
<td>Y-</td>
<td>Y</td>
<td>(Y - 0.2)</td>
<td>Y</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>Die Thermal Conductivity</td>
<td>Y-</td>
<td>Y</td>
<td></td>
<td>Y (-1.0)</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>b Die Density</td>
<td>-</td>
<td>-</td>
<td></td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>b Die Density</td>
<td>-</td>
<td>-</td>
<td></td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>*Die Specific Heat Capacity</td>
<td>Y-</td>
<td>Y</td>
<td>(Y - 0.2)</td>
<td>Y</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>b Die Specific Heat Capacity</td>
<td>Y-</td>
<td>Y</td>
<td></td>
<td>Y</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>*Die Thickness</td>
<td>Y</td>
<td>Y</td>
<td>(Y - 0.2)</td>
<td>Y</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>b Die Thickness</td>
<td>Y</td>
<td>Y</td>
<td></td>
<td>Y (-1.0)</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>*Initial Die Temp.</td>
<td>-</td>
<td>-</td>
<td></td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>b Initial Die Temp.</td>
<td>-</td>
<td>-</td>
<td></td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>C.line Heat Transfer C.</td>
<td>Y</td>
<td>Y</td>
<td></td>
<td>Y (-0.2)</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>C.line Heat Transfer C.</td>
<td>Y</td>
<td>Y</td>
<td></td>
<td>Y</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>*Air Heat Transfer C.</td>
<td>Y</td>
<td>Y</td>
<td>(Y - 0.2)</td>
<td>Y</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>*Air Heat Transfer C.</td>
<td>Y</td>
<td>Y</td>
<td></td>
<td>Y</td>
<td>Y</td>
<td>Y</td>
</tr>
<tr>
<td>*Die/Casting Heat Transfer C.</td>
<td>-</td>
<td>-</td>
<td></td>
<td>Y</td>
<td>Y</td>
<td>-</td>
</tr>
<tr>
<td>*Die/Casting Heat Transfer C.</td>
<td>Y</td>
<td>Y</td>
<td></td>
<td>(Y -0.2)</td>
<td>Y</td>
<td>Y</td>
</tr>
</tbody>
</table>

Table 3.43: Sensitivity matrix (*: for thin wall castings 0.02 to 0.06 m, b: for thick castings 0.10 to 0.14 m, exceptions reported in parenthesis)
Results indicated the following parameters did not result in any changes at the three critical time instants of the cycle. These parameters are excluded from the next step, the structural analysis.

- Density of the die material
- Initial die temperature
- Air gap heat transfer coefficient
- Air gap temperature (casting surface temperature at which air gap formation starts)
- Spray initiation (delay) times and duration
Remaining parameters listed below were used in the upcoming 2-D ABAQUS thermal and structural analyses.

- Thermal conductivity of the die material
- Specific heat capacity of the die material
- Die thickness
- Cooling line heat transfer coefficient
- Die/air heat transfer coefficient
- Die/casting heat transfer coefficient
- Spray fluid temperature
- Spray heat transfer coefficient

While the four critical factors somewhat affected by the remaining parameters, the maximum temperature of the thin wall castings seemed only to be influenced by the parameters controlling the interface conditions between the die and the casting. These parameters are the thermal conductivity, specific heat capacity, die/casting, die/air and spray heat transfer coefficients. The thermal gradient for the maximum point was also affected by the same factors in addition to the cooling line heat transfer coefficient. For the thick wall castings, the maximum temperature was only affected by the spray heat transfer coefficient. The thermal gradient was also affected by the most of the factors including the ones controlling the interface with the exception of the spray heat transfer coefficient. The difference between the thick and the thin wall casting groups can be explained with the limited heat content of the thin walls allowing the maximum temperature to be controlled by the certain parameters mentioned above. The previous tables, Table 3.43 and 3.44 were created to gather the results for simple comparisons. The very detailed discussions regarding each parameter should be found at the previous pages for more comprehensive approach.
3.3 2-D Thermal and Structural (Stress/Strain) Analysis

The focus of this section of the research was to continue the previous 1-D thermal analysis to further associate the parameters studied in the first section of Chapter 3 with the structural state within tooling. Surface temperature histories (3 cycles from the start up) from 1-D BINORM analyses were used in 2-D ABAQUS models and were imposed along the die cavity surface to determine the effects of geometry, thermal process parameters and properties of the die material (H-13) on the thermal and consequent stress/strain state. The approach was taken believing that the imposing BINORM temperature profile will not only capture the actual surface temperature history but also will carry on the impacts of the parameters on the thermal fields of the dies to the structural analysis. There are other methods which could be used. The first one is modeling the casting as a part of the FEM model. This requires detailed modeling efforts with issues such as cavity pressure, temperature changes during solidification, air gap formation and so on. This course was not taken due to the amount of work required. It is almost equivalent to writing another computer analysis code in BINORM's scale. The second alternative could be replicating the heat input from the castings to the dies through a time varying heat flux using an approximation approach. This approach will not deliver results as near as actual temperature die cavity surface history obtained by BINORM analysis. Thus, it was not elected to be utilized.

ABAQUS analyses were conducted in a sequential fashion. At first the thermal analysis was completed and this was followed by the sequentially coupled structural analysis. This method is applicable when the thermal field is the driving force for the structural analysis, but the thermal solution does not depend on the structural solution. In this case the temperature solution, which normally depends on position and time, is read into the structural analysis as a predefined field. [ABAQUS, 1997].

As mentioned previously, upon the completion of the 1-D thermal study, some parameters found to be not effective determining the three critical points of the die thermal cycle due to either their behavior or very little change in them. These parameters are:
• Density of the die material
• Initial die temperatures
• Air gap heat transfer coefficients
• Air gap temperatures (casting surface temperature at which air gap formation starts)
• Spray initiation (delay) times

The parameters listed above were not included in the 2-D sensitivity analyses since they did not have any major impact on the three critical points of the thermal cycle; the initial, maximum and minimum temperatures.

The remaining parameters listed below were used in the 2-D ABAQUS thermal and structural analyses. Ranges of values used in the BINORM analysis were also used in the 2-D ABAQUS models with some exceptions. The exceptions were mentioned in the further pages of this section.

• Thermal conductivity of the die material (Table 3.4)
• Specific heat capacity of the die material (Table 3.6)
• Die thickness (Table 3.7)
• Cooling line heat transfer coefficient (Table 3.9)
• Die/air heat transfer coefficient (Table 3.10)
• Die/casting heat transfer coefficient (Table 3.11)
• Spray fluid temperature (Table 3.13)
• Spray heat transfer coefficient (3.14)
3.3.1 Thermal and Structural Modeling

3.3.1.1 Thermal Model

Impacts of geometry, thermal process parameters and physical properties of the die material (H-13) on the thermal state within a flat plate die are covered in this section. A simple FEM model was designed and built for this portion of the study. The model included only a 2-D die insert for one die half without the presence of the die block. The geometry of the die insert is shown in Figure 3.16. The die insert included a cooling line. It was placed in 3 different locations (0.0254, 0.0348 and 0.0508 below the die surface) to see the impact of the proximity of the cooling line to the die cavity surface. The die thickness term was used for the distance between the cooling line and the die cavity surface as it was done for BINORM. The other die half was also omitted with a symmetry assumption. The die insert was divided into fine meshes with element sizes of 0.00254 - 0.00501 meter. Finer free local meshing with average element size less than 0.002 meter was also used for the die insert cavity surface area.

Figure 3.16: 2-D model of a die insert with casting thickness of 0.004 m (dimensions in meters)
A 2-D thermal analysis was conducted to be sequentially coupled with the structural analysis. The equation governing the thermal fields within the die insert is the Fourier's heat conduction equation given in Appendix H.

The thermo-physical properties of the die material (H-13), which were used in the simulations, were taken through a best fit of the data presented in Appendix E. The following tables present the values used in the reference FEM model. A constant density value (7,700 kg/m³) is used for all the simulations. Different constant values of specific heat capacity and thermal conductivity of H-13 were also used in the 2-D sensitivity analysis.

The die insert was assumed to be at a uniform initial temperature 100 °C.

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Thermal Conductivity (W/m K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>50</td>
<td>24.75</td>
</tr>
<tr>
<td>100</td>
<td>24.93</td>
</tr>
<tr>
<td>150</td>
<td>25.11</td>
</tr>
<tr>
<td>200</td>
<td>25.29</td>
</tr>
<tr>
<td>250</td>
<td>25.47</td>
</tr>
<tr>
<td>300</td>
<td>25.65</td>
</tr>
<tr>
<td>350</td>
<td>25.83</td>
</tr>
<tr>
<td>400</td>
<td>26.01</td>
</tr>
<tr>
<td>450</td>
<td>26.19</td>
</tr>
<tr>
<td>500</td>
<td>26.37</td>
</tr>
<tr>
<td>550</td>
<td>26.55</td>
</tr>
<tr>
<td>600</td>
<td>26.73</td>
</tr>
<tr>
<td>650</td>
<td>26.91</td>
</tr>
<tr>
<td>700</td>
<td>27.09</td>
</tr>
<tr>
<td>750</td>
<td>27.27</td>
</tr>
<tr>
<td>800</td>
<td>27.45</td>
</tr>
<tr>
<td>850</td>
<td>27.63</td>
</tr>
<tr>
<td>900</td>
<td>27.81</td>
</tr>
</tbody>
</table>

Table 3.45: Temperature dependent thermal conductivity for H-13
<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Specific Heat Capacity (J/kg K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>50</td>
<td>469.9</td>
</tr>
<tr>
<td>100</td>
<td>479.3</td>
</tr>
<tr>
<td>150</td>
<td>488.8</td>
</tr>
<tr>
<td>200</td>
<td>498.2</td>
</tr>
<tr>
<td>250</td>
<td>507.6</td>
</tr>
<tr>
<td>300</td>
<td>517.0</td>
</tr>
<tr>
<td>350</td>
<td>526.4</td>
</tr>
<tr>
<td>400</td>
<td>535.9</td>
</tr>
<tr>
<td>450</td>
<td>545.3</td>
</tr>
<tr>
<td>500</td>
<td>554.7</td>
</tr>
<tr>
<td>550</td>
<td>564.1</td>
</tr>
<tr>
<td>600</td>
<td>573.5</td>
</tr>
<tr>
<td>650</td>
<td>583.0</td>
</tr>
<tr>
<td>700</td>
<td>592.4</td>
</tr>
<tr>
<td>750</td>
<td>601.8</td>
</tr>
<tr>
<td>800</td>
<td>611.2</td>
</tr>
<tr>
<td>850</td>
<td>620.6</td>
</tr>
<tr>
<td>900</td>
<td>630.1</td>
</tr>
</tbody>
</table>

Table 3.46: Temperature dependent specific heat capacity for H-13

The thermal FEM model and the boundary conditions are shown in Figure 3.17. The boundary conditions are as follows:

- Zero heat flux \( (q=0) \) between the die insert-the die block
- Zero heat flux \( (q=0) \) between at the interface of the die halves
- Convection at the cooling line (through a constant heat transfer coefficient and cooling fluid temperature)
- Time varying temperatures imposed along the die cavity surface including the bottom part of the parting line as a runner

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Figure 3.17: 2-D thermal model (with no heat transfer along the die block/insert interface and between die halves and BINORM temperature profile being imposed along the die cavity. Tcl: cooling fluid temperature and hcl: cooling line heat transfer coefficient)

The assumptions were made and their justifications were:

- Heat transfer between the die insert and the die block at the die block/insert interface was assumed to be negligible since the actual interface heat transfer coefficients in real applications are no more than a few kW/m² K for die insert/block interaction (see Table 2.12). The temperature difference between the back portion of the insert and the die block is not much either due to relatively large distance from die cavity surface.
• Heat transfer between the die halves (inserts) was also assumed to be zero since maximum reported heat transfer coefficients between die halves were 10 kW/m² K (see Table 2.12). The heat transfer coefficients are usually less than this extreme value. The temperature difference along this interface between the two die halves is small as well due to the axisymmetry in the simple model used.

• A temperature profile representing a casting thermal cycle (die insert/cavity surface temperature history obtained through BINORM) was imposed along the die cavity surface to translate the effect of a variety of factors into FEM modeling. This boundary condition is represented in Figure 3.17 with a set of dark lines along the die cavity.

• The cooling line-die insert thermal interaction was defined through heat convection with a constant cooling fluid temperature and a constant heat transfer coefficient since the problem is a 2-D problem.

• Another assumption made was no flash allowance at the parting line while the runner was included.

3.3.1.2 Structural (Stress-Strain) Model

Structural analysis was conducted to determine the impacts of geometry, thermal process parameters, and physical properties of the die material (H-13) on the stress/strain distribution within the same flat plate die used in the previous section of this chapter. The thermal and structural analyses were sequentially coupled through the same FEM mesh structure.

The structural model was assumed not to be fully coupled with the thermal model as previously explained. The tooling model was also assumed to be elastic. It required the sequential solution of both thermal and structural models. The die material, H-13, was
assumed as ideally plastic. The equations governing the sequentially coupled thermoelastic problem are given in Appendix H.

Data regarding mechanical properties of the die material (H-13) can be found in Appendix E. Due to the similarities and replication among the literature resources, similar figures were obtained. Since the main focus of the research was the thermal management and thermal fatigue, mechanical material properties of H-13 were chosen through a best fit of the values presented in Appendix E. The impact of mechanical properties of the die material on structural state of the dies was not investigated. The impact of mechanical properties on determining the number of cycles to the onset of heat checking is covered in Chapter 4. The properties used in the simulations are listed in the following tables.

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Linear Coefficient of Thermal Expansion (1/°C) x10^-5</th>
<th>Young's Modulus (MPa)</th>
<th>Yield Strength (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>-</td>
<td>213,700</td>
<td>1213</td>
</tr>
<tr>
<td>93</td>
<td>1.188</td>
<td>206,800</td>
<td>1131</td>
</tr>
<tr>
<td>204</td>
<td>1.224</td>
<td>199,900</td>
<td>1103</td>
</tr>
<tr>
<td>315</td>
<td>1.224</td>
<td>186,200</td>
<td>1048</td>
</tr>
<tr>
<td>426</td>
<td>1.296</td>
<td>179,300</td>
<td>965</td>
</tr>
<tr>
<td>537</td>
<td>1.302</td>
<td>151,700</td>
<td>855</td>
</tr>
<tr>
<td>649</td>
<td>1.404</td>
<td>110,300</td>
<td>634</td>
</tr>
</tbody>
</table>

Table 3.47: Material properties used in the structural analysis (:- Reference temperature for thermal expansion, ν (Poisson's ratio): 0.30)

Initial conditions which were applied to the structural model is stated below:

- Structure model is initially free of residual stresses.

- At the initiation of a casting cycle, the dies were assumed to be clamped bringing a pre-load and pre-strain at the interface between the die halves.
The boundary conditions are shown in Figure 3.18 as well as the structural FEM model. The boundary conditions are as follows:

- Cavity pressure was applied inside the die cavity surface.
- Interface pressure was applied at the interface of the die halves.
- The die insert was restrained in horizontal and/or vertical directions \((u(1) \text{ and/or } u(2) = 0\) respectively) by the die block as shown in Figure 3.18. The horizontal interfaces are restrained in \(2(y)\) direction. The vertical interfaces are restrained in \(1(x)\) direction. The corner nodes are restrained in both directions.

The following assumptions were made and their justifications were:

- Pressure was applied within the die cavity on specific elements shown in Figure 3.18. Metal pressure was calculated using the following the procedure. A sample triangular pressure history is later given and explained in this section.

  Fill time is calculated as follows using Equation 3.1 with compatible units. Calculations can be seen in the appendices. Numerical values for some of the parameters from Equation 3.1 are not specified. These parameters ranged over the numerical values described in the previous chapter for the analysis.
Figure 3.18: 2-D structural model (Nodes along the die block/insert interface are restrained ∇ in the direction of the arrow head, while cavity is exposed to metal pressure and interface pressure is defined between the die halves)

\[
\tau_{\text{fill}} = \frac{(T_g - T_{\text{liq}})c_p W}{(T_g - T_d)hA_c}
\]  

(3.1)

where \( \tau_{\text{fill}} \): Fill time of cavity (sec)

- \( T_g \): Cast metal temperature at the gate (657°C)
- \( T_{\text{liq}} \): Liquidus temperature for the cast metal (581 °C)
- \( c_p \): Specific heat capacity for the cast metal (1120 J/kg K)
- \( W \): Mass of casting (\( \rho \times V \) (Density \times Volume)) (kg)
- \( T_d \): Average die cavity surface temperature (205 °C)
- \( h \): Overall heat transfer coefficient at the die/casting interface (W/m² K)
- \( A_c \): Surface area of casting (m²)
Maximum possible metal pressure \( (P_m) \), which may be experienced by the die cavity, is calculated through Equation 3.2.

\[
P_m = 0.0098 \frac{\rho V_g^2}{2gC^2} \tag{3.2}
\]

Where:
- \( P_m \): Maximum metal pressure within the cavity (Pa)
- \( V_g \): Cast metals' velocity at the gate (29 m/s)
- \( \rho \): Density of the casting metal (2,720 kg/m\(^3\))
- \( g \): Gravitational acceleration (9.806 m/s\(^2\))
- \( C \): Coefficient for energy losses due resistance to flow of cast metal (0.5-0.9)

At the initiation of the cycle, there is no pressure on the die cavity surface. Maximum pressure occurs at the fill time, \( \tau_{\text{fill}} \). A linear relation was assumed between the initial and the peak points of the thermal cycle. After the maximum metal pressure, \( P_m \), is reached, pressure drop also follows a linear pattern indicating no cavity pressure when dies open. The triangular pressure profile assumption was based on the outcome of some experimental studies (see Figure 2.22) [Hatamura et al, 1989].

A sample triangular pressure history is shown in Figure 3.19. The * symbol shown in the figure represents the fill time. The pressure profile was based on the idea that no intensification occurs after completion of filling. The intensification step should keep the pressure at a high level for a longer time period giving more blunt peak point in the pressure profile.
Interface pressures were also used in structural modeling. Only one die halve was included within the model, thus interface pressure became a necessity to replicate the interaction between the two die halves. A sample interface pressure profile is shown in Figure 3.20. Once the maximum metal pressure, $P_m$, is calculated, and the locking force, $F_i$, can be determined through Equation 3.3. 110\% of the metal injection force was chosen for the locking force, $F_i$, to assure locking of dies.

$$F_i = 1.1 F_m = 1.1 P_m A_p$$  \hspace{1cm} (3.3)$$

where $A_p$: Projected die cavity surface area (where metal pressure applies to separate the two die halves

$F_m$: Force applied by the maximum metal pressure
Figure 3.20: Interface pressure profile for a casting cycle (*: $t_{\text{fill}} > 0$ and in milliseconds)

Table 3.45 represents the critical time instants and intervals determining the interface pressure profile. A sample interface profile is shown in Figure 3.20.

<table>
<thead>
<tr>
<th>Time Instant/Interval</th>
<th>Interface Pressure $P_i$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0 (at the initiation of the casting cycle)</td>
<td>$F_i / A_i$</td>
</tr>
<tr>
<td>$0 - T_2$ ($0 &lt; t_{\text{fill}} &lt; T_2$)</td>
<td>$(F_i - F_m) / A_i$</td>
</tr>
<tr>
<td>$T_2$ (end of the cycle)</td>
<td>$F_i / A_i$</td>
</tr>
</tbody>
</table>

Table 3.48: Interface pressure calculations ($A_i$: interface area – the contact area of die halves)

- The stress state of the die insert was independent of that of the casting.
Plane strain elements made up the FEM model. The die insert was assumed to be long enough to justify the plane strain model. Constant cooling fluid temperature through the die insert and instant fill assumptions also support the plane strain approach since these two have resulted in no change in the thermal conditions in 3 (z) direction. A symmetry assumption between 2 (y) and 3 (z) axes was made by Hattel and Hansen [Hattel et al, 1994] to simplify the model. However, this option was nullified in this case by the existence of the cooling line in the model studied.

The die block restrained the die insert. (All the nodes along the die insert/block interface were restrained in either 1 (x) or 2 (y) direction, some (corner nodes) in both directions). There is no bending of die insert [Hattel et al, 1994].

The following comments cover the shortcomings and the advantages of both thermal and structural models used in this study.

In the thermal model, all the nodes on the die cavity surface experience the same temperature profile throughout a cycle. This also means that the temperature differences in 2 (y) – or vertical direction along the die cavity boundary are neglected. At the beginning of the thermal cycle, assuming instant fill (cavity fills in milliseconds) this assumption can be justified. Later in the cycle it may become a bad assumption depending upon the proximity of cooling line to die cavity surface. If the height of the casting is as small as in the model studied in this chapter, differences in distances between any points of the die cavity surface to the cooling line and the temperature differences along the die cavity become negligible. This issue is shown in Figure 3.21.
• Both thermal and structural models capture the changes in time. The main goal of the modeling was to study the thermal/structural state within the substrate. The 2-D flat plate die model captures this well with the help from BINORM temperature profiles.

• The well-constrained insert and no-bending assumptions can be justified with small casting wall concept simplifying but limiting the structural model [Hattel et al, 1994].

• Some limited geometry impacts can be seen through 2-D modeling even though temperature difference in 2 (y) direction is neglected. Using different die and casting wall thickness values also extended the scope of the geometric study.

• Intensification step was not included within the cavity pressure profile. This will influence the resulting stress profile on the cavity. However, this study was based on studying the extreme stress points and the ranges. This assumption does not have any influence on these factors.

• A coarse mesh structure was chosen for FEM modeling and this structure was replaced in the substrate by a finer mesh structure to obtain more accurate results. Finer mesh structure used only in the previously specified area to limit the size of the...
FEM model. The area between the coarse and the fine grid was occupied by an automatically created grid structure for smooth transition.

3.3.2.1 Structural Analysis for Steady State Condition

Stress and strain histories for normal stress and strain components in 1 (x) and 2 (y) directions ($\varepsilon_{11}, \varepsilon_{22}, \sigma_{11}, \sigma_{22}$) at the critical points of the die cavity surface were studied. Elements from the critical areas of the tooling with stress concentrations and from the die cavity surface were included in this section of the research. Elements, which are at and near the corners and in the middle of the die cavity surface, were chosen. A sample plot indicating the difference in the stress values due to stress concentrations is shown in Figure 3.22. This study also included the results from different integration points of each critical element to understand the detailed reaction of each element to the changing factors. This also yielded data from very close points to the die cavity surface. Figure 3.23 shows a plane strain element with its four integration points. Those elements, which were considered, are shown in Figure 3.24. Since sharp corners in 2-D FEM models may cause singularities and affect the local results around them, the results from corner elements were not utilized during further studies mentioned in this chapter of the dissertation.
Figure 3.22: The FEM model and important finite elements
Critical times during a die-casting cycle, which may impact the structural state within tooling, were determined. Some critical times of the die-casting cycle are considered and listed below:
• An instant right after injection
• An instant right after thermal peak (at the die cavity surface) occurs
• An instant right after spray is initiated
• An instant right after spray is stopped

The contour plots indicated crucial information about displacements and the critical factors such as stresses, strains and their components (thermal, elastic and plastic components in both 1(x) and 2(y) directions $\sigma_{11,22}, \varepsilon_{11,22}, \varepsilon_{11,22}, \varepsilon_{11,22,22}$ and the shear components $\sigma_{12,21,12}$). No plastic deformation (both $\varepsilon_{p11}$ and $\varepsilon_{p22}$=0) was observed by any of the cases studied.

### 3.3.2.1 Structural and Sensitivity analysis

Minimum, maximum, range and average values of the normal stress $\sigma_{22}$ for a cavity element were studied. Element 55 was chosen for this study since it is on the die cavity surface in direct contact with the casting and is not affected by any singularity. All the elements located on the cavity surface yielded very similar figures indicating no variation in $\sigma_{22}$ levels along the die cavity surface in 2 (y) direction.

Figure 3.25 represents the temperature history and the temperature differences of two nodes belonging to Element 55. A sample set of two plots, Figure 3.26 and 3.27 are also included within this section. These plots show the $\sigma_{22}$ stress and $\varepsilon_{22}$ strain histories for a steady state casting cycle in 2 (y) direction. Following section is based on the principal normal stress, $\sigma_{22}$ since die cavity surface is allowed to expand in 1 (x) direction and, $\sigma_{22}$ capture the die’s responses to the changes in the parameters well. The shear stresses were also studied and they were considerably (three orders of magnitude) lower than the normal stresses.
Figure 3.25: Temperature history from two different nodes of element 55, one at the die cavity surface, the other 2 mm below the surface

Figure 3.26: $\sigma_{22}$ history for a casting cycle of element 55
Minimum point of the $\sigma_{22}$ stress cycle is the indicator of the thermal shock and maximum compressive stress value for the casting cycle. On the contrary maximum point is the indicator of spray cooling and the maximum tensile stress instant for the casting cycle. Range and average parameters for the $\sigma_{22}$ profile are described below:

\[
Range = \left| \sigma_{22\text{, max}} - \sigma_{22\text{, min}} \right|
\]  

(3.4)

\[
Average = \frac{\sigma_{22\text{, max}} + \sigma_{22\text{, min}}}{2}
\]

(3.5)

Following section covers the structural reaction of die cavity surface to changing process conditions and die material properties. 2-D ABAQUS analyses were conducted for 0.004 m and 0.01 m castings. These two castings were chosen from the groups of thick and thin wall castings studied in the first section of Chapter 3. Both thick and thin...
wall castings had similar behaviors. Thus, quantitative results for only 0.004 m wall castings were presented in the following section.

3.3.2.2 Thermal conductivity

Similar to the specific heat capacity study in the previous chapter, thermal conductivity—temperature functions were excluded from the sensitivity analysis.

Increased thermal conductivity values caused a 2% decrease in $\sigma_{22\text{min}}$. Stress levels were at around $10^2$ MPa at the minimum point. The initial and maximum temperatures of the thermal cycle were both affected at a very low level causing very little change in the temperature range during injection and consequently in $\sigma_{22\text{min}}$.

A 90% decrease was observed in $\sigma_{22\text{max}}$ with increasing thermal conductivity and thermal diffusivity values. Stress levels were initially around $10^2$ MPa at the maximum point of the cycle. While the thermal shock during filling is not controlled by thermal conductivity due to being a very fast phenomenon, spray as the main factor for the maximum point of the stress cycle is affected by thermal conductivity since the thermal conductivity is a crucial factor with other means of cooling determining die surface temperature at the spray initiation.
Figure 3.28: $\sigma_{22\text{min}}$ versus thermal conductivity of the die material

Figure 3.29: $\sigma_{22\text{max}}$ versus thermal conductivity of the die material
Figure 3.30: $\sigma_{22\text{range}}$ versus thermal conductivity of the die material

Figure 3.31: $\sigma_{22\text{average}}$ versus thermal conductivity of the die material
Decreasing minimum and maximum points lead to a decreasing range with 9% change and shifting average more towards the compressive side. Observed change in the average was 8%.

3.3.2.3 Specific heat capacity

Only constant specific heat capacity values were utilized in this study while temperature dependent specific heat capacity functions were excluded.

Increasing $c_p$ resulted in a decrease in $\sigma_{22\text{min}}$. The minimum stress point indicated 7% change. Stress levels were around $10^2$ MPa for the minimum point. Greater $c_p$ values lowered the maximum temperature of the thermal cycle because of greater thermal diffusivity while increasing initial temperature of the thermal cycle due to increased heat storage capability. This caused reduced temperature ranges at the injection and reduced $\sigma_{22\text{min}}$.

The maximum stress values also decreased and 22% change was observed. Stress levels were around $10^1$ MPa for the maximum point. Greater $c_p$ values shifted the die surface temperature at the initiation and the end of spray upward at similar amounts. The only difference among different cases with different $c_p$ values was the slope of the spray curve. The less steep slope of greater specific heat capacity values can explain the decrease in the maximum stress value.

The stress range decreased 9% due to the decrease in both critical stress points. The average stress became less compressive with increasing $c_p$ values. A 16% change was observed in the average stress values.
Figure 3.32: $\sigma_{22\text{min}}$ versus specific heat capacity of the die material

Figure 3.33: $\sigma_{22\text{max}}$ versus specific heat capacity of the die material
Figure 3.34: $\sigma_{22\text{range}}$ versus specific heat capacity of the die material

Figure 3.35: $\sigma_{22\text{average}}$ versus specific heat capacity of the die material
3.3.2.4 Die thickness

The 0.1 m thick die was not used due to its inaccurate results mentioned in the previous section of BINORM thermal analysis.

Increasing die thickness values resulted in a decrease in $\sigma_{22\text{min}}$ due to increases in the initial and maximum temperatures at different rates (initial temperature experienced more increase compared with the peak temperature), and the consequent reduction in the temperature range during injection. Observed change was 5%.

The maximum stress values were increased and a 127% change was observed during the thickness change from 0.0394 – 0.050 m. Dies with 0.050 m thickness run at higher temperatures. $\sigma_{22\text{max}}$ became sensitive to the spray cooling and spray’s impact was increased due to greater die surface temperatures at the spray initiation.

The stress range was not sensitive to die thickness indicating 5% change since the changes in the minimum and maximum stress points cancelled each other (even though maximum point experienced more changes, stress levels were less compared (around $10^1$ MPa) with those of the minimum point (around $10^2$ MPa)).

Average stress values decreased towards less compressive side with 14% change due to increasing die thickness values.
Figure 3.36: $\sigma_{22\text{min}}$ versus die thickness

Figure 3.37: $\sigma_{22\text{max}}$ versus die thickness
Figure 3.38: $\sigma_{22\text{range}}$ versus die thickness

Figure 3.39: $\sigma_{22\text{average}}$ versus die thickness
3.3.2.5 Cooling line heat transfer coefficient

Increasing cooling line heat transfer coefficients resulted in an increase in \(\sigma_{22\text{min}}\) even though it was around 5%. Thermal shock during filling is influenced by the decreasing initial temperature of the thermal cycle since the temperature range during injection is increased with greater cooling line heat transfer coefficients. More change was observed for the initial temperature when compared with the peak temperature.

Increasing cooling line heat transfer coefficients resulted in a decrease in \(\sigma_{22\text{max}}\). The change was 326%. Spray's effect is minimized through better internal cooling with cooling lines and subsequent lower die surface temperatures at the spray initiation.

The stress range decreased 5%. The average stress values became more compressive. Observed change in the average stress values was 17%.

![Graph showing \(\sigma_{22\text{min}}\) versus cooling line heat transfer coefficient](image)

Figure 3.40: \(\sigma_{22\text{min}}\) versus cooling line heat transfer coefficient
Figure 3.41: $\sigma_{22\text{max}}$ versus cooling line heat transfer coefficient

Figure 3.42: $\sigma_{22\text{range}}$ versus cooling line heat transfer coefficient
3.3.2.6 Die/air heat transfer coefficient

Increasing air heat transfer coefficients resulted in an increase in the minimum point of $\sigma_{22}$. The range for $\sigma_{22\text{min}}$ indicated 13% change. Greater air heat transfer coefficients caused major drops in the initial temperatures of the thermal cycle while also reducing the maximum temperature at a considerably lower rate, thus increasing the temperature range during injection. This explains why the die cavity surface had greater compressive stresses at injection.

Maximum value of $\sigma_{22}$ decreased and at very high air heat transfer coefficients tensile stress values became compressive. This is the result of extensive cooling with air through convection, causing spray to lose its impact (since spray starts at lower die surface temperatures) and its ability to exert tensile stresses on the die cavity surface. The observed change for $\sigma_{22\text{max}}$ was 1078%.
The range of $\sigma_{22}$ was reduced only 5% for the range of the air heat transfer coefficient values used. Since there was a great percentile change in the maximum point but the magnitude of maximum point (around $10^1$ MPa) was a lot smaller than the minimum point values (around $10^2$ MPa), the range did not experience any major changes.

Average stress values shifted downward for more compressive stress levels. 36% change was observed.

Figure 3.44: $\sigma_{22\min}$ versus die/air heat transfer coefficient
Figure 3.45: $\sigma_{22\text{max}}$ versus die/air heat transfer coefficient

Figure 3.46: $\sigma_{22\text{range}}$ versus die/air heat transfer coefficient
3.3.2.7 Die/casting heat transfer coefficient

Increasing die/casting interface heat transfer coefficients resulted in an increase in the minimum point of $\sigma_{22}$ cycle. The range for $\sigma_{22\text{min}}$ values indicated 17% change. Stress values at the die surface reflected greater compressive stresses during injection due to the change in die/casting heat transfer coefficients. The changes in die/casting heat transfer coefficients affected the peak temperature of the cycle. The initial temperature was not impacted by the changes causing an increase in the temperature range and minimum stress point.

The maximum value of $\sigma_{22}$ did only experienced 3% change and found to be not sensitive as expected since the maximum stress point of the cycle is mainly controlled by spray and air cooling of cavity surfaces and internal cooling lines.

The range of $\sigma_{22}$ increased due to the increase in the minimum point. 26% change was observed.
The average stress values shifted downward toward more compressive stress levels. 36% change in average stress values was observed.

![Graph](image)

**Figure 3.48: \(\sigma_{22\text{min}}\) versus die/casting interface heat transfer coefficient**

### 3.3.2.8 Spray fluid temperature

\(\sigma_{22\text{min}}\) indicated a very low 0.0633% decrease with increasing spray fluid temperatures indicating that the minimum stress point is not sensitive to the changes in spray fluid temperatures.

The maximum point responded to increasing spray fluid temperatures with a decrease since the die cavity surface was sprayed upon with warmer spray fluid and spray's cooling performance was reduced. The change in maximum point was 25%. The range was decreased and observed change in the range was 3%. The average stress values shifted more on the compressive side. A 3% change in the average stress values was observed.
Figure 3.49: $\sigma_{22\text{max}}$ versus die/casting interface heat transfer coefficient

Figure 3.50: $\sigma_{22\text{range}}$ versus die/casting interface heat transfer coefficient
Figure 3.51: $\sigma_{22\text{average}}$ versus die/casting interface heat transfer coefficient

Figure 3.52: $\sigma_{22\text{min}}$ versus spray fluid temperature
Figure 3.53: $\sigma_{22\text{max}}$ versus spray fluid temperature

Figure 3.54: $\sigma_{22\text{range}}$ versus spray fluid temperature
3.3.2.9 Spray heat transfer coefficient

Increasing spray heat transfer coefficients did impact $\sigma_{22\text{min}}$ around 8% since the minimum temperature of the cycle was determined after spraying and spray's effect was consequently carried on to the initial point of the thermal cycle.

The maximum point $\sigma_{22\text{max}}$ was impacted as much as 2,380% since temperature reduction through spray cooling was maximized. Steeper spray cooling curves with greater spray heat transfer coefficients can be the explanation for high levels of $\sigma_{22\text{max}}$.

The range increased with increasing spray heat transfer coefficients. Observed change in the range was 34%.

The average stress values move towards less on the compressive side. Observed change in the average values was 24%.

Figure 3.55: $\sigma_{22\text{average}}$ versus spray fluid temperature
Figure 3.56: $\sigma_{22\text{min}}$ versus spray heat transfer coefficient

Figure 3.57: $\sigma_{22\text{max}}$ versus spray heat transfer coefficient
Figure 3.58: $\sigma_{22\text{range}}$ versus spray heat transfer coefficient

Figure 3.59: $\sigma_{22\text{average}}$ versus spray heat transfer coefficient
3.3.2.10 Cooling line fluid temperature

Higher cooling line fluid temperature resulted in a 2% increase in $\sigma_{22\text{min}}$. Within the given range of cooling line fluid temperatures, both the initial and peak temperatures of the thermal cycle were shifted upward at almost similar amounts. No more than a few percent change were observed in the temperature range causing very little impact on $\sigma_{22\text{min}}$.

Increasing cooling line fluid temperature increased $\sigma_{22\text{max}} 22\%$ since dies will run at high temperatures and spray will be initiated at greater die surface temperatures indicating more impact.

The range was not sensitive and change in range was 0.044% since the minimum point of the stress cycle showed almost no reaction and the maximum stress values indicated some response to the changes in cooling line fluid temperature values, but the stress levels at the maximum point were less (around $10^1 \text{ MPa}$) compared with the other (around $10^2 \text{ MPa}$).

The average stress becomes less on the compressive side. A 5% change in the average stress values was observed.
Figure 3.60: $\sigma_{22\min}$ versus cooling line fluid temperature

Figure 3.61: $\sigma_{22\max}$ versus cooling line fluid temperature
Figure 3.62: $\sigma_{22\text{range}}$ versus cooling line fluid temperature

Figure 3.63: $\sigma_{22\text{average}}$ versus cooling line fluid temperature
3.3.3 Results of Structural Analysis

Results of the structural analysis can be summarized through a matrix describing the effects of the parameters on the maximum, the range and the average stress levels of the steady die-casting cycle of concern. The minimum stress point was not included in the matrix since it is the maximum compressive stress of the cycle and compressive stresses are not as detrimental as tensile stresses according to the present fatigue literature. However, the minimum point is still an important factor determining range and average stress values. These three factors can be utilized to understand the relative role of the parameters.

Literature resources strongly indicate that lower tensile stresses (for the maximum point of the stress cycle), lower stress ranges and more compressive average values for the stress cycle are the desired conditions for loading in tooling. According to Goodman relationship given in Equation 3.6, as the tensile average stress $\sigma_{\text{average}}$ increases the stress amplitude $\sigma_a$ ($\sigma_a = \sigma_{\text{range}}/2$) must decrease for material to withstand the stresses [Askeland, 1994].

$$\sigma_a = \sigma_{fs} \left[ 1 - \left( \frac{\sigma_{\text{average}}}{\sigma_t} \right) \right] \quad (3.6)$$

where $\sigma_a$: amplitude of the stress cycle (half the stress range)

$\sigma_{fs}$: fatigue strength of the material

$\sigma_{\text{average}}$: average stress of the stress cycle

$\sigma_t$: tensile strength of the material

Dowling also states that for a given stress amplitude, tensile average (mean) stresses give shorter fatigue lives than does zero average stress of the completely reversed stress cycling, and compressive average stresses give longer lives. Average stress lowers or raises the S-N curve, so that for a given stress amplitude or stress range that can be
allowed is lower if the average stress is tensile, or higher if the average stress is compressive [Dowling, 1993].

Table 3.49 presents the reaction of the three critical factors to the increases in the parameters. Changes in the parameters are also reported in parenthesis in the same table. Table 3.49 can be restated with decreasing parameters assumption, and the reaction of critical factors will have an opposite impact on them (direction of arrows will be opposite).

- Greater values of thermal conductivity, specific heat capacity, spray fluid temperature and heat transfer coefficients for cooling line and die/air interface coefficients all reduced the maximum stress levels as desired.

- However, greater spray fluid temperature reduces the heat removal through spray and greater specific heat capacity (consequently decreasing thermal diffusivity) result in greater die temperatures making them undesirable from a thermal management point of view.

- Greater thermal conductivity, specific heat capacity, cooling line and die/air heat transfer coefficient also reduce the stress range values. This is also desirable. These are the parameters reducing the maximum stress levels, with the exclusion of spray fluid temperature.

- Greater specific heat capacity does not move the average stress values to more compressive levels, and this is not desirable. Thus, it can be excluded from the previous list of critical parameter changes. Greater thermal conductivity, die/air and cooling line heat transfer coefficients are the only ones from the initial list of parameters shown in Table 3.49 with the ability of influencing all the critical factors in the desired way.
<table>
<thead>
<tr>
<th>Parameter (↑)\Factor</th>
<th>Maximum Stress</th>
<th>Stress Range</th>
<th>Average Stress (&lt;0)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal Conductivity (32%)</td>
<td>90% ↓</td>
<td>9% ↓</td>
<td>8% ↑</td>
</tr>
<tr>
<td>Specific Heat Capacity (18%)</td>
<td>22% ↓</td>
<td>9% ↓</td>
<td>16% ↓</td>
</tr>
<tr>
<td>Die Thickness (90%)</td>
<td>127% ↑</td>
<td>-</td>
<td>14% ↓</td>
</tr>
<tr>
<td>Cooling Line Heat Transfer Coefficient (378%)</td>
<td>325% ↓</td>
<td>5% ↓</td>
<td>17% ↑</td>
</tr>
<tr>
<td>Die/Air Heat Transfer Coefficient (1,900%)</td>
<td>1,077% ↓</td>
<td>5% ↓</td>
<td>36% ↑</td>
</tr>
<tr>
<td>Die/Casting Heat Transfer Coefficient (702%)</td>
<td>-</td>
<td>26% ↑</td>
<td>36% ↑</td>
</tr>
<tr>
<td>Spray Fluid Temperature (250%)</td>
<td>25% ↓</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Spray Heat Transfer Coefficient (1,155%)</td>
<td>2,380% ↑</td>
<td>34% ↑</td>
<td>24% ↓</td>
</tr>
<tr>
<td>Cooling Line Fluid Temperature (250%)</td>
<td>22% ↑</td>
<td>-</td>
<td>5% ↓</td>
</tr>
</tbody>
</table>

Table 3.49: Sensitivity matrix ("-" denote either insensitivity or very small percentile change (< 5%) while "." indicates properties with the desired reaction mentioned in page 230,"↑" indicates increases, and "↓" indicates decreases)
• If the change in the parameters in Table 3.49 is assumed as decreases rather than increases, the results indicate that lower spray heat transfer coefficients, lower cooling line temperature and lower die thickness will cause the desired changes in the reaction of the critical factors.

• Greater die/casting heat transfer coefficient values also result in more compressive average stress values. However, they increase the stress range as well. Thus, intermediate values of this factor can be used to keep average stress and stress range values at desired levels.

Since Table 3.49 only reports the absolute response of the three critical factors and the change in parameters are reported and but not included in the study, another table was created. This table, Table 3.50 reports the ratio of % changes in the factors (responses) to % changes in the parameters. Studying these values we can see and evaluate the relative impact of any change can be evaluated in a parameter since each parameter indicated a different range window. Further comparisons using the information from Table 3.50 revealed that increasing thermal conductivity values might be the most influential parameter change to reduce the detrimental structural loading followed by the increased cooling line and die/air heat transfer coefficients. Thermal conductivity values can be controlled through alloying. The other two are controlled through increasing the flow rate and the turbulence consequently. Following Table 3.51 lists the values of the preferred values based on the outcome of the 2-D thermal and structural analyses. The values are either taken from the extremes or an intermediate value as explained above.
<table>
<thead>
<tr>
<th>Parameter/Factor (↑)</th>
<th>Maximum Stress</th>
<th>Stress Range</th>
<th>Average Stress (&lt;0)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal Conductivity</td>
<td>2.850 ↓</td>
<td>0.288 ↓</td>
<td>0.253 ↑</td>
</tr>
<tr>
<td>Specific Heat Capacity</td>
<td>1.230 ↓</td>
<td>0.467 ↓</td>
<td>0.875 ↓</td>
</tr>
<tr>
<td>Die Thickness</td>
<td>1.410 ↑</td>
<td></td>
<td>0.156 ↓</td>
</tr>
<tr>
<td>Cooling Line Heat Transfer Coefficient</td>
<td>0.860 ↓</td>
<td>0.013 ↓</td>
<td>0.044 ↑</td>
</tr>
<tr>
<td>Die/Air Heat Transfer Coefficient</td>
<td>0.567 ↓</td>
<td>0.002 ↓</td>
<td>0.018 ↑</td>
</tr>
<tr>
<td>Die/Casting Heat Transfer Coefficient</td>
<td>-</td>
<td>0.037 ↑</td>
<td>0.052 ↑</td>
</tr>
<tr>
<td>Spray Fluid Temperature</td>
<td>0.100 ↓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Spray Heat Transfer Coefficient</td>
<td>2.060 ↑</td>
<td>0.030 ↑</td>
<td>0.020 ↓</td>
</tr>
<tr>
<td>Cooling Line Fluid Temperature</td>
<td>0.090 ↑</td>
<td></td>
<td>0.021 ↓</td>
</tr>
</tbody>
</table>

Table 3.50: Sensitivity matrix (Responses of the 3 critical factors to changes in parameters)
<table>
<thead>
<tr>
<th>Parameter</th>
<th>Desired Value</th>
<th>High/Low/Intermediate</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal conductivity of H-13</td>
<td>32.25 W/m K</td>
<td>High</td>
</tr>
<tr>
<td>Cooling line heat transfer coefficient</td>
<td>6,000 W/m² K</td>
<td>High</td>
</tr>
<tr>
<td>Die/air heat transfer coefficient</td>
<td>840 W/m² K</td>
<td>High</td>
</tr>
<tr>
<td>Die/casting heat transfer coefficient</td>
<td>40,000 W/m² K</td>
<td>Intermediate</td>
</tr>
<tr>
<td>Specific heat capacity of H-13</td>
<td>543.9 J/kg K</td>
<td>Intermediate</td>
</tr>
<tr>
<td>Spray fluid temperature</td>
<td>25 °C</td>
<td>Intermediate</td>
</tr>
<tr>
<td>Spray heat transfer coefficient</td>
<td>1,000 W/m² K</td>
<td>Low</td>
</tr>
<tr>
<td>Cooling line fluid temperature</td>
<td>10 °C</td>
<td>Low</td>
</tr>
<tr>
<td>Die thickness</td>
<td>0.026 m</td>
<td>Low</td>
</tr>
</tbody>
</table>

Table 3.51: List of preferred values taken from the data reported from the literature (Die thickness represents the location of the cooling line relative to the cavity surface)

The discussion above is based on the idea that the tensile stress is the cause of thermal fatigue damage. The results of the 2-D simulations indicated that the compressive stress due to the thermal shock during filling is far more dominant than the tensile stress created by the air and spray cooling of die cavity surfaces. This finding also matches with the outcomes of other thermal fatigue studies such as the one by Rosbrook [Rosbrook, 1992]. Since there is a question whether high compressive stresses are the causes of thermal fatigue of H-13 die
casting dies, the issue needs to be studied further with experimental determination of the influence of loading with average compressive stresses on the fatigue performance. Table 3.52 lists the average values and the range of values used for the simulations. The values falling within the range presented in the literature resulted in realistic stress and strain levels in the system studied.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Average Values</th>
<th>Range of Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thermal conductivity of H-13 (W/m K)</td>
<td>28.45</td>
<td>24.56 – 32.25</td>
</tr>
<tr>
<td>Cooling line heat transfer coefficient (W/m² K)</td>
<td>3350</td>
<td>1,256 – 6,006</td>
</tr>
<tr>
<td>Die/air heat transfer coefficient (W/m² K)</td>
<td>335</td>
<td>42 - 837</td>
</tr>
<tr>
<td>Die/casting heat transfer coefficient (W/m² K)</td>
<td>41,868</td>
<td>10,010 – 80,311</td>
</tr>
<tr>
<td>Specific heat capacity of H-13 (J/kg K)</td>
<td>543.9</td>
<td>460.2 – 603.2</td>
</tr>
<tr>
<td>Spray fluid temperature (°C)</td>
<td>25</td>
<td>10 - 35</td>
</tr>
<tr>
<td>Spray heat transfer coefficient (W/m² K)</td>
<td>5,024</td>
<td>1,000- 12,560</td>
</tr>
<tr>
<td>Cooling line fluid temperature (°C)</td>
<td>25</td>
<td>10 - 35</td>
</tr>
<tr>
<td>Die thickness (m)</td>
<td>0.0394</td>
<td>0.026 – 0.052</td>
</tr>
</tbody>
</table>

Table 3.52: List of average and range of values taken from the data reported from the literature (Die thickness represents the location of the cooling line relative to the cavity surface)
CHAPTER 4

CASE STUDY: THERMAL, STRUCTURAL AND FATIGUE ANALYSES

4.1 Introduction

The objective of this chapter is to develop a concept for an effective thermal fatigue analysis approach for die-casting dies by conducting and merging thermal, structural and fatigue analyses, and by analyzing real life die-casting applications to support the concept and the outcome of numerical studies.

A fatigue analysis procedure is proposed which consists of the following two steps:

- A stress-strain analysis to determine critical areas in dies and to estimate stress and strain response in these critical areas due to certain loading conditions occurring during processing,
- A damage and fatigue analysis to determine the onset of heat checking.

The analysis to estimate the onset of heat checking involved the following activities in the following sequence:

- Conducting reliable numerical studies can only be realized if there exists a reliable and accurate material property and thermal database. Thus, creation of a H-13 die material property and die-casting thermal database was the first major task of this portion of the study. Material property data can be found in the appendices while thermal data are included within the thermal management chapter, Chapter 2.
• Development of a realistic thermal and subsequent structural finite element model for respective die thermal and stress-strain analyses. Determination of the type of the deformation, elastic or plastic, to recognize the fatigue regime and to diagnose the areas susceptible to heat checking.

• Correlation of material thermal fatigue data with the calculated stress and strain values to predict the number of cycles to the onset of heat checking.

A 3-D model of a real casting application was studied with the information supplied by a collaborating company regarding the dies and the process conditions. Data supplied by the company lacked information making some assumptions necessary. The assumptions were based on the data from the literature or obtained through testing. The simulation results were compared with the actual data given by the company for the diagnosis of the critical areas and for predicting the number of cycles to the onset of heat checking.

The simulations were carried out by numerical analysis tools PROCAST and ABAQUS. PROCAST was utilized in the thermal simulations while ABAQUS was used for sequential thermal/structural analyses. Both PROCAST and ABAQUS software are capable of working with FEM (finite element method) mesh structures created in IDEAS. Both software are advanced tools of FEM analysis avoiding finite difference methods with several disadvantages such as inability of dealing with complex geometries, and temperature dependent material properties and thermal conditions. The same mesh structure was used initially for both PROCAST and ABAQUS analyses. Later, ABAQUS model size was minimized for less computational requirements. The initial goal of using PROCAST was to analyze the actual die/casting system in concern thermally to determine the heat input from the castings to the dies and the consequent thermal fields within the dies. PROCAST does have structural analysis ability even though the stress module was not available to the author. Besides that, PROCAST results cannot be transferred directly to ABAQUS with ease since there is no interface currently available between the two softwares. Before the structural study of tooling through ABAQUS could be realized, another thermal analysis with ABAQUS had to be conducted.
4.2 3-D Thermal and Structural Analyses of an Actual Die-Casting Die with Heat Checking Problem

This section of the dissertation is designated to the modeling and the results of the die/casting system case study. The casting of concern is a two-piece A380 transmission case. H-13 tool steel inserts are used for the operation. Especially the side cores have a severe heat-checking problem starting at as early as 15,000 cycles. Metallurgical reports indicated that there is no loss of carbon (decarburization) from dies surfaces, no evidence of undesired inclusions within the die material, and neither major erosion nor corrosion damage. The only recorded damage is tempering of the die cavity surface and the immediate substrate. This problem became an attractive case for numerical estimation of onset of heat checking, since heat checking was the major damage mechanism.

The following section covers thermal modeling of the die/casting system through PROCAST software.

4.2.1 Thermal Analysis with PROCAST

Finite element model for thermal PROCAST analysis included only the following 5 components of the die/casting system due to complexity of the system and large dimensions of its elements. Elements are as shown in Figure 4.1 and listed below:

1. Casting
2. Ejector insert (with drum core)
3. Pan core
4. Side core
5. Cover insert

The model did not include the die blocks, the tie-bars and the platens. Their impacts on the inserts were not ignored. The interaction between the die-blocks and the inserts were incorporated into the thermal PROCAST model.
Figure 4.1: Geometry of dies
Figure 4.2: Solid model of the die/casting system including the casting

PROCAST model was created using 98,288 4 node tetrahedral elements with 21,297 nodes. Mesh structure for each component of the system was created separately through I-DEAS’ free and local free meshing features. Once the FEM mesh structures were created, they were assembled in MESHCAST. Each insert is made of AISI H-13 tool steel and the casting material is A380 aluminum alloy. The casting alloy is at 655 °C while dies are at 66 °C as default values at the initiation of the simulations. Material property and thermal process databases of PROCAST was used in the simulations without any modifications.

The casting cycle is 90 seconds. The cycle was broken into two major stages assuming that clamping, filling, die opening and ejection actions are all instantaneous:
- Dwell – closed die stage (Solidification and cooling of casting) (30 seconds)
- Open die stage (first open die step, spray cooling, air blowing and second open die steps before the very next cycle) (60 seconds)

![Figure 4.3: Mesh structure for the thermal PROCAST model](image)

For dwell (or closed die) stage, 10 material pairs were created for defining interfaces, which are listed in Table 4.1. During this stage, the inserts, the cores and the casting will be in physical contact at the interfaces described in Table 4.1. Tables 4.2 and 4.3 define the interface conditions regulating the thermal interaction among the elements of the system. The interface heat transfer coefficients described in the following tables are used for the first 30 seconds of the casting cycle, which is the dwell stage. Since all the tooling (inserts and cores) are made of H-13, only two interface conditions were defined: steel-steel for contact of any couple of tools and steel-casting between any of the tooling and the casting.
Table 4.1: Material pair definitions for interface interaction (a: excluded in some models to direct the heat flow paths towards the drum core)

<table>
<thead>
<tr>
<th>Time (Seconds)</th>
<th>Interface Heat Transfer Coefficient (W/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>2,000</td>
</tr>
<tr>
<td>30.0</td>
<td>2,000</td>
</tr>
<tr>
<td>30.0001</td>
<td>0.0</td>
</tr>
<tr>
<td>90.0</td>
<td>0.0</td>
</tr>
</tbody>
</table>

Table 4.2: Steel – steel (H-13 – H-13) thermal interface condition
Interface conditions were described for the thermal interaction in the dwell stage. Besides these interface conditions, 4 thermal and 1 symmetry boundary conditions were also prescribed. Symmetry boundary condition became necessary after simplifying the die/casting system since it became axisymmetric. The dimensions of the actual system were also very large requiring thousands of more finite elements and nodes. Figure 7.1 represents the one half model.
Symmetry plane is YZ as shown in Figure 4.5. The four thermal boundary conditions are as follows and explained below:

<table>
<thead>
<tr>
<th>Time (Seconds)</th>
<th>Interface Heat Transfer Coefficient (W/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>10,000</td>
</tr>
<tr>
<td>30.0</td>
<td>10,000</td>
</tr>
<tr>
<td>30.0001</td>
<td>0.0</td>
</tr>
<tr>
<td>90.0</td>
<td>0.0</td>
</tr>
</tbody>
</table>

Table 4.3: Steel - casting (H-13 – A380) thermal interface condition for the first cycle

- Exterior heat boundary condition
- Interior heat boundary condition
- Cooling line boundary condition
- Shot cylinder boundary condition

Exterior heat boundary condition is prescribed among outer surfaces of the existing tooling elements and the excluded die blocks. It is a steel-to-steel interaction. Since the collaborating company did not report it, various values of die block temperatures were used. Table 4.4 shows the default value used alike other tables for other boundary conditions. Other values used are included in the further sections of this chapter with the results of PROCAST analysis. This boundary condition was applied during the whole cycle.
Figure 4.5: Symmetry plane and other boundary conditions (exterior boundary condition is also applied on the back side of the cover insert)
<table>
<thead>
<tr>
<th>Die Block Temperature</th>
<th>Heat Transfer Coefficient</th>
</tr>
</thead>
<tbody>
<tr>
<td>(°C)</td>
<td>(W/m² K)</td>
</tr>
<tr>
<td>25</td>
<td>2,000</td>
</tr>
</tbody>
</table>

Table 4.4: Exterior heat boundary condition

Interior heat boundary condition is prescribed for the interface surfaces. The interior heat boundary condition defined for the first cycle is shown below in Table 4.5. Once the dies open, these interface surfaces will be open to air (at 30.0001 seconds) assuming instant ejection of the casting. This condition is defined with natural convective heat transfer from interfaces to the ambient. At 42.0001 seconds spray cooling step starts for 11 seconds. After completion of spraying, interfaces are exposed to forced convection through blown air from fans. The last step of the open die stage is the second open die step with natural convective heat transfer from interfaces starting 66.0001 seconds after the cycle starts.

Cooling lines were modeled through the cooling line boundary condition as shown in Table 4.6. Cooling line is always on to remove heat from the drum core. It was modeled as an infinite heat sink with a constant coolant fluid temperature of 30°C.

Shot cylinder boundary condition is prescribed for the thermal interaction of shot cylinder and plunger pair for the first 30 seconds during the dwell stage and shot cylinder and ambient air for the rest of the cycle, which is the open die stage. The prescribed boundary conditions are shown in Table 4.7.
<table>
<thead>
<tr>
<th>Time (Seconds)</th>
<th>Heat Transfer Coefficient (W/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>30.0001</td>
<td>126</td>
</tr>
<tr>
<td>42.0001</td>
<td>126</td>
</tr>
<tr>
<td>42.0001</td>
<td>4,982</td>
</tr>
<tr>
<td>53.0001</td>
<td>4,982</td>
</tr>
<tr>
<td>53.0001</td>
<td>419</td>
</tr>
<tr>
<td>66.0001</td>
<td>419</td>
</tr>
<tr>
<td>66.0001</td>
<td>126</td>
</tr>
<tr>
<td>90.0001</td>
<td>126</td>
</tr>
</tbody>
</table>

Table 4.5: Interior heat boundary condition (Ambient temperature is 25 °C for ambient air, forced air and spray coolant)

<table>
<thead>
<tr>
<th>Time (Seconds)</th>
<th>Heat Transfer Coefficient (W/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>6,000</td>
</tr>
<tr>
<td>90.0</td>
<td>6,000</td>
</tr>
</tbody>
</table>

Table 4.6: Cooling line boundary condition for the first cycle
<table>
<thead>
<tr>
<th>Time (Seconds)</th>
<th>Heat Transfer Coefficient (W/m² K)</th>
<th>Ambient Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>2,000</td>
<td>66</td>
</tr>
<tr>
<td>30.0</td>
<td>2,000</td>
<td>66</td>
</tr>
<tr>
<td>30.0001</td>
<td>126</td>
<td>25</td>
</tr>
<tr>
<td>90.0</td>
<td>126</td>
<td>25</td>
</tr>
</tbody>
</table>

Table 4.7: Shot cylinder boundary condition for the first cycle

4.2.2 Results of PROCAST Analysis

A small BINORM analysis was conducted to determine the number of cycles for the die/casting system to reach the quasi-steady state. BINORM analysis results showed that using 5 cycles was a good approximation for the quasi-steady state. Thus, PROCAST simulations were limited to maximum 5 cycles and in the fifth cycle die/casting system was assumed to be in a quasi-steady state. Since the casting does have small walls, the assumption is reasonable.

As previously mentioned the initial goal of thermal PROCAST analysis was to determine the thermal loads released by the casting during solidification and cooling. To assure the capture of accurate thermal loads and consequent thermal fields within tooling, the actual data supplied by the collaborating company and the PROCAST results were compared. Even though the data supplied by the collaborator lacked some information such as temperature history throughout a whole casting cycle it was still good enough for the comparisons to be made. The supplied data included the portions of actual die/casting system geometry in IGES file format supported by hard copies of additional drawings.
and the available process data. Besides the information mentioned just above, some reliable thermal simulation results and open die temperature measurements were also obtained. This information included the following data for the inserts and the cores:

- the average temperatures of the cavity surface during one full casting cycle at quasi-steady state,
- the temperature range experienced at the cavity surface during one full casting cycle at quasi-steady state,
- the post spray (or pre-injection) temperature fields,
- the open die infrared temperature measurements just prior to spray cooling step.

Some of these data were used for preliminary comparisons. Side core is the most vulnerable component of the system in terms of heat checking. Thus, the temperature fields of the side core cavity surface prior to injection and spray activities were chosen for comparisons. Data chosen are as shown in the following tables, Table 4.8 and 4.9, as temperature ranges. Hotter areas of the side core cavity surface are described as higher temperature fields while the opposite is represented with the term, lower temperature fields. The temperature measurements reported by the collaborator had an accuracy of 10 °C.

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>93 - 116</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>116 - 160</td>
</tr>
</tbody>
</table>

Table 4.8: Actual temperatures of the side core cavity surface for pre-injection (post spray cooling)
Areas | Temperature Range (°C)
---|---
Lower temperature fields | 197 - 238
Higher temperature fields | 238 - 265

Table 4.9: Actual temperatures of the side core cavity surface prior to spray cooling step

Figures 4.6 and 4.7 shows the thermal fields belonging to side core cavity prior to injection and spray instants respectively. The results of PROCAST thermal analysis are also presented below through tables. Tables should be studied in couples since interchangeably one table represents the figure for the pre-injection while the other is for the pre-spray instant.

Areas | Temperature Range (°C)
---|---
Lower temperature fields | 88 - 105
Higher temperature fields | 105 - 121

Table 4.10: PROCAST analysis results for the pre-injection instant with initial die temperature of 66 °C, forced air convective heat transfer coefficient of 419 W/m² K for 5 cycles

Areas | Temperature Range (°C)
---|---
Lower temperature fields | 99 - 118
Higher temperature fields | 118 - 155

Table 4.11: PROCAST analysis results for the pre-spray instant with initial die temperature of 66 °C, forced air convective air heat transfer coefficient of 419 W/m² K for 5 cycles
<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>107 - 124</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>124 - 141</td>
</tr>
</tbody>
</table>

Table 4.12: PROCAST analysis results for the pre-injection instant with initial die temperature of 100 °C, forced air convective air heat transfer coefficient of 419 W/m² K for 5 cycles

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>117 - 155</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>155 - 174</td>
</tr>
</tbody>
</table>

Table 4.13: PROCAST analysis results for the pre-spray instant with initial die temperature of 100 °C, forced air convective heat transfer coefficient of 419 W/m² K for 5 cycles

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>89 - 105</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>105 - 121</td>
</tr>
</tbody>
</table>

Table 4.14: PROCAST analysis results for the pre-injection instant with initial die temperature of 66 °C, forced air convective heat transfer coefficient of 1256 W/m² K for 5 cycles
<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>100 – 119</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>119 – 157</td>
</tr>
</tbody>
</table>

Table 4.15: PROCAST analysis results for the pre-spray instant with initial die temperature of 66 °C, forced convective heat transfer coefficient of 1256 W/m² K for 5 cycles

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>107 – 124</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>124 – 140</td>
</tr>
</tbody>
</table>

Table 4.16: PROCAST analysis results for the pre-injection instant with initial die temperature of 100 °C, forced air convective heat transfer coefficient of 419 W/m² K for 3 cycles

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>112 – 130</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>130 – 167</td>
</tr>
</tbody>
</table>

Table 4.17: PROCAST analysis results for the pre-spray instant with initial die temperature of 100 °C, forced air convective heat transfer coefficient of 419 W/m² K for 3 cycles
Figure 4.6: Temperature field on the side core prior to injection (Results of this simulation are summarized in Table 4.16)
Figure 4.7: Temperature field on the side core prior to injection (Results of this simulation are summarized in Table 4.17)
<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>126 - 143</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>143 - 159</td>
</tr>
</tbody>
</table>

Table 4.18: PROCAST analysis results for the pre-injection instant with initial die temperature of 120 °C, forced air convective heat transfer coefficient of 419 W/m² K for 5 cycles

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>114 - 132</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>132 - 168</td>
</tr>
</tbody>
</table>

Table 4.19: PROCAST analysis results for the pre-spray instant with initial die temperature of 120 °C, forced air convective heat transfer coefficient of 419 W/m² K for 5 cycles

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>109 - 126</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>126 - 144</td>
</tr>
</tbody>
</table>

Table 4.20: PROCAST analysis results for the pre-injection instant with initial die temperature of 100 °C, forced air convective heat transfer coefficient of 419 W/m² K for 3 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores)
Areas | Temperature Range (°C)
--- | ---
Lower temperature fields | 119 - 139
Higher temperature fields | 139 - 178

Table 4.21: PROCAST analysis results for the pre-spray instant with initial die temperature of 100 °C, forced air convective heat transfer coefficient of 419 W/m² K for 3 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores)

Areas | Temperature Range (°C)
--- | ---
Lower temperature fields | 101 - 144
Higher temperature fields | -

Table 4.22: PROCAST analysis results for the pre-injection instant with initial die temperature of 100 °C, forced air convective heat transfer coefficient of 419 W/m² K for 5 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores)

Areas | Temperature Range (°C)
--- | ---
Lower temperature fields | 123 - 144
Higher temperature fields | 144 - 182

Table 4.23: PROCAST analysis results for the pre-spray instant with initial die temperature of 100 °C, forced convective heat transfer coefficient of 419 W/m² K for 5 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores)
Table 4.24: PROCAST analysis results for the pre-injection instant with initial die temperature of 100 °C, forced convective air heat transfer coefficient of 419 W/m$^2$ K for 10 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores) (air heat transfer coefficient 38 W/m$^2$ K, initial casting temperature 660 °C and die block temperature 70 °C)

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>97 – 114</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>114 – 147</td>
</tr>
</tbody>
</table>

Table 4.25: PROCAST analysis results for the pre-spray instant with initial die temperature of 100 °C, forced convective air heat transfer coefficient of 419 W/m$^2$ K for 10 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores) (air heat transfer coefficient 38 W/m$^2$ K, initial casting temperature 660 °C, die block temperature 70 °C)

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>118 - 138</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>138 - 178</td>
</tr>
</tbody>
</table>

Table 4.26: PROCAST analysis results for the pre-injection instant with initial die temperature of 100 °C, forced convective air heat transfer coefficient of 419 W/m$^2$ K for 5 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores) (air heat transfer coefficient 38 W/m$^2$ K, initial casting temperature 677 °C, die block temperature 70 °C, cooling line heat transfer coefficient 4187 W/m$^2$ K)

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>102 - 123</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>123 - 144</td>
</tr>
<tr>
<td>Areas</td>
<td>Temperature Range (°C)</td>
</tr>
<tr>
<td>--------------------</td>
<td>------------------------</td>
</tr>
<tr>
<td>Lower temperature fields</td>
<td>102 - 123</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>123 - 208</td>
</tr>
</tbody>
</table>

Table 4.27: PROCAST analysis results for the pre-spray instant with initial die temperature of 100 °C, forced convective air heat transfer coefficient of 419 W/m² K for 5 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores) (air heat transfer coefficient 38 W/m² K, initial casting temperature 677 °C, die block temperature 70 °C, cooling line heat transfer coefficient 4187 W/m² K)

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>107 - 125</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>125 - 143</td>
</tr>
</tbody>
</table>

Table 4.28: PROCAST analysis results for the pre-injection instant with initial die temperature of 100 °C, forced convective air heat transfer coefficient of 419 W/m² K for 5 cycles (no interaction assumption between ejector and cover inserts was made to direct the heat flow towards the side and drum cores) (air heat transfer coefficient 38 W/m² K, initial casting temperature 677 °C, die block temperature 100 °C, cooling line heat transfer coefficient 4187 W/m² K)

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>115 - 134</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>134 - 172</td>
</tr>
</tbody>
</table>

Table 4.29: PROCAST analysis results for the pre-spray instant with initial die temperature of 100 °C, forced convective air heat transfer coefficient of 419 W/m² K for 5 cycles (no interaction between the ejector and cover inserts to direct the heat flow towards the side and drum cores) (air heat transfer coefficient 38 W/m² K, initial casting temperature 677 °C, die block temperature 100 °C, cooling line heat transfer coefficient 4187 W/m² K)
The actual data and the outcome of the PROCAST analysis had similar thermal fields with similar contour patterns. Some PROCAST simulations were conducted for only 3 cycles while others were for 5 and 10 cycles to see the effect of the number of simulations on the results. The outcome indicated very little effect of the number of cycles on the temperature distribution.

Thermal PROCAST analysis showed that none of the cases mentioned in the previous tables produced matching temperature fields for the pre-spray instant. Almost all cases yielded matching temperature fields for the pre-injection data. As mentioned previously, the geometry of the die/casting system was simplified causing some of the details to be lost even though the model used was a good representative of the die/casting system geometrically. This was the major contributing factor for the existence of the differences in the thermal analysis results. Although, the difference caused the thermal analysis to be repeated with ABAQUS, it gave a good picture of the influence of some parameters on the die thermal fields. It also assisted in identifying the thermal zones of the casting. Studying thermal field results with PROCAST, the following 6 regions in casting defined as seen in Figure 4.8.

- Thick section
- Thin section
- Extension
- Gate
- Runner
- Biscuit
4.2.3 Thermal-Structural Analysis with ABAQUS

A 3-D thermal-stress analysis was conducted within the real dies to identify the type and magnitude of fatigue damage, to locate the areas susceptible to heat checking and finally to find a better way of running them for the delayed onset of heat checking. This section includes both the sources of thermal and mechanical loading in the structural study of the tooling. ABAQUS models consisted of only the following 4 components of the die/casting system due to complexity of the system and large dimensions of its elements:

- Cover insert
- Ejector insert (with drum core)
- Side core
- Pan core

![Diagram](image.png)

Figure 4.8: Thermal regions in the casting
The models did not include the die blocks, the tie-bars, the platens and the casting. Even though these components were left out of the models to simplify and to minimize the size of the models, their impact on the inserts were not ignored. In some cases, such as clamping the impact was simplified and modeled as a uniformly distributed pressure. The interaction between the die-blocks and the inserts (both thermally and structurally) were incorporated into the models. The thermal impact of the casting was also included as thermal loads in the thermal ABAQUS models.

Initial ABAQUS models included identical mesh structures to the ones used in PROCAST analysis. PROCAST model was created using 98,288 4 node tetrahedral elements having total number of 21,297 nodes. Later on, this structure was replaced with a coarser one with 60,450 elements and 12,613 nodes to reduce the computational requirements for simulations. Mesh structure for each element of the system was created separately in I-DEAS. Free and local free meshing features were used. Once the FEM mesh structures were completed and exported out of I-DEAS as ABAQUS input files, the ABAQUS input files were merged to one final ABAQUS input file. Dies are made from AISI H-13. Average values of thermal and mechanical properties of H-13 were used initially as it was in 2-D thermal-stress analysis. These values can be found in the listings of the ABAQUS codes in the appendices. Later in this chapter (Section 4.2.5), a study of mechanical properties was included to give information about the actual mechanical properties of the die material due to cycling and temperature issues. Tooling is assumed to be 100 °C at the beginning of the simulations since earlier PROCAST analyses suggested that lower initial die temperatures were not high enough to reach the actual quasi-steady state temperature levels.

The ABAQUS casting cycle was broken into discrete steps. The steps are as shown in Table 4.8. The time interval between the injection and the ejection of the casting, or the dwell stage is 30 seconds and the total cycle time is 90 seconds.
Table 4.30: Break-up of the very first casting cycle (Clamping, filling and ejection activities are all assumed to be momentary. Dies are clamped immediately at the initiation of the cycle)

The dwell stage was modeled with the heat input from the casting to the dies, as thermal loads on die cavity surfaces. Interface conditions prescribed in PROCAST modeling was not a part of the dwell stage in thermal ABAQUS models since PROCAST results indicated very little interaction along the interfaces. Therefore no thermal interaction along the interface assumption was made.

The thermal loads upon which the dies are exposed were computed by first simulating the die geometry in PROCAST and using the outputs of this simulation to estimate them. The data obtained from the collaborating company were also used to estimate $K$ and $m$ values. The thermal loads were calculated using $K$ and $m$ values and they were applied onto the cavity surfaces. The loads were calculated using the following approach developed at the Ohio State University by Dr. Allen Miller's research group. The group has contributed in many areas of the die-casting process simulation including the die deflections and the structural state of dies.
The simplest approach to estimate die temperatures consists of carrying out a thermal analysis for which the magnitude of the released heat from solidification and cooling of the casting is prescribed. Various experimental studies have shown that heat is released by the casting into the dies approximately at an exponentially decaying rate. Padiyar utilized this behavior to model the thermal loads upon the dies. His assumption was that the heat escaping from the casting flows along a perpendicular path into the cavity surface, and also the casting is very thin [Padiyar et al, 1994].

The total heat \( Q_{\text{total}} \) released by the casting in question for the part configuration is given by the following equation,

\[
Q_{\text{total}} = m_{\text{casting}} c_p, l (T_{\text{injection}} - T_{\text{liquidus}}) + m_{\text{casting}} L + m_{\text{casting}} c_p, s (T_{\text{solidus}} - T_{\text{ejection}}) \tag{4.1}
\]

where:

\( T_{\text{injection}} \): Bulk temperature of the cast metal (655 °C)
\( T_{\text{liquidus}} \): Liquidus temperature of the cast metal (581 °C)
\( T_{\text{solidus}} \): Solidus temperature of the cast metal (540 °C)
\( T_{\text{ejection}} \): Bulk ejection temperature of the casting (location dependent)
\( m_{\text{casting}} \): Mass of the casting
\( c_p, l \): Specific heat of the liquid metal (1,120 J/kg K)
\( c_p, s \): Specific heat of the solid metal (693 J/kg K)
\( L \): Latent heat of solidification (456,710 J/kg)

It can be first assumed that the heat flux at the time of ejection has decayed to around \( 1/100^{th} \) of the initial value at the time of injection. Figure 4.9 shows the assumed heat input flux history. Based on this assumption, the heat flux is estimated using the following expressions:

\[
q'(t) = K 10^{m t} \tag{4.2}
\]
where $K, m$: constants which are determined numerically

$q'(t)$: heat flux as a function of time ($W/m^2$)

\[
\text{slope} = m
\]

Figure 4.9: Assumed heat input flux history [Dedhia, 1997]

\[
q'(t_{\text{eject}}) = q'(t_{\text{inject}})/X \quad (4.3)
\]

where $X$ lies within the range of 50 to 100

The value of $X$ determines how fast is the heat added to the die as a function of time. A greater $X$ value would add heat rapidly during initial cycle time and then heat input decreases rapidly later in the cycle while a lower value of $X$ would add heat at a constant rate during the whole the cycle. $X$ values of 50 and 75 were used besides 100.
The amount of total heat flow, $Q_{\text{total}}$, can be found by integrating the heat flux function, $q'(t)$ within the cycle time period and multiplying it with the total die cavity surface area ($A_{\text{die}}$) in contact with the casting.

Given a total heat release, $Q_{\text{total}}$, for a given region, constants $K$ and $m$ in equation can be computed based on the constraints imposed by the following equations:

$$K = \frac{Q_{\text{total}}m \ln(10)}{A_{\text{die}}(10^m - 10^{m-\epsilon})} \quad (4.4)$$

$$m = -\frac{\log_{10}(X)}{t_{\text{eject}} - t_{\text{inject}}} \quad (4.5)$$

The accuracy of the heat load approximation depends on the values used for $K$ and $m$.

In calculating the heat fluxes, the casting is divided into several thermal zones and the heat flux is calculated by applying Equation 4.1 to each thermal zone. For each region a different value for $T_{\text{ejection}}$ is assumed. The ejection temperatures for each zone can be obtained by thermal simulation in PROCAST. The total amount of heat released by the casting can be estimated integrating the heat flux from Equation 4.2 and multiplying the result with the total die surface area. The data obtained from the collaborating company yielded the die cavity surface temperatures right after dies open. These temperature values were also used as casting ejection temperatures knowing that the die and the casting surfaces will be in thermal balance before the dies open. As previously mentioned in this chapter, PROCAST results showed that $K$ and $m$ values could be calculated more realistically through the actual data supplied by the collaborator. Therefore, PROCAST results were only used to break the casting into thermal zones. These $K$ and $m$ values are used in ABAQUS thermal simulations. Thermal patterns will then be compared with the actual data, and the values of $K$ and $m$ can be modified if significant differences exist. This process is repeated until temperature fields in both simulations, the one with the casting and the one without the casting but with the heat load to substitute it are similar.
Besides the thermal loading from castings into the dies, 2 thermal and 1 symmetry boundary conditions were also prescribed for the dwell stage. Symmetry boundary condition was previously explained in this chapter and is shown in Figure 4.5. The two thermal boundary conditions are as follows and explained below:

- Exterior heat boundary condition
- Cooling line boundary condition

Exterior heat boundary condition is same as the one utilized in PROCAST modeling. It defines the thermal interaction between the elements of the FEM model and the die blocks, which were excluded from the model. This boundary condition represents a steel-steel interaction. The same values from PROCAST analysis were used and they can be found also in the following table.

<table>
<thead>
<tr>
<th>Die Block Temperature (°C)</th>
<th>Heat Transfer Coefficient (W/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
<td>2,000</td>
</tr>
</tbody>
</table>

Table 4.31: Exterior heat boundary condition

Cooling lines were modeled through the condition shown in Table 4.32. Since the lines are always on, the same boundary condition is also applied to the rest of the cycle.

After the dwell stage was defined, the open die stage was broken into four steps as shown previously in Table 4.30. These steps were represented with 3 thermal and 1 symmetry boundary conditions. The symmetry boundary condition is same as the
previous ones since the same model is used as the other PROCAST and ABAQUS modeling. The 3 thermal boundary conditions used were:

<table>
<thead>
<tr>
<th>Time (Seconds)</th>
<th>Heat Transfer Coefficient (W/m² K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>6,000</td>
</tr>
<tr>
<td>30.0</td>
<td>6,000</td>
</tr>
</tbody>
</table>

Table 4.32: Cooling line boundary condition for the first cycle (Coolant fluid temperature was 30 °C)

- Exterior heat boundary condition
- Interior heat boundary condition
- Cooling line boundary condition

The exterior heat and cooling line boundary conditions are also same as the ones from the dwell stage with the only difference that they are valid for the rest of the cycle. Interior heat boundary condition was also similar to the one from the PROCAST analysis and was described in Table 4.5.

To model the mechanical loading and structural restraints to which die-casting dies are exposed, a realistic model of the die-casting machine and die system must be established. Following section explains the structural analysis issues [Dedhia, 1997] and the model used in this study. A symbolic sketch of a cold chamber machine is shown in Figure 4.10.
Figure 4.10: Schematic showing a cross section of a cold chamber die-casting machine [Sully, 1988]
In reality, die blocks are attached to the die-casting machine by clamps and the inserts are attached to the dies by bolts. These boundary conditions could be applied in ABAQUS by using TIE command. With this feature the nodes on the two surfaces are tied together to have same degrees of freedom, which causes these pairs of nodes to move together.

During modeling of cover and ejector platen, spring elements can be used. In case of a linear spring, a constant value of stiffness is provided and in the case of non-linear spring, the force is applied as a function of relative displacement in the spring and the spring is defined by giving force and relative displacement values in ascending order of relative displacement. Another approach could also assume the cover platen is a rigid body.

Interfaces between different components could be modeled in ABAQUS by using contact surface feature. The concept of contact surface is shown in Figure 4.10. To define contact surfaces, ABAQUS uses a master-slave concept to enforce contact constraint. The contact direction is normal to the master surface. The nodes of the master surface can penetrate into the slave surface, but the nodes of the slave surface don't have the ability to penetrate into the master surface. Generally master surface should be chosen as the surface with stiffer body if the materials are different, or as the surface with the coarser mesh structure if the materials are same. When defining contact surface between two bodies of which only one is rigid, then the rigid surface must be the master surface [Dedhia, 1997].
ABAQUS has the ability of modeling surface contact in different ways. Different models may be used to facilitate the numerical solution at the expense of the accuracy. In the default model, labeled hard contact, full contact pressure is developed at the moment that contact is detected, regardless of how localized or intermittent this contact is. When the clearance between contact surfaces is positive, there is no contact and consequently no pressure being transmitted. In the over closed model, no contact is reached until contact surfaces overlap by a distance. Surfaces do not separate until a normal tensile stress is generated among them. The last model presents a softened condition, in which contact pressure increases exponentially as mating surfaces get closer to each other. This model is particularly suitable for case where a coating on one or both contacting surfaces exists [Dedhia, 1997]

During the clamping of dies a compressive stress state exists at the parting plane. This compressive stress provides the necessary compressive force to keep die halves together. This force is the pre-load. ABAQUS is a finite element code with the ability to predict contact stresses at the interface of the contacting bodies.
Structural modeling a die casting operation must include the effect of clamping force and cavity pressure in addition to the thermal loads. The clamping force determines the magnitude and nature of the pre-stress. The cavity pressure along with the thermal loads determines the parting plane separation of the die halves and also, contributes to the stress state within the dies. The parting plane separation may cause flushing. The two commonly used two types of design are: insert that flush with the die surface at the parting plane, inserts that protrude from the die surface at the parting plane. It is believed that when the inserts are allowed to emerge from the pocket, they generate a higher pre-load as compared to the case with flush inserts [Dedhia, 1997].

The structural model used in this study was a simple model. As previously mentioned, it lacked the die blocks, the tie-bars and the platens. The model was simplified to avoid the actual complexity of the system and minimize the computational requirements. The elements of the model are shown in Figure 4.12. The boundary conditions of the model are as follows and can also be seen from Figure 4.12 as well:

- Cavity pressure on the die cavity surfaces
- Clamping pressure on the back surface of the ejector insert (applied immediately after the start of the cycle)
- Restraints on the back surface of the cover insert (translation in z-direction was restrained)
- Symmetry boundary condition with restraints on the symmetry plane (YZ) plane (translation in x-direction and rotations in y and z directions were restrained)
- Local restraints on the top, bottom and left (opposite side to the symmetry surface) surfaces of the ejector insert (translation in y-direction was restrained for the top and bottom surfaces while translation in x direction was not allowed for the left surface)
- Local restraints on the top, bottom and left (opposite side to the symmetry surface) surfaces of the cover insert. (translation in y-direction was restrained for the top and bottom surfaces while translation in x direction was not allowed for the left surface)
• Local restraints on the top, bottom and left (opposite side to the symmetry surface) surfaces of the side core. (translation in y-direction was restrained for the top and bottom surfaces while translation in x direction was not allowed for the left surface)

• Local restraints on the top surface of the pan core (translation in y-direction was restrained)

• Contact surface interaction between the interface surfaces defined in Figure 4.4.

During the casting process cavity pressure and clamping force are the only sources of mechanical loading. Cavity pressure was assumed to be applied uniformly over the element faces in the cavity. It is also assumed that the maximum cavity pressure would be reached at the completion of the die cavity fill (at the fill time). After the peak pressure is reached, pressure reduces linearly and is set to zero at the end of the dwell period when dies open. This assumption is shown in Figure 3.19. Fill time was calculated considering the smallest wall thickness of the casting through Equation 3.1. Cavity pressure was calculated using the data obtained from the collaborator. The average accumulator pressure was given as 9.5 MPa. With the assumption that the diameter of cylinder to diameter of plunger ratio is 1.5, the maximum metal pressure in the cavity was calculated through the following relation:

\[ P_m = P_a \left( \frac{A_{cyl}}{A_p} \right) \]  \hspace{1cm} (4.6)

where \( P_m \): is the maximum metal pressure

\( P_a \): Accumulator pressure

\( A_{cyl} \): Cross-sectional area of the cylinder

\( A_p \): Cross-sectional area of the plunger

Results yielded 105.2 milliseconds and 21.4 MPa respectively.

Clamping forces are also distributed evenly at the back of the ejector insert as a uniform pressure field. Clamping force was estimated 10% over the peak cavity pressure.
to prevent die opening during injection. Locking pressure was found as 3.56 MPa through using Equation 3.3 and dividing the locking force by the back surface area of the ejector insert.

Since the cover insert is fixed, restraints on the back of the cover insert were applied. This boundary condition allowed no translation in z direction for all the nodes, which are on the back surface of the cover.

Symmetry condition became necessary once the actual system was converted into an axisymmetric one as it was for the thermal PROCAST and ABAQUS models. Some of the nodes which are on the top and the bottom of the cover insert, the ejector insert, and the side core were not allowed move in y direction while some left surface nodes of these elements were restrained in x direction assuming that all these inserts will be restrained by the die blocks which are holding them inside. One section of the top surface of the pan core was also restrained in y direction through the same assumption.

Contact surfaces were defined knowing that all the tooling is made from the same material and each element carried a different mesh structure. Elements with coarser mesh structure were chosen as master surfaces. Default contact interaction, hard labeled contact, was used between any two tooling elements. Table 4.33 shows the average mesh
Table 4.33: Element sizes

<table>
<thead>
<tr>
<th>Element</th>
<th>Average mesh size (m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ejector insert</td>
<td>0.02286</td>
</tr>
<tr>
<td>Side core</td>
<td>0.02032</td>
</tr>
<tr>
<td>Pan core</td>
<td>0.04316</td>
</tr>
<tr>
<td>Cover insert</td>
<td>0.02546</td>
</tr>
</tbody>
</table>

Figure 4.12: Elements and boundary conditions of the structural ABAQUS model
Table 4.34: Master-slave surface definitions

<table>
<thead>
<tr>
<th>Master Surface</th>
<th>Slave Surface</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ejector insert</td>
<td>Side core</td>
</tr>
<tr>
<td>Pan core</td>
<td>Ejector insert</td>
</tr>
<tr>
<td>Cover insert</td>
<td>Side core</td>
</tr>
<tr>
<td>Pan core</td>
<td>Cover insert</td>
</tr>
<tr>
<td>Cover insert</td>
<td>Ejector insert</td>
</tr>
<tr>
<td>Pan core</td>
<td>Side core</td>
</tr>
</tbody>
</table>

Size for each element while Table 4.34 indicates master-slave definitions used in structural ABAQUS modeling. The elements of the tooling were assumed to be free of residual stresses.

4.2.4 Results of Thermal ABAQUS Analysis

Since there were some differences between the PROCAST results and the actual data supplied by the collaborator, it was decided to utilize the actual data to create the thermal loads for thermal ABAQUS analysis. Following tables include the constants, $K$ and $m$, for the thermal load calculations. The values of these constants were obtained using the temperature measurements at the initiation of the open die stage obtained by the collaborator. These measured values were used as casting ejection temperatures knowing that the casting and the die cavity surface will be in thermal balance when the dies open. Three $X$ values: 50, 75, and 100 were utilized. As stated previously, the casting was divided into 6 thermal zones upon the completion of PROCAST analysis.
<table>
<thead>
<tr>
<th>Thermal zone of the casting</th>
<th>Dwell Stage (Solidification and Cooling Steps)</th>
<th>m (sec⁻¹)</th>
<th>K (W/m²) x 10⁶</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thick region</td>
<td></td>
<td>-0.05663</td>
<td>1.80</td>
</tr>
<tr>
<td>Thin region</td>
<td></td>
<td>-0.05663</td>
<td>0.62</td>
</tr>
<tr>
<td>Extension</td>
<td></td>
<td>-0.05663</td>
<td>0.59</td>
</tr>
<tr>
<td>Gate</td>
<td></td>
<td>-0.05663</td>
<td>1.15</td>
</tr>
<tr>
<td>Runner</td>
<td></td>
<td>-0.05663</td>
<td>1.21</td>
</tr>
<tr>
<td>Biscuit</td>
<td></td>
<td>-0.05663</td>
<td>3.93</td>
</tr>
</tbody>
</table>

Table 4.35: K and m values for X=50

<table>
<thead>
<tr>
<th>Thermal zone of the casting</th>
<th>Dwell Stage (Solidification and Cooling Steps)</th>
<th>m (sec⁻¹)</th>
<th>K (W/m²) x 10⁶</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thick region</td>
<td></td>
<td>-0.0625</td>
<td>1.98</td>
</tr>
<tr>
<td>Thin region</td>
<td></td>
<td>-0.0625</td>
<td>0.68</td>
</tr>
<tr>
<td>Extension</td>
<td></td>
<td>-0.0625</td>
<td>0.65</td>
</tr>
<tr>
<td>Gate</td>
<td></td>
<td>-0.0625</td>
<td>0.13</td>
</tr>
<tr>
<td>Runner</td>
<td></td>
<td>-0.0625</td>
<td>1.33</td>
</tr>
<tr>
<td>Biscuit</td>
<td></td>
<td>-0.0625</td>
<td>4.30</td>
</tr>
</tbody>
</table>

Table 4.36: K and m values for X=75

286
Thermal zone of the casting

<table>
<thead>
<tr>
<th>Thermal zone of the casting</th>
<th>Dwell Stage (Solidification and Cooling Steps)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>m (sec^{-1})</td>
</tr>
<tr>
<td>Thick region</td>
<td>-0.06667</td>
</tr>
<tr>
<td>Thin region</td>
<td>-0.06667</td>
</tr>
<tr>
<td>Extension</td>
<td>-0.06667</td>
</tr>
<tr>
<td>Gate</td>
<td>-0.06667</td>
</tr>
<tr>
<td>Runner</td>
<td>-0.06667</td>
</tr>
<tr>
<td>Biscuit</td>
<td>-0.06667</td>
</tr>
</tbody>
</table>

Table 4.37: K and m values for X=100

Spray heat transfer coefficient was reduced to 2,982 W/m²K to increase the minimum temperature of cavity surface temperature history of the side core. Only 3 cycles were used to limit the time spent and the amount of data produced by simulating since previous PROCAST results did not report major differences between 5^{th} and the 3^{rd} cycles.

Before presenting the results of the ABAQUS thermal analysis, the data supplied by the collaborator is presented. The actual data supplied by the collaborating company were already mentioned in the PROCAST modeling section. However, following factors from the supplied data were used to make comparisons between the actual data and the ABAQUS simulation results. All the factors chosen are related to the side core cavity since the side core is the element under the major heat checking threat during die-casting operation in concern.

- The temperature distribution of the side core cavity surface prior to the initiation of spray
- The temperature distribution of the side core cavity surface prior to the injection
The temperature range that is observed by the side core cavity surface during a whole quasi-steady state cycle

The average temperature that is observed by the side core cavity surface during a whole quasi steady state cycle

Following pages summarize the data supplied by the collaborator. The data were simplified and presented in the format below for better understanding of die surface cavity thermal fields. As mentioned earlier, the main concern was to study the side core cavity surface. The side core cavity geometry is shown in Figure 4.13. Geometry of the model is a simplified representative of the actual die/casting system leading to elimination of some details of the casting and the die cavity surfaces. Earlier PROCAST analyses suggested the break-up of the side core cavity surface into several regions. These regions are also shown in Figure 4.13.

The temperature distribution prior to the spray was obtained through infrared measurements. However, the actual time instant for this temperature distribution of the side core was the very time instant prior to the initiation of the spray.

The data from the infrared measurements were summarized through a bar chart and the bar chart indicated two major temperature ranges for the regions of the side core cavity surface as shown in Table 4.38. The thin region had the lower range while the other two cavity surface regions: the thick region and the extension surface experienced the higher thermal fields.

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thin region</td>
<td>197 - 238</td>
</tr>
<tr>
<td>Thick region</td>
<td>238 - 265</td>
</tr>
<tr>
<td>Extension surface</td>
<td>238 - 265</td>
</tr>
</tbody>
</table>

Table 4.38: Actual temperatures of the side core cavity surface for prior to spray
Figure 4.13: Side core cavity with its 3 different regions

Pre-injection temperatures, which are presented below in Table 4.39 and 4.40, were also obtained from the collaborator. All these data supplied by the collaborator are the product of their thermal simulation results confirmed by the other data available to them.

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>93 - 116</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>116 - 138</td>
</tr>
</tbody>
</table>

Table 4.39: Actual temperatures of the side core cavity surface for the thin region for pre-injection (post spray step)
<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower temperature fields</td>
<td>116 - 138</td>
</tr>
<tr>
<td>Higher temperature fields</td>
<td>138 - 160</td>
</tr>
</tbody>
</table>

Table 4.40: Actual temperatures of the side core cavity surface for the thick region and the extension surface for pre-injection (post spray cooling step) on the side core cavity surface

Temperature ranges were obtained from the contour plots supplied by the collaborator. The results indicated a similar trend compared with the pre-injection and the pre-spray thermal fields. Ranges for extension and thick regions were same since they were exposed to similar heat flux inputs as thermal loads. Previous PROCAST analyses also confirmed this. The thin region indicated the greatest temperature range. Even though heat content the thin region exposed to was limited, it was still enough to bring the surface temperatures of the region up to an extent while limited heat input was removed by the impact of the spray bringing the surface to lower temperatures compared with the other regions.

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature Range (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thin region</td>
<td>182 - 204</td>
</tr>
<tr>
<td>Thick region</td>
<td>160 - 182</td>
</tr>
<tr>
<td>Extension surface</td>
<td>160 - 182</td>
</tr>
</tbody>
</table>

Table 4.41: Actual temperature ranges of the side core cavity surface for a whole quasi-steady state cycle
Average cavity surface temperatures for a whole quasi-steady state cycle were also obtained. The values are shown in Table 4.42.

<table>
<thead>
<tr>
<th>Areas</th>
<th>Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thin region</td>
<td>160</td>
</tr>
<tr>
<td>Thick region</td>
<td>182</td>
</tr>
<tr>
<td>Extension surface</td>
<td>204</td>
</tr>
</tbody>
</table>

Table 4.42: Average temperatures of the side core cavity surface for a whole quasi-steady state cycle

The data obtained by the collaborator showed that thin region was exposed to the highest temperature changes even though average temperatures in this region is not as high as the other two regions due to spray cooling and exposure to limited heat content of the corresponding portion of the casting. The thick region and the extension surface experienced same temperature ranges due to exposure to similar heat contents. The extension surface had the higher average temperature of the two and the highest average of the all three due to its proximity to the cooling line. Cooling line’s impact on the extension surface was minimized since the lines were not extending up to the extension surface and the ejector/cover insert interface was hindering the heat flow from the extension surface to the cooling lines. The outcome of the thermal ABAQUS simulation results suggested that the thin region could further be divided into other regions. On the contrary, the thick region and extension surface did not show any variation within them. The thin region was divided into:

- The thin region near the thick region
- The thin region near the pan core/symmetry plane
- The thin region away from the thick region
The thin region near the extension surface behaved like the extension surface. Since the extension surface portion of the model had to be altered from the actual geometry because of the limitations on the data supplied by the collaborator. Thus, the extension surface and the thin region near the extension surface were not included in the comparisons.

Figure 4.14: Sub regions of the thin region

Following figure shows a set of thermal cycles experienced by the thick region for X=75 as determined by ABAQUS simulations. Figure 4.15 also includes the critical temperatures, which were utilized in the comparisons.
Figure 4.15: The thermal cycle experienced by the thick region (X=75)

Comparisons of the actual data with ABAQUS simulation results are presented in the next two pages through tables. Upper and lower limit represents the extreme values taken from the actual data. Values in bold are the simulation results falling within the ranges marked by these extremes. If none of the values from ABAQUS simulations is within the range, the closest to the actual values is marked.
### Table 4.43: Comparison of the temperature fields - the thick region

<table>
<thead>
<tr>
<th>Case</th>
<th>Temperature (°C)</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>T&lt;sub&gt;1&lt;/sub&gt;</td>
<td>T&lt;sub&gt;2&lt;/sub&gt;</td>
<td>T&lt;sub&gt;range&lt;/sub&gt;</td>
<td>T&lt;sub&gt;average&lt;/sub&gt;</td>
</tr>
<tr>
<td>X=50</td>
<td>275</td>
<td>160</td>
<td>167</td>
<td>219</td>
</tr>
<tr>
<td>X=75</td>
<td>265</td>
<td>152</td>
<td>162</td>
<td>210</td>
</tr>
<tr>
<td>X=100</td>
<td>255</td>
<td>147</td>
<td>160</td>
<td>214</td>
</tr>
<tr>
<td>Lower limit</td>
<td>238</td>
<td>116</td>
<td>160</td>
<td>182</td>
</tr>
<tr>
<td>Upper limit</td>
<td>265</td>
<td>160</td>
<td>182</td>
<td>182</td>
</tr>
</tbody>
</table>

(T<sub>i</sub>: temperature prior to spray, T<sub>z</sub>: temperature prior to injection, T<sub>range</sub>: Temperature range for a quasi-steady state cycle (maximum - minimum), T<sub>average</sub>: Average temperature for a quasi-steady state cycle (maximum + minimum)/2)

### Table 4.44: Comparison of the temperature fields - the thin region near the thick region

<table>
<thead>
<tr>
<th>Case</th>
<th>Temperature (°C)</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>T&lt;sub&gt;1&lt;/sub&gt;</td>
<td>T&lt;sub&gt;2&lt;/sub&gt;</td>
<td>T&lt;sub&gt;range&lt;/sub&gt;</td>
<td>T&lt;sub&gt;average&lt;/sub&gt;</td>
</tr>
<tr>
<td>X=50</td>
<td>260</td>
<td>130</td>
<td>224</td>
<td>195</td>
</tr>
<tr>
<td>X=75</td>
<td>250</td>
<td>120</td>
<td>218</td>
<td>188</td>
</tr>
<tr>
<td>X=100</td>
<td>237</td>
<td>115</td>
<td>216</td>
<td>183</td>
</tr>
<tr>
<td>Lower limit</td>
<td>218</td>
<td>105</td>
<td>171</td>
<td>171</td>
</tr>
<tr>
<td>Upper limit</td>
<td>252</td>
<td>149</td>
<td>193</td>
<td>171</td>
</tr>
</tbody>
</table>

(T<sub>i</sub>: temperature prior to spray, T<sub>z</sub>: temperature prior to injection, T<sub>range</sub>: Temperature range for a quasi-steady state cycle (maximum - minimum), T<sub>average</sub>: Average temperature for a quasi-steady state cycle (maximum + minimum)/2)
<table>
<thead>
<tr>
<th>Case</th>
<th>Temperature (°C)</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_1$</td>
<td>$T_2$</td>
<td>$T_{\text{range}}$</td>
<td>$T_{\text{average}}$</td>
</tr>
<tr>
<td>X=50</td>
<td>222</td>
<td>125</td>
<td>175</td>
<td>173</td>
</tr>
<tr>
<td>X=75</td>
<td>220</td>
<td>120</td>
<td>178</td>
<td>174</td>
</tr>
<tr>
<td>X=100</td>
<td>215</td>
<td>116</td>
<td>172</td>
<td>174</td>
</tr>
<tr>
<td>Lower limit</td>
<td>197</td>
<td>116</td>
<td>182</td>
<td>160</td>
</tr>
<tr>
<td>Upper limit</td>
<td>238</td>
<td>160</td>
<td>204</td>
<td>160</td>
</tr>
</tbody>
</table>

Table 4.45: Comparison of the temperature fields – the thin region near the pan core interface ($T_1$: temperature prior to spray, $T_2$: temperature prior to injection, $T_{\text{range}}$: Temperature range for a quasi-steady state cycle (maximum – minimum), $T_{\text{average}}$: Average temperature for a quasi-steady state cycle (maximum + minimum)/2.

<table>
<thead>
<tr>
<th>Case</th>
<th>Temperature (°C)</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$T_1$</td>
<td>$T_2$</td>
<td>$T_{\text{range}}$</td>
<td>$T_{\text{average}}$</td>
</tr>
<tr>
<td>X=50</td>
<td>235</td>
<td>137</td>
<td>150</td>
<td>181</td>
</tr>
<tr>
<td>X=75</td>
<td>238</td>
<td>135</td>
<td>146</td>
<td>177</td>
</tr>
<tr>
<td>X=100</td>
<td>225</td>
<td>130</td>
<td>148</td>
<td>174</td>
</tr>
<tr>
<td>Lower limit</td>
<td>197</td>
<td>116</td>
<td>182</td>
<td>160</td>
</tr>
<tr>
<td>Upper limit</td>
<td>238</td>
<td>160</td>
<td>204</td>
<td>160</td>
</tr>
</tbody>
</table>

Table 4.46: Comparison of the temperature fields – the thin region away from the thick region ($T_1$: temperature prior to spray, $T_2$: temperature prior to injection, $T_{\text{range}}$: Temperature range for a quasi-steady state cycle (maximum – minimum), $T_{\text{average}}$: Average temperature for a quasi-steady state cycle (maximum + minimum)/2.
Information in the tables above indicated that there was a match between the actual data and the ABAQUS simulation results. The model with \( X=100 \) assumption gave the best overall match for all the considered regions of the side core cavity surface. Knowing the lack in the actual data and the improvement over PROCAST analysis results, it was concluded that a good match between these groups of data occurred.

4.2.5 Study of Mechanical and Fatigue Properties of H-13 Tool Steel

This section includes a short study conducted on the mechanical and fatigue properties of the die material (H-13). Some important issues, which are explained below, had to be addressed before the completion of a more accurate structural and fatigue analysis.

- The stress-strain data available in the literature is the outcome of uni-axial static (monotonic) tensile tests. These data should be converted to cyclic stress-strain data. Landgraf presented the relation between the monotonic and cyclic yield strength for various classes of ferrous alloys including martensitic alloys as shown in Figure 7.16. Points above the 45° line indicate cyclic-hardening alloys, those below the line are cyclic softening alloys. The pronounced softening of low and intermediate hardness ferrous alloys is clearly shown while slightly tempered martensites exhibit stable to hardening tendencies [Landgraf, 1996]. Effect of very slow cycling is detrimental for low-intermediate hardness tool steels [Conway et al, 1991]. However, Landgraf does not give more information regarding what conditions of cyclic yield points were obtained.

- The literature included stress-strain data for H-13 with greater hardness compared with the actual dies. The hardness of cavity surface of the actual case study dies was 41-42 HRc (Rockwell C scale) while the data from the literature included properties of H-13 within the hardness range of 43-45 HRc. This brings the
necessity of conversion of figures from greater hardness values to the actual hardness levels.

Figure 4.16: Monotonic versus cyclic yield points of martensitic ferrous alloys (Under symmetric loading profiles of unknown frequency) [Landgraf, 1996].

- Tempering of the die cavity surface should also be taken into account. The die surface had softened from 41-42 HRc to 37-38 HRc during the operation due to exposure to thermal elements. This also led to change in the critical points of the stress-strain curve.
The three issues mentioned above along with some other considerations were utilized in determining the mechanical properties of the die material to be used in simulations. Further information regarding material properties can be found in the appendices.

First factor studied was the 0.2% yield strength, \( \sigma_{0.2} \). Data from three references were taken for analysis. First set was chosen from Engineering Properties of Steel Handbook [Harvey, 1982] and is shown in Table 4.47. These data included temperature dependent yield strength values for 45, 52 and 56 HRc. All three hardness levels produced parallel curves with the exception at 600 °C between 45 – 52 HRc. First, yield points of 41.5, which is the average hardness of the cavity surface HRc, was determined through linear extrapolation using the conversion factors taken from Table 4.47. Later the impact of tempering was studied using the same conversion factors and the yield strength values for 37 HRc was determined as shown in Table 4.48. This is the lowest hardness value of the die cavity surface. After studying these two issues, the cyclic data was obtained through Figure 4.16. The outcome can be seen in Table 4.51.

<table>
<thead>
<tr>
<th>Temperature (^{\circ}\mathrm{C})</th>
<th>( \sigma_{0.2} ) at 45 HRc</th>
<th>( \sigma_{0.2} ) at 52 HRc</th>
<th>( \sigma_{0.2} ) at 56 HRc</th>
<th>Conversion Factor(^{a})</th>
<th>Conversion Factor(^{b})</th>
</tr>
</thead>
<tbody>
<tr>
<td>100</td>
<td>1470</td>
<td>1750</td>
<td>1970</td>
<td>1.19</td>
<td>1.13</td>
</tr>
<tr>
<td>200</td>
<td>1390</td>
<td>1660</td>
<td>1900</td>
<td>1.19</td>
<td>1.14</td>
</tr>
<tr>
<td>300</td>
<td>1310</td>
<td>1590</td>
<td>1840</td>
<td>1.21</td>
<td>1.16</td>
</tr>
<tr>
<td>400</td>
<td>1220</td>
<td>1500</td>
<td>1715</td>
<td>1.23</td>
<td>1.14</td>
</tr>
<tr>
<td>500</td>
<td>1090</td>
<td>1350</td>
<td>1570</td>
<td>1.24</td>
<td>1.16</td>
</tr>
<tr>
<td>600</td>
<td>900</td>
<td>910</td>
<td>1050</td>
<td>1.01</td>
<td>1.15</td>
</tr>
</tbody>
</table>

Table 4.47: Yield strength values (in MPa) for 45, 52 and 56 HRc (\(a: \sigma_{0.2-52}/\sigma_{0.2-45}\) b: \(\sigma_{0.2-56}/\sigma_{0.2-52}\))[Harvey, 1982].
A second set of data chosen is shown in Table 4.48. It was taken from a Benedyk, Moracz and Wallace paper [Benedyk et al, 1970]. They presented the yield strength values of H-13 of 45 HRc. This data were also converted to 41.5 and 37 HRc levels as shown in Table 4.49. The conversions were based on the data presented in Table 4.47. This was followed and the conversion process was completed by conversion to cyclic data using Figure 4.16. The outcome can be seen in Table 4.52.
<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>$\sigma_{0.2}$ at 45 HR$_c^*$</th>
<th>$\sigma_{0.2}$ at 41.5 HR$_c$</th>
<th>$\sigma_{0.2}$ at 37 HR$_c^*$</th>
<th>Conversion Factor$^c$</th>
<th>Conversion Factor$^d$</th>
</tr>
</thead>
<tbody>
<tr>
<td>100</td>
<td>1470.0</td>
<td>1323.0</td>
<td>1151.0</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>200</td>
<td>1390.0</td>
<td>1251.0</td>
<td>1088.3</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>300</td>
<td>1310.0</td>
<td>1179.0</td>
<td>1025.7</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>400</td>
<td>1220.0</td>
<td>1098.0</td>
<td>955.2</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>500</td>
<td>1090.0</td>
<td>981.0</td>
<td>853.4</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>600</td>
<td>900.0</td>
<td>891.0</td>
<td>882.0</td>
<td>0.99</td>
<td>0.99</td>
</tr>
</tbody>
</table>

Table 4.48: Estimated yield strength values (in MPa) for 45, 41.5 and 37 HR$_c^*$ (c:$\sigma_{0.2}$-41.5/$\sigma_{0.2}$-45, d:$\sigma_{0.2}$-37/$\sigma_{0.2}$-41.5, *: [Harvey, 1982]).

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>$\sigma_{0.2}$ at 45 HR$_c$</th>
<th>$\sigma_{0.2}$ at 41.5 HR$_c$</th>
<th>$\sigma_{0.2}$ at 37 HR$_c$</th>
<th>Conversion Factor$^e$</th>
<th>Conversion Factor$^f$</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>1175.9</td>
<td>1058.3</td>
<td>920.7</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>93</td>
<td>1151.1</td>
<td>1036.0</td>
<td>901.3</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>204</td>
<td>1114.0</td>
<td>1002.6</td>
<td>872.2</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>316</td>
<td>1052.1</td>
<td>946.9</td>
<td>823.8</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>427</td>
<td>959.2</td>
<td>863.3</td>
<td>751.1</td>
<td>0.9</td>
<td>0.87</td>
</tr>
<tr>
<td>538</td>
<td>804.5</td>
<td>724.1</td>
<td>629.9</td>
<td>0.99</td>
<td>0.99</td>
</tr>
</tbody>
</table>

Table 4.49: Estimated yield strength values (in MPa) for 45, 41.5 and 37 HR$_c^*$ (e:$\sigma_{0.2}$-41.5/$\sigma_{0.2}$-45, f:$\sigma_{0.2}$-37/$\sigma_{0.2}$-41.5) [Benedyk et al, 1970]
Third data set was gathered from different references. First of all, room temperature hardness-tensile strength relation data were obtained. The ferrous alloy data in the literature indicated that tensile and yield strength can be related through a conversion factor of $0.8 \times 1 (\sigma_{0.2}/\sigma_u)$ [Tlustý, 1999]. This relation for H-13 is within the range of 0.75-0.9 [Landgraf, 1996]. Studying the H-13 data from different sources a conversion factor of 0.83 was selected [Landgraf, 1996][Harvey, 1982]. Using this factor Tlustý's data became yield strength-hardness relation at 20 °C. Tlustý's data are shown in the following table. Figure 4.18 also indicates the hardness versus tensile and yield strength and consequently yield versus tensile strength relations for H-13 [Harvey, 1982].

<table>
<thead>
<tr>
<th>Hardness (HRc)</th>
<th>$\sigma_u$ (MPa)</th>
<th>$\sigma_{0.2}$ (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>35</td>
<td>1083.0</td>
<td>898.8</td>
</tr>
<tr>
<td>37*</td>
<td>1324.4</td>
<td>956.3</td>
</tr>
<tr>
<td>40</td>
<td>1256.0</td>
<td>1042.4</td>
</tr>
<tr>
<td>41.5*</td>
<td>1152.2</td>
<td>1099.2</td>
</tr>
<tr>
<td>45</td>
<td>1484.0</td>
<td>1231.7</td>
</tr>
</tbody>
</table>

Table 4.50: Hardness versus tensile and yield strength for ferrous alloys at 20 °C (a: obtained through linear interpolation) [Tlustý, 1999]

The values from the Table 4.48 and 4.49 are converted to cyclic yield point values and these are presented in the following tables, 4.51 and 4.52. Values obtained through Tlustý's [Tlustý, 1999] and Schruff's data [Schruff, 1990] were in an agreement with Benedyk's data. Therefore Benedyk's data were used in the structural analysis.
Figure 4.18: Hardness versus tensile and yield strength and consequent yield versus tensile strength relation for H-13 [Harvey, 1982]
<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>$\sigma_{0.2}$ - cyclic (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>100</td>
<td>740.6</td>
</tr>
<tr>
<td>200</td>
<td>703.0</td>
</tr>
<tr>
<td>300</td>
<td>665.4</td>
</tr>
<tr>
<td>400</td>
<td>645.5</td>
</tr>
<tr>
<td>500</td>
<td>635.3</td>
</tr>
<tr>
<td>600</td>
<td>638.2</td>
</tr>
</tbody>
</table>

Table 4.51: Estimated cyclic yield strength values (original data are from [Harvey, 1982])

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>$\sigma_{0.2}$ - cyclic</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>642.0</td>
</tr>
<tr>
<td>93</td>
<td>640.1</td>
</tr>
<tr>
<td>204</td>
<td>637.2</td>
</tr>
<tr>
<td>316</td>
<td>632.3</td>
</tr>
<tr>
<td>427</td>
<td>625.1</td>
</tr>
<tr>
<td>538</td>
<td>564.9</td>
</tr>
</tbody>
</table>

Table 4.52: Estimated cyclic yield strength values (in MPa) (original data are from [Benedyk et al, 1970])

The second parameter investigated was the ultimate tensile strength, $\sigma_u$. Two sets of data were utilized for this portion of the study. The first set was obtained from a Benedyk, Moracz and Wallace paper [Benedyk et al, 1970]. Since they did not include tensile property data, the cyclic yield strength values obtained from their paper were
converted to cyclic tensile data through using the same conversion factor (0.83) utilized in the previous pages of this chapter.

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>uy - cyclic (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>773.5</td>
</tr>
<tr>
<td>93</td>
<td>771.2</td>
</tr>
<tr>
<td>204</td>
<td>767.7</td>
</tr>
<tr>
<td>316</td>
<td>761.8</td>
</tr>
<tr>
<td>427</td>
<td>753.1</td>
</tr>
<tr>
<td>538</td>
<td>680.6</td>
</tr>
</tbody>
</table>

Table 4.53: Estimated cyclic ultimate tensile strength values (original data is from [Benedyk et al, 1970])

The second set of ultimate strength data was obtained from a paper by Schruff [Schruff, 1990]. The data and its conversion into cyclic and 37 HRc H-13 are shown in Table 4.53. Both sets produced similar data and were in pretty much agreement at the temperature range observed by the dies. Thus, Benedyk’s data were chosen.

The third factor studied was Young’s modulus (E). Three sets of data were considered. The data are reported in MPa to make it easy to see the impact of tensile strength to Young’s modulus ratio on the fatigue life. The first set was taken from a Thyssen bulletin paper [Schruff, 1990], the second was obtained from a Benedyk, Moracz and Wallace paper [Benedyk et al, 1970] while the third was taken from Engineering Properties of Steel Handbook [Harvey, 1982]. As it was mentioned before, more data about Elastic Modulus can be found in appendices. Schruff’s paper indicated very little difference among the elastic behavior of annealed, hardened and heat-treated (austenitized and tempered to 43-44 HRc) H-13 at 20 °C.
Temperature (°C) | \( \sigma_u \) at 43-44 HR\(_c\) | \( \sigma_u \) at 41.5 HR\(_c\) | \( \sigma_u \) at 37 HR\(_c\) | \( \sigma_{u\text{-cyclic}} \) at 37 HR\(_c\) |
--- | --- | --- | --- | --- |
100 | 1400.0 | 1300.0 | 1130.0 | 768.0 |
200 | 1360.0 | 1260.0 | 1100.0 | 764.7 |
300 | 1300.0 | 1210.0 | 1050.0 | 759.9 |
400 | 1230.0 | 1140.0 | 995.0 | 754.2 |
500 | 1120.0 | 1040.0 | 906.0 | 745.3 |
600 | 800.0 | 792.0 | 784.0 | 689.6 |
700 | 200.0 | 198.0 | 196.0 | 196.4 |

Table 4.54: Ultimate tensile strength values (in MPa) for 43-44, 41.5 and 37 HR\(_c\) [Schruff, 1990]

<table>
<thead>
<tr>
<th>Condition</th>
<th>E (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Annealed</td>
<td>(2.169 \times 10^5)</td>
</tr>
<tr>
<td>Hardened</td>
<td>(2.078 \times 10^5)</td>
</tr>
<tr>
<td>Heat-treated*</td>
<td>(2.162 \times 10^5)</td>
</tr>
</tbody>
</table>

Table 4.55: Young’s modulus of annealed, hardened, and heat treated H-13 at 20 °C (a: Heat treatment included austenitization and tempering to 43-44 HR\(_c\)) [Schruff, 1990]

Benedyk’s data were obtained from 45 HR\(_c\) hard specimens, while Schruff used specimens of 43-44 HR\(_c\). The harder the steel, the smaller the values of E, Young’s modulus. The figures from both of the literature sources shown in Table 4.56 and 4.57 agree with this statement contradicting the data from Table 4.55 [Benedyk et al, 305]
1970)[Schruff, 1990]. Therefore, an average set of values, which are shown in Table 4.57, were used.

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>E (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>194,954</td>
</tr>
<tr>
<td>93</td>
<td>188,765</td>
</tr>
<tr>
<td>204</td>
<td>179,481</td>
</tr>
<tr>
<td>316</td>
<td>173,292</td>
</tr>
<tr>
<td>427</td>
<td>167,103</td>
</tr>
<tr>
<td>538</td>
<td>151,631</td>
</tr>
<tr>
<td>649</td>
<td>99,024</td>
</tr>
</tbody>
</table>

Table 4.56: Young’s modulus values [Benedyk et al, 1970]

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>E (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>216,200</td>
</tr>
<tr>
<td>100</td>
<td>211,600</td>
</tr>
<tr>
<td>200</td>
<td>205,800</td>
</tr>
<tr>
<td>300</td>
<td>199,500</td>
</tr>
<tr>
<td>400</td>
<td>193,000</td>
</tr>
<tr>
<td>500</td>
<td>184,900</td>
</tr>
<tr>
<td>600</td>
<td>173,600</td>
</tr>
<tr>
<td>700</td>
<td>161,000</td>
</tr>
</tbody>
</table>

Table 4.57: Young’s modulus values [Schruff, 1990]
<table>
<thead>
<tr>
<th>Temperature ($^\circ$C)</th>
<th>E (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>213,700</td>
</tr>
<tr>
<td>93</td>
<td>206,800</td>
</tr>
<tr>
<td>204</td>
<td>199,900</td>
</tr>
<tr>
<td>316</td>
<td>186,200</td>
</tr>
<tr>
<td>427</td>
<td>179,300</td>
</tr>
<tr>
<td>538</td>
<td>151,700</td>
</tr>
<tr>
<td>649</td>
<td>110,300</td>
</tr>
</tbody>
</table>

Table 4.58: Young's modulus values used in the structural analysis

The fourth factor studied was the logarithmic ductility, D (=ln (1-Ra)). There were limited data available in the literature especially in the range of low temperatures experienced in the study. The references indicate values of 0.6-0.8 for the temperature ranges experienced by die cavity surfaces. More data on logarithmic ductility can also be found Appendix E.

One of the goals of this dissertation was to investigate the applicability of numerical methods in determining the onset of heat checking in die-casting dies. This issue is the focus of the final section of this research study. The data obtained through assumptions and approximations in this section were utilized in the next section.

Following pages of this section discuss the influence of the following factors on fatigue life of H-13. The arguments are supported with S-N curves when the data are available. Only factor used was mean stress, the other factors were only mentioned here and due to unavailability of the data for H-13, the author was not able to incorporate these factors in his models.

- Surface conditions of the die material (pre-existing surface damage, surface residual stresses)
• Loading conditions
  o Mean stress
  o Stress range
  o Frequency
  o Asymmetry of the loading profile
• Microstructure of the die material (steel making practice and grade)
• Temperature

The impact of manufacturing processes used in fabrication of die-casting dies on the die life is important since the life of dies is affected by it. The processes used in die-making determine the surface conditions and the residual stresses within tooling. Iwanaga pointed that the pre-existing surface damage works as a source for crack initiation [Iwanaga, 1997]. Conventional machining scratches, wavy patterns and indentations cause the surface damage. Others processes such as EDM (Electro-Discharge Machining) or ECM (Electro-Chemical Machining) also result in damage in the form of micro-cracks or through grain boundary corrosion. During EDM processing micro-cracks occur when the surface stresses created by EDM exceed the ultimate tensile strength of H-13. The first layer on the EDM surface is the brittle re-cast layer with high tensile residual stresses. It is removed with other damaged areas during polishing with stones, grinders or ECM. The cracks caused by EDM can even reach the over-tempered layer, which is below the second layer, the re-hardened layer of un-tempered martensite. The ECM process is a chemical process and may cause grain boundary corrosion and consequent degradation of the material unless controlled. The experimental analysis by Blaine Lilly indicated that the residual stresses created by EDM processing are not a major factor determining the die life [Lilly, 1998]. On the contrary, he stated that the micro-cracks have the major factor reducing the fatigue life of the material. Figure 4.19 compares the three different processes: EDM, ECM and grinding. The results indicated EDM has the
lowest life in terms of mechanical fatigue followed by the ECM and the grinding process. As stated above, the micro-cracks are reducing fatigue life of the material processed by EDM. The ECM can also prolong the die life removing the damaged surface layers if the process is kept under control. However, the fatigue life of an ECM processed material is inferior to those of ground and manually polished. The difference among these processes can be explained with grain boundary corrosion of ECM processed material and the compressive residual stresses due to grinding. On the contrary, excessive grinding was found to be detrimental. Other processes such as shot peening are also known to induce compressive residual stresses and to increase fatigue life.

Another issue influencing the fatigue life performance is the loading. The influence of the mean stress and the stress range are explained below. Cyclic properties of a material are usually obtained from a completely reversed (non-zero mean) constant
amplitude strain-controlled tests. In real life applications this is not the case, generally some mean stress or strain is present. Many experimental studies indicated that if the mean stress is compressive the fatigue life is prolonged. The impact of mean stress was found to be dominant at low plastic strain ranges or in high-cycle fatigue. On the contrary, tensile mean stress reduces the fatigue life. Chapter 3 also includes the influence of mean stress in the fatigue models. Stress range is also an important factor to examine a cyclic loading situation. High stress ranges are known to be reducing the fatigue life. It sometimes is replaced by the factor, stress ratio (minimum stress to maximum stress) shown in Table 4.59:

<table>
<thead>
<tr>
<th>Condition</th>
<th>R</th>
</tr>
</thead>
<tbody>
<tr>
<td>Completely reversed</td>
<td>-1</td>
</tr>
<tr>
<td>Tensile mean stress</td>
<td>0 to -1</td>
</tr>
<tr>
<td>Tensile mean stress with zero minimum</td>
<td>0</td>
</tr>
<tr>
<td>Static loading</td>
<td>1</td>
</tr>
<tr>
<td>Compressive mean stress</td>
<td>-1 to $-\infty$ (minus infinity)</td>
</tr>
<tr>
<td>Compressive mean stress with zero minimum</td>
<td>$-\infty$</td>
</tr>
</tbody>
</table>

Table 4.59: Stress ratios for various cycle patterns

Experimental data indicated that the fatigue life decreases when the stress ratios is changed within the range of -1 to 1. Some previous case studies with major heat checking problem did not report tensile dominant cyclic loading. They revealed cyclic loading patterns with mean compressive stresses bringing the issue “if the material is actually yielding under compression” into attention. However, there is no H-13 data available in the literature on loading conditions with compressive average (with negative R ratios of -6 or more) stresses such as encountered during this research activity.
Various experimental studies indicated that the slow cycling or low frequencies are detrimental in terms of fatigue life of alloy steels [Conway, 1991][Forrest, 1962]. However the data regarding mechanical fatigue of H-13 are obtained at higher frequency than those of the actual casting process. There is no data available at the desired frequency levels.

There are no data available regarding the asymmetry of a cyclic wave pattern of stress or strain loading. There are various methods dealing with irregular loading histories and they are based on identifying identical cycles with same ranges and counting them to see the total influence of the segments of the irregular loading pattern.

By frequent examinations of hot work tool steel production lots it was verified that good and prolonged service performance of H-13 tool steel is influenced and determined to a high degree by melting, casting and hot working conditions and by subsequent heat treatment. The key to influencing the properties of the steel is found knowing how to control the inhomogeneities during the solidification of the ingot. To achieve prolonged and a satisfactory die life, it is essential to use die blocks which are free from segregations and of excellent cleanliness produced by such techniques as vacuum-arc remelting (VAR) or electro-slag remelting (ESR). Microsegregations exercise their immediate effects on a micro-scale. Figure 4.20 shows the influence of steel making practice on the fatigue life. However as a whole they also determine the properties of the H-13 tool steel’s grade as far as the macro-area is concerned [Schindler et al, 1977].

The microstructure of the H-13 tool steel varies depending upon the quality of it determined by the way the tool materiel is made. There are standard compositions of H-13 steel as well as premium grades. The following table lists the compositions for a premium (ISOBLOC 2000) and a standard grade H-13.
Figure 4.20: Fatigue strength of H-13 tool steel related to inhomogeneity caused by steel making practice [Schindler et al, 1977]

<table>
<thead>
<tr>
<th>Element</th>
<th>%weight H-13 Premium</th>
<th>%weight H-13 Standard</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>0.4</td>
<td>0.38</td>
</tr>
<tr>
<td>Si</td>
<td>1.06</td>
<td>1.00</td>
</tr>
<tr>
<td>Mn</td>
<td>0.36</td>
<td>0.35</td>
</tr>
<tr>
<td>P</td>
<td>0.02</td>
<td>0.021</td>
</tr>
<tr>
<td>S</td>
<td>0.001</td>
<td>0.005</td>
</tr>
<tr>
<td>Cr</td>
<td>5.12</td>
<td>5.03</td>
</tr>
<tr>
<td>Mo</td>
<td>1.22</td>
<td>1.24</td>
</tr>
<tr>
<td>V</td>
<td>0.92</td>
<td>0.91</td>
</tr>
<tr>
<td>Ni</td>
<td>0.12</td>
<td>0.11</td>
</tr>
</tbody>
</table>

Table 4.60: Compositions of premium H-13 (ISOBLOC 2000) and standard grade H-13 [Kogler et al, 1989]
Compositions of the both grades studied by Kogler were almost same. However, the premium grade H-13 has a lower amount of non-metallic inclusions and microsegregations, as well as lower sulphur content. Another difference was that the premium grade has uniformly distributed microsegregations leading to more compositional homogeneity and higher isotropy. The results of the fatigue tests of both are shown in Figure 4.21. The results indicated that the fatigue life for conventionally produced H-13 is considerably lower than the premium grade H-13 [Kogler et al, 1989].

![Figure 4.21: Comparison of fatigue strengths of different grade notched and un-notched H-13 test samples [Kogler et al, 1989]](image)

The influence of the elevated temperatures on the fatigue life data is discussed below. There is a tendency for the endurance limit of steels to increase at low temperatures [Forrest, 1962]. At high temperatures the endurance limit for steels
disappear due to the mobilization of dislocations [Bannantine et al, 1990]. The fatigue data available on H-13 are obtained either from room temperature mechanical fatigue tests or from thermal fatigue tests for direct observation of heat checking and thermal fatigue failure. There are no elevated temperature mechanical fatigue data for H-13 available in the literature. However, there are plenty of data regarding the elevated temperature mechanical properties of H-13. These are presented in the appendices of this dissertation. Empirical fatigue models presented in Chapter 3 utilize these data. Even though there is no direct data available, the impact of the temperature on the fatigue life was determined through the use of the fatigue models. The influence of the elevated temperatures and the temperature cycling on the mechanical properties and the fatigue life were studied in the next section of this chapter.

4.2.6 Results of Structural ABAQUS and Fatigue Analysis

Upon the completion of thermal analysis and study of critical material properties through ABAQUS, the research efforts continued with the structural ABAQUS and fatigue analyses. The model used for structural analysis was covered to its fine details in section 4.2.3. The results of the structural analysis are blended with the components and the outcome of the fatigue analysis.

ABAQUS structural analysis results did not predict any plastic deformation within tooling in quasi-steady state. The transient state did not show any major difference compared with the quasi-steady state. Table 4.61 presents the components of the multi-axial elastic and equivalent uni-axial strain ranges experienced by each 4 critical regions of the side core cavity surface. The equivalent uni-axial strain ranges are calculated using the following relation, Equation 4.6:
Table 4.61: Strain ranges for each region of the die cavity surface for quasi-steady state
(I: The thick region, II: The thin region near the thick region, III: The thin region near the pan core interface, IV: The thin region away from the thick region)

<table>
<thead>
<tr>
<th>Strain Ranges (m/m)</th>
<th>Die Cavity Surface Regions</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>I</td>
</tr>
<tr>
<td>Δε_{e11}</td>
<td>0.00120</td>
</tr>
<tr>
<td>Δε_{e22}</td>
<td>0.00043</td>
</tr>
<tr>
<td>Δε_{e33}</td>
<td>0.00093</td>
</tr>
<tr>
<td>Δε_{equi}</td>
<td>0.00043</td>
</tr>
</tbody>
</table>

\[ \Delta \varepsilon_{equi} = \frac{\sqrt{2}}{3} \left[ (\Delta \varepsilon_{e11} - \Delta \varepsilon_{e22})^2 + (\Delta \varepsilon_{e22} - \Delta \varepsilon_{e33})^2 + (\Delta \varepsilon_{e33} - \Delta \varepsilon_{e11})^2 \right]^{1/2} \quad (4.6) \]

Table 4.62 presents Von Mises equivalent uni-axial stresses and the stress-ranges for the critical regions of the die cavity surface through the Von Mises criterion indicated in the following relation, Equation 4.7. These figure were obtained from the quasi-steady state. The Von Mises criterion postulates yielding will occur when some value of the root-mean shear stress shown in Equation 4.7 reaches a critical value. The shear stress components on the die cavity surface were also studied. The shear stresses and the stress ranges were considerably lower (10-20%) compared with those of normal stresses.

\[ \sigma_{mises} = \left[ (\sigma_{11} - \sigma_{22})^2 + (\sigma_{22} - \sigma_{33})^2 + (\sigma_{33} - \sigma_{11})^2 \right]^{1/2} \quad (4.7) \]
Table 4.62: Von Mises equivalent stresses and stress ranges for each chosen region of the die cavity surface for quasi-steady state (I: The thick region, II: The thin region near the thick region, III: The thin region near the pan core interface, IV: The thin region away from the thick region)

<table>
<thead>
<tr>
<th>Mises Stresses (MPa)</th>
<th>Die Cavity Surface Regions</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>I</td>
</tr>
<tr>
<td>Maximum</td>
<td>174.6</td>
</tr>
<tr>
<td>Minimum</td>
<td>75.0</td>
</tr>
<tr>
<td>Range</td>
<td>99.6</td>
</tr>
</tbody>
</table>

Even though estimated cyclic yield strength values as low as 600 MPa were used in the structural analyses, they did not result in yielding. This outcome can be explained with the loss of some cavity surface details. Mises stress values over 400 MPa were experienced by the side core cavity surface. This figure could probably have been greater with the addition of cavity surface details working as stress risers.

The fatigue study in this dissertation was based on the current available models and fatigue data. As it is seen from the literature review, the most available data and the established models are limited within the mechanical fatigue field. Besides, most of the thermal fatigue data of tool materials available in the literature are on failure and fatigue crack growth. The current fatigue analysis methods such as Coffin-Manson and Morrow were developed from the traditional mechanical fatigue test, for which the loading conditions and material behavior is different. These models were also aimed to determine the fatigue life to failure. They are based on the data created through testing of test smooth and notched specimens. The issue of relating data obtained from these tests to actual cases with a heat-checking problem can be addressed through the local stress-strain concept. The basic idea behind the local stress-strain approach is that the local fatigue response of the material at the critical point, that is, the site of crack initiation, is
analogous to the fatigue response of a small, smooth specimen subjected to the same cyclic stress and strains [Collins, 1993]. This means that the crack initiation can be predicted through the fatigue tests of small and smooth specimens and their failure can be associated with the crack initiation of actual structures. A few researchers utilized this concept converting the models of fatigue life determination to models of fatigue crack initiation with no or very little modifications [Nyamekye et al, 1998]. For thermal fatigue analysis these mechanical fatigue based models can still be valid. However, since a temperature field is involved, it is difficult to relate the physical and the mechanical properties to the stress or the strain amplitude. Properties such as thermal conductivity (k), coefficient of thermal expansion (α) must be considered. Any region of the tooling is subjected to localized tensile/compressive stresses introduced by other regions surrounding it during non-uniform and rapid heating and cooling. The most vulnerable region is the surface layer since it is exposed to the highest thermal cycling. Although the method of universal slopes originated in room temperature tests, Coffin-Manson had run many tests to confirm it at elevated temperatures. The concept has been applied at elevated temperatures. It was proposed to use tensile properties of the materials at elevated temperatures, however this approach predicted fatigue-life values, which were higher than those actually observed. Manson studied this temperature influence in some detail. Both the crack initiation and crack propagation stages of fatigue damage were studied. Satisfactory results were not obtained, it was suggested that it would be just as accurate and a lot less complicated to assign a factor of 10% to each stage and to estimate the fatigue life at elevated temperature by taking 10% of that calculated by the method. This concept is known as the "10% rule". It was also discovered that the 10% rule also overestimated fatigue life in those cases where very low frequency were encountered at elevated temperatures [Conway et al, 1991]. This condition for 10% rule well describes the die-casting process with very low frequencies.
Since the simulations did not predict any plastic deformation, plastic strain range term was not included in the Coffin-Manson Equation 2.22 yielding Equation 4.8. Equation 2.22 was modified to 4.8 through local stress-strain concept with the only change of removing \((N_f)\) life cycles to failure and replacing it with \((N_i)\) cycles to onset of heat checking.

\[
\Delta \varepsilon_{el} = \frac{3.5\sigma_u (N_i)^{0.12}}{E}
\]  

(4.8)

where  \(\Delta \varepsilon_{el}\): range of elastic strain \((\varepsilon_{max} - \varepsilon_{min})\)

\(\sigma_u\): ultimate tensile strength

\(E\): Young Modulus

\(N_i\): number of cycles to onset of heat checking

Since the number of cycles to onset of heat checking is known as 15,000, this figure was placed into Equation 4.8 to determine the corresponding elastic strain ranges. Once the strain ranges are determined, they will be compared with the ones from numerical structural analysis as shown in Table 4.61. Since thermal cycling at a certain temperature range occurs besides softening of die cavity surfaces due to tempering, these two issues needed to be considered.

The impact of thermal cycling can be represented either with the maximum temperature or with the average temperature of the thermal cycle. Material properties \(\sigma_u\) (ultimate tensile strength) and \(E\) (Young Modulus), which are all from Equation 4.8, were chosen at these two temperature levels. As stated previously, the material property data were obtained from the literature through assumptions and approximations. The data presented in the following tables were obtained through linear interpolations from those data.
<table>
<thead>
<tr>
<th>Property</th>
<th>$T_{\text{max}}$ (255 °C)</th>
<th>$T_{\text{ave}}$ (214 °C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_u$</td>
<td>765</td>
<td>767</td>
</tr>
<tr>
<td>$E$</td>
<td>176,663</td>
<td>178,928</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{el}$</td>
<td>0.00478</td>
<td>0.00473</td>
</tr>
</tbody>
</table>

Table 4.63: $\sigma_u$ (ultimate tensile strength), $E$ (Young Modulus) (both in MPa) and corresponding elastic strain ranges (m/m) for the thick region [Benedyk et al, 1970].

<table>
<thead>
<tr>
<th>Property</th>
<th>$T_{\text{max}}$ (237 °C)</th>
<th>$T_{\text{ave}}$ (183 °C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_u$</td>
<td>766</td>
<td>768</td>
</tr>
<tr>
<td>$E$</td>
<td>177,657</td>
<td>181,237</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{el}$</td>
<td>0.00476</td>
<td>0.00468</td>
</tr>
</tbody>
</table>

Table 4.64: $\sigma_u$ (ultimate tensile strength), $E$ (Young Modulus) (both in MPa) and corresponding elastic strain ranges (m/m) for the thin region near the thick region [Benedyk et al, 1970].

<table>
<thead>
<tr>
<th>Property</th>
<th>$T_{\text{max}}$ (215 °C)</th>
<th>$T_{\text{ave}}$ (174 °C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_u$</td>
<td>767</td>
<td>768</td>
</tr>
<tr>
<td>$E$</td>
<td>178,873</td>
<td>181,990</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{el}$</td>
<td>0.00473</td>
<td>0.00466</td>
</tr>
</tbody>
</table>

Table 4.65: $\sigma_u$ (ultimate tensile strength), $E$ (Young Modulus) (both in MPa) and corresponding strain ranges (m/m) for the thin region near the pan core interface [Benedyk et al, 1970].
Table 4.66: $\sigma_u$ (ultimate tensile strength), $E$ (Young Modulus) (both in MPa) and corresponding elastic ranges (m/m) for the thin region away from the thick region [Benedyk et al, 1970]

<table>
<thead>
<tr>
<th>Property</th>
<th>$T_{\text{max}}$ (225 °C)</th>
<th>$T_{\text{ave}}$ (174 °C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_u$</td>
<td>763</td>
<td>769</td>
</tr>
<tr>
<td>$E$</td>
<td>178,320</td>
<td>181,990</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{el}$</td>
<td>0.00472</td>
<td>0.00466</td>
</tr>
</tbody>
</table>

Results indicate there are no major differences among the values of the properties at these two temperature levels. 15,000 cycles was used as number of cycles to onset of heat checking ($N_i$) in the Coffin-Manson equation to obtain the corresponding elastic range values presented above in Tables 4.63-4.66. When compared with the structural ABAQUS analysis results from Table 4.61, these data did not match with them. The numerical analysis predicted $10^6$ cycles for the initiation in contrast to the actual data of $10^4$ cycles. ABAQUS structural analysis clearly produced the lower strain values. Equation 2.29 also yielded similar values to the results of the Coffin-Manson equation.

The difference between the actual data and the analyses results were caused by the lack in the actual data, consequent approximations and assumptions including the changes in the geometry. All these were necessary to solve the problem at least with certain accuracy and within the limits of available resources and the time span.

Following section addresses the sources of errors and offers brief suggestions to the problems. Detailed analysis of the issues can be found in the concluding chapter of this dissertation.

- Thermal fatigue is not the only mechanism causing damage in the tooling. Mechanical fatigue (through mechanical loading), erosion, corrosion are the other major players contributing deterioration of cavity surfaces. Mechanical loading through metal injection pressure and clamping pressure were both included within
the study while erosion and corrosion were not a part of the study. Since problem
case did not exhibit great amount of erosion and corrosion damage, neglecting
these two mechanisms should not have major impact on the outcome. The
collaborating company had ruled out the other damage mechanisms such as
decarburization and existence of inclusions within the die material to be crucial
prior to the start of this study.

- The universal slopes is a general empirical equation. It is based on the macro
properties of the materials tested. It does not consider the microscopic properties
of any material. H-13 is an inhomogeneous material due to the non-uniform
distribution of carbides and inclusions such as sulfides, oxides and, cracks.
Application of pressure or hot working while increasing the strength of the
material also causes direction dependent mechanical properties or anisotropy. In
the direction of hot work the die life would be greater. This information was not
available adding more uncertainty to the accuracy of the model used in prediction
of onset of heat checking. Since the influence of the microscopic properties are
not included within the macroscopic model, the analysis will most likely result in
greater number of cycles to crack initiation than it is supposed to be.

- The actual data supplied by the company lacked information regarding the
process conditions and the portions of the die/casting geometry. These gaps were
compensated with some assumptions and approximations.

- Data regarding die material properties, thermal process parameters, mechanical
and thermal fatigue were obtained from the literature. There are not enough data
in the literature for a healthy thermal fatigue study in die-casting dies.
Furthermore, some of the data used in this study are based on approximations and
assumptions. Better approach will be obtaining these data directly from self-tests.
Elevated temperature tensile tests, elevated temperature (isothermal) mechanical
fatigue tests, cyclic loading tests (for cyclic stress-strain curve at very low
frequencies), softening tests (tempering) and actual thermal fatigue tests especially designed for detection of onset of heat checking should be conducted. More important than this there is no criterion defining the initiation of heat checking damage.

- The geometry of the die/casting system was simplified causing some details of the die/casting surface geometry to be lost. These details actually work as stress risers and can be a major issue in terms of determining local stress and strain distributions or concentrations. Even though structural analysis results indicated no plastic deformation, actual case is most likely driven by fatigue damage of accumulating local plastic strains. If this is not the case, or the actual stress field does not exceed the yield strength, the lost details on the die cavity surface at least should have worked as stress risers and would have increased the actual stress and corresponding elastic strain levels. If there exists a plastic strain range, the life cycles to onset of heat checking can be determined through either Coffin-Manson or Morrow's equation.

- Fatigue life analysis was based on the mechanical fatigue data. The mechanical fatigue data and models can still be used with the thermal fatigue damage. However previously mentioned precautions and approximations should be taken into effect. Even though these issues are considered, since there exists a lack in the data and it leads to a smaller but an existing error margin in the fatigue damage analysis. Some studies in the literature presented an interesting approach of associating easy and inexpensive mechanical fatigue testing with thermal fatigue testing. These studies were based on the concept that if the both mechanisms produce identical stress-strain (hysteresis) curves, they are equivalent. This concept was applied to some materials and elevated temperature (isothermal) cyclic mechanical tests at either the average or the maximum temperature of the thermal cycle has indicated equivalency with thermal cycling [Taira, 1973]. Tests simulating actual die-casting conditions can also be used.
Currently popular thermal fatigue tests do not replicate the thermal conditions within the dies [Benedyk et al, 1970].

- Other crucial issues such as surface roughness and residual stresses can also be included [Lilly, 1998]. Using the previously established analogy between mean (average) and residual stresses, in case of low cycle fatigue the impact of residual stresses can be included in the Morrow’s equation [Landgraf, 1996].

\[
2N_f = \left( \frac{\sigma_a}{\sigma_f - \sigma_m - \sigma_r} \right)^{1/b}
\]

(4.9)

where 2N_f: fatigue life in reversals
\( \sigma_f \): true fracture strength
\( b \): fatigue strength (Basquin exponent)
\( \sigma_m \): mean (average) stress
\( \sigma_a \): stress amplitude

- The issue of asymmetric loading was not addressed.

- If a stress based fatigue analysis approach is followed in a 3-D study, the impact of the tensile/compressive stresses should be considered while utilizing equivalent stress quantities such as Mises stresses. The transition from multi-axial to uni-axial conditions will cause some additional problems. Although equivalent and uni-axial stresses are the same, the crack initiation lives can differ. This is because multi-axial fatigue behavior does not depend only on the multi-axial stress component ranges, but also their instantaneous directions. Crack initiation lives can also significantly differ for tension/compression loading and for torsion although the equivalent fatigue stresses are same. Correction factors can be used to eliminate the impact of these issues [Hanschmann et al, 1994].
Even though there was a good match between the actual and the ABAQUS thermal fields (surface temperature distributions), looking at a temperature profile of the side core cavity surface, it can easily be seen that it does not really simulate the thermal shock experienced by the actual die cavity surface. In other words, the temperature gradient or the heat flux for the heat input is not same for both cases, the actual and the simulated. This is due to a shortcoming of Padiyar's model, which was used for modeling the heat input. Although, PROCAST has a structural module, it was not available to the author during his studies. The outcome of the study forced the author to the use of Padiyar's approach and the use of ABAQUS. Since the model used did not adequately represent the actual shock, the stress and the strain fields especially at the injection instant were underestimated.

The very last pages of this chapter deal with determining critical areas of tooling through numerical analysis. This is nothing new and the following results also confirmed that it could be accomplished easily.

Normally the case dies are observed to show visible evidence of heat checking after about 15,000 cycles. The heat checking begins at the corners of the boss areas on the side cores. With increasing number of shots, excited cracks propagate and new cracks form in the saddle areas of the side cores. The saddle area is a relatively flat area on the top of the transmission case or the lower portion of the side cores in the die-casting machine because the transmission is cast upside down. This is the area where the most heat checking is evident.

Figure 4.22 shows the actual side core geometry. Areas with lighter tone of gray are the defective areas in the IGES transfer files. Figure 4.23 shows the actual heat checking damage on the side core. Figure 4.23 indicates the cracks on the boss areas and well-established crack networks in the saddle area of the side core. Cracks originated at the boss areas due to stress concentrations. The boss areas are also near the two hot areas (the thick region and the extension surface) of the cavity surface.
Figure 4.22: Actual side core geometry

Figure 4.23: Heat-checking damage on the side core
Table 4.67: Strain ranges for each region of the die cavity surface (V: The thin region near the thick region and the symmetry plane, VI: The boss area between the thick and thin regions at the midpoint, VII: The boss between the thick and thin regions near the symmetry plane)

Table 4.68 presents the Mises equivalent uni-axial stresses and the stress-ranges for the critical regions of the die cavity surface through the Von Mises criterion indicated in Equation 4.7.

Table 4.68: Von Mises equivalent stresses and stress ranges for each chosen region of the die cavity surface (V: The thin region near the thick region and the symmetry plane, VI: The boss area between the thick and thin regions at the midpoint, VII: The boss between the thick and thin regions near the symmetry plane)
Figure 4.21 shows the side core geometry used in the numerical analysis process. If Figures 4.19 and 4.21 are compared with each other, the matching areas and the simplifications made for modeling can easily been detected. As stated previously, the extension surface and the side-core pan core interface do not match the very details seen in the actual geometry. These areas were simplified keeping the major original dimensions and the geometry. They were not expected to reflect a close behavior to the actual thermal and structural performance. Thus, they were not included in this portion of the study.

Following argument is based on the information gathered from the previous tables: Tables 4.61, 4.62, 4.67 and 4.68.

The thin region away from the thick region and the thick region did not show high strains. Midpoint of the two had 0.00032 and 0.00043 m/m respectively. They also were exposed to lower stress ranges and maximum stresses due to their location and the corresponding thermal cycling.

Structural analysis showed that the thin region near the thick region, the boss areas and the areas near the symmetry plane experienced considerably higher stress and strains compared with the rest of the cavity surface. Table 4.69 and 4.70 gives more information about these areas. These areas area also identified in Figure 4.21.
Figure 4.21: Solid model of the side core after simplifications (g: The thin region near the thick region at midpoint, h: the Thin region near the thick region and the symmetry plane, i: The boss are near the thick region at midpoint, j: the boss area near the thick region and the symmetry plane)

<table>
<thead>
<tr>
<th>Region\Area</th>
<th>Elastic Strain Range (m/m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>The thin region near the thick region at midpoint - g</td>
<td>0.00082</td>
</tr>
<tr>
<td>The thin region near the thick region and the symmetry plane - h</td>
<td>0.00154</td>
</tr>
<tr>
<td>The boss area near the thick region at midpoint - i</td>
<td>0.00124</td>
</tr>
<tr>
<td>The boss area near the thick region and the symmetry plane - j</td>
<td>0.00142</td>
</tr>
</tbody>
</table>

Table 4.69: Elastic strain ranges for the critical areas of the cavity surface
<table>
<thead>
<tr>
<th>Region/Area</th>
<th>Maximum V.Mises Stresses (MPa)</th>
<th>Stress Ranges (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>The thin region near the thick region at midpoint - g</td>
<td>379.7</td>
<td>324.7</td>
</tr>
<tr>
<td>The thin region near the thick region and the symmetry plane - h</td>
<td>409.3</td>
<td>329.3</td>
</tr>
<tr>
<td>The boss area near the thick region at midpoint - i</td>
<td>344.4</td>
<td>294.4</td>
</tr>
<tr>
<td>The boss area near the thick region and the symmetry plane - j</td>
<td>365.7</td>
<td>275.7</td>
</tr>
</tbody>
</table>

Table 4.70: Maximum Von Mises stresses and stress ranges experienced by the critical areas of the die cavity surface

The thin region near the thick region and the boss area between the thick region and the thin region are the two regions with a good chance of crack initiation according to the results presented above. Some areas of these regions, which are near the interfaces (such as pan core/side interface and near the symmetry plane), indicated even greater strains. These results agree with the actual data. The cracks originate near the bosses and they propagate into the saddle area resulting in heavy heat check damage especially along the symmetry plane. The thin region and the boss areas near the thick region experience greater temperature changes than any other region of the cavity surface. They also have geometric features such as changes in the geometry and the surface impressions to work as stress risers. Areas nearby the symmetry plane are also impacted by the change of the geometry even though it is not abrupt as the one in the boss areas. On the contrary, the extension surface and the thick region had greater average temperatures. This may lead to
tempering damage. This is also adding another factor for explaining the cracks near the boss areas next to the thick region. Since the extension surface was modified, it was not considered in details. Besides having the impacts of the geometry, tempering may also be a factor for that portion of the cavity surface. The overall conclusions for this study can be found in the next section.

4.2.7 Influence of Cavity Pressure on Structural State

The influence of die cavity pressure was investigated. To achieve this goal a simulation without the cavity pressure is conducted. The results are shown in the Tables 4.71 and 4.72. They were compared to the simulation results with mechanical loading cavity pressure shown in Tables 4.61 and 4.62.

<table>
<thead>
<tr>
<th>Strain Ranges (m/m)</th>
<th></th>
<th>Die Cavity Surface Regions</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>II</td>
<td>III</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{e11}$</td>
<td>0.00126</td>
<td>0.00203</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{e22}$</td>
<td>0.00068</td>
<td>0.00030</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{e33}$</td>
<td>0.00214</td>
<td>0.00065</td>
</tr>
<tr>
<td>$\Delta \varepsilon_{\text{eq}}$</td>
<td>0.00085</td>
<td>0.00106</td>
</tr>
</tbody>
</table>

Table 4.71: Strain ranges for each region of the die cavity surface for quasi-steady state (II: The thin region near the thick region, III: The thin region near the pan core interface, IV: The thin region away from the thick region)
Table 4.72: Von Mises equivalent stresses and stress ranges for each chosen region of the die cavity surface for quasi-steady state (II: The thin region near the thick region, III: The thin region near the pan core interface, IV: The thin region away from the thick region)

<table>
<thead>
<tr>
<th>Von Mises Stresses (MPa)</th>
<th>Die Cavity Surface Regions</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>II</td>
</tr>
<tr>
<td>Maximum</td>
<td>369.7</td>
</tr>
<tr>
<td>Minimum</td>
<td>55.0</td>
</tr>
<tr>
<td>Range</td>
<td>314.7</td>
</tr>
</tbody>
</table>

Comparing the data in the tables above, the impact of cavity pressure was determined. Von Mises stress comparisons showed that there was a pattern of decrease in the maximum stress values. The decrease was approximately 10-15 MPa and is consistent with the missing cavity pressure, which is around 23.2 MPa. On the contrary, minimum Von Mises values and strain ranges indicated either very little or no change. Thus, it was concluded that the cavity pressure had a relatively low impact in this case compared with the thermal loads.
CHAPTER 5

CONCLUSIONS AND FUTURE WORK

5.1 Conclusions

This dissertation was based on the idea of determining the possible utilization of numerical analysis on estimating number of shots to the onset of heat checking. The idea is not unique in engineering science however it has not been applied to an actual die-casting application. Previous attempts either dealt with test samples or core pins. To achieve the main goal, an accurate set of thermal process, heat transfer, and material property data were needed as well as information regarding fatigue data and models. This study gathered most of the available data from the literature creating a database. Spray tests were also included within the scope of the study to determine spray heat transfer coefficients under actual lubricant spray conditions. Besides obtaining these data, the study encompassed and associated the important factors with understanding the heat-checking phenomenon. These factors are tempering, cyclic loading, temperature cycling, mechanical loading due to cavity pressure. The influence of these factors on the mechanical properties and the fatigue life of the die material were studied.

Research activities in this dissertation can be divided into three major segments. These are; the thermal study of dies with 1-D thermal analysis, the thermal-structural study of dies through 2-D ABAQUS modeling, and a compound study of thermal, structural, and fatigue analyses of an actual die-casting case. The objective of the first segment was to identify the impact of factors such as thermal process parameters, thermo-physical properties of the die material, and the simple geometric elements of the
die/casting system on determining the thermal fields within the dies. Before identifying the impacts of these factors, there was a preliminary activity which included a literature review as well as spray tests especially to determine the heat transfer coefficients. Die material (H-13 and similar martensitic tool steels) properties were also within the scope of the literature review. The outcome of the literature review indicated that the heat transfer coefficients are a function of the following factors:

- **Die/casting heat transfer coefficient**
  - Die cavity surface temperature
  - Casting temperature
  - Interface layer
  - Die cavity surface roughness
  - Pressure within the cavity

- **Die/air heat transfer coefficient**
  - Die cavity surface temperature
  - Ambient temperature
  - Size and position of die surface relative to ground (horizontal or vertical and if horizontal face up or down)

- **Spray heat transfer coefficient**
  - Die cavity surface temperature
  - Spray flux density
  - Spray fluid temperature

- **Die/die interface heat transfer coefficient**
  - Temperature of the die halves or different elements of tooling which are in contact
  - Interface conditions (Including contact pressure)

Surface temperature history of a simple 1-D die/casting system, which is in quasi-steady state, was the focus of this research activity. The response of the die surface temperature
to the changes in the previously mentioned groups of parameters was studied through the visual assistance of graphics. The break-down of these groups are listed below:

- Physical properties of the die material
  - Thermal conductivity
  - Density
  - Specific heat capacity
- Die/casting system geometry
  - The casting wall thickness
  - The die insert wall thickness
- Thermal process parameters
  - The superheat of the casting material (associated with the casting wall thickness)
  - The initial die temperature
  - Heat transfer coefficients (for open die stage, dwell stage, and spray cooling of the die cavity surface)
  - Heat transfer coefficient at the cooling line
  - Air gap formation conditions (the casting surface temperature at which air gap forms, the air gap heat transfer coefficient)
- Spray process conditions
  - The die surface temperature at the initiation of the spray (spray initiation time)
  - Duration of the spray
  - The spray fluid temperature

The results of the 1-D thermal analysis yielded detailed information regarding the conditions existing within the die/casting system. Different casting wall thickness values were used. Two groups, thick and thin wall castings, were created for this study. Each set included a default set of input parameters and each default set was modified according to the data for parametric analysis. Each group of the castings had the same superheat, and the cycle time data. The outcome of this study is summarized below:
• Higher thermal conductivity values reflected lower die surface temperatures and temperature ranges. The initial point of the thermal cycle was the most influenced parameter, while the minimum point was not affected by the thermal conductivity values.

• The reported density values showed very little variation and consequently did not have any impact on the thermal cycle.

• Greater specific heat capacity values resulted in greater die surface temperatures since the die material's heat storage capability increased as well. This caused lower maximum (peak) points due to the reduction in thermal diffusivity. The impact of the heat capacity was more evident in the dwell and the open die stages.

• The closer the cooling lines to the cavity surface (meaning thinner dies in BINORM) the lower the die surface operating temperatures. The initial point showed the most influence of the changes in the die insert thickness. This factor was followed by the other two critical points; the maximum point is being the lowest.

• The initial die temperatures had negligible impact on the three critical points. The die/casting system includes the same die and casting geometry and the materials for the each case meaning that the heat content and the storage ability of the system will be almost the same even considering the change in the dies' heat storage ability with the changing temperatures. There is no major difference in the materials heat storage ability. The only difference among these cases with the different die initial temperature should be the number of cycles at which the system reaches the quasi-steady state.
• Dies run at considerably higher temperatures when the cooling line heat transfer coefficient is very low. The impact of the cooling line heat transfer coefficient is more significant at the initial and minimum points of the thermal cycle and during the dwell and open die stages. The impact on these two stages is due to the gradual but continuous heat removal through the cooling lines.

• Changes in air heat transfer coefficient moved the whole surface profile. The initial point was the most influenced point followed by the minimum point.

• Increasing the die/casting heat transfer coefficient mainly affected the peak, more explicitly the maximum temperature, and the shape of the spike during thermal shock. Greater heat transfer coefficients were translated to higher peaks.

• Air gap formation was studied through both air gap formation temperature (casting surface temperature at which air gap forms) and air gap heat transfer coefficient. Two cases with air gap formation and without were compared. The case with air gap formation (at 400 °C) resulted in lower surface temperatures during the dwell stage, since the heat flow from the casting to the die cavity surface was blocked compared with the case with no air gap formation. No air gap formation case was simulated setting the air gap formation temperature to 10°C, which is well below the casting ejection temperature. Increasing air gap temperature also allowed an increase the die cavity surface temperatures during the dwell stage. None of the three critical points were affected by the air gap formation.

• The greater the spray heat transfer coefficient, the steeper the temperature drop on the die cavity surface during the spray. There were significant differences in the minimum and the initial points due to the variation in the
spray heat transfer coefficient. On the contrary, the peak point of the thermal cycle was not influenced by those changes in the spray heat transfer coefficient.

- Higher cooling line fluid temperatures were converted to greater die surface temperatures. The two critical points of the thermal cycle were somewhat impacted by the varying coolant temperatures, in contrast to the peak point. The temperature profiles were translated in a parallel fashion without indicating major differences.

- Neither the spray initiation (delay) time nor the spray duration was a dominant player in determining the thermal fields. This can be explained with similar die surface temperatures at initiation of the spray for the different cases studied.

- The role of the spray fluid temperature was also found to be negligible. The minimum and the initial points of the thermal cycle indicated very little reaction to the changes in spray fluid temperatures.

The outcome of the first thermal study yielded detailed and crucial information regarding the die/casting system. However, it was decided to proceed further with a sensitivity analysis to understand the relative impacts of the parameters mentioned above in a more objective way supported by numerical comparisons. In addition to the minimum, the maximum and the initial points (temperatures) of the quasi-steady state thermal cycle, the thermal gradients at these three critical time instants (cycle points) were in concern. Upon the completion of the 1-D thermal sensitivity study, some parameters found to be not effective determining the three critical points of the die thermal cycle due to either their behavior or very little change in them. These parameters are listed below:
• The density of the die material
• The initial die temperature
• The air gap heat transfer coefficient
• The air gap temperatures
• The spray initiation (delay) time

Further analysis of associating the previously mentioned groups of parameters with the structural state of the die cavity surface was achieved by employing the temperature profiles obtained through the 1-D thermal analysis and imposing them along the die cavity/casting interface in 2-D ABAQUS models. The thermal gradient below the die cavity surface was the major factor determining the stress-strain state of the cavity surface. The parameters, which were found to be not effective in BINORM analysis, were excluded from this segment of the research efforts. The following conclusions were drawn assuming that greater tensile stresses, greater stress ranges and more tensile (or less compressive) average stress are detrimental. The results of the second segment of the study are summarized below:

• Greater values of thermal conductivity, specific heat capacity, spray fluid temperature and heat transfer coefficients for cooling line and die/air interface coefficients all reduced the maximum stress levels as desired.

• However, greater spray fluid temperature reduces the heat removal through spray and greater specific heat capacity (consequently decreasing thermal diffusivity) result in greater die working temperatures making them undesirable in thermal management point of view.

• Greater thermal conductivity, specific heat capacity, cooling line and die/air heat transfer coefficient also reduce the stress range values. This is also desirable. These are the parameters reducing the maximum stress levels, with the exclusion of spray fluid temperature.
• Greater specific heat capacity does not move the average stress values to more compressive levels, and this is not desirable. Thus, it can be excluded from the previous list of critical parameter changes. Greater thermal conductivity, die/air and cooling line heat transfer coefficients are the only ones from the initial list of parameters shown in Table 3.49 with the ability of influencing all the critical factors in the desired way.

• If the change in the parameters in Table 3.49 is assumed as decreases rather than increases, the results indicate that lower spray heat transfer coefficients, cooling line temperature and lower die thickness will cause the desired changes in the reaction of the critical factors.

• Greater die/casting heat transfer coefficient values also result in more compressive average stress values. However, they increase the stress range as well. Thus, intermediate values of this factor can be used to keep average stress and stress range values at desired levels.

Since Table 3.49 only reports the absolute response of the three critical factors and the change in parameters are reported and but not included in the study, another table was created. This table, Table 3.50 reports the ratio of % changes in factors (responses) to % changes in parameters. Studying these values we can see and evaluate the relative impact of any change in a parameter since each parameter indicated a different range window. Further comparisons using the information from Table 3.50 revealed that greater thermal conductivity values might be the most influential parameter change to reduce the detrimental structural loading followed by the greater cooling line and die/air heat transfer coefficients. Thermal conductivity values can be controlled through alloying. The other two are controlled through increasing the flow rate and the turbulence consequently. Table 3.51 lists the values of the preferred values. The values in the table are either taken from the extremes or an intermediate value as explained previously. Table 3.42 summarized the average and the range of parameters used in the simulations.
yielding realistic results.

One of the most important findings of the second segment of the study is that the compressive stresses \( \text{R}=-6 \) were dominant compared with the tensile stresses. This brings the question of the role of compressive stresses in heat checking. Since there is no material fatigue data available for asymmetric loading profiles with compressive mean stresses at elevated temperatures encountered in die-casting, there is a need of obtaining them through experimental studies.

Upon the completion of the second research segment, the actual die-casting case became the focus of the research efforts. This third segment of the research activities included the thermal analysis and the structural analyses of the case through two different FEM software, followed by the fatigue analysis for the onset of heat checking. The thermal and structural analyses yielded comparable results to the actual data, while the fatigue analysis failed to produce the desired results in estimating the number of cycles to heat checking. On the contrary, the structural and fatigue analyses were successful in diagnosing of the critical tooling areas which are prone to heat checking. The structural analysis also indicated that the cavity pressure has a relatively lower effect on the case studied compared with the thermal loads.

Like 2-D analyses the case study revealed that the compressive stresses due to thermal shock are more dominant compared with the tensile loading of spray and air cooling. Some previous studies such as Rosbrook's also indicated similar results [Rosbrook, 1992].

The difference between the actual data and the fatigue analysis results was caused by the incomplete actual data, consequent approximations and assumptions of the modeling stage including the changes in the geometry. All these were necessary to solve the problem at least with certain accuracy and within the limits of available resources and the time span.
Following statements address the sources of errors.

- Thermal fatigue is not the sole mechanism causing damage in the tooling. Mechanical fatigue (through mechanical loading), erosion, corrosion are the other major players contributing deterioration of cavity surfaces. Mechanical loading was included within the study while erosion and corrosion were not a part of the study. Since the problem case did not exhibit great amount of erosion and corrosion damage, neglecting these two mechanisms should not have major impact on the outcome. Decarburization and existence of inclusions within the die material had also been ruled out as crucial players prior to the start of this study.

- The universal slopes is a general empirical equation. It is based on the macro properties of the materials tested. It does not consider the microscopic properties of any material. H-13 is an inhomogeneous material due to the non-uniform distribution of carbides and inclusions such as sulfides. Application of pressure or hot working while increasing the strength of the material also causes direction dependent mechanical properties or anisotropy. In the direction of hot work the die life would be greater. This information was not available adding more uncertainty to the accuracy of the model used. Since the influence of the microscopic properties are not included within the macroscopic model, the analysis will most likely result in greater number of cycles to crack initiation than it is supposed to be.

- The actual data supplied by the company was incomplete as to process conditions or portions of the die/casting geometry and these gaps were compensated with some assumptions through inter and extrapolations.
• Data regarding die material properties, thermal process, mechanical and thermal fatigue were obtained from the literature. There are not enough data in the literature for a healthy thermal fatigue study in die-casting dies. Furthermore, some of the data used in this study are based on approximations and assumptions.

• The geometry of the die/casting system was simplified causing some details of the die/casting surface geometry to be lost. These details actually work as stress risers and can be a major issue in terms of determining local stress and strain distributions or concentrations. Even though structural analysis results indicated no plastic deformation, the actual case is most likely to be driven by fatigue damage of accumulating local plastic strains. If this is not the case, or the actual stress field does not exceed the yield strength, the lost details on the die cavity surface at least should have worked as stress risers and would have increased increase the actual stress and corresponding elastic strain levels. If there exists a plastic strain range, the life cycles to onset of heat checking can be determined through either Coffin-Manson or Morrow’s equation.

• The fatigue life analysis was based on mechanical fatigue data. Mechanical fatigue data and models can still be used with the thermal fatigue damage. However previously mentioned precautions and approximations due to tempering, elevated temperatures and temperature cycling should be taken into effect. Even if these issues are considered, there exists a lack in the data and it leads to a smaller but an existing error margin in the fatigue damage analysis.

• If a stress based fatigue analysis approach is followed in a 3-D study, the impact of the tensile/compressive stresses should be considered while utilizing equivalent stress quantities such as Von Mises stresses. The transition from multi-axial to uni-axial conditions will cause some additional problems. Although equivalent and uni-axial stresses are the same, the crack initiation lives can differ. This is because multi-axial fatigue behavior does not depend only on the multi-axial
stress component ranges, but also their instantaneous directions. Crack initiation 
lives can also significantly differ for tension/compression loading and for torsion 
although the equivalent fatigue stresses are same.

- Even though, there was a good match between the actual and the ABAQUS 
  thermal fields (surface temperature distributions), looking at a temperature profile 
of the side core cavity surface, it can easily be seen that it does not really simulate 
the thermal shock experienced by the actual die cavity surface. The temperature 
gradient or the heat flux values for the heat input is not same for both cases. This 
is due to the shortcomings of Padiyar's model, which was used for modeling the 
heat input. Although, PROCAST has a structural module, it was not available to 
the author during his studies. The outcome of the study forced the author to the 
use of Padiyar's approach and the use of ABAQUS. Since the model used did not 
adequately represent the actual thermal shock, the stress and the strain fields 
especially in the injection moment were underestimated.

- Issues such as asymmetric loading, frequency of loading, residual stresses and 
surface effects on fatigue life was not considered.

5.2 Future work

This section is designated to the future work in this area to yield better findings.

- Interactions among the parameters perhaps can be studied for a better 1-D thermal 
  and 2-D thermal-structural investigations. Furthermore design of experiments 
  concepts can be involved in the first two and may be the third segment of this 
  dissertation.

- A near future study should employ the actual die/casting geometry avoiding 
simplifications.
• A better approach will be obtaining the required data directly from self-tests. Elevated temperature (isothermal) mechanical fatigue tests with R ratios of -6 and frequency of 0.01 Hz, cyclic loading tests (for cyclic stress-strain curves at very low frequencies encountered in die-casting), softening (due to tempering) tests and thermal fatigue tests replicating the actual die-casting process conditions especially designed for detection of onset of heat checking should be conducted.

• Other crucial issues such surface roughness and residual stresses can also be included within the scope of a near future study. Using the previously established analogy between average (mean) and residual stresses, in case of low cycle fatigue the impact of residual stresses can be included in the Morrow’s stress based equation. The impact of the surface roughness on the fatigue properties can be deduced from the available data to be included as a factor for deterioration of the die material.

• Correction factors should be used to eliminate the impact of stress components and their directions in the Von Mises equation for more accurate results.

• Coupled PROCAST thermal/structural modules should be used to avoid issues like data transfer, use of two separate programs and Padiyar’s approximation, which was found to be inadequate modeling the thermal shock of the injection.

• Current fatigue models are based on mechanical fatigue phenomenon. The results of this and other studies indicate the need of developing thermal fatigue based models.
APPENDIX A

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APPENDIX B

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APPENDIX C

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  37417, 6396, 4255, 6394, 6388

*ELEMENT, TYPE=DC3D4, ELSET=EPANC
  37418, 8118, 8120, 8010, 8145
  37419, 8118, 8010, 7994, 8145
  39404, 8256, 8060, 8233, 8231
  39405, 7892, 8256, 7891, 7887

*ELEMENT, TYPE=DC3D4, ELSET=ECOVERD
  39406, 8985, 10385, 10959, 10993
  39407, 12604, 12240, 8338, 8272
  60449, 9424, 9536, 9324, 9332
  60450, 9513, 9023, 9084, 9032

*SOLID SECTION, ELSET=EEJECTD, MATERIAL=H13
*SOLID SECTION, ELSET=ESIDEC, MATERIAL=H13
*SOLID SECTION, ELSET=EPANC, MATERIAL=H13
*SOLID SECTION, ELSET=ECOVERD, MATERIAL=H13
*MATERIAL, NAME=H13
*CONDUCTIVITY, TYPE=ISO
  0.2475E+2, 0.2493E+2, 0.2511E+2, 0.2529E+2, 0.2547E+2

353
0.2565E+2, 300.0
0.2583E+2, 350.0
0.2601E+2, 400.0
0.2619E+2, 450.0
0.2637E+2, 500.0
0.2655E+2, 550.0
0.2673E+2, 600.0
0.2691E+2, 650.0
0.2709E+2, 700.0
0.2727E+2, 750.0
0.2745E+2, 800.0
0.2763E+2, 850.0
0.2781E+2, 900.0
*DENSITY
0.77E+4
*SPECIFIC HEAT
0.4699E+3, 50.0
0.4793E+3, 100.0
0.4888E+3, 150.0
0.4982E+3, 200.0
0.5076E+3, 250.0
0.5170E+3, 300.0
0.5264E+3, 350.0
0.5359E+3, 400.0
0.5453E+3, 450.0
0.5547E+3, 500.0
0.5641E+3, 550.0
0.5735E+3, 600.0
0.5830E+3, 650.0
0.5924E+3, 700.0
0.6018E+3, 750.0
0.6112E+3, 800.0
0.6206E+3, 850.0
0.6301E+3, 900.0
**AMPLITUDE FOR HEAT INPUT
*AMPLITUDE, NAME=THICKR, TIME= TOTAL TIME
0.2106390.0, 0.25, 2027085.3, 0.5, 1950766.4, 0.75, 1877320.9, 1.0, 1806640.6, 1

**CYCLE1
*INITIAL CONDITIONS, TYPE=TEMPERATURE
NEJECTD, 100.0
NSIDEC, 100.0
NPANC, 100.0
NCOVERD, 100.0
** DIES ALREADY ASSUMED TO BE CLAMPED
** STEP 1 - INJECTION AND SOLIDIFICATION (Dwell)
*STEP, AMPLITUDE=STEP, INC=200
*HEAT TRANSFER, DELTLMX=50
.25, 30.0, .001, 2.0
*RESTART, WRITE, FREQUENCY=50
**HEAT TRANSFER BETWEEN INSERTS AND BLOCKS
**
**EJECTD
**
** TOPEJECT
*FILM, OP=NEW
BS000101, F1, 80.0, 2.0000E+03
BS000102, F2, 80.0, 2.0000E+03
BS000103, F3, 80.0, 2.0000E+03
BS000104, F4, 80.0, 2.0000E+03
** BACKEJECT
*FILM, OP=NEW
BS000105, F1, 80.0, 2.0000E+03
BS000106, F2, 80.0, 2.0000E+03
BS000107, F3, 80.0, 2.0000E+03
BS000108, F4, 80.0, 2.0000E+03
** BOTTOMEJECT
*FILM, OP=NEW
BS000109, F1, 80.0, 2.0000E+03
BS000110, F2, 80.0, 2.0000E+03
BS000111, F3, 80.0, 2.0000E+03
BS000112, F4, 80.0, 2.0000E+03
** LEFTEJECT
*FILM, OP=NEW
BS000113, F1, 80.0, 2.0000E+03
BS000114, F2, 80.0, 2.0000E+03
BS000115, F3, 80.0, 2.0000E+03
BS000116, F4, 80.0, 2.0000E+03
** WLINE1
*FILM, OP=NEW
BS000117, F1, 30.0, 6.0000E+03
BS000118, F2, 30.0, 6.0000E+03
7406, F3, 30.0, 6.0000E+03
BS000119, F4, 30.0, 6.0000E+03
** WLINE2
*FILM, OP=NEW
BS000120, F1, 30.0, 6.0000E+03
BS000121, F2, 30.0, 6.0000E+03
BS000122, F4, 30.0, 6.0000E+03
** WLINE3
*FILM, OP=NEW
BS000123, F1, 30.0, 6.0000E+03
BS000124, F2, 30.0, 6.0000E+03
13546, F3, 30.0, 6.0000E+03
BS000125, F4, 30.0, 6.0000E+03
** WLINE4
*FILM, OP=NEW
BS000126, F1, 30.0, 6.0000E+03
BS000127, F2, 30.0, 6.0000E+03
BS000128, F4, 30.0, 6.0000E+03
** ** SIDECORE
**
** TOPSIDEC
*FILM, OP=NEW
BS000201, F1, 80.0, 2.0000E+03
BS000202, F2, 80.0, 2.0000E+03
BS000203, F3, 80.0, 2.0000E+03
BS000204, F4, 80.0, 2.0000E+03
** LEFTSIDEC

355
*FILM, OP=NEW
BS000205, F1, 80.0, 2.000E+03
BS000206, F2, 80.0, 2.000E+03
BS000207, F3, 80.0, 2.000E+03
BS000208, F4, 80.0, 2.000E+03
** BOTTOMSIDE
*FILM, OP=NEW
BS000209, F1, 80.0, 2.000E+03
BS000210, F2, 80.0, 2.000E+03
BS000211, F3, 80.0, 2.000E+03
BS000212, F4, 80.0, 2.000E+03
**
** PAN CORE
**
** TOPPANC
*FILM, OP=NEW
BS000301, F1, 80.0, 2.000E+03
BS000302, F2, 80.0, 2.000E+03
BS000303, F3, 80.0, 2.000E+03
BS000304, F4, 80.0, 2.000E+03
**
** COVER DIE
**
** BACKCOVERD
*FILM, OP=NEW
BS000401, F1, 80.0, 2.000E+03
BS000402, F2, 80.0, 2.000E+03
BS000403, F3, 80.0, 2.000E+03
BS000404, F4, 80.0, 2.000E+03
**
** TOPCOVERD
*FILM, OP=NEW
BS000405, F1, 80.0, 2.000E+03
BS000406, F2, 80.0, 2.000E+03
BS000407, F3, 80.0, 2.000E+03
BS000408, F4, 80.0, 2.000E+03
**
** LEFTCOVERD
*FILM, OP=NEW
BS000409, F1, 80.0, 2.000E+03
BS000410, F2, 80.0, 2.000E+03
BS000411, F3, 80.0, 2.000E+03
BS000412, F4, 80.0, 2.000E+03
**
** BOTTOMCOVERD
*FILM, OP=NEW
BS000413, F1, 80.0, 2.000E+03
BS000414, F2, 80.0, 2.000E+03
BS000415, F3, 80.0, 2.000E+03
BS000416, F4, 80.0, 2.000E+03
** HEAT INPUT
**
** EJECTOR DIE
**
** DRUMEJECT
*DFLUX, AMP=THIN
BS000138, S1, 1.000E+01
BS000139, S2, 1.000E+01

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** THICKREJECT

*DFLUX, AMP=THICKR

BS000142, S1, .1000E+01
  5816, S2, .1000E+01
  8436, S2, .1000E+01
  20497, S2, .1000E+01

BS000143, S4, .1000E+01

** EXTENSIONREJECT

*DFLUX, AMP=EXTENSION

BS000144, S1, .1000E+01
  16855, S2, .1000E+01
  16775, S3, .1000E+01
  16875, S3, .1000E+01
  16885, S3, .1000E+01

BS000145, S4, .1000E+01

** SIDE CORE

** CAVITISIDEC

*DFLUX, AMP=THIN

BS000229, S1, .1000E+01
BS000230, S2, .1000E+01
BS000231, S3, .1000E+01
BS000232, S4, .1000E+01

** BISCUITSIDEC

*DFLUX, AMP=BISCUIT

21739, S1, .1000E+01
23494, S1, .1000E+01
23980, S1, .1000E+01
23971, S2, .1000E+01
24097, S2, .1000E+01
24169, S2, .1000E+01
23917, S3, .1000E+01
24043, S3, .1000E+01

BS000233, S4, .1000E+01

** RUNNERSIDEC

*DFLUX, AMP=RUNNER

23485, S1, .1000E+01
BS000234, S4, .1000E+01

** GATESIDEC

*DFLUX, AMP=GATE

27924, S1, .1000E+01
29356, S3, .1000E+01

BS000235, S4, .1000E+01

** THICKRSIDEC

*DFLUX, AMP=THICKR

BS000236, S1, .1000E+01
BS000237, S2, .1000E+01
BS000238, S3, .1000E+01
BS000239, S4, .1000E+01

** EXTENSIONSIDEC

*DFLUX, AMP=EXTENSION

BS000240, S1, .1000E+01
** PAN CORE **

** CAVITYPANC **
*DFLUX, AMP=THIN
BS000317, S1, .1000E+01
BS000318, S2, .1000E+01
38650, S3, .1000E+01
38695, S3, .1000E+01
BS000319, S4, .1000E+01

** THICKRPANC **
*DFLUX, AMP=THICKR
37691, S1, .1000E+01
BS000320, S2, .1000E+01
37686, S3, .1000E+01
38903, S3, .1000E+01
BS000321, S4, .1000E+01

** EXTENSIONPANC **
*DFLUX, AMP=EXTENSION
38230, S2, .1000E+01
38226, S3, .1000E+01
38227, S3, .1000E+01
38229, S4, .1000E+01

** COVER DIE **

** BISCUITCOVERD **
*DFLUX, AMP=BISCUIT
BS000426, S1, .1000E+01
57601, S2, .1000E+01
59201, S2, .1000E+01
57361, S3, .1000E+01
BS000427, S4, .1000E+01

** RUNNERCOVERD **
*DFLUX, AMP=RUNNER
57161, S2, .1000E+01
57351, S3, .1000E+01
BS000428, S4, .1000E+01

** GATECOVERD **
*DFLUX, AMP=GATE
56701, S1, .1000E+01
56711, S1, .1000E+01
52111, S2, .1000E+01
BS000429, S4, .1000E+01

** EXTENSIONCOVERD **
*DFLUX, AMP=EXTENSION
BS000430, S1, .1000E+01
BS000431, S2, .1000E+01
55921, S3, .1000E+01
57297, S3, .1000E+01
BS000432, S4, .1000E+01
*NODE FILE, FREQUENCY=50, GLOBAL=YES
NT
*NODE PRINT,FREQUENCY=0
*EL PRINT,FREQUENCY=0
*END STEP
** STEP2 - OPEN DIE STAGE (AIR COOLING OF INTERFACES AND CAVITY SURFACES)
*STEP,AMPLITUDE=STEP,INC=100
*HEAT TRANSFER, DELTMX=50
.5.11.9999, .01, 1.0
*RESTART, WRITE, FREQUENCY=50
**HEAT TRANSFER BETWEEN INSERTS AND BLOCKS
**
** EJECTD
**
** TOPEJECT
*FILM, OP=NEW
BS000101, F1, 80.0, 2.0000E+03
BS000102, F2, 80.0, 2.0000E+03
BS000103, F3, 80.0, 2.0000E+03
BS000104, F4, 80.0, 2.0000E+03
** BACKEJECT
*FILM, OP=NEW
BS000105, F1, 80.0, 2.0000E+03
BS000106, F2, 80.0, 2.0000E+03
BS000107, F3, 80.0, 2.0000E+03
BS000108, F4, 80.0, 2.0000E+03
** BOTTOMEJECT
*FILM, OP=NEW
BS000109, F1, 80.0, 2.0000E+03
BS000110, F2, 80.0, 2.0000E+03
BS000111, F3, 80.0, 2.0000E+03
BS000112, F4, 80.0, 2.0000E+03
** LEFTEJECT
*FILM, OP=NEW
BS000113, F1, 80.0, 2.0000E+03
BS000114, F2, 80.0, 2.0000E+03
BS000115, F3, 80.0, 2.0000E+03
BS000116, F4, 80.0, 2.0000E+03
** WLINE1
*FILM, OP=NEW
BS000117, F1, 30.0, 6.0000E+03
BS000118, F2, 30.0, 6.0000E+03
7406, F3, 30.0, 6.0000E+03
BS000119, F4, 30.0, 6.0000E+03
** WLINE2
*FILM, OP=NEW
BS000120, F1, 30.0, 6.0000E+03
BS000121, F2, 30.0, 6.0000E+03
BS000122, F4, 30.0, 6.0000E+03
** WLINE3
*FILM, OP=NEW
BS000123, F1, 30.0, 6.0000E+03
BS000124, F2, 30.0, 6.0000E+03
13546, F3, 30.0, 6.0000E+03
BS000125, F4, 30.0, 6.0000E+03
** WLINE4
*FILM, OP=NEW
BS000126, F1, 30.0, 6.0000E+03
BS000127, F2, 30.0, 6.0000E+03
BS000128, F4, 30.0, 6.0000E+03
**
** SIDE CORE
**
** TOPSIDE
*FILM, OP=NEW
BS000201, F1, 80.0, 2.0000E+03
BS000202, F2, 80.0, 2.0000E+03
BS000203, F3, 80.0, 2.0000E+03
BS000204, F4, 80.0, 2.0000E+03
** LEFTSIDE
*FILM, OP=NEW
BS000205, F1, 80.0, 2.0000E+03
BS000206, F2, 80.0, 2.0000E+03
BS000207, F3, 80.0, 2.0000E+03
BS000208, F4, 80.0, 2.0000E+03
** BOTTOMSIDE
*FILM, OP=NEW
BS000209, F1, 80.0, 2.0000E+03
BS000210, F2, 80.0, 2.0000E+03
BS000211, F3, 80.0, 2.0000E+03
BS000212, F4, 80.0, 2.0000E+03
**
** PAN CORE
**
** TOPPANC
*FILM, OP=NEW
BS000301, F1, 80.0, 2.0000E+03
BS000302, F2, 80.0, 2.0000E+03
BS000303, F3, 80.0, 2.0000E+03
BS000304, F4, 80.0, 2.0000E+03
**
** COVER DIE
**
** BACKCOVERD
*FILM, OP=NEW
BS000401, F1, 80.0, 2.0000E+03
BS000402, F2, 80.0, 2.0000E+03
BS000403, F3, 80.0, 2.0000E+03
BS000404, F4, 80.0, 2.0000E+03
** TOPCOVERD
*FILM, OP=NEW
BS000405, F1, 80.0, 2.0000E+03
BS000406, F2, 80.0, 2.0000E+03
BS000407, F3, 80.0, 2.0000E+03
BS000408, F4, 80.0, 2.0000E+03
** LEFTCOVERD
*FILM, OP=NEW
BS000409, F1, 80.0, 2.0000E+03
BS000410, F2, 80.0, 2.0000E+03
BS000411, F3, 80.0, 2.0000E+03
BS000412, F4, 80.0, 2.0000E+03
** BOTTOM COVERD
*FILM.OP=NEW
BS000413, F1, 80.0, 2.0000E+03
BS000414, F2, 80.0, 2.0000E+03
BS000415, F3, 80.0, 2.0000E+03
BS000416, F4, 80.0, 2.0000E+03
** AIR COOLING OF CAVITY SURFACES
**
** EJECTOR DIE
**
** DRU MEJECT
*FILM.OP=NEW
BS000138, F1, 25.0, 1.256E+02
BS000139, F2, 25.0, 1.256E+02
BS000140, F3, 25.0, 1.256E+02
BS000141, F4, 25.0, 1.256E+02
** THICKREJECT
*FILM.OP=NEW
BS000142, F1, 5816, F2, 25.0, 1.256E+02
8436, F2, 25.0, 1.256E+02
20497, F2, 25.0, 1.256E+02
BS000143, F4, 25.0, 1.256E+02
** EXTENSION EJECT
*FILM.OP=NEW
BS000144, F1, 16855, F2, 25.0, 1.256E+02
16775, F3, 25.0, 1.256E+02
16875, F3, 25.0, 1.256E+02
16885, F3, 25.0, 1.256E+02
BS000145, F4, 25.0, 1.256E+02
**
** SIDE CORE
**
** CA VITI SIDE DEC
*FILM.OP=NEW
BS000229, F1, 25.0, 1.256E+02
BS000230, F2, 25.0, 1.256E+02
BS000231, F3, 25.0, 1.256E+02
BS000232, F4, 25.0, 1.256E+02
** BISCUIT SIDE DEC
*FILM.OP=NEW
21739, F1, 25.0, 1.256E+02
23494, F1, 25.0, 1.256E+02
23980, F1, 25.0, 1.256E+02
23971, F2, 25.0, 1.256E+02
24097, F2, 25.0, 1.256E+02
24169, F2, 25.0, 1.256E+02
23917, F3, 25.0, 1.256E+02
24043, F3, 25.0, 1.256E+02
BS000233, F4, 25.0, 1.256E+02
** RUNNERS SIDE DEC
*FILM.OP=NEW
23485, F1, 25.0, 1.256E+02
BS000234, F4, 25.0, 1.256E+02
** GATESIDE
*FILM, OP=NEW
   27924, F1, 25.0, 1.256E+02
   29356, F3, 25.0, 1.256E+02
BS000235, F4, 25.0, 1.256E+02
** THICKRSIDE
*FILM, OP=NEW
BS000236, F1, 25.0, 1.256E+02
BS000237, F2, 25.0, 1.256E+02
BS000238, F3, 25.0, 1.256E+02
BS000239, F4, 25.0, 1.256E+02
** EXTENSIONSIDE
*FILM, OP=NEW
BS000240, F1, 25.0, 1.256E+02
BS000241, F2, 25.0, 1.256E+02
BS000242, F3, 25.0, 1.256E+02
BS000243, F4, 25.0, 1.256E+02
**
** PAN CORE
**
** CAVITYPANC
*FILM, OP=NEW
BS000317, F1, 25.0, 1.256E+02
BS000318, F2, 25.0, 1.256E+02
   38650, F3, 25.0, 1.256E+02
   38695, F3, 25.0, 1.256E+02
BS000319, F4, 25.0, 1.256E+02
** THICKRPANC
*FILM, OP=NEW
   37691, F1, 25.0, 1.256E+02
BS000320, F2, 25.0, 1.256E+02
   37686, F3, 25.0, 1.256E+02
   38903, F3, 25.0, 1.256E+02
BS000321, F4, 25.0, 1.256E+02
** EXTENSIONPANC
*FILM, OP=NEW
   38230, F2, 25.0, 1.256E+02
   38226, F3, 25.0, 1.256E+02
   38227, F3, 25.0, 1.256E+02
   38229, F4, 25.0, 1.256E+02
**
** COVER DIE
**
** BISCUITCOVERD
*FILM, OP=NEW
BS000426, F1, 25.0, 1.256E+02
   57601, F2, 25.0, 1.256E+02
   59201, F2, 25.0, 1.256E+02
   57361, F3, 25.0, 1.256E+02
BS000427, F4, 25.0, 1.256E+02
** RUNNERCOVERD
*FILM, OP=NEW
   57161, F2, 25.0, 1.256E+02
   57351, F3, 25.0, 1.256E+02
** GATECOVERD
*FILM, OP=NEW
  56701,  F1, 25.0, 1.256E+02
  56711,  F1, 25.0, 1.256E+02
  52111,  F2, 25.0, 1.256E+02
BS000429,  F4, 25.0, 1.256E+02
** EXTENSIONCOVERD
*FILM, OP=NEW
BS000430,  F1, 25.0, 1.256E+02
BS000431,  F2, 25.0, 1.256E+02
  55921,  F3, 25.0, 1.256E+02
  57297,  F3, 25.0, 1.256E+02
BS000432,  F4, 25.0, 1.256E+02
** AIR COOLING OF INTERFACES
**
** EJECTOR DIE
**
** INTWPE
*FILM, OP=NEW
BS000129,  F1, 25.0, 1.256E+02
BS000130,  F2, 25.0, 1.256E+02
BS000131,  F3, 25.0, 1.256E+02
BS000132,  F4, 25.0, 1.256E+02
** INTWSE
*FILM, OP=NEW
BS000133,  F1, 25.0, 1.256E+02
BS000134,  F2, 25.0, 1.256E+02
BS000135,  F3, 25.0, 1.256E+02
BS000136,  F4, 25.0, 1.256E+02
** INTWCE
*FILM, OP=NEW
  16605,  F1, 25.0, 1.256E+02
  17565,  F1, 25.0, 1.256E+02
  17575,  F1, 25.0, 1.256E+02
  16865,  F2, 25.0, 1.256E+02
  17655,  F2, 25.0, 1.256E+02
  17485,  F3, 25.0, 1.256E+02
  17605,  F3, 25.0, 1.256E+02
BS000137,  F4, 25.0, 1.256E+02
**
** SIDE CORE
**
** INTWES
*FILM, OP=NEW
BS000213,  F1, 25.0, 1.256E+02
BS000214,  F2, 25.0, 1.256E+02
BS000215,  F3, 25.0, 1.256E+02
BS000216,  F4, 25.0, 1.256E+02
** INTWPS
*FILM, OP=NEW
BS000217,  F1, 25.0, 1.256E+02
BS000218,  F2, 25.0, 1.256E+02
BS000219,  F3, 25.0, 1.256E+02
BS000220,  F4, 25.0, 1.256E+02

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** INTWP2S  
*FILM, OP=NEW  
BS000221, F1, 25.0, 1.256E+02  
BS000222, F2, 25.0, 1.256E+02  
BS000223, F3, 25.0, 1.256E+02  
BS000224, F4, 25.0, 1.256E+02  
** INTWCS  
*FILM, OP=NEW  
BS000225, F1, 25.0, 1.256E+02  
BS000226, F2, 25.0, 1.256E+02  
BS000227, F3, 25.0, 1.256E+02  
BS000228, F4, 25.0, 1.256E+02  
** PAN CORE  
**  
** INTWEP  
*FILM, OP=NEW  
39164, F1, 25.0, 1.256E+02  
39365, F1, 25.0, 1.256E+02  
BS000305, F2, 25.0, 1.256E+02  
BS000306, F3, 25.0, 1.256E+02  
BS000307, F4, 25.0, 1.256E+02  
** INTWS2P  
*FILM, OP=NEW  
BS000308, F1, 25.0, 1.256E+02  
BS000309, F2, 25.0, 1.256E+02  
BS000310, F3, 25.0, 1.256E+02  
BS000311, F4, 25.0, 1.256E+02  
** INTWPC  
*FILM, OP=NEW  
BS000312, F1, 25.0, 1.256E+02  
38237, F2, 25.0, 1.256E+02  
38792, F2, 25.0, 1.256E+02  
BS000313, F4, 25.0, 1.256E+02  
** INTWSC  
*FILM, OP=NEW  
BS000417, F1, 25.0, 1.256E+02  
BS000418, F2, 25.0, 1.256E+02  
BS000419, F3, 25.0, 1.256E+02  
BS000420, F4, 25.0, 1.256E+02  
** INTWPC  
*FILM, OP=NEW  
BS000421, F1, 25.0, 1.256E+02  
BS000422, F2, 25.0, 1.256E+02  
BS000423, F3, 25.0, 1.256E+02  
BS000424, F4, 25.0, 1.256E+02
** INTWEC
*FILM,OP=NEW
    55791,  F2, 25.0, 1.256E+02
    56091,  F2, 25.0, 1.256E+02
    55881,  F3, 25.0, 1.256E+02
    56021,  F3, 25.0, 1.256E+02
BS000425,  F4, 25.0, 1.256E+02
*NODE FILE,FREQUENCY=50,GLOBAL=YES
NT
*NODE PRINT,FREQUENCY=0
*EL PRINT, FREQUENCY=0
*END STEP
** STEP 3 - OPEN DIE STAGE (AIR COOLING OF INTERFACES AND SPRAYING ON CAVITY)
*STEP,AMPLITUDE=STEP,INC=100
*HEAT TRANSFER, DELTMX=50
  0.25,10.9999 ,.01,1.0
*RESTART, WRITE, FREQUENCY=50
**HEAT TRANSFER BETWEEN INSERTS AND BLOCKS
**
**EJECTD
**
** TOPEJECT
*FILM,OP=NEW
BS000101,  F1, 80.0, 2.0000E+03
BS000102,  F2, 80.0, 2.0000E+03
BS000103,  F3, 80.0, 2.0000E+03
BS000104,  F4, 80.0, 2.0000E+03
** BACKEJECT
*FILM,OP=NEW
BS000105,  F1, 80.0, 2.0000E+03
BS000106,  F2, 80.0, 2.0000E+03
BS000107,  F3, 80.0, 2.0000E+03
BS000108,  F4, 80.0, 2.0000E+03
** BOTOMEJECT
*FILM,OP=NEW
BS000109,  F1, 80.0, 2.0000E+03
BS000110,  F2, 80.0, 2.0000E+03
BS000111,  F3, 80.0, 2.0000E+03
BS000112,  F4, 80.0, 2.0000E+03
** LEFTEJECT
*FILM,OP=NEW
BS000113,  F1, 80.0, 2.0000E+03
BS000114,  F2, 80.0, 2.0000E+03
BS000115,  F3, 80.0, 2.0000E+03
BS000116,  F4, 80.0, 2.0000E+03
** WLINE1
*FILM,OP=NEW
BS000117,  F1, 30.0, 6.0000E+03
BS000118,  F2, 30.0, 6.0000E+03
    7406,  F3, 30.0, 6.0000E+03
BS000119,  F4, 30.0, 6.0000E+03
** WLINE2
*FILM,OP=NEW
BS000120,  F1, 30.0, 6.0000E+03
BS000121, F2, 30.0, 6.0000E+03
BS000122, F4, 30.0, 6.0000E+03
** WLINE3
*FILM, OP=NEW
BS000123, F1, 30.0, 6.0000E+03
BS000124, F2, 30.0, 6.0000E+03
13546, F3, 30.0, 6.0000E+03
BS000125, F4, 30.0, 6.0000E+03
** WLINE4
*FILM, OP=NEW
BS000126, F1, 30.0, 6.0000E+03
BS000127, F2, 30.0, 6.0000E+03
BS000128, F4, 30.0, 6.0000E+03
**
** SIDE CORE
**
** TOPSIDEC
*FILM, OP=NEW
BS000201, F1, 80.0, 2.0000E+03
BS000202, F2, 80.0, 2.0000E+03
BS000203, F3, 80.0, 2.0000E+03
BS000204, F4, 80.0, 2.0000E+03
** LEFTSIDEC
*FILM, OP=NEW
BS000205, F1, 80.0, 2.0000E+03
BS000206, F2, 80.0, 2.0000E+03
BS000207, F3, 80.0, 2.0000E+03
BS000208, F4, 80.0, 2.0000E+03
** BOTTOMSIDEC
*FILM, OP=NEW
BS000209, F1, 80.0, 2.0000E+03
BS000210, F2, 80.0, 2.0000E+03
BS000211, F3, 80.0, 2.0000E+03
BS000212, F4, 80.0, 2.0000E+03
**
** PAN CORE
**
** TOPPANC
*FILM, OP=NEW
BS000301, F1, 80.0, 2.0000E+03
BS000302, F2, 80.0, 2.0000E+03
BS000303, F3, 80.0, 2.0000E+03
BS000304, F4, 80.0, 2.0000E+03
**
** COVER DIE
**
** BACKCOVERD
*FILM, OP=NEW
BS000401, F1, 80.0, 2.0000E+03
BS000402, F2, 80.0, 2.0000E+03
BS000403, F3, 80.0, 2.0000E+03
BS000404, F4, 80.0, 2.0000E+03
** TOPCOVERD
*FILM, OP=NEW
BS000405, F1, 80.0, 2.0000E+03

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BS000406,  F2,  80.0,  2.0000E+03
BS000407,  F3,  80.0,  2.0000E+03
BS000408,  F4,  80.0,  2.0000E+03
**  LEFTCOVERD
*FILM, OP=NEW
BS000409,  F1,  80.0,  2.0000E+03
BS000410,  F2,  80.0,  2.0000E+03
BS000411,  F3,  80.0,  2.0000E+03
BS000412,  F4,  80.0,  2.0000E+03
**  BOTTOMCOVERD
*FILM, OP=NEW
BS000413,  F1,  80.0,  2.0000E+03
BS000414,  F2,  80.0,  2.0000E+03
BS000415,  F3,  80.0,  2.0000E+03
BS000416,  F4,  80.0,  2.0000E+03
**  SPRAY COOLING OF CAVITY SURFACES
**
**  EJECTOR DIE
**
**  DRUM EJECT
*FILM, OP=NEW
BS000138,  F1,  25.0,  2.982E+03
BS000139,  F2,  25.0,  2.982E+03
BS000140,  F3,  25.0,  2.982E+03
BS000141,  F4,  25.0,  2.982E+03
**  THICK REJECT
*FILM, OP=NEW
BS000142,  F1,  25.0,  2.982E+03
  5816,  F2,  25.0,  2.982E+03
  8436,  F2,  25.0,  2.982E+03
  20497, F2,  25.0,  2.982E+03
BS000143,  F4,  25.0,  2.982E+03
**  EXTENSION EJECT
*FILM, OP=NEW
BS000144,  F1,  25.0,  2.982E+03
  16855, F2,  25.0,  2.982E+03
  16775, F3,  25.0,  2.982E+03
  16875, F3,  25.0,  2.982E+03
  16885, F3,  25.0,  2.982E+03
BS000145,  F4,  25.0,  2.982E+03
**  SIDE CORE
**
**  CAVITY SIDEC
*FILM, OP=NEW
BS000229,  F1,  25.0,  2.982E+03
BS000230,  F2,  25.0,  2.982E+03
BS000231,  F3,  25.0,  2.982E+03
BS000232,  F4,  25.0,  2.982E+03
**  BISCUIT SIDEC
*FILM, OP=NEW
  21739, F1,  25.0,  2.982E+03
  23494, F1,  25.0,  2.982E+03
  23980, F1,  25.0,  2.982E+03
  23971, F2,  25.0,  2.982E+03
24097, F2, 25.0, 2.982E+03
24169, F2, 25.0, 2.982E+03
23917, F3, 25.0, 2.982E+03
24043, F3, 25.0, 2.982E+03
BS000233, F4, 25.0, 2.982E+03
** RUNNERSIDEC
*FILM, OP=NEW
23485, F1, 25.0, 2.982E+03
BS000234, F4, 25.0, 2.982E+03
** GATESIDEC
*FILM, OP=NEW
27924, F1, 25.0, 2.982E+03
29356, F3, 25.0, 2.982E+03
BS000235, F4, 25.0, 2.982E+03
** THICKRSIDEC
*FILM, OP=NEW
BS000236, F1, 25.0, 2.982E+03
BS000237, F2, 25.0, 2.982E+03
BS000238, F3, 25.0, 2.982E+03
BS000239, F4, 25.0, 2.982E+03
** EXTENSIONSIDEDEC
*FILM, OP=NEW
BS000240, F1, 25.0, 2.982E+03
BS000241, F2, 25.0, 2.982E+03
BS000242, F3, 25.0, 2.982E+03
BS000243, F4, 25.0, 2.982E+03
**
** PAN CORE
**
** CAVITYPANC
*FILM, OP=NEW
BS000317, F1, 25.0, 2.982E+03
BS000318, F2, 25.0, 2.982E+03
38650, F3, 25.0, 2.982E+03
38695, F3, 25.0, 2.982E+03
BS000319, F4, 25.0, 2.982E+03
** THICKRPANC
*FILM, OP=NEW
37691, F1, 25.0, 2.982E+03
BS000320, F2, 25.0, 2.982E+03
37686, F3, 25.0, 2.982E+03
38903, F3, 25.0, 2.982E+03
BS000321, F4, 25.0, 2.982E+03
** EXTENSIONPANC
*FILM, OP=NEW
38230, F2, 25.0, 2.982E+03
38226, F3, 25.0, 2.982E+03
38227, F3, 25.0, 2.982E+03
38229, F4, 25.0, 2.982E+03
**
** COVER DIE
**
** BISCUITCOVERD
*FILM, OP=NEW
BS000426, F1, 25.0, 2.982E+03

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**RUNNER COVERD**
*FILM, OP=NEW*
57601, F2, 25.0, 2.982E+03
59201, F2, 25.0, 2.982E+03
57361, F3, 25.0, 2.982E+03
BS000427, F4, 25.0, 2.982E+03

**GATE COVERD**
*FILM, OP=NEW*
57161, F2, 25.0, 2.982E+03
57351, F3, 25.0, 2.982E+03
BS000428, F4, 25.0, 2.982E+03

**EXTENSION COVERD**
*FILM, OP=NEW*
56701, F1, 25.0, 2.982E+03
56711, F1, 25.0, 2.982E+03
52111, F2, 25.0, 2.982E+03
BS000429, F4, 25.0, 2.982E+03

**AIR COOLING OF INTERFACES**

**EJECTOR DIE**

**INTWPE**
*FILM, OP=NEW*
BS000129, F1, 25.0, 1.256E+02
BS000130, F2, 25.0, 1.256E+02
BS000131, F3, 25.0, 1.256E+02
BS000132, F4, 25.0, 1.256E+02

**INTWSE**
*FILM, OP=NEW*
BS000133, F1, 25.0, 1.256E+02
BS000134, F2, 25.0, 1.256E+02
BS000135, F3, 25.0, 1.256E+02
BS000136, F4, 25.0, 1.256E+02

**INTWCE**
*FILM, OP=NEW*
16605, F1, 25.0, 1.256E+02
17565, F1, 25.0, 1.256E+02
17575, F1, 25.0, 1.256E+02
16865, F2, 25.0, 1.256E+02
17655, F2, 25.0, 1.256E+02
17485, F3, 25.0, 1.256E+02
17605, F3, 25.0, 1.256E+02
BS000137, F4, 25.0, 1.256E+02

**SIDE CORE**

**INTWES**
*FILM, OP=NEW*
BS000213, F1, 25.0, 1.256E+02
BS000214, F2, 25.0, 1.256E+02
BS000215, F3, 25.0, 1.256E+02
BS000216, F4, 25.0, 1.256E+02
** INTWPS
*FILM, OP=NEW
BS000217, F1, 25.0, 1.256E+02
BS000218, F2, 25.0, 1.256E+02
BS000219, F3, 25.0, 1.256E+02
BS000220, F4, 25.0, 1.256E+02
** INTWPS
*FILM, OP=NEW
BS000221, F1, 25.0, 1.256E+02
BS000222, F2, 25.0, 1.256E+02
BS000223, F3, 25.0, 1.256E+02
BS000224, F4, 25.0, 1.256E+02
** INTWCS
*FILM, OP=NEW
BS000225, F1, 25.0, 1.256E+02
BS000226, F2, 25.0, 1.256E+02
BS000227, F3, 25.0, 1.256E+02
BS000228, F4, 25.0, 1.256E+02
** PAN CORE
**
** INTWEP
*FILM, OP=NEW
39164, F1, 25.0, 1.256E+02
39365, F1, 25.0, 1.256E+02
BS000305, F2, 25.0, 1.256E+02
BS000306, F3, 25.0, 1.256E+02
BS000307, F4, 25.0, 1.256E+02
** INTWSP
*FILM, OP=NEW
BS000308, F1, 25.0, 1.256E+02
BS000309, F2, 25.0, 1.256E+02
BS000310, F3, 25.0, 1.256E+02
BS000311, F4, 25.0, 1.256E+02
** INTWCP
*FILM, OP=NEW
BS000312, F1, 25.0, 1.256E+02
38237, F2, 25.0, 1.256E+02
38792, F2, 25.0, 1.256E+02
BS000313, F4, 25.0, 1.256E+02
** INTWS2P
*FILM, OP=NEW
BS000314, F1, 25.0, 1.256E+02
BS000315, F2, 25.0, 1.256E+02
38215, F3, 25.0, 1.256E+02
BS000316, F4, 25.0, 1.256E+02
**
** COVER DIE
**
** INTWSC
*FILM, OP=NEW
BS000417, F1, 25.0, 1.256E+02
BS000418, F2, 25.0, 1.256E+02
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** INTWPC
*FILM, OP=NEW
BS000421, F1, 25.0, 1.256E+02
BS000422, F2, 25.0, 1.256E+02
BS000423, F3, 25.0, 1.256E+02
BS000424, F4, 25.0, 1.256E+02

** INTWEC
*FILM, OP=NEW
55791, F2, 25.0, 1.256E+02
56091, F2, 25.0, 1.256E+02
55881, F3, 25.0, 1.256E+02
56021, F3, 25.0, 1.256E+02
BS000425, F4, 25.0, 1.256E+02

*NODE FILE, FREQUENCY=50, GLOBAL=NO
NT
*NODE PRINT, FREQUENCY=0
*EL PRINT, FREQUENCY=0
*END STEP

** STEP 4 - OPEN DIE STAGE (AIR COOLING OF INTERFACES AND FORCED AIR ON CAVITY)
*STEP, AMPLITUDE=STEP, INC=100
*HEAT TRANSFER, DELTMX=50
0.25, 12.9999, .01, 1.0
*RESTART, WRITE, FREQUENCY=50
** HEAT TRANSFER BETWEEN INSERTS AND BLOCKS
**
** EJECTD
**
** TOPEJECT
*FILM, OP=NEW
BS000101, F1, 80.0, 2.0000E+03
BS000102, F2, 80.0, 2.0000E+03
BS000103, F3, 80.0, 2.0000E+03
BS000104, F4, 80.0, 2.0000E+03

** BACKEJECT
*FILM, OP=NEW
BS000105, F1, 80.0, 2.0000E+03
BS000106, F2, 80.0, 2.0000E+03
BS000107, F3, 80.0, 2.0000E+03
BS000108, F4, 80.0, 2.0000E+03

** BOTTOMEJECT
*FILM, OP=NEW
BS000109, F1, 80.0, 2.0000E+03
BS000110, F2, 80.0, 2.0000E+03
BS000111, F3, 80.0, 2.0000E+03
BS000112, F4, 80.0, 2.0000E+03

** LFEJECT
*FILM, OP=NEW
BS000113, F1, 80.0, 2.0000E+03
BS000114, F2, 80.0, 2.0000E+03
BS000115, F3, 80.0, 2.0000E+03
BS000116, F4, 80.0, 2.0000E+03

** WLINE1

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*FILM, OP=NEW
BS000117, F1, 30.0, 6.0000E+03
BS000118, F2, 30.0, 6.0000E+03
    7406, F3, 30.0, 6.0000E+03
BS000119, F4, 30.0, 6.0000E+03
** WLINE2
*FILM, OP=NEW
BS000120, F1, 30.0, 6.0000E+03
BS000121, F2, 30.0, 6.0000E+03
BS000122, F4, 30.0, 6.0000E+03
** WLINE3
*FILM, OP=NEW
BS000123, F1, 30.0, 6.0000E+03
BS000124, F2, 30.0, 6.0000E+03
    13546, F3, 30.0, 6.0000E+03
BS000125, F4, 30.0, 6.0000E+03
** WLINE4
*FILM, OP=NEW
BS000126, F1, 30.0, 6.0000E+03
BS000127, F2, 30.0, 6.0000E+03
BS000128, F4, 30.0, 6.0000E+03
**
** SIDE CORE
**
** TOPSIDE C
*FILM, OP=NEW
BS000201, F1, 80.0, 2.0000E+03
BS000202, F2, 80.0, 2.0000E+03
BS000203, F3, 80.0, 2.0000E+03
BS000204, F4, 80.0, 2.0000E+03
** LEFTSIDE C
*FILM, OP=NEW
BS000205, F1, 80.0, 2.0000E+03
BS000206, F2, 80.0, 2.0000E+03
BS000207, F3, 80.0, 2.0000E+03
BS000208, F4, 80.0, 2.0000E+03
** BOTTOMSIDE C
*FILM, OP=NEW
BS000209, F1, 80.0, 2.0000E+03
BS000210, F2, 80.0, 2.0000E+03
BS000211, F3, 80.0, 2.0000E+03
BS000212, F4, 80.0, 2.0000E+03
**
** PAN CORE
**
** TOPPANC
*FILM, OP=NEW
BS000301, F1, 80.0, 2.0000E+03
BS000302, F2, 80.0, 2.0000E+03
BS000303, F3, 80.0, 2.0000E+03
BS000304, F4, 80.0, 2.0000E+03
**
** COVER DIE
**
** BACKCOVERD
*FILM, OP=NEW
BS000401, F1, 80.0, 2.0000E+03
BS000402, F2, 80.0, 2.0000E+03
BS000403, F3, 80.0, 2.0000E+03
BS000404, F4, 80.0, 2.0000E+03
** TOPCOVERD
*FILM, OP=NEW
BS000405, F1, 80.0, 2.0000E+03
BS000406, F2, 80.0, 2.0000E+03
BS000407, F3, 80.0, 2.0000E+03
BS000408, F4, 80.0, 2.0000E+03
** LEFTCOVERD
*FILM, OP=NEW
BS000409, F1, 80.0, 2.0000E+03
BS000410, F2, 80.0, 2.0000E+03
BS000411, F3, 80.0, 2.0000E+03
BS000412, F4, 80.0, 2.0000E+03
** BOTTOMCOVERD
*FILM, OP=NEW
BS000413, F1, 80.0, 2.0000E+03
BS000414, F2, 80.0, 2.0000E+03
BS000415, F3, 80.0, 2.0000E+03
BS000416, F4, 80.0, 2.0000E+03
** FORCED AIR COOLING OF CAVITY SURFACES
**
** EJECTOR DIE
**
** DRUM EJECT
*FILM, OP=NEW
BS000138, F1, 25.0, 4.187E+02
BS000139, F2, 25.0, 4.187E+02
BS000140, F3, 25.0, 4.187E+02
BS000141, F4, 25.0, 4.187E+02
** THICKREJECT
*FILM, OP=NEW
BS000142, F1, 25.0, 4.187E+02
  5816, F2, 25.0, 4.187E+02
  8436, F2, 25.0, 4.187E+02
  20497, F2, 25.0, 4.187E+02
BS000143, F4, 25.0, 4.187E+02
** EXTENSIONEJECT
*FILM, OP=NEW
BS000144, F1, 25.0, 4.187E+02
  16855, F2, 25.0, 4.187E+02
  16775, F3, 25.0, 4.187E+02
  16875, F3, 25.0, 4.187E+02
  16885, F3, 25.0, 4.187E+02
BS000145, F4, 25.0, 4.187E+02
**
** SIDE CORE
**
** CAVITISIDEC
*FILM, OP=NEW
BS000229, F1, 25.0, 4.187E+02
BS000230, F2, 25.0, 4.187E+02

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** BISCUITSIDEC
*FILM, OP=NEW
   21739, F1, 25.0, 4.187E+02
   23494, F1, 25.0, 4.187E+02
   23980, F1, 25.0, 4.187E+02
   23971, F2, 25.0, 4.187E+02
   24097, F2, 25.0, 4.187E+02
   24169, F2, 25.0, 4.187E+02
   23917, F3, 25.0, 4.187E+02
   24043, F3, 25.0, 4.187E+02
** RUNNERSIDEC
*FILM, OP=NEW
   23485, F1, 25.0, 4.187E+02
** GATESIDEC
*FILM, OP=NEW
   27924, F1, 25.0, 4.187E+02
   29356, F3, 25.0, 4.187E+02
** THICKRSIDEC
*FILM, OP=NEW
** EXTENSIONSIDEC
*FILM, OP=NEW
** PAN CORE
**
** CAVITYPANC
*FILM, OP=NEW
   37430, F1, 25.0, 4.187E+02
   37431, F1, 25.0, 4.187E+02
   37432, F1, 25.0, 4.187E+02
   37433, F2, 25.0, 4.187E+02
   37434, F2, 25.0, 4.187E+02
   37435, F2, 25.0, 4.187E+02
   37436, F3, 25.0, 4.187E+02
   37437, F3, 25.0, 4.187E+02
   37438, F3, 25.0, 4.187E+02
   37439, F4, 25.0, 4.187E+02
   37440, F4, 25.0, 4.187E+02
** THICKRPANC
*FILM, OP=NEW
   37441, F1, 25.0, 4.187E+02
   37442, F1, 25.0, 4.187E+02
   37443, F1, 25.0, 4.187E+02
   37444, F2, 25.0, 4.187E+02
   37445, F2, 25.0, 4.187E+02
   37446, F2, 25.0, 4.187E+02
   37447, F3, 25.0, 4.187E+02
   37448, F3, 25.0, 4.187E+02
** EXTENSIONPANC
*FILM, OP=NEW
   38220, F2, 25.0, 4.187E+02
   38221, F2, 25.0, 4.187E+02
   38222, F3, 25.0, 4.187E+02
   38223, F3, 25.0, 4.187E+02
   38224, F3, 25.0, 4.187E+02

**COVER DIE**

****

**BISCUITCOVERD**

*FILM,OP=NEW*

BS000426, F1, 25.0, 4.187E+02
57601, F2, 25.0, 4.187E+02
59201, F2, 25.0, 4.187E+02
57361, F3, 25.0, 4.187E+02

BS000427, F4, 25.0, 4.187E+02

**RUNNERCOVERD**

*FILM,OP=NEW*

57161, F2, 25.0, 4.187E+02
57351, F3, 25.0, 4.187E+02

BS000428, F4, 25.0, 4.187E+02

**GATECOVERD**

*FILM,OP=NEW*

56701, F1, 25.0, 4.187E+02
56711, F1, 25.0, 4.187E+02
52111, F2, 25.0, 4.187E+02

BS000429, F4, 25.0, 4.187E+02

**EXTENSIONCOVERD**

*FILM,OP=NEW*

BS000430, F1, 25.0, 4.187E+02
BS000431, F2, 25.0, 4.187E+02
55921, F3, 25.0, 4.187E+02
57297, F3, 25.0, 4.187E+02

BS000432, F4, 25.0, 4.187E+02

**EJECTOR DIE**

****

**INTWPE**

*FILM,OP=NEW*

BS000129, F1, 25.0, 1.256E+02
BS000130, F2, 25.0, 1.256E+02
BS000131, F3, 25.0, 1.256E+02
BS000132, F4, 25.0, 1.256E+02

**INTWSE**

*FILM,OP=NEW*

BS000133, F1, 25.0, 1.256E+02
BS000134, F2, 25.0, 1.256E+02
BS000135, F3, 25.0, 1.256E+02
BS000136, F4, 25.0, 1.256E+02

**INTWCE**

*FILM,OP=NEW*

16605, F1, 25.0, 1.256E+02
17565, F1, 25.0, 1.256E+02
17575, F1, 25.0, 1.256E+02
16865, F2, 25.0, 1.256E+02
17655, F2, 25.0, 1.256E+02
17485, F3, 25.0, 1.256E+02
17605, F3, 25.0, 1.256E+02
BS000137, F4, 25.0, 1.256E+02  
**  
** SIDE CORE  
**  
** INTWES  
*FILM, OP=NEW  
BS000213, F1, 25.0, 1.256E+02  
BS000214, F2, 25.0, 1.256E+02  
BS000215, F3, 25.0, 1.256E+02  
BS000216, F4, 25.0, 1.256E+02  
** INTWPS  
*FILM, OP=NEW  
BS000217, F1, 25.0, 1.256E+02  
BS000218, F2, 25.0, 1.256E+02  
BS000219, F3, 25.0, 1.256E+02  
BS000220, F4, 25.0, 1.256E+02  
** INTWP2S  
*FILM, OP=NEW  
BS000221, F1, 25.0, 1.256E+02  
BS000222, F2, 25.0, 1.256E+02  
BS000223, F3, 25.0, 1.256E+02  
BS000224, F4, 25.0, 1.256E+02  
** INTWCS  
*FILM, OP=NEW  
BS000225, F1, 25.0, 1.256E+02  
BS000226, F2, 25.0, 1.256E+02  
BS000227, F3, 25.0, 1.256E+02  
BS000228, F4, 25.0, 1.256E+02  
**  
** PAN CORE  
**  
** INTWEP  
*FILM, OP=NEW  
  39164, F1, 25.0, 1.256E+02  
  39365, F1, 25.0, 1.256E+02  
BS000305, F2, 25.0, 1.256E+02  
BS000306, F3, 25.0, 1.256E+02  
BS000307, F4, 25.0, 1.256E+02  
** INTWSP  
*FILM, OP=NEW  
BS000308, F1, 25.0, 1.256E+02  
BS000309, F2, 25.0, 1.256E+02  
BS000310, F3, 25.0, 1.256E+02  
BS000311, F4, 25.0, 1.256E+02  
** INTWCP  
*FILM, OP=NEW  
BS000312, F1, 25.0, 1.256E+02  
  38237, F2, 25.0, 1.256E+02  
  38792, F2, 25.0, 1.256E+02  
BS000313, F4, 25.0, 1.256E+02  
** INTWS2P  
*FILM, OP=NEW  
BS000314, F1, 25.0, 1.256E+02  
BS000315, F2, 25.0, 1.256E+02  
  38215, F3, 25.0, 1.256E+02  
376
** COVER DIE **

** INTWSC **

*FILM, OP=NEW

BS000417, F1, 25.0, 1.256E+02
BS000418, F2, 25.0, 1.256E+02
BS000419, F3, 25.0, 1.256E+02
BS000420, F4, 25.0, 1.256E+02

** INTWPC **

*FILM, OP=NEW

BS000421, F1, 25.0, 1.256E+02
BS000422, F2, 25.0, 1.256E+02
BS000423, F3, 25.0, 1.256E+02
BS000424, F4, 25.0, 1.256E+02

** INTWEC **

*FILM, OP=NEW

55791, F2, 25.0, 1.256E+02
56091, F2, 25.0, 1.256E+02
55881, F3, 25.0, 1.256E+02
56021, F3, 25.0, 1.256E+02

BS000425, F4, 25.0, 1.256E+02

*NODE FILE, FREQUENCY=50, GLOBAL=YES

** END STEP **

** STEP 5 - OPEN DIE STAGE (AIR COOLING OF INTERFACES AND CAVITY SURFACES) **

*STEP, AMPLITUDE=STEP, INC=100

*HEAT TRANSFER, DELTMX=50

.5, 23.9999, .01, 2.0

*RESTART, WRITE, FREQUENCY=50

** HEAT TRANSFER BETWEEN INSERTS AND BLOCKS **

** EJECTD **

** TOPEJECT **

*FILM, OP=NEW

BS000101, F1, 80.0, 2.0000E+03
BS000102, F2, 80.0, 2.0000E+03
BS000103, F3, 80.0, 2.0000E+03
BS000104, F4, 80.0, 2.0000E+03

** BACKEJECT **

*FILM, OP=NEW

BS000105, F1, 80.0, 2.0000E+03
BS000106, F2, 80.0, 2.0000E+03
BS000107, F3, 80.0, 2.0000E+03
BS000108, F4, 80.0, 2.0000E+03

** BOTTOMEJECT **

*FILM, OP=NEW

BS000109, F1, 80.0, 2.0000E+03
BS000110, F2, 80.0, 2.0000E+03
BS000111, F3, 80.0, 2.0000E+03

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BS000112,   F4,  80.0,  2.0000E+03
** LEFTEJECT
*FILM,OP=NEW
BS000113,   F1,  80.0,  2.0000E+03
BS000114,   F2,  80.0,  2.0000E+03
BS000115,   F3,  80.0,  2.0000E+03
BS000116,   F4,  80.0,  2.0000E+03
** WLINE1
*FILM,OP=NEW
BS000117,   F1,  30.0,  6.0000E+03
BS000118,   F2,  30.0,  6.0000E+03
BS000119,   F4,  30.0,  6.0000E+03
** WLINE2
*FILM,OP=NEW
BS000120,   F1,  30.0,  6.0000E+03
BS000121,   F2,  30.0,  6.0000E+03
BS000122,   F4,  30.0,  6.0000E+03
** WLINE3
*FILM,OP=NEW
BS000123,   F1,  30.0,  6.0000E+03
BS000124,   F2,  30.0,  6.0000E+03
BS000125,   F4,  30.0,  6.0000E+03
** WLINE4
*FILM,OP=NEW
BS000126,   F1,  30.0,  6.0000E+03
BS000127,   F2,  30.0,  6.0000E+03
BS000128,   F4,  30.0,  6.0000E+03
** **SIDE CORE
**
** TOPSIDEDEC
*FILM,OP=NEW
BS000201,   F1,  80.0,  2.0000E+03
BS000202,   F2,  80.0,  2.0000E+03
BS000203,   F3,  80.0,  2.0000E+03
BS000204,   F4,  80.0,  2.0000E+03
** LEFTSIDEDEC
*FILM,OP=NEW
BS000205,   F1,  80.0,  2.0000E+03
BS000206,   F2,  80.0,  2.0000E+03
BS000207,   F3,  80.0,  2.0000E+03
BS000208,   F4,  80.0,  2.0000E+03
** BOTTOMSIDEDEC
*FILM,OP=NEW
BS000209,   F1,  80.0,  2.0000E+03
BS000210,   F2,  80.0,  2.0000E+03
BS000211,   F3,  80.0,  2.0000E+03
BS000212,   F4,  80.0,  2.0000E+03
**
**PAN CORE
**
** TOPPANC
*FILM,OP=NEW

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**COVER DIE**

**BACKCOVERD**
*FILM, OP=NEW*

| BS000301 | F1, 80.0, 2.0000E+03 |
| BS000302 | F2, 80.0, 2.0000E+03 |
| BS000303 | F3, 80.0, 2.0000E+03 |
| BS000304 | F4, 80.0, 2.0000E+03 |

**TOPOVERD**
*FILM, OP=NEW*

| BS000401 | F1, 80.0, 2.0000E+03 |
| BS000402 | F2, 80.0, 2.0000E+03 |
| BS000403 | F3, 80.0, 2.0000E+03 |
| BS000404 | F4, 80.0, 2.0000E+03 |

**LEFTCOVERD**
*FILM, OP=NEW*

| BS000409 | F1, 80.0, 2.0000E+03 |
| BS000410 | F2, 80.0, 2.0000E+03 |
| BS000411 | F3, 80.0, 2.0000E+03 |
| BS000412 | F4, 80.0, 2.0000E+03 |

**BOTTOMCOVERD**
*FILM, OP=NEW*

| BS000413 | F1, 80.0, 2.0000E+03 |
| BS000414 | F2, 80.0, 2.0000E+03 |
| BS000415 | F3, 80.0, 2.0000E+03 |
| BS000416 | F4, 80.0, 2.0000E+03 |

**AIR COOLING OF CAVITY SURFACES**

**EJECTOR DIE**

**DRUMEJECT**
*FILM, OP=NEW*

| BS000138 | F1, 25.0, 1.256E+02 |
| BS000139 | F2, 25.0, 1.256E+02 |
| BS000140 | F3, 25.0, 1.256E+02 |
| BS000141 | F4, 25.0, 1.256E+02 |

**THICKREJECT**
*FILM, OP=NEW*

| BS000142 | F1, 5816, 25.0, 1.256E+02 |
| BS000143 | F4, 25.0, 1.256E+02 |

**EXTENSIONEJECT**
*FILM, OP=NEW*

| BS000144 | F1, 16855, 25.0, 1.256E+02 |
| BS000145 | F3, 25.0, 1.256E+02 |

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** SIDE CORE **

** CAVITISIDEC *
*FILM, OP=NEW
BS000229, F1, 25.0, 1.256E+02
BS000230, F2, 25.0, 1.256E+02
BS000231, F3, 25.0, 1.256E+02
BS000232, F4, 25.0, 1.256E+02

** BISCUITSIDEC *
*FILM, OP=NEW
21739, F1, 25.0, 1.256E+02
23494, F1, 25.0, 1.256E+02
23980, F1, 25.0, 1.256E+02
23971, F2, 25.0, 1.256E+02
24097, F2, 25.0, 1.256E+02
24169, F2, 25.0, 1.256E+02
23917, F3, 25.0, 1.256E+02
24043, F3, 25.0, 1.256E+02
BS000233, F4, 25.0, 1.256E+02

** RUNNERSIDEC *
*FILM, OP=NEW
23485, F1, 25.0, 1.256E+02
BS000234, F4, 25.0, 1.256E+02

** GATESIDEC *
*FILM, OP=NEW
27924, F1, 25.0, 1.256E+02
29356, F3, 25.0, 1.256E+02
BS000235, F4, 25.0, 1.256E+02

** THICKRSIDEC *
*FILM, OP=NEW
BS000236, F1, 25.0, 1.256E+02
BS000237, F2, 25.0, 1.256E+02
BS000238, F3, 25.0, 1.256E+02
BS000239, F4, 25.0, 1.256E+02

** EXTENSIONSIDEC *
*FILM, OP=NEW
BS000240, F1, 25.0, 1.256E+02
BS000241, F2, 25.0, 1.256E+02
BS000242, F3, 25.0, 1.256E+02
BS000243, F4, 25.0, 1.256E+02

** PAN CORE **

** CAVITYPANC *
*FILM, OP=NEW
BS000317, F1, 25.0, 1.256E+02
BS000318, F2, 25.0, 1.256E+02
38650, F3, 25.0, 1.256E+02
38695, F3, 25.0, 1.256E+02
BS000319, F4, 25.0, 1.256E+02

** THICKRPANC *
*FILM, OP=NEW
37691, F1, 25.0, 1.256E+02
BS000320, F2, 25.0, 1.256E+02
  37686, F3, 25.0, 1.256E+02
  38903, F3, 25.0, 1.256E+02
BS000321, F4, 25.0, 1.256E+02
** EXTENSIONFANC
*FILM, OP=NEW
  38230, F2, 25.0, 1.256E+02
  38226, F3, 25.0, 1.256E+02
  38227, F3, 25.0, 1.256E+02
  38229, F4, 25.0, 1.256E+02
**
** COVER DIE
**
** BISCUITCOVERD
*FILM, OP=NEW
BS000426, F1, 25.0, 1.256E+02
  57601, F2, 25.0, 1.256E+02
  59201, F2, 25.0, 1.256E+02
  57301, F3, 25.0, 1.256E+02
BS000427, F4, 25.0, 1.256E+02
** RUNNERCOVERD
*FILM, OP=NEW
  57161, F2, 25.0, 1.256E+02
  57351, F3, 25.0, 1.256E+02
BS000428, F4, 25.0, 1.256E+02
** GATECOVERD
*FILM, OP=NEW
  56701, F1, 25.0, 1.256E+02
  56711, F1, 25.0, 1.256E+02
  52111, F2, 25.0, 1.256E+02
BS000429, F4, 25.0, 1.256E+02
** EXTENSIONCOVERD
*FILM, OP=NEW
BS000430, F1, 25.0, 1.256E+02
BS000431, F2, 25.0, 1.256E+02
  55921, F3, 25.0, 1.256E+02
  57297, F3, 25.0, 1.256E+02
BS000432, F4, 25.0, 1.256E+02
** AIR COOLING OF INTERFACES
**
** EJECTOR DIE
**
** INTWPE
*FILM, OP=NEW
BS000129, F1, 25.0, 1.256E+02
BS000130, F2, 25.0, 1.256E+02
BS000131, F3, 25.0, 1.256E+02
BS000132, F4, 25.0, 1.256E+02
** INTWSE
*FILM, OP=NEW
BS000133, F1, 25.0, 1.256E+02
BS000134, F2, 25.0, 1.256E+02
BS000135, F3, 25.0, 1.256E+02
BS000136, F4, 25.0, 1.256E+02
** INTWCE
*FILM,OP=NEW
  16605,  F1, 25.0, 1.256E+02
  17565,  F1, 25.0, 1.256E+02
  17575,  F1, 25.0, 1.256E+02
  16865,  F2, 25.0, 1.256E+02
  17655,  F2, 25.0, 1.256E+02
  17485,  F3, 25.0, 1.256E+02
  17605,  F3, 25.0, 1.256E+02
  BS000137, F4, 25.0, 1.256E+02
**
**SIDE CORE
**
** INTWES
*FILM,OP=NEW
  BS000213, F1, 25.0, 1.256E+02
  BS000214, F2, 25.0, 1.256E+02
  BS000215, F3, 25.0, 1.256E+02
  BS000216, F4, 25.0, 1.256E+02
** INTWPS
*FILM,OP=NEW
  BS000217, F1, 25.0, 1.256E+02
  BS000218, F2, 25.0, 1.256E+02
  BS000219, F3, 25.0, 1.256E+02
  BS000220, F4, 25.0, 1.256E+02
** INTWP2S
*FILM,OP=NEW
  BS000221, F1, 25.0, 1.256E+02
  BS000222, F2, 25.0, 1.256E+02
  BS000223, F3, 25.0, 1.256E+02
  BS000224, F4, 25.0, 1.256E+02
** INTWCS
*FILM,OP=NEW
  BS000225, F1, 25.0, 1.256E+02
  BS000226, F2, 25.0, 1.256E+02
  BS000227, F3, 25.0, 1.256E+02
  BS000228, F4, 25.0, 1.256E+02
**
**PAN CORE
**
** INTWEP
*FILM,OP=NEW
  39164,  F1, 25.0, 1.256E+02
  39365,  F1, 25.0, 1.256E+02
  BS000305, F2, 25.0, 1.256E+02
  BS000306, F3, 25.0, 1.256E+02
  BS000307, F4, 25.0, 1.256E+02
** INTWSP
*FILM,OP=NEW
  BS000308, F1, 25.0, 1.256E+02
  BS000309, F2, 25.0, 1.256E+02
  BS000310, F3, 25.0, 1.256E+02
  BS000311, F4, 25.0, 1.256E+02
** INTWCP
*FILM,OP=NEW
  BS000312, F1, 25.0, 1.256E+02

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38237, F2, 25.0, 1.256E+02
38792, F2, 25.0, 1.256E+02
BS000313, F4, 25.0, 1.256E+02
** INTWS2P
*FILM, OP=NEW
BS000314, F1, 25.0, 1.256E+02
BS000315, F2, 25.0, 1.256E+02
38215, F3, 25.0, 1.256E+02
BS000316, F4, 25.0, 1.256E+02
**
** COVER DIE
**
** INTWS2C
*FILM, OP=NEW
BS000417, F1, 25.0, 1.256E+02
BS000418, F2, 25.0, 1.256E+02
BS000419, F3, 25.0, 1.256E+02
BS000420, F4, 25.0, 1.256E+02
** INTW2FC
*FILM, OP=NEW
BS000421, F1, 25.0, 1.256E+02
BS000422, F2, 25.0, 1.256E+02
BS000423, F3, 25.0, 1.256E+02
BS000424, F4, 25.0, 1.256E+02
** INTW2EC
*FILM, OP=NEW
55791, F2, 25.0, 1.256E+02
56091, F2, 25.0, 1.256E+02
55881, F3, 25.0, 1.256E+02
56021, F3, 25.0, 1.256E+02
BS000425, F4, 25.0, 1.256E+02
*NODE FILE, FREQUENCY=50, GLOBAL=YES
*NODE PRINT, FREQUENCY=0
*EL PRINT, FREQUENCY=0
*END STEP
** CYCLE 2 and CYCLE 3 are identical to the very first cycle with the exception
of time instants used
*ELSET, ELSET=BS000101
3388, 3398, 3788, 4298, 12649, 12889, 13009, 13539, 14059, 14259, 14269, 14369, 14379
14529, 14939
*ELSET, ELSET=BS000102
1379, 1439, 1819, 4328, 4399, 4439, 14199, 14349, 14479, 14809, 14839, 15019
*ELSET, ELSET=BS000103
1349, 3808, 3938, 3978, 14149, 14159, 14489
*ELSET, ELSET=BS000104
1083, 1219, 1329, 1359, 1399, 1409, 1429, 1449, 1469, 1539, 1699, 1709, 1729, 1759, 1779
1789, 1809, 1839, 2089, 2109, 2539, 2549, 2569, 3428, 3738, 3818, 4008, 4038, 4419, 4489
4499, 4529, 12719, 12859, 12869, 13049, 13459, 13509, 13569, 13579, 13959, 14109, 14129
14179, 14189, 14209, 14229, 14289, 14329, 14339, 14419, 14429, 14449, 14469, 14779
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APPENDIX D

INPUT FILE FOR THE CASE STUDY:
ABAQUS 3-D STRUCTURAL ANALYSIS

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SDRC I-DEAS ABAQUS FILE TRANSLATOR 27-Apr-99 12:16:20

*NODE, SYSTEM=R, NSET=NEJECTD
  1, -2.7199978E-01, -2.9450650E-01, 4.5278789E-01
  2, -3.6004208E-01, -4.1497334E-01, 7.1967473E-01
  4248, -2.7716128E-01, -2.6719267E-01, 3.9838137E-01
  4249, -3.4476935E-01, -3.9831787E-01, 7.1484558E-01

*NODE, SYSTEM=R, NSET=NSIDEC
  4250, -4.0884516E-01, -2.4218702E-01, 7.725515E-01
  4251, -3.739693E-01, -2.3495847E-01, 4.0326232E-01
  7753, -3.7309573E-01, -2.4823352E-01, 3.8286726E-01
  7754, -3.9163827E-01, -2.3985269E-01, 3.8897649E-01

*NODE, SYSTEM=R, NSET=NCOVERD
  8261, -3.939433E-01, -5.3391781E-01, 1.3033019E-01
  8262, -4.1896769E-01, -1.3440668E-01, 1.7321927E-01
  12613, -4.3398349E-01, -5.0082060E-02, 2.0845785E-01
  12612, -4.3398349E-01, -9.3422040E-02, 1.7323385E-01

*ELEMENT, TYPE=C3D4, ELSET=EEJECTD
  1, 801, 1669, 47, 565
  2, 2467, 2419, 3416, 4142
  20583, 1084, 4017, 2055, 1082
  20584, 2961, 67, 2959, 2947

*ELEMENT, TYPE=C3D4, ELSET=ESIDEC
  20585, 4684, 5946, 5207, 6963
  20586, 4310, 6653, 4588, 4784
  37416, 4475, 4705, 4473, 4449
  37417, 6396, 4255, 6394, 6388

*ELEMENT, TYPE=C3D4, ELSET=EPANC
  37418, 8118, 8120, 8010, 8145
  37419, 8118, 8010, 7994, 8145
  39404, 8256, 8060, 8233, 8231
  39405, 7892, 8256, 7891, 7887

*ELEMENT, TYPE=C3D4, ELSET=ECOVERD
  39406, 8985, 10385, 10959, 10993
  39407, 12604, 12240, 8338, 8272
  60449, 9424, 9536, 9324, 9332
  60450, 9513, 9023, 9084, 9032

*SOLID SECTION, ELSET=EEJECTD, MATERIAL=H13
*SOLID SECTION, ELSET=ESIDEC, MATERIAL=H13
*SOLID SECTION, ELSET=EPANC, MATERIAL=H13
*SOLID SECTION, ELSET=ECOVERD, MATERIAL=H13
*MATERIAL, NAME=H13

*EXPANSION, ZERO=21.111
  1.188E-5, 93.333
  1.224E-5, 204.444
  1.224E-5, 315.555
  1.296E-5, 426.666
  1.332E-5, 537.777
  1.404E-5, 648.888

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*ELASTIC
2.137E+11, 0.3, 21.111
2.068E+11, 0.3, 93.333
1.999E+11, 0.3, 204.444
1.862E+11, 0.3, 315.555
1.793E+11, 0.3, 426.666
1.517E+11, 0.3, 537.777
1.103E+11, 0.3, 648.888
*PLASTIC
1.213E+9. 0., 21.111
1.131E+9. 0., 93.333
1.103E+9. 0., 204.444
1.048E+9. 0., 315.555
0.965E+9, 0., 426.666
0.855E+9, 0., 537.777
0.634E+9, 0., 648.888
*SURFACE DEFINITION, NAME=INTWSE
BS000133, S1
BS000134, S2
BS000135, S3
BS000136, S4
*SURFACE DEFINITION, NAME=INTWES
BS000213, S1
BS000214, S2
BS000215, S3
BS000216, S4
*CONTACT PAIR, SMALL SLIDING, INTERACTION=CONT, HCRIT=0.2
INTWES, INTWSE
*SURFACE DEFINITION, NAME=INTWEP
39164, S1
39365, S1
BS000305, S2
BS000306, S3
BS000307, S4
*SURFACE DEFINITION, NAME=INTWPE
BS000129, S1
BS000130, S2
BS000131, S3
BS000132, S4
*CONTACT PAIR, SMALL SLIDING, INTERACTION=CONT, HCRIT=0.2
INTWPE, INTWEP
*SURFACE DEFINITION, NAME=INTWSC
BS000417, S1
BS000418, S2
BS000419, S3
BS000420, S4
*SURFACE DEFINITION, NAME=INTWCS
BS000225, S1
BS000226, S2
BS000227, S3
BS000228, S4
*CONTACT PAIR, SMALL SLIDING, INTERACTION=CONT, HCRIT=0.2
INTWCS, INTWSC
*SURFACE DEFINITION, NAME=INTWCP
BS000312, S1

387
*CONTACT PAIR, SMALL SLIDING, INTERACTION=CONT, HCRIT=0.2
INTWPC, INTWCP

*CONTACT PAIR, SMALL SLIDING, INTERACTION=CONT, HCRIT=0.2
INTWCE, INTWEC

*CONTACT PAIR, SMALL SLIDING, INTERACTION=CONT, HCRIT=0.2
INTWPS, INTWSP

**AMPLITUDES FOR PRESSURE PROFILE

**AMPLITUDE, NAME=CAVPRESSURE, TIME=TOTAL TIME
0.0, 0.0, 0.1, 0.17, 2.1375, 0.0

**AMPLITUDE, NAME=CAVPRESSUR2, TIME=TOTAL TIME
90.0, 90.0, 105.17, 2.1375, 0.0

388
*AMPLITUDE, NAME=CAVPRESSUR3, TIME=TOTAL TIME
180.0, 180.10517, 21375000, 210.0, 0.

**CYCLE1

*INITIAL CONDITIONS, TYPE=TEMPERATURE
NEJECTD, 100.0
NSIDEC, 100.0
NPANC, 100.0
NCOVERD, 100.0

** DIES ALREADY ASSUMED TO BE CLAMPED
** STEP 1 - INJECTION AND SOLIDIFICATION (Dwell)
*STEP, AMPLITUDE=STEP, INC=200

*STATIC
.25, 30.0, 0.001, 1.0

**RESTART, WRITE, FREQUENCY=50

**RERAINTS

** BACKCOVER

*BOUNDARY, OP=NEW
BS005001, 3, , .000000E+00

**LSIDEJ
1157, 1, , .0000E+00
1177, 1, , .0000E+00
1175, 1, , .0000E+00
1159, 1, , .0000E+00
1155, 1, , .0000E+00
1161, 1, , .0000E+00

**LSIDESC
5206, 1, , .0000E+00
5244, 1, , .0000E+00
5238, 1, , .0000E+00
5208, 1, , .0000E+00
5232, 1, , .0000E+00
5242, 1, , .0000E+00

**LSIDECD
9749, 1, , .0000E+00
9751, 1, , .0000E+00
10009, 1, , .0000E+00
9991, 1, , .0000E+00
10023, 1, , .0000E+00
9989, 1, , .0000E+00

**TOPEJECT
2695, 2, , .0000E+00
2685, 2, , .0000E+00
2683, 2, , .0000E+00
2649, 2, , .0000E+00
2659, 2, , .0000E+00
2651, 2, , .0000E+00
2657, 2, , .0000E+00

**BOTTOMJECT
1301, 2, , .0000E+00
1297, 2, , .0000E+00
1295, 2, , .0000E+00
1309, 2, , .0000E+00
1323, 2, , .0000E+00
1293, 2, , .0000E+00

**TOPSIDECD
6050.2, .0000E+00
6016.2, .0000E+00
6020.2, .0000E+00
6024.2, .0000E+00
6048.2, .0000E+00
** BOTTOMSIDE
5518.2, .0000E+00
5506.2, .0000E+00
5514.2, .0000E+00
5486.2, .0000E+00
5508.2, .0000E+00
** TOPPANC
7941.2, .0000E+00
7954.2, .0000E+00
7942.2, .0000E+00
7945.2, .0000E+00
7943.2, .0000E+00
** TOPCOVER
11359.2, .0000E+00
11357.2, .0000E+00
11327.2, .0000E+00
11355.2, .0000E+00
11237.2, .0000E+00
11353.2, .0000E+00
11239.2, .0000E+00
** BOTTOMCOVER
10303.2, .0000E+00
10273.2, .0000E+00
10299.2, .0000E+00
10271.2, .0000E+00
10183.2, .0000E+00
10287.2, .0000E+00
10185.2, .0000E+00
** SYMMETRY RESTRAINTS
BS006001,  1, , .00000E+00
BS006001,  5, 6, .00000E+00
BS007001,  1, , .00000E+00
BS007001,  5, 6, .00000E+00
BS008001,  1, , .00000E+00
BS008001,  5, 6, .00000E+00
BS009001,  1, , .00000E+00
BS009001,  5, 6, .00000E+00
** CLAMPING PRESSURE
** BACKEJECTOR
*DLOAD
BS000105, P1, 3.56000E+06
BS000106, P2, 3.56000E+06
BS000107, P3, 3.56000E+06
BS000108, P4, 3.56000E+06
** CAVITY PRESSURE
** DRUMEJECT
*DLOAD, AMP=CAVPRESSURE
BS000138, P1, .1000E+01
BS000139, P2, .1000E+01
BS000140, P3, .1000E+01
BS000141, P4, \(1.000E+01\)
** THICKREJECT
*DLOAD, AMP=CAVPRESSURE
BS000142, P1, \(1.000E+01\)
5816, P2, \(1.000E+01\)
8436, P2, \(1.000E+01\)
20497, P2, \(1.000E+01\)
BS000143, P4, \(1.000E+01\)
** EXTENSIONREJECT
*DLOAD, AMP=CAVPRESSURE
BS000144, P1, \(1.000E+01\)
16855, P2, \(1.000E+01\)
16775, P3, \(1.000E+01\)
16875, P3, \(1.000E+01\)
16885, P3, \(1.000E+01\)
BS000145, P4, \(1.000E+01\)
** CAVITISIDE
*DLOAD, AMP=CAVPRESSURE
BS000229, P1, \(1.000E+01\)
BS000230, P2, \(1.000E+01\)
BS000231, P3, \(1.000E+01\)
BS000232, P4, \(1.000E+01\)
** BISCUITSIDE
*DLOAD, AMP=CAVPRESSURE
21739, P1, \(1.000E+01\)
23494, P1, \(1.000E+01\)
23980, P1, \(1.000E+01\)
23971, P2, \(1.000E+01\)
24097, P2, \(1.000E+01\)
24169, P2, \(1.000E+01\)
23917, P3, \(1.000E+01\)
24043, P3, \(1.000E+01\)
BS000233, P4, \(1.000E+01\)
** RUNNERSIDE
*DLOAD, AMP=CAVPRESSURE
23485, P1, \(1.000E+01\)
BS000234, P4, \(1.000E+01\)
** GATESIDE
*DLOAD, AMP=CAVPRESSURE
27924, P1, \(1.000E+01\)
29356, P3, \(1.000E+01\)
BS000235, P4, \(1.000E+01\)
** THICKRSIDE
*DLOAD, AMP=CAVPRESSURE
BS000236, P1, \(1.000E+01\)
BS000237, P2, \(1.000E+01\)
BS000238, P3, \(1.000E+01\)
BS000239, P4, \(1.000E+01\)
** EXTENSIONSIDE
*DLOAD, AMP=CAVPRESSURE
BS000240, P1, \(1.000E+01\)
BS000241, P2, \(1.000E+01\)
BS000242, P3, \(1.000E+01\)
BS000243, P4, \(1.000E+01\)
** CAVITYPANC
*DLOAD, AMP=CAVPRESSURE
BS000317, P1, .1000E+01
BS000318, P2, .1000E+01
38650, P3, .1000E+01
38695, P3, .1000E+01
BS000319, P4, .1000E+01
** THICKRPANC
*DLOAD, AMP=CAVPRESSURE
37691, P1, .1000E+01
BS000320, P2, .1000E+01
37686, P3, .1000E+01
38903, P3, .1000E+01
BS000321, P4, .1000E+01
** EXTENSIONPANC
*DLOAD, AMP=CAVPRESSURE
38230, P2, .1000E+01
38226, P3, .1000E+01
38227, P3, .1000E+01
38229, P4, .1000E+01
** BISCUITCOVERD
*DLOAD, AMP=CAVPRESSURE
BS000426, P1, .1000E+01
57601, P2, .1000E+01
59201, P2, .1000E+01
57361, P3, .1000E+01
BS000427, P4, .1000E+01
** RUNNERCOVERD
*DLOAD, AMP=CAVPRESSURE
57161, P2, .1000E+01
57351, P3, .1000E+01
BS000428, P4, .1000E+01
** GATECOVERD
*DLOAD, AMP=CAVPRESSURE
56701, P1, .1000E+01
56711, P1, .1000E+01
52111, P2, .1000E+01
BS000429, P4, .1000E+01
** EXTENSIONCOVERD
*DLOAD, AMP=CAVPRESSURE
BS000430, P1, .1000E+01
BS000431, P2, .1000E+01
55921, P3, .1000E+01
57297, P3, .1000E+01
BS000432, P4, .1000E+01
*TEMPERATURE, FILE=thermo3, BSTEP=1
*NODE FILE, FREQUENCY=0, GLOBAL=YES
*EL FILE, FREQUENCY=100, POSITION=NODES
S E EE PE THE
*NODE PRINT, FREQUENCY=0
*EL PRINT, FREQUENCY=0
*CONTACT PRINT, SLAVE=INTWES, MASTER=INTWSE, FREQUENCY=100
CSTRESS,CDISP
*CONTACT PRINT, SLAVE=INTWPE, MASTER=INTWEP, FREQUENCY=100
CSTRESS,CDISP
*CONTACT PRINT, SLAVE=INTWCS, MASTER=INTWSC, FREQUENCY=100
CSTRESS,CDISP
*CONTACT PRINT, SLAVE=INTWPC, MASTER=INTWCP, FREQUENCY=100
CSTRESS,CDISP
*CONTACT PRINT, SLAVE=INTWCE, MASTER=INTWEC, FREQUENCY=100
CSTRESS,CDISP
*CONTACT PRINT, SLAVE=INTWPS, MASTER=INTWSP, FREQUENCY=100
CSTRESS,CDISP
*END STEP
** STEP 2 - OPEN DIE STAGE
*STEP, AMPLITUDE=STEP, INC=200
*STATIC
.25, 11.9999, .0001, 1.0
*RESTART, WRITE, FREQUENCY=50
**RERAINTS
** BACKCOVER
*BOUNDARY, OP=NEW
BS005001, 3,, .00000E+00
**LSIDEJ
  1157, 1,, .00000E+00
  1177, 1,, .00000E+00
  1175, 1,, .00000E+00
  1159, 1,, .00000E+00
  1155, 1,, .00000E+00
  1161, 1,, .00000E+00
**LSIDES
  5206, 1,, .00000E+00
  5244, 1,, .00000E+00
  5238, 1,, .00000E+00
  5208, 1,, .00000E+00
  5232, 1,, .00000E+00
  5242, 1,, .00000E+00
**LSIDECD
  9749, 1,, .00000E+00
  9751, 1,, .00000E+00
  10009, 1,, .00000E+00
  9991, 1,, .00000E+00
  10023, 1,, .00000E+00
  9989, 1,, .00000E+00
** TOPEJECT
  2695, 2,, .00000E+00
  2685, 2,, .00000E+00
  2683, 2,, .00000E+00
  2649, 2,, .00000E+00
  2659, 2,, .00000E+00
  2651, 2,, .00000E+00
  2657, 2,, .00000E+00
** BOTOMEJECT
  1301, 2,, .00000E+00
  1297, 2,, .00000E+00
  1295, 2,, .00000E+00
  1309, 2,, .00000E+00

393
1323,2,,.0000E+00
1293,2,,.0000E+00
** TOPSIDE C
6050,2,,.0000E+00
6016,2,,.0000E+00
6020,2,,.0000E+00
6024,2,,.0000E+00
6048,2,,.0000E+00
** BOTTOMSIDE C
5518,2,,.0000E+00
5506,2,,.0000E+00
5514,2,,.0000E+00
5486,2,,.0000E+00
5508,2,,.0000E+00
** TOPPANCE
7941,2,,.0000E+00
7954,2,,.0000E+00
7942,2,,.0000E+00
7945,2,,.0000E+00
7943,2,,.0000E+00
** TOPCOVERD
11359,2,,.0000E+00
11357,2,,.0000E+00
11327,2,,.0000E+00
11355,2,,.0000E+00
11237,2,,.0000E+00
11353,2,,.0000E+00
11239,2,,.0000E+00
** BOTTOMCOVERD
10303,2,,.0000E+00
10273,2,,.0000E+00
10299,2,,.0000E+00
10271,2,,.0000E+00
10183,2,,.0000E+00
10287,2,,.0000E+00
10185,2,,.0000E+00
** SYMMETRY RESTR ANTS
BS006001,1,,.00000E+00
BS006001,5,6,.00000E+00
BS007001,1,,.00000E+00
BS007001,5,6,.00000E+00
BS008001,1,,.00000E+00
BS008001,5,6,.00000E+00
BS009001,1,,.00000E+00
BS009001,5,6,.00000E+00
*TEMPERATURE, FILE=thermo3, BSTEP=2
*NODE FILE, FREQUENCY=0, GLOBAL=YES
*EL FILE, FREQUENCY=100, POSITION=NODES
S
E
EE
PE
THE
*NODE PRINT, FREQUENCY=0
*EL PRINT, FREQUENCY=0

394
*END STEP
** STEP 3 - SPRAY COOLING
*STEP,AMPLITUDE=STEP,INC=200
*STATIC
 .25,10.999,.0001,1.0
*RESTART, WRITE, FREQUENCY=50
** RESTRAINTS
** BACKCOVER
*BOUNDARY,OP=NEW
BS005001, 3,, .00000E+00
**LSIDEEJ
  1157,1,, .00000E+00
  1177,1,, .00000E+00
  1175,1,, .00000E+00
  1159,1,, .00000E+00
  1155,1,, .00000E+00
  1161,1,, .00000E+00
**LSIDESC
  5206,1,, .00000E+00
  5244,1,, .00000E+00
  5238,1,, .00000E+00
  5208,1,, .00000E+00
  5232,1,, .00000E+00
  5242,1,, .00000E+00
**LSIDECR
  9749,1,, .00000E+00
  9751,1,, .00000E+00
  10009,1,, .00000E+00
  9991,1,, .00000E+00
  10023,1,, .00000E+00
  9989,1,, .00000E+00
** TOPEJECT
  2695,2,, .00000E+00
  2685,2,, .00000E+00
  2683,2,, .00000E+00
  2649,2,, .00000E+00
  2659,2,, .00000E+00
  2651,2,, .00000E+00
  2657,2,, .00000E+00
** BOTOMEJECT
  1301,2,, .00000E+00
  1297,2,, .00000E+00
  1295,2,, .00000E+00
  1309,2,, .00000E+00
  1323,2,, .00000E+00
  1293,2,, .00000E+00
** TOPSIDECD
  6050,2,, .00000E+00
  6016,2,, .00000E+00
  6020,2,, .00000E+00
  6024,2,, .00000E+00
  6048,2,, .00000E+00
** BOTTOMSIDECD
  5518,2,, .00000E+00
  5506,2,, .00000E+00
** TOPPANC
7941, 2, .0000E+00
7954, 2, .0000E+00
7942, 2, .0000E+00
7945, 2, .0000E+00
7943, 2, .0000E+00

** TOPCOVERD
11359, 2, .0000E+00
11357, 2, .0000E+00
11327, 2, .0000E+00
11355, 2, .0000E+00
11237, 2, .0000E+00
11353, 2, .0000E+00
11239, 2, .0000E+00

** BOTTOMCOVERD
10273, 2, .0000E+00
10299, 2, .0000E+00
10271, 2, .0000E+00
10183, 2, .0000E+00
10287, 2, .0000E+00
10185, 2, .0000E+00

** SYMMETRY RESTRAINTS
BS006001, 1,, .00000E+00
BS006001, 5, 6, .00000E+00
BS007001, 1,, .00000E+00
BS007001, 5, 6, .00000E+00
BS008001, 1,, .00000E+00
BS008001, 5, 6, .00000E+00
BS009001, 1,, .00000E+00
BS009001, 5, 6, .00000E+00

*TEMPERATURE, FILE=thermo3, BSTEP=3
*NODE FILE, FREQUENCY=0, GLOBAL=YES
*EL FILE, FREQUENCY=100, POSITION=NODES
*END STEP

** STEP 4 - FORCED AIR COOLING
*STEP, AMPLITUDE=STEP, INC=200
*STATIC
.25, 12.9999, .0001, 1.0
*RESTART, WRITE, FREQUENCY=50

** RESTRAINTS
** BACKCOVER
*BOUNDARY, OP=NEW
BS005001, 3,, .00000E+00

** LSIDE

396
1157.1, .0000E+00
1177.1, .0000E+00
1175.1, .0000E+00
1159.1, .0000E+00
1155.1, .0000E+00
1161.1, .0000E+00
**LSIDESC
5206.1, .0000E+00
5244.1, .0000E+00
5238.1, .0000E+00
5208.1, .0000E+00
5232.1, .0000E+00
5242.1, .0000E+00
**LSIDEC
9749.1, .0000E+00
9751.1, .0000E+00
10009.1, .0000E+00
9991.1, .0000E+00
10023.1, .0000E+00
9989.1, .0000E+00
** TOPEJECT
2695.2, .0000E+00
2685.2, .0000E+00
2683.2, .0000E+00
2649.2, .0000E+00
2659.2, .0000E+00
2651.2, .0000E+00
2657.2, .0000E+00
** BOTTOMEJECT
1301.2, .0000E+00
1297.2, .0000E+00
1295.2, .0000E+00
1309.2, .0000E+00
1323.2, .0000E+00
1293.2, .0000E+00
** TOPSIDEC
6050.2, .0000E+00
6016.2, .0000E+00
6020.2, .0000E+00
6024.2, .0000E+00
6048.2, .0000E+00
** BOTTOMSIDEC
5518.2, .0000E+00
5506.2, .0000E+00
5514.2, .0000E+00
5486.2, .0000E+00
5508.2, .0000E+00
** TOPPANC
7941.2, .0000E+00
7954.2, .0000E+00
7942.2, .0000E+00
7945.2, .0000E+00
7943.2, .0000E+00
** TOPCOVERD
11359.2, .0000E+00
**BOTTOMCOVERD**

10303, 2., .0000E+00
10273, 2., .0000E+00
10299, 2., .0000E+00
10271, 2., .0000E+00
10183, 2., .0000E+00
10287, 2., .0000E+00
10185, 2., .0000E+00

**SYMMETRY RERAINTS**

BS006001, 1,, .00000E+00
BS006001, 5, 6, .00000E+00
BS007001, 1,, .00000E+00
BS007001, 5, 6, .00000E+00
BS008001, 1,, .00000E+00
BS008001, 5, 6, .00000E+00
BS009001, 1,, .00000E+00
BS009001, 5, 6, .00000E+00

*TEMPERATURE, FILE=thermo3, BSTEP=4

*NODE FILE, FREQUENCY=0, GLOBAL=YES

*EL FILE, FREQUENCY=100, POSITION=NODES

S
E
EE
PE
THE

*NODE PRINT, FREQUENCY=0

*EL PRINT, FREQUENCY=0

**END STEP**

** STEP 5 - OPEN DIE STAGE**

**STEP, AMPLITUDE=STEP, INC=200**

**STATIC**

.50, 23.9999, .0001, 2.0

*RESTART, WRITE, FREQUENCY=50

**RERAINTS**

** BACKCOVER**

*BOUNDARY, OP=NEW

BS005001, 3,, .00000E+00

**LSDIEEJ**

1157, 1,, .0000E+00
1177, 1,, .0000E+00
1175, 1,, .0000E+00
1159, 1,, .0000E+00
1155, 1,, .0000E+00
1161, 1,, .0000E+00

**LSIDESC**

5206, 1,, .0000E+00
5244, 1,, .0000E+00
5238, 1,, .0000E+00
5208, 1,, .0000E+00
5232,1, .0000E+00
5242,1, .0000E+00
**LSIDECD
9749,1, .0000E+00
9751,1, .0000E+00
10009,1, .0000E+00
9991,1, .0000E+00
10023,1, .0000E+00
9989,1, .0000E+00
** TOPEJECT
2695,2, .0000E+00
2685,2, .0000E+00
2683,2, .0000E+00
2649,2, .0000E+00
2659,2, .0000E+00
2651,2, .0000E+00
2657,2, .0000E+00
** BOTOMEJECT
1301,2, .0000E+00
1297,2, .0000E+00
1295,2, .0000E+00
1309,2, .0000E+00
1323,2, .0000E+00
1293,2, .0000E+00
** TOPSIDECD
6050,2, .0000E+00
6016,2, .0000E+00
6020,2, .0000E+00
6024,2, .0000E+00
6048,2, .0000E+00
** BOTTOMSIDECD
5518,2, .0000E+00
5506,2, .0000E+00
5514,2, .0000E+00
5486,2, .0000E+00
5508,2, .0000E+00
** TOPPANC
7941,2, .0000E+00
7954,2, .0000E+00
7942,2, .0000E+00
7945,2, .0000E+00
7943,2, .0000E+00
** TOPCOVERD
11359,2, .0000E+00
11357,2, .0000E+00
11327,2, .0000E+00
11355,2, .0000E+00
11237,2, .0000E+00
11353,2, .0000E+00
11239,2, .0000E+00
** BOTTOMCOVERD
10303,2, .0000E+00
10273,2, .0000E+00
10299,2, .0000E+00
10271,2, .0000E+00
399
**SYMMETRY RERAINTS**

BS006001, 1,, .00000E+00
BS006001, 5, 6, .00000E+00
BS007001, 1,, .00000E+00
BS007001, 5, 6, .00000E+00
BS008001, 1,, .00000E+00
BS008001, 5, 6, .00000E+00
BS009001, 1,, .00000E+00
BS009001, 5, 6, .00000E+00

*TEMPERATURE, FILE=thermo3, BSTEP=5
*NODE FILE, FREQUENCY=0, GLOBAL=YES
*EL FILE, FREQUENCY=100, POSITION=NODES
S
E
EE
PE
THE

*NODE PRINT, FREQUENCY=0
*EL PRINT, FREQUENCY=0
*END STEP

**CYCLE 2 AND CYCLE 3 ARE IDENTICAL TO THE VERY FIRST CYCLE WITH THE EXCEPTITION OF TIME INSTANTS USED**

*NSET, NSET=BS005001
8292, 8298, 8307, 8319, 8322, 8325, 8328, 8397, 8400, 8403, 8469, 8472, 8475, 8484, 8487
Figure E.1: Density of H-13 tool steel as a function of temperature [Granchi et al, 1983]

Figure E.2: Elastic modulus of H-13 as a function of temperature
Figure E.3: Yield strength of H-13 as a function of temperature

Figure E.4: Ultimate tensile strength of H-13 as a function of temperature
Figure E.5: Coefficient of linear expansion for H-13 as a function of temperature

Figure E.6: Thermal Conductivity for H-13 as a function of temperature

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Figure E.7: Logarithmic ductility for H-13 at elevated temperatures (Rothman, 1988)
Longitudinal short time tensile data 1: Material tempered 2+ 2 hours at 527°C to 52 HRc.
2: Material tempered 2+ 2 hours at 575°C to 48 HRc, 3: Material tempered 2+ 2 hours at 605°C to 44 HRc.

Figure E.8: Specific heat of H-13 as a function of temperature
Table F.1: Recommended composition of A380 aluminum alloy [ADCI, 1977]

<table>
<thead>
<tr>
<th>Content</th>
<th>% Weight</th>
</tr>
</thead>
<tbody>
<tr>
<td>Si</td>
<td>7.5 – 9.5</td>
</tr>
<tr>
<td>Fe</td>
<td>&lt; 1.3</td>
</tr>
<tr>
<td>Cu</td>
<td>3.0 – 4.0</td>
</tr>
<tr>
<td>Mn</td>
<td>&lt; 0.5</td>
</tr>
<tr>
<td>Mg</td>
<td>&lt; 0.1</td>
</tr>
<tr>
<td>Ni</td>
<td>&lt; 0.5</td>
</tr>
<tr>
<td>Zn</td>
<td>&lt; 3.0</td>
</tr>
<tr>
<td>Sn</td>
<td>&lt; 0.35</td>
</tr>
<tr>
<td>others</td>
<td>&lt; 0.5</td>
</tr>
</tbody>
</table>

Table F.2: Standard specified composition for H-13 die material [GM, 1997]

<table>
<thead>
<tr>
<th>Content</th>
<th>% Weight</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>0.37 – 0.42</td>
</tr>
<tr>
<td>Mn</td>
<td>0.20 – 0.50</td>
</tr>
<tr>
<td>Si</td>
<td>0.80 – 1.20</td>
</tr>
<tr>
<td>S</td>
<td>&lt; 0.005</td>
</tr>
<tr>
<td>Cr</td>
<td>5.00 – 5.50</td>
</tr>
<tr>
<td>V</td>
<td>0.80 – 1.20</td>
</tr>
<tr>
<td>Mo</td>
<td>1.20 – 1.75</td>
</tr>
<tr>
<td>P</td>
<td>&lt; 0.020</td>
</tr>
</tbody>
</table>
REFERENCE INPUT PARAMETERS FOR THIN WALL CASTINGS (IN CGS UNITS):

CASTING IS ALUMINUM 380
CONDUCTIVITY OF CASTING = 0.23000 + 0.00000000 * T
DENSITY OF CASTING = 2.70
SPECIFIC HEAT OF CASTING = 0.20000 + 0.00015000 * T
LIQUIDUS OF CASTING = 581.0
SOLIDUS OF CASTING = 540.0
HEAT OF FUSION = 109.0
LIQUID SPECIFIC HEAT = 0.260
DIE IS H13 HOT WORK, AIR HARDENABLE MARTENSITIC STEEL
WITH ABOUT 5 WEIGHT PERCENT CHROMIUM
CONDUCTIVITY OF DIE = 0.05870 + 0.0000086 * T
DENSITY OF DIE = 7.70
SPECIFIC HEAT OF DIE = 0.11000 + 0.0000450 * T
CASTING THICKNESS = 0.40
DIE ONE THICKNESS = 5.00
DIE TWO THICKNESS = 5.00
NUMBER OF ELEMENTS IN CASTING = 10
NUMBER OF ELEMENTS IN DIE ONE = 19
NUMBER OF ELEMENTS IN DIE TWO = 19
INITIAL SUPERHEAT = 25.0
THE INITIAL DIE ONE TEMPERATURE = 100.0
THE INITIAL DIE TWO TEMPERATURE = 100.0
THE AIR TEMPERATURE IS 25.0
THE COOLANT TEMPERATURE IS 25.0
H TO THE AIR = 0.0030
H TO THE COOLANT FROM DIE ONE = 0.0300
H TO THE COOLANT FROM DIE TWO = 0.0300
H FROM THE CASTING TO DIE ONE = 1.0000
H FROM THE CASTING TO DIE TWO = 1.0000
DIE CLOSED TIME PER CYCLE = 10.00
DIE OPEN TIME PER CYCLE = 20.00
THE TOTAL NUMBER OF CYCLES SIMULATED IS 31
HCONTACT AFTER AIR GAP FORMATION = 0.1000
TEMPERATURE FOR AIR GAP FORMATION = 10.00
FROM 6.0 TO 7.5 SECONDS AFTER DIE OPENING FOR DIE ONE
FROM 6.0 TO 7.5 SECONDS AFTER DIE OPENING FOR DIE TWO
SPRAY AT A TEMPERATURE OF 25.0
CAUSING A HEAT TRANSFER COEFFICIENT OF
0.1200 + 0.000001 * T
REFERENCE INPUT PARAMETERS FOR THICK WALL CASTINGS (IN CGS UNITS):

CASTING IS ALUMINUM 380
CONDUCTIVITY OF CASTING = 0.23000 + 0.0000000 T
DENSITY OF CASTING = 2.70
SPECIFIC HEAT OF CASTING = 0.20000 + 0.00015000 T
LIQUIDUS OF CASTING = 581.0
SOLIDUS OF CASTING = 540.0
HEAT OF FUSION = 109.0
LIQUID SPECIFIC HEAT = 0.260
DIE IS H13 HOT WORK, AIR HARDENABLE MARTENSITIC STEEL WITH ABOUT 5 WEIGHT PERCENT CHROMIUM
CONDUCTIVITY OF DIE = 0.05870 + 0.0000086 T
DENSITY OF DIE = 7.70
SPECIFIC HEAT OF DIE = 0.11000 + 0.0000450 T
CASTING THICKNESS = 1.00
DIE ONE THICKNESS = 5.00
DIE TWO THICKNESS = 5.00
NUMBER OF ELEMENTS IN CASTING = 10
NUMBER OF ELEMENTS IN DIE ONE = 19
NUMBER OF ELEMENTS IN DIE TWO = 19
INITIAL SUPERHEAT = 25.0
THE INITIAL DIE ONE TEMPERATURE = 100.0
THE INITIAL DIE TWO TEMPERATURE = 100.0
THE AIR TEMPERATURE IS 25.0
THE COOLANT TEMPERATURE IS 25.0
H TO THE AIR = 0.0030
H TO THE COOLANT FROM DIE ONE = 0.0300
H TO THE COOLANT FROM DIE TWO = 0.0300
H FROM THE CASTING TO DIE ONE = 1.0000
H FROM THE CASTING TO DIE TWO = 1.0000
DIE CLOSED TIME PER CYCLE = 45.00
DIE OPEN TIME PER CYCLE = 30.00
THE TOTAL NUMBER OF CYCLES SIMULATED IS 31
HCONTACT AFTER AIR GAP FORMATION = 0.1000
TEMPERATURE FOR AIR GAP FORMATION = 10.0
FROM 6.0 TO 11.0 SECONDS AFTER DIE OPENING FOR DIE ONE
FROM 6.0 TO 11.0 SECONDS AFTER DIE OPENING FOR DIE TWO
SPRAY AT A TEMPERATURE OF 25.0
CAUSING A HEAT TRANSFER COEFFICIENT OF 0.12000 + 0.000001 T

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APPENDIX H

GOVERNING EQUATIONS FOR FEM THERMAL-STRUCTURAL ANALYSIS
H.1 Equations Governing Thermal-Structural Analyses

The ABAQUS analyses used in the research activities were based on the idea that the die system studied behaves thermoelastically. The tool material, H-13 is assumed to be ideally plastic meaning the material will flow continuously once the yield point is achieved. Following section covers the thermoelastic problem [Rosbrook, 1992].

H.1.1 Thermoelastic Problem

The general thermoelastic problem consists of sixteen equations and sixteen unknown functions of position $x_k$ and time $t$ along with appropriate boundary and initial conditions [Kovalenko, 1969] mentioned previously in the modeling sections of this dissertation. The sixteen unknowns are the six components of the symmetric stress tensor $\sigma$, six components $(e_{ij})$ of the symmetric strain tensor $\varepsilon$, three components $(u_i)$ of the displacement vector, and the scalar temperature $(T)$. The sixteen equations belong to four following sets.

1. Three equations of motions

$$\sigma_{ij,t} = \rho u_{it}$$ \hspace{1cm} (H.1)

2. Heat conduction equation

$$T_{ij} - \frac{1}{a} \dot{T} - \frac{T_0(3\lambda + 2\mu)\alpha}{k} \dot{\varepsilon}_{ik} = 0$$ \hspace{1cm} (H.2)

where $k$: is the thermal conductivity

$a$: is the thermal diffusivity ($=k/c_e$ or thermal conductivity – specific heat at constant strain ratio)

$T_0$: is the temperature corresponding to a stress-less state
\( \alpha \): is the coefficient of linear expansion

\( \lambda \): is one of the Lame' constants \( = \mu \frac{\nu}{E(1+\nu)(1-2\nu)} \) where \( E \) is Young's modulus and \( \nu \) is Poisson's ratio = 0.3

\( \mu \): is one of the Lame' constants \( = \frac{E}{2(1+\nu)} \)

The derivation of H.2 assumes that the specific heat and the thermal conductivity are independent of temperature.

3. six stress-strain relations through Hooke’s Law

\[
\sigma_{ij} = 2\mu \varepsilon_{ij} + (\lambda + 2\mu) \alpha \Delta T \delta_{ij}
\]

where \( \delta_{ij} \) is the Kronecker delta function (1 if \( i=j \) and 0 otherwise)

4. six strain-displacement relations

\[
\varepsilon_{ij} = \frac{1}{2} (\varepsilon_{i,j} + \varepsilon_{j,i})
\]

The fifteen relation in sets 1, 3 and 4 can be arranged into one partial differential equation that along with the heat conduction equation and appropriate initial and boundary conditions, describes the general thermoelastic problem namely:

\[
\mu u_{i,kt} + (\lambda + \mu) u_{k,ki} - (3\lambda + 2\mu) \alpha T_j = \rho \ddot{u}_i
\]

The initial and boundary conditions in terms of the displacements \( u_i \) and the temperature \( T \) are given below in H.6 and H.7:

\[
u_i = g_i^{(1)} (x_k), \quad u_i = g_i^{(2)} (x_k), \quad T = G^{(1)} (x_k)
\]
The three conditions in H.6 are the initial displacements, the initial velocities and the initial temperature field.

The boundary conditions are typically combinations of functions of the following forms presented in H.7.

\[ u_i = g_i^{(3)}(x, t), \ T = G^{(2)}(x, t) \]  

(H.7)

The superscripts in H.6 and H.7 are used to distinguish distinct functions of the coordinates \( x_k \).

Equations H.2 and H.5 can be restated in vector notation,

\[
\mu \nabla^2 \ddot{u} + (\lambda + \mu) \nabla (\nabla \cdot \ddot{u}) - (3\lambda + 2\mu) \alpha \nabla T = \rho \dddot{u} \]  

(H.8)

\[
\nabla^2 T - \frac{1}{a} \frac{T_0 (3\lambda + 2\mu) \alpha}{k} (\nabla \cdot \dddot{u}) = 0 \]  

(H.9)

Simultaneous solution of these two equations with initial and boundary conditions gives a complete description of the linear elastic temperature and displacement fields within the boundary as a function of time. Strains and stress can be found through Equations H.3 and H.4.

Equations H.8 and H.9 can be simplified when an external heat source causes non-uniform heating [Kovalenko, 1969]. It can be assumed that the temperature field is independent of the strains that it causes. In other words the strains caused by the temperature field do not affect the temperature field. This makes the thermoelastic model valid without the coupling term. The following coupling terms can be dropped from the Equations H.2 and H.9 respectively,

\[
-\frac{T_0 (3\lambda + 2\mu) \alpha}{k} \varepsilon_{kk} \]  

(H.10)
ABAQUS can solve this problem as a sequentially coupled fashion rather than fully (simultaneously) coupled [ABAQUS, 1997]. The temperature distribution can be calculated first through the Fourier’s heat conduction equation if the thermo-physical properties (density ($\rho$), thermal conductivity ($k$) and, specific heat capacity ($c$)) of the die material are constant,

\[
\frac{T_0(3\lambda + 2\mu)\alpha}{k} (\nabla \cdot \mathbf{u}) = 0 \quad (H.11)
\]

If the thermo-physical properties are not constant or temperature dependent, the following is the valid form of the Fourier’s heat conduction:

\[
\frac{\partial k}{\partial T} = 0 \quad \text{or} \quad \nabla^2 T - \frac{1}{a} \frac{\partial T}{\partial T} = 0 \quad (H.12)
\]

The resulting temperature field can be used for solving Equation H.8. A further simplification can be realized under usual conditions of heat transfer [Kovalenko, 1969]. In this case, the rate of change of temperature is small compared with the speed of sound in the die material. Therefore, the inertia terms of the Equation H.5 and H.8 can be dropped leading to the two following equations respectively,

\[
\mu u_{i,i} + (\lambda + \mu) u_{k,k} - (3\lambda + 2\mu)\alpha T_{,i} = 0 \quad (H.14)
\]

\[
\mu \nabla^2 u + (\lambda + \mu) \nabla (\nabla \cdot \mathbf{u}) - (3\lambda + 2\mu)\alpha \nabla T = 0 \quad (H.15)
\]

When the heat and the displacement problems are uncoupled and without the inertia terms from the displacement problem, the problem is termed quasi-static and time 415
is no longer a variable in the displacement problem. Time can be treated as a parameter in
the displacement problem. The following method can now be used to solve the
 thermoelastic problem. First the dynamic temperature field is determined through H.12
and appropriate thermal initial and boundary conditions. Then, the displacement problem
is solved for a particular time using the temperature field at that time along with
appropriate displacement boundary conditions.
LIST OF REFERENCES


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Data on World Wide Metals and Alloys (AISI H-13 Hot Work Tool Steel), Alloy Digest, May 1967, Engineering Alloy Digest Inc. Upper Montclair NJ.

Database of Procast Software, UES Inc.

Database of MagmaSoft Software, Magma Foundry Technologies.


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