INFORMATION TO USERS

This manuscript has been reproduced from the microfilm master. UMI films the text directly from the original or copy submitted. Thus, some thesis and dissertation copies are in typewriter face, while others may be from any type of computer printer.

The quality of this reproduction is dependent upon the quality of the copy submitted. Broken or indistinct print, colored or poor quality illustrations and photographs, print bleedthrough, substandard margins, and improper alignment can adversely affect reproduction.

In the unlikely event that the author did not send UMI a complete manuscript and there are missing pages, these will be noted. Also, if unauthorized copyright material had to be removed, a note will indicate the deletion.

Oversize materials (e.g., maps, drawings, charts) are reproduced by sectioning the original, beginning at the upper left-hand corner and continuing from left to right in equal sections with small overlaps. Each original is also photographed in one exposure and is included in reduced form at the back of the book.

Photographs included in the original manuscript have been reproduced xerographically in this copy. Higher quality 6” x 9” black and white photographic prints are available for any photographs or illustrations appearing in this copy for an additional charge. Contact UMI directly to order.

UMI
A Bell & Howell Information Company
300 North Zeeb Road, Ann Arbor MI 48106-1346 USA
313/761-4700 800/521-0600
CHARACTERIZATION OF LOADS IN DIE CASTING AND PREDICTION OF DIE DEFLECTIONS

DISSERTATION

Presented in Partial Fulfillment of the Requirements for the Degree Doctor of Philosophy in the Graduate School of The Ohio State University

by

Horacio Ahuett-Garza, M.S.

The Ohio State University

1996

Dissertation Committee:

Professor R. Allen Miller, Adviser
Professor Gary L. Kinzel, Adviser
Professor Henry Busby
Professor Rajiv Shivpuri

Approved by

Adviser

Department of Mechanical Engineering
Die casting dies deflect during a casting operation. These deflections affect the quality of the casting operation and the part. The goal of this work is to model and predict die casting die deflections. The theoretical problem is that of determining the thermoelastic deflections of a body under periodic thermo mechanical boundary conditions. This problem is complicated by the existence of several different time and dimension scales. From the perspective of the loads, there are two different time scales:

- Cavity pressures due to the processes of filling and intensification are applied in fractions of a second
- Clamping forces are applied throughout the duration of the casting cycle

The thermal response of the die is characterized also by at least two time scales:

- Time for solidification (seconds)
- Time to reach quasi-steady state conditions (hours)
For results to be of practical use, deflections must be computed with a resolution of fractions of a millimeter in models whose size is in the order of a meter. Because of the wide range of conditions, a very important task in this work is that of sorting out the interaction between the different loads, and clarifying what loads are relevant. A modeling approach was developed using ABAQUS, a commercial FEM system. The die behavior predicted by this approach is consistent with what is observed in the field. The procedure is also tested on a limited basis against field data. As a result, values of certain parameters and boundary conditions are defined. Based on the results of the simulations, the behavior of the die has been characterized and deflection data has been prepared in a form that can be used for design purposes. A global perspective of the characteristics of loads in die casting has been prepared. This characterization has allowed a correlation of the behavior of the die with certain die design parameters and process conditions.
Dedicated to my wife, Maria Alejandra, and my daughters, Victoria Alejandra and Diana Patricia.
ACKNOWLEDGMENTS

I wish to thank my advisers, R. Allen Miller and Gary L. Kinzel, for their technical support and encouragement. I could not overemphasize the importance of Dr. Miller's guidance, patience and trust for the completion of my degree.

I am grateful to Walter Smith and his die design / manufacture team at DCD Technologies (Cleveland, OH), for taking the time to listen to my questions and sharing their expertise and concerns with me.

This work was enriched by the contributions from the members of the North American Die Casting Association's Computer Modeling Task Group. Bill Walkington, leader of this group, deserves a special recognition. In addition to contributing his knowledge, it is his perseverance in bringing science to the die casting shop floor that has resulted in funding for me and others who do research in this field.

I would like to thank Warren Bishenden (Exco Engineering, Ontario), Charles Ramsey (Matrix Technologies, Muncie, IN), Dick Smith (Chrysler, Kokomo, IN), Greg Prince (Walker Die Casting, Lewisburg, TN) and Danny
Swartz (Delaware Machinery, Muncie, IN) for providing field data used for validation of the results of this work.

I also wish to thank my friends and colleagues, Aswin K. Choudhury and Sanjay Dedhia for their help in running simulations and collecting some of the data that is reported throughout this document.

This research was supported by a grant from the Department of Energy.
VITA

May 21, 1964......................................................... Born- Monterrey, N. L. México.

1986............................................................... B.S. Mechanical Engineering,
ITESM
Monterrey, N. L. México.

1987-1989....................................................... Process Engineer,
Fabricación de Máquinas, S. A.
Monterrey, N. L. México

1990............................................................... Instructor, M.E. Department,
ITESM
Monterrey, N. L. México

1990 - 1991...................................................... Graduate Teaching Associate,
The Ohio State University.

1991 - Present................................................ Graduate Research Associate,
The Ohio State University.
FIELDS OF STUDY

Major Field: Mechanical Engineering.
TABLE OF CONTENTS

Dedication.......................................................................................................................iv  
Acknowledgments.........................................................................................................v  
Vita ..................................................................................................................................vii  
Table of Contents..........................................................................................................ix  
List of Tables................................................................................................................xiv  
List of Illustrations.......................................................................................................xv  
List of Figures..............................................................................................................xvi  
Chapters:  
1. Introduction and Problem Statement.........................................................................1  
   1.1 Introduction...........................................................................................................1  
   1.2 Die casting .........................................................................................................2  
   1.3 The die casting die............................................................................................6  
   1.4 Deflections of die casting dies.........................................................................10  

ix
1.5 Die casting die design tools ................................................................. 12
1.6 Problem statement ............................................................................. 19
1.7 Research objectives ........................................................................... 21
1.8 Research contributions ..................................................................... 22
1.9 Dissertation organization .................................................................. 23

2. Literature Review .................................................................................. 25

2.1 Introduction ......................................................................................... 25
2.2 Heat and fluid flow in casting ............................................................. 27
2.3 Heat and fluid flow in die casting ....................................................... 29
   2.3.1 Research by Wallace's team ....................................................... 29
   2.3.2 Research funded by CSIRO ....................................................... 30
   2.3.3 Research funded by the National Research Council of
       Canada ....................................................................................... 32
   2.3.4 Research at General Motors ...................................................... 34
   2.3.5 Other relevant research ............................................................ 36
   2.3.6 Trends in the simulation of heat and fluid flow ......................... 39
2.4 Pressure effects in die casting ............................................................. 39
2.5 Stresses in die casting dies ................................................................. 42
2.6 Experimental work ............................................................................ 43
2.7 Work at Ohio State ............................................................................ 45
2.8 Remarks ............................................................................................. 46

3. Characterizing the Loads in a Die Casting Operation ......................... 50

3.1 Introduction ......................................................................................... 50
3.2 Locking action .................................................................................. 54
3.3 Filling Stage ....................................................................................... 58
   3.3.1 Heat released during fill .......................................................... 63
   3.3.2 The effects of heat released during fill .................................... 70

5.1 Dies for transmission casings and other power train components ...........................................177
5.2 Structural characteristics of transmission case dies .........................................................179
5.3 Slide deflection ........................................................................................................183
5.4 Lock design .............................................................................................................183
5.5 Comments on the lock design ..................................................................................189
5.6 Simulating slide blowback ......................................................................................191
5.7 Methodology for developing an FEM model to predict slide blowback ...........................................191
5.8 Slide blowback: field tests ......................................................................................194
5.9 Comments on experimental data ..............................................................................201
5.10 An FEM model to predict slide blowback ..............................................................202
5.11 Application of model to the analysis of an alternate slide lock design ...........................................214
5.12 Variations to lock design ......................................................................................221
5.13 Further applications: effect of projected area of die on die deflections .................................229
5.14 Shutoff pressure profile ..........................................................................................235
5.15 Correlation with process conditions ......................................................................237
5.16 Concluding remarks .............................................................................................238

6. Conclusions and Future Work ..........................................................................................239

6.1 Introduction ..............................................................................................................239
6.2 Research contributions ...........................................................................................240
6.3 Conclusions ..............................................................................................................241
6.4 Failure modes and resolution of results..............................................245
6.5 Future work..............................................................................................245
List of References.......................................................................................................250
# LIST OF TABLES

<table>
<thead>
<tr>
<th>Table</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>4.1. Assumed casting conditions</td>
<td>126</td>
</tr>
<tr>
<td>4.2. Assumed steps in a single casting cycle</td>
<td>126</td>
</tr>
<tr>
<td>4.3. Thermal zones and respective K and m values</td>
<td>143</td>
</tr>
<tr>
<td>4.4. Process conditions for the different simulation trials</td>
<td>153</td>
</tr>
<tr>
<td>5.1. Sensitivity of deflection to variation in friction factor and boundary conditions</td>
<td>208</td>
</tr>
<tr>
<td>5.2. Sensitivity of deflection to variation in friction factor and boundary conditions</td>
<td>210</td>
</tr>
<tr>
<td>5.3. Comparison of results, over closed surface contact</td>
<td>215</td>
</tr>
<tr>
<td>5.4. Comparison of results, softened surface contact</td>
<td>215</td>
</tr>
<tr>
<td>5.5. Blowback data Delaware die-carrier supported by cover die</td>
<td>220</td>
</tr>
<tr>
<td>5.6. Blowback data Delaware die-carrier supported only by pin</td>
<td>223</td>
</tr>
<tr>
<td>5.7. Cases for parametric study</td>
<td>225</td>
</tr>
<tr>
<td>Illustration</td>
<td>Page</td>
</tr>
<tr>
<td>--------------</td>
<td>------</td>
</tr>
<tr>
<td>5.1. Details of final model and test results</td>
<td>208</td>
</tr>
<tr>
<td>5.2. Details of original model and test results</td>
<td>210</td>
</tr>
<tr>
<td>5.3. Schematic of combination lock and test results</td>
<td>220</td>
</tr>
<tr>
<td>5.4. Modified model and simulation results for combination lock</td>
<td>223</td>
</tr>
<tr>
<td>5.5. Parametric study of Exco's horn pin design. Parameters and variations</td>
<td>225</td>
</tr>
</tbody>
</table>
## LIST OF FIGURES

<table>
<thead>
<tr>
<th>Figure</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1. Schematics of a) cold chamber and b) hot chamber die casting machines. From SME's &quot;Metals Handbook&quot; Vol. 15 [2].</td>
<td>4</td>
</tr>
<tr>
<td>1.2. Schematic of a cold chamber operation. From Fleming's &quot;Solidification Casting&quot; [3].</td>
<td>5</td>
</tr>
<tr>
<td>1.4. Two parts that present the same projection, yet have different heat contents.</td>
<td>17</td>
</tr>
<tr>
<td>3.1 Different stages and corresponding loads in a die casting operation.</td>
<td>52</td>
</tr>
<tr>
<td>3.1 (Continued)</td>
<td>53</td>
</tr>
<tr>
<td>3.2. Schematic model of the locking action of the die casting machine. All forces are computed by multiplying the corresponding pressures by the area upon which they act.</td>
<td>55</td>
</tr>
<tr>
<td>3.3. Schematic model for definition of simple solidification front in die casting.</td>
<td>68</td>
</tr>
</tbody>
</table>
3.4. Testing the validity of results of scale analysis of heat released during fill. Data taken from [62], reprinted with permission from NADCA.................................................................68

3.5. Testing the effect of instant fill vs. non instant fill conditions. Description of geometry and conditions.......................................................71

3.6. a) Maximum temperature difference for any node between consecutive cycles, for instant fill conditions. b) Projected number of cycles to reach periodic conditions.................................................................75

3.7. a) Maximum temperature difference for any node between consecutive cycles, for non instant fill conditions. b) Projected number of cycles to reach periodic conditions.................................................................76

3.8. (a) Temperature fields and (b) deflection patterns at end of 15 casting cycles. Instant fill conditions .................................................................77

3.9. (a) Temperature fields and (b) deflection patterns at end of 15 casting cycles. Non - instant fill conditions .................................................................78

3.10. (a) Temperature fields and (b) deflection patterns illustrating the difference between instant fill and non instant fill conditions..........80

3.11. Analysis of momentum related deflections. a) Model of tip / gate / fluid stream. b) Table describing magnitude of deflections for different slide tip arrangements.................................................................84

3.12. Schematic diagram of hydraulic actuation of die casting machine. ....92
3.13. a) Assuming no friction, plunger experiences negligible back pressure from cavity. b) Pressure at plunger tip begins to rise considerably as runner begins to fill.................................................................95

3.14 Field data: velocity and pressure vs. plunger position. Data provided by Exco, Engineering (Newmarket, Ontario, Canada).................................101

3.15. Forces acting on plunger. Free body diagram..............................................101

3.16. Traces of plunger velocity, force on tip of plunger and estimated cavity pressure vs time. True cavity pressure would be represented only by the last segment. Force and pressure are computed by applying a scale factor to the acceleration curve (not shown)..................104

3.17. Ejector and cover die/platen arrangement used for dynamic analysis.................................................................107

3.18. Lower natural frequencies and mode shapes, cover side. a) 537 Hz. b) 640 Hz. c) 647 Hz.................................................................................................................................108

3.18. (Continued)......................................................................................................109

3.19. Lower natural frequencies and mode shapes, ejector side. a) 563 Hz. b) 758 Hz. c) 851 Hz.................................................................................................................................110

3.19. (Continued)......................................................................................................111

3.20. Cover side: excitation and response under two different conditions...113

3.21. Ejector side: excitation and response under two different conditions. 114

3.22. Shock response curve for cover side...............................................................115

3.23. Shock response curve for ejector side...............................................................115

xviii
4.1. Flowchart for a typical simulation session...........................................122

4.2. Schematic of the geometry used for simulations. Dimensions are
given in inches..........................................................................................124

4.3. Wireframes of (a) actual casting, (b) modified geometry and (c) die
used in FEM work......................................................................................128

4.4. Issues when modeling isolated inserts. (a) Insert mounted on die.
(b) Schematic of mechanical boundary conditions. (c) Schematic of
heat transfer boundary conditions..........................................................132

4.5. Flowchart to determine constants K, m..............................................141

4.6 Regions into which part was divided for the purpose of modeling
the heat released during solidification. Results from a simulation
with MAGMA.........................................................................................142

4.7. (a) Schematic arrangement of die casting machine. (b) Relevant free
body diagrams.......................................................................................149

4.8. Characteristics of models. (a) Stress analysis. (b) Heat transfer........151

4.9. As the plate thickness increases, the maximum parting plane
separation decreases..............................................................................154

4.10. Chart showing the parting plane separation. The maximum occurs
at the moment the pressure is applied.................................................156

4.11. Prior to the application of pressure, the cavity space measured across
the thickness of the plate has shrunk slightly. As the pressure is
applied the cavity space grows. Once the pressure is released, the
space shrinks again..............................................................................157

xix
4.12. The overall growth of the die is illustrated by the displacement of a node located in the back of the plate.

4.13. Contribution to overall deflection by each type of load at selected locations.

4.14. Effects of different loads. (a) Clamping force. (b) Clamping force and cavity pressure. (c) Clamping force and heat from solidification. (d) All loads combined.

4.15. Parting plane separation at midpoint of plate. After the second cycle this point shows the maximum separation. For each cycle, only the deflection that occurs between the instants of metal injection and part ejection has been recorded. Estimated values were extrapolated based on the results of a single cycle.

4.16. Schematic view of location of displacement sensors on structure of die for a transmission casing. Die has three slides that use a horn pin type lock. Reproduced with permission from Matrix Technologies, Inc.

4.17. (a) Behavior of parting plane separation as reported by Matrix. (b) Process conditions (scaled for ease of display).

4.18. Schematic comparison of die support. (a) original model. (b) Modified model that accounts for elasticity of support. (c) Comparison of simulation results, outside corner of die.

4.19. Nature of stress patterns at parting plane. (a) Flat parting surface. (b) Effect of proud inserts. Case (b) presents a stress pattern that opposes deflection due to cavity pressure more efficiently.
5.1. Some power train components typically produced by die casting. 178

5.2. Typical die construction with a sliding core. From Exco Engineering's product catalog (Newmarket, Ontario). 180

5.3. Different slide pull activation mechanisms. From [4]. 182

5.4. Loads upon slide during die casting operation. 184

5.5. The model of the slide support depends upon the performance of the lock. 184

5.6. Outboard lock construction. From [4]. 185

5.7. Inboard lock construction. Design data provided by DCD Technologies (Cleveland, Ohio). 187

5.8. Pillar-horn pin lock construction. From Exco Engineering's product catalog (Newmarket, Ontario). 188

5.9. Slide blowback trace. Data courtesy of Warren Bishenden, Exco Engineering, Newmarket, Ontario. 190

5.10. Schematic slide / lock arrangement showing blowback direction. 192

5.11. Schematic illustrating the most important characteristics of the die and casting for which data was made available. Slide tips and details of cavity are not shown. 195

5.12. Schematic placement of LVDT's behind top slide. 196

5.13. Field data: slide blowback, timing of intensification =0.1. 198

5.14. Field data: slide blowback, timing of intensification =0.2. 198

xxi
5.15. Field data: slide blowback, timing of intensification = 0.5 ..................... 199
5.16. Field data: slide blowback, timing of intensification = 0.6 ..................... 199
5.17. Slide blowback trace: timing of intensification = 1.5 ............................... 200
5.18. Overall dimensions of Exco's slide carrier and pin ................................. 205
5.19. a) Model description for computation of slide blowback (Exco's Design) b) Typical simulation results ................................................................. 206
5.20. Effect of varying the number of elements upon computational expense and results .................................................................................................. 212
5.21. Models for defining contact surface behavior in ABAQUS ....................... 213
5.22. Modified lock design. Schematic of shape and dimensions. Dimensions in inches ........................................................................................................ 216
5.23. Slide blowback in the case of a combination lock .................................. 218
5.24. a) Schematic model of Delaware slide pin with boundary conditions for computation of slide blowback. b) Typical simulation results .......... 219
5.25. a) Schematic model of Delaware slide pin with boundary conditions for computation of slide blowback. b) Typical simulation results .......... 222
5.26. Force vs. displacement plot used to compute stiffness of lock ................. 226
5.27. Comparison of characteristic behavior of slide / lock arrangements for different cases in parametric study ................................................................. 228
5.28. Comparison of dimensions for analysis of effect of projected area ......... 231

xxii
5.29. Maximum parting plane separation at corner of cavity. Multiple cycles. ..............................................................233

5.30. Maximum separation at a point close to middle of the cavity. ............234
Chapter 1. Introduction and Problem Statement

1.1 Introduction

Over the years, the analysis of the die casting process has attracted considerable interest from the scientific community. For example, materials scientists have worked in the development and selection of materials that are compatible with die casting from the point of view of the product and tooling. Similarly, numerous empirical and theoretical studies regarding the mechanisms of die fill have been conducted. Their main goal has been to determine the effects of fill upon the quality of the casting operation.

On the other hand, it was not until very recently that any attention had been placed on the mechanical design of the die. While there is a clear idea of what the role of the die is, very little is understood about its behavior.

The research described in this document was originated from a relatively straightforward task: apply an established tool, namely the Finite Element Method (FEM), to the structural analysis of a die casting die. The analysis is concentrated on the prediction of the deflections of the die.

There are many commercial FEM systems that can handle the thermoelastic problem in question, provided that suitable models for the
boundary conditions can be found. It is precisely the definition of the boundary conditions what makes this a unique exercise. While attempts have been made at predicting the thermal response of the die, the fact remains that there is not enough documented data in the literature, from either empirical or theoretical studies, to support broad statements about the behavior of die casting dies, particularly if the response to mechanical loads is sought. As a consequence, any conclusions drawn from this research regarding the nature of behavior of the die under both mechanical and thermal loads will be unique.

1.2 Die casting

The die casting industry’s origin dates back to the beginning of this century, when H. H. Doehler obtained a patent for the design of a die casting machine. Today, die casting comprises the second largest foundry industry segment in the United States. In 1992, the US die casting industry generated $5.75 billion in the form of about 1.4 billion pounds of aluminum products, 375 million pounds of zinc products and 46 million pounds of magnesium products [1]. The US die casting industry is made up of roughly 1200 facilities and approximately 8000 die casting machines. Around 80,000 people work in this industry, giving an average of 67 employees at each die casting plant. Parts of intricate shape and thin walls that range from small ornaments to
automobile transmission casings and engine blocks can be mass produced with the die casting process.

In general, there are two types of die casting processes: the hot chamber process, which is the original process invented by Doehler, and the cold chamber process. The difference resides in the location of the hydraulic actuator within the die casting machine. In the hot chamber process, the actuator is located within the container where the molten metal is stored before injection. Materials of lower melting point such as zinc and lead are used in this process. Magnesium alloys are also processed in hot chamber machines. In the cold chamber version, the hydraulic actuator is placed away from the container of the molten metal, thus reducing the likelihood of a reaction between the pump material and the casting material. Aluminum and copper alloys are commonly processed in cold chamber die casting machines. Figure 1.1 shows a schematic of both types of machine arrangements.

For both cases, the actual casting processes are very similar. Figure 1.2 presents a schematic of the cold chamber operation. In this case, the molten metal is ladled into the shot sleeve, while the die is being closed and locked. To minimize heat loss, thus guaranteeing proper fluidity of the casting material, the molten metal is rapidly pushed by the plunger into the die cavity. A large pressure (~70 MPa) is necessary to inject the metal into the
Figure 1.1 Schematics of a) cold chamber and b) hot chamber die casting machines. From SME's "Metals Handbook" Vol. 15 [2].
Figure 1.2. Schematic of a cold chamber operation. From Fleming's "Solidification Casting" [3].
cavity during this period. After the part solidifies, the die opens up and the part is knocked out of the die. Before the next cycle, the cavity is typically sprayed with a lubricant that lowers the surface temperature and facilitates the ejection process.

1.3 The die casting die

The die is the heart of the die casting operation. Because a die must open to allow for the removal of the finished product, the die must be split into at least two sections. Due to manufacturing constraints, die halves are usually made up of different components assembled together. Depending upon the shape of the part, one or more moving mechanisms (sliding cores) may be required to produce features that could otherwise be impossible to generate without obstructing the die opening. Other moving mechanisms are also needed to eject the casting (ejector pins). Die production costs can be lowered by using cheaper materials in those parts of the die that are not subject to extreme loads. For this reason, different regions of the die are made up of different materials. Figure 1.3 shows a schematic of a die casting die.

During each cycle, the die must perform a variety of functions while being subject to a set of loads that are characteristic of the particular situation. In general, the loads are:
Figure 1.3. Schematic of a multiple cavity die casting die. From SME's "Metals Handbook" Vol. 15 [2].
• The clamping force exerted by the die casting machine. This force prevents the die from opening during the injection cycle. The size of the machine is typically described in terms of the clamping force it can apply to the dies. The smallest die casting machines can apply approximately 250 metric tons of force, while 3500 ton-machines are commonly found in the auto industry.

• The injection pressure. Die casting parts are characterized by thin walls. These thin sections can solidify in a fraction of a second. To avoid premature solidification which leads to defective parts, the injection process must be completed in as quickly as a few hundredths of a second. As a consequence, the die is hammered with a high temperature slurry during fill. At the end of fill, the static pressure in the cavity of the die can exceed 10,000 psi (~70 MPa).

• The intensification pressure. To ensure that even the smallest features within the die cavity are filled, as well as to reduce shrinkage defects, the pressure within the cavity is raised a few moments after injection has been completed. This pressure may reach as high as twice the injection pressure.

• The heat released by the part as it solidifies within the die. Typical pouring temperatures for aluminum alloys are in the order of
660°C. Depending upon the size of the part, its temperature may drop to 300 °C after only a few seconds, at which point the part is ejected.

- The thermal shock produced during the application of the lubricant spray.

The following is a list of the functions that the die must perform during the casting cycle, as well as the systems that perform such functions.

- Distribute molten metal into all regions of the cavity. This is accomplished through the runner and feeder system.

- Allow for the proper evacuation of air from the cavity as the molten metal fills it. Vents are provided for this purpose.

- Provide space for the deposit of debris and oxides. Overflows serve this purpose.

- Safely maintain the geometric integrity of the cavity. If the die opens prematurely, molten metal can escape the die and cause damage to property and even personal injuries. If the cavity is distorted, the product may not reach its expected final shape.

- Provide channels for the removal of heat during the rapid solidification of the part. Cooling lines and the die structure perform this operation.
During their usable life time, dies are expected to perform up to several million casting cycles. A small die may cost $50,000 and require 3-4 months for delivery. Large dies may cost more than $500,000 and require 9-12 months for delivery. High manufacturing costs prohibit most prototyping work and, in the great majority of cases, dies must be modified after an exploratory production run.

1.4 Deflections of die casting dies

Die casting dies deflect elastically due to the loads caused by the casting process. The auto industry presents some of the most dramatic examples of die deflections. Currently, most of the large components of the drive train are die cast. The majority of the features of these parts requires the introduction of moving die members which deflect and move in unpredictable directions. A typical die for a transmission casing costs over $1,000,000, requires over a year for delivery, and may produce a couple of hundred thousand castings before its usable life is finished. The elastic deflections of such dies may result in a variety of problems such as:

- Dimensional variation of the die cavity with respect to its nominal size resulting in parts that may be out of dimensional tolerances.
- Flashing, i.e. the injection of metal into sections of the die that are not part of the cavity. This phenomena has important effects on the performance of the moving mechanisms of a die and the
dimensional integrity of the part. Additional operations to trim the excess material from the product may also be required.

- Operational problems such as binding of sliding members, difficulties with part ejection, and bent, distorted or broken castings.
- Unbalanced loads on the die casting machine structure.
- Malfunction of vacuum systems, which results in parts with porosity.
- Catastrophic failure of the die.

Each of these impacts the ability to hold tolerances and to economically produce quality castings. Die casters may address these problems in several ways. At the design stage, the size of holder blocks for cover and ejector plates, as well as the slide cores may be increased. Because no analysis is performed (and bearing in mind that, while the deflections due to pressure loads may be reduced by increasing the size of a die element, the deflections due to the thermal component of the loads may actually increase), this approach may be successful only when the deflection patterns are already known, that is, in the case of replacement dies. Pillar supports may also be added to the ejector block. During operation, casters deal with deflections by modifying the process conditions and, in extreme cases, by shimming the die halves and slides to counter the effects of deflections. This last resource may actually be harmful to the die. Typically, die casters will struggle with the
deflections of a die until it is retired and a replacement die, with modifications, is put in place. By predicting the magnitude of the die deflections, design modifications may be adopted before a die is put in place, thus improving the quality of the casting process.

The magnitude of the elastic deflections of dies may be estimated through a computer simulation. In general, the numerical techniques and software (Finite Element Codes, Boundary Element Codes) to generate the deflection data are well established and may be obtained commercially. However, the expertise required to prepare the models as well as to interpret the output data is still missing. This last fact is the result of the lack of models to describe loads and boundary conditions in a die casting operation, and the great amount of time and effort required to generate a numerical model, solve it, and interpret its results. Currently, design parameters such as the size of the different components, amount of preload and shape of locks are defined based purely on rules of thumb.

1.5 Die casting die design tools

In spite of the fact that die casting dies can be considered high performance mechanical components, the design process is still an art form. Although most die manufacturers will prepare CAD models of their die designs, they do so mainly for the purpose of generating CNC programs for the manufacture of the die. Currently, there are tools that assist the designer
in only two areas of die design: the design of the runner and feeder system, and the design of the cooling lines.

Based on research done in the late 60's and early 70's, organizations such as the North American Die Casting Association (NADCA) and the International Lead and Zinc Research Organization (ILZRO) have prepared guidelines, tables and software that help with the design of feeder / runner systems in die casting dies. The recommended designs are intended to guarantee the proper fill of the cavity as well as a smooth surface finish of the finished product. Industry often combines these guidelines with their own expertise to design these elements of the die. NADCA also provides software that can assist the designer in defining the location of the cooling lines. In addition, there are several packages such as Magma and ProCAST that, although not specifically designed for die casting, may be used to predict the solidification pattern of the casting and the thermal loads upon the die. With this software, the designer can simulate, usually at great expense, different cooling line arrangements before arriving at a final design. Still, only the larger die casting die manufacturers routinely use software to simulate the casting process.

The structural design of a die is a totally different story. Currently, neither NADCA nor ILZRO possess the data required to make sound recommendations about the structural design of dies. Die designers
determine the size and shape of the individual components based on rules of thumb or personal experience. There is no die manufacturer in the North American market that routinely analyzes the mechanical design of a die. In fact, hardly anyone has the tools and knowledge to even attempt to simulate the mechanical performance of a die. Such a situation has emerged not as the result of a lack of interest from the die manufacturers to simulate the die performance, but rather as the result of a lack of understanding of the loads involved in the die casting operation.

Formal treatment of the structural design of dies in the literature is not very extensive. Only a handful of references are found in the English literature. To give an idea of how these sources help the designer, a quote from Doehler is presented [4]. In this exercise, Doehler intends to explain how the number and location of cavities (impressions) within the die may be established:

"The impressions in a die should be arranged symmetrically and uniformly, each impression being equidistant from the other. Impressions may be disposed in a straight or in a circular form. If arranged in a line, the total amount of impressions should be of an even number since it is desirable to have the same number of impressions on each side. In a circular arrangement the impressions may either be odd or even, as long as they are uniformly balanced. Balancing of the impressions is necessary to obtain uniformity of heat distribution in the die"

"Sufficient space should always be provided between the edge of the impressions and the outer edge of the die block. This area, usually termed the metal seal, generally should measure not less than 3 in. If less than this minimum, the molten metal may shoot out through the parting line, especially if the die blocks 'blow' or part slightly under the pressure of the metal entering the die"
A couple of die-structure design principles are embedded within this explanation: metal seal (often referred to as "shutoff area") and thermal balance. Sufficient metal seal is necessary for a safe casting operation. Thermal balance is needed for several reasons, such as:

- to allow proper solidification of the part
- to guarantee uniform die growth due to changes in temperature
- to guarantee uniform loads upon the structure of the die and the casting machine

Because of the effect that part geometry has upon the thermal and mechanical loads, it is difficult to instruct the designer as to how his particular case should be approached. Some attempts have been made to provide the designer with engineering approximations that can help him/her make decisions about the die design. For example, an important factor that needs to be considered when defining the location of the cavities is the tie bar load balance. In this case, the objective of the designer is to distribute the load evenly among the tie bars. In his book, Herman [5] presents a methodology by which the load on each tie bar may be computed. In this methodology, the projection of the cavity, the runner system and other impressions are looked at from the perspective of the shot sleeve. The center of gravity of the projected area with respect to some arbitrary datum is then computed.
Assuming the pressure within the cavity is hydrostatic, the designer can balance the tie bar load by locating the center of gravity of the projected area at the geometric center of the tie bar arrangement.

In spite of the fact that Herman bases his analysis upon accepted engineering principles, it has been reported that Herman's methodology does not result in an evenly distributed tie bar load. One possible explanation is that this methodology does not account for the interaction of the thermal / mechanical effects of the loads. In essence, a mechanical load balance does not guarantee a thermal load balance. Because only the projected area of the impression is needed to compute the center of gravity of the part, two parts having the same projection but different depth will be placed in the same location with respect to the tie bars. In the case shown in Figure 1.4, part (a) will release a different amount of heat when compared to part (b). This different heat loads result in uneven deformation of the die, which results in an uneven tie bar load. From this example, a poor understanding of the loads involved in the die casting process is evident.

As already explained, most die designers lack the tools necessary to predict deflection patterns. At best, approximations are made to analyze the effects of a few independent loads. However, very little is known about the interaction of loads. In many cases, intuition may not provide all the answers. For example, intuitively one would expect that, as dies grow due to
Figure 1.4. Two parts that present the same projection, yet have different heat contents.

A rise in temperature, the preload on the die would be higher, making it more difficult to allow flash to escape. However, our own simulation results show that, due to the non uniform thermal growth, conditions for flashing may actually get worse in certain regions of the die after the die reaches operating temperature.

As will be shown later, there is an extensive amount of work in the literature that deals with the analysis of the die casting process.
Unfortunately, most of this work has concentrated on the solution of the mathematical equations that describe the process and, in order to make the problem tractable, significant simplifying assumptions are usually made. A case in point is the "instant fill" assumption, in which heat transfer is assumed negligible during cavity fill. The practical validity of this assumption has rarely been tested. However, this assumption is needed to reduce the complexity of the heat transfer analysis of the process. Because of assumptions like this, most of this research remains highly theoretical and hardly any of it has been made available to the designer of die casting dies in the form of design tools and guidelines.

The lack of understanding of the loads in die casting dies is evident when talking to die designers and even die casters. Die deflections are a routine event in die casting. There is probably no caster that has not seen proof of the shift of cores or the blow of the parting line by analyzing finished castings. The more advanced die manufacturing companies have even measured some of these deflections. However, when one inquires about the loads that cause these deflections, each individual is likely to have a different perception of the problem. Furthermore, not many have the data to substantiate an answer.

In general, die casting die designers follow a series of recipes and guidelines to come up with their design. Most of these guidelines are the
result of years of experience. However, the reliability of these guidelines is so questionable, that the usual practice is to leave as much material in the die as possible during the manufacturing stage. For example, because of the unpredictability of the fluid flow runners are machined without actually letting them feed the cavity, e.g. the gates are not machined. After a trial run, the die is re-machined to create the necessary gates, or to re-dimension features within the cavity. This is usually an iterative, time consuming, uneconomical process.

In sharp contrast with the tools available to the die casting die designer, a variety of CAE tools are available for other manufacturing technologies. For example, injection mold designers can use C-Mold\textsuperscript{1} or Mold Flow\textsuperscript{1}, sheet metal forming die designers have Pam-Stamp\textsuperscript{1} and forging die designers can count on Deform\textsuperscript{1}. Although these packages may not specifically address the issue of structural die design, they do provide the designer with a customized, integrated analysis system for the prediction of manufacturing loads upon the structure of the die.

1.6 Problem statement

The structural design of die casting dies depends completely upon the experience of the designer. Little, if anything, has been done to analyze the combined effect of thermal and mechanical loads upon the die structure. There exist neither customized tools nor a set of guidelines to use generic
software (Finite Elements or Boundary Elements) for the purpose of designing dies. A partial understanding of the loads upon dies is one of the reasons why the structural design of dies has remained an art form.

By predicting the magnitude of the elastic deflections of die casting dies, design modifications may be adopted before a die is put in place. Directly, the dimensional quality of the product may be improved. Indirectly, the quality of the die manufacturing process may also be improved because of the reduction in the number of trial runs. The quality of the casting process may also be improved, as operational problems such as flashing can be reduced or isolated to locations of the die where they are easily accessible.

There are software and engineering guidelines that can help the designer predict certain types of loads, individually. A variety of computer packages (such as MAGMA) that can help in predicting solidification patterns in the casting are commercially available. These packages may be used to estimate thermal loads during filling (under certain conditions), and solidification. There is commercial software that can assist in the preliminary design of cooling lines. The commercial solidification packages can also help analyze the thermal effects of cooling lines. The combined effects of the loads is not predicted by any of these systems.

In summary, in spite of the fact that dies are high performance mechanical components, die casting die design remains an art. To this date, a
global perspective of the behavior of the die has never been prepared. No work has ever attempted to present a picture of the role that the full range of loads play on the magnitude of die deflections.

1.7 Research objectives

This research work focused on the structural design of dies. The goal of this research is to define and test procedures and models that can be used to analyze, through computer simulations, the structural performance of die casting dies. The particular objectives of this work are:

- Characterize the different loads upon a die. This requires the definition and description of loads in such a way that the performance of a die or a given mechanism can be evaluated within a scale that is significant in terms of the quality of the casting operation.

- Characterize the behavior of the die. By understanding how a die behaves, statements may be made about the characteristics that are desirable when the goal is to reduce deflection related problems.

- Establish the manner in which die deflection data can be analyzed and presented.

Some of the questions that this work should answer are:
What loads should be considered if the objective is to simulate die deflections? From a different perspective, why should one worry about loads other than heat from solidification, which is what commercial FEA systems can address?

What kind of resolution can be expected from a simulation?

What issues need to be addressed in order to increase the resolution of our results?

What recommendations can be made to improve current die design practices?

As a test, the procedures developed in this research were applied to the prediction and analysis of sliding core deflections.

1.8 Research contributions

This research addresses the characteristics of the die casting process from a unique perspective: that of deflections of the die. As a result, a global view of the characteristics of loads in die casting has been prepared. To this date, there is no other formal study of such characteristics. Furthermore, this characterization has allowed us to correlate the behavior of the die with certain die design parameters (in those cases that have been analyzed). Perhaps more importantly, specific process conditions such as injection and intensification pressure, or plunger deceleration characteristics may be
correlated with the behavior of the die. Rarely is the correlation of die behavior with process conditions addressed in the literature.

From the perspective of the process of designing the structure of the die, procedures and recommendations for modeling die deflections have been prepared and tested with several specific cases. These procedures are general enough to be applied to any die geometry. Details about the scales of resolution of models and results have been addressed. At least one failure mode from the perspective of die deflections has been established: parting plane separation (i.e. elastic deflection). With this failure mode in mind, the manner in which deflection data can be displayed for analysis and design purposes has been defined. Finally, a picture of the response of a die casting die has been prepared.

1.9 Dissertation organization

This dissertation is organized in six chapters plus a list of references. In the first chapter, the state of the art in die casting die design has been described. Based on this description, the objectives of the proposed work have been established. Chapter 2 presents a review of some of the work that relates to the analysis of the die casting process and the design of die casting dies. Chapter 3 presents a detailed description of the loads upon a die casting die. Chapter 4 introduces the procedure for modeling die deflections, with a detailed description of the issues that need to be addressed. Throughout this
chapter, the procedures are illustrated by applying them to the analysis of
deflections in an open-close die, that is, a die that contains no moving
members and that has a flat parting plane. In Chapter 5, these procedures are
used to analyze an isolated component: a sliding core. Finally, Chapter 6
summarizes the conclusions from this work and suggests directions for
future research.
Chapter 2. Literature Review

2.1 Introduction

In general, there are four steps in a typical cold chamber die casting cycle: ladling, injection, solidification, and ejection. Because of their importance to the quality of the finished product, the analysis of fill and solidification in die casting has been an important research exercise during the last thirty years. At first, the bulk of the research consisted of experimental work, complemented with engineering analysis. Lately, the emergence of affordable computer power has shifted the attention of the research work towards the numerical analysis of the die casting process.

For the analysis of the die casting process, each investigator usually defines a set of assumptions, simplifications and solution techniques that make his or her work unique. However, the foundations of most of the published research work can be traced back to at least one of a few basic approaches.

In one of these approaches, the complexity of the filling process is neglected by assuming the flow to be laminar, with a continuous front. Heat
transfer between the mold and the die is predicted during the filling process as well as after fill.

It has been argued that such analysis is more representative of other casting processes. The cornerstone of another type of analysis is the "instant fill" assumption. Its basic principle is that the injection process is so fast that no heat is released from the molten metal onto the die during filling. Further simplifications are usually made to solve the equations that provide a description of the fragmented filling flow. After this, solidification can be predicted assuming that the initial temperature of the melt in the cavity is uniform.

Very few researchers have tried to simulate the die casting process for any of the following conditions: the process is three dimensional, non isothermal, with turbulent flow. Two facts are also evident:

- The purpose of analyzing the injection flow is to predict the filling patterns. No attention is paid to the transfer of momentum between the flow and the cavity faces.
- The modeling of the solidification usually starts with a fluid at a uniform temperature. The thermal differential that may be found in the melt within the shot sleeve is not considered.

With the objective of gaining an understanding about the loading mechanisms during a die casting operation, a review of some of the literature
relevant to the analysis and modeling of the die casting process is presented in this chapter. Both, theoretical and experimental work are included. In addition, an article that describes some of the new trends in flow and solidification modeling is reviewed. Also, those few cases in which the structure of the die is analyzed have been reviewed.

2.2 Heat and fluid flow in casting

In general, the coupled heat transfer and fluid flow analysis of metal solidification systems became a common activity during the 1980's. The numerical simulation of complex phenomena such as macrosegregation as well as the modeling of solidification coupled with stress or microstructure analysis for gravity and low pressure casting have been reported in the literature. Voller [6] presents an overview of the state of the art, including a description of the numerical approach to solve a coupled flow/solidification system and some of the unresolved issues. Ono et al [7] introduce a formulation to describe flow/solidification mechanisms in gravity casting of metal that is in contact with different mold materials simultaneously. Their analysis assumes laminar, unidirectional flow. Their approach is clearly more suitable for low pressure die casting process than the high pressure process.

Lipinski et al [8] present the numerical basis of MAGMAsoft, a finite difference (FD) solver for the simulation of gravity castings. In this article,
they state that the FD approach is favored over an FEM formulation because of the large CPU times required by the latter when applied to real life situations. Though MAGMAsoft is better suited to gravity casting, the application of this software to the analysis of a die casting case is presented by Kallien and Sturm [9]. Although at first this software was advertised as a good tool to predict thermal patterns in dies only, current reports seem to indicate that in certain cases its flow solver will predict flow-related problems adequately¹. Still, this software is subject to the shortcomings typical of the finite difference method when it comes to reproducing the geometry that is intended to be modeled.

Ohnaka et al [10] compared FD simulations of fluid flow with experimental results of water, mercury and molten aluminum flowing into a flat cavity. Their modeling approach seemed to match experimental data with some degree of success in the case of water. However, results did not match the observed flow patterns of mercury and molten aluminum particularly well. They concluded that their approach could be improved by improving their treatment of the free surface formation and surface tension properties of the flowing metals.

Finally, Muttin et al [11] utilized a finite element formulation in a Lagrangian frame of reference to solve the Navier-Stokes equations for small

¹ MAGMAsoft users meeting, Columbus, Ohio, October 1994.
Reynolds numbers. Though they claim that their formulation is very well suited for the computation of the free surfaces of the flow and that it results in lower computational costs, questions regarding the need for remeshing during the simulation and the modeling of surface tension and turbulent flows remain unanswered.

2.3 Heat and fluid flow in die casting

Because of the complexity of the injection process, the study of die casting has not progressed at the same pace when compared with other manufacturing technologies. The 1960's and 1970's witnessed the beginning of the engineering analysis of the die casting process. Research was funded by organizations such as the Die Casting Research Foundation and the Commonwealth Scientific Industrial Research Organization (CSIRO) in Australia.

2.3.1 Research by Wallace's team

Funded by the Die Casting Research Foundation, Jack Wallace's team at Case Western Reserve University (Cleveland, Ohio) conducted pioneering research in die casting during the early 60's. Their contributions in the areas of runner and gate design as well as fluid flow and heat transfer analysis in die casting are too numerous to list here. Nevertheless, a paper by Lindsey and Wallace [12] is mentioned in this section because it provides an excellent
overview of some of the loads upon the tooling during a die casting operation. In this article, the authors present a comprehensive description of the mechanisms by which the melt loses heat during the die casting operation. A series of equations to quantify these losses is also included. In addition, nomograms for determining optimum fill time for several alloys are introduced. In essence, this particular paper summarizes the results of an important part of the research that Wallace's group conducted during that time. The guidelines for determining optimum fill times were tested against laboratory data. However, there is no indication of how these tools perform under field conditions.

2.3.2 Research funded by CSIRO

During the late 60's, ILZRO, CSIRO and the Australian Zinc Development Association funded a series of studies whose main goal was to define gate/runner system design rules. Siauw and Davis [13] present an analysis of the flow in tapered runners. Their work focused in the flow through the runner and gate. They discretized the domain (they subdivided the runner into smaller portions, whose centers were denominated nodes) and applied the continuity equations to this arrangement. Using Bernoulli's equation for the energy of the fluid and neglecting terms associated with potential energy due to position and gravitational effects, they were able to compute the fluid velocity and pressure at each node. They claim that their
analysis is valid for both the transient and steady state stages of the runner fill. A computer program was developed based on this work. In a supplementary paper, Davis and Cope [14] describe the results of the application of tapered tangential runners and runner sprues in actual zinc die casing dies. These types of tools have been tested in dies for zinc die casting. They are capable of providing an idea of how flow proceeds in the runner system and how mixing of air and molten metal can be reduced. However they are incapable of predicting more complex fill problems within the casting, such as cold shuts.

CSIRO's Division of Manufacturing Technology developed a package to perform thermal analysis of the die casting process. Based on a boundary element formulation, this software solves a two dimensional model of the steady-state heat conduction in dies. Because of the nature of the software, the determination of the loads as well as the cross sections of the die depends upon the experience of the user. Siaw and Nguyen [15] present a series of recommendations to use this software. In particular, they propose that the heat flux per unit area upon the die be computed by dividing the total heat released by the melt by the total cycle time and the cavity area. Andresen [16] presents "Metlflow", a computer package that helps determine heat flux and metal flow during the injection process. For their numerical analysis, all surfaces of the casting are laid out as flat planes. The software is based on the
results of the research conducted in the late 60's - early 70's. Finally, Lee et al [17] performed temperature measurements in an experimental die that was heated with electric heaters to temperatures between 150 °C and 300 °C. Their attention focused upon the heat released by the die during coolant spraying. They concluded that the liquid flux density of the spray upon the cavity has the dominant effect on the heat transfer at the surface.

2.3.3 Research funded by the National Research Council of Canada

For over a decade, the National Research Council of Canada through its Industrial Material Institute has funded an important die casting research program. Some of the work has already been published and is listed in the following paragraphs.

Kappel and Salloum [18] present a general description of the principles governing the process of non-isothermal metal flow during die casting. They propose an approach similar to Andresen's for the analysis of the fill/solidification mechanisms. In this approach, the cavity is laid flat and it is discretized into small control volumes. They present simulation results that supposedly match experimental data. However, the data they handled was related mainly to macroscopic conditions such as injection pressures and plunger velocities. An effort to illustrate how this tool is capable of addressing other fill related issues is not made.
Frayce and Loong [19] describe the difficulties involved in performing a numerical simulation of the die casting process. They review some of the literature, and conclude that, for a given problem with the same number of degrees of freedom, an FEM solution will result in larger computation times than other approaches based on Finite Difference schemes. They also provide a brief description of some of the exact and approximate methods for the solution of the filling process in die casting. In their work, an approximate method is used to analyze a filling flow test case. Finally, they introduce Prometheus-3D, a software developed to predict three dimensional metal flow in a die casting operation. They also present some of the simulation results using this code, which are matched to partial shots. The paper by Loong et al [20] expands upon the results of this last simulation trial and complements it with a comparison to experimental results. They claim that partial shots confirmed the simulation results. However, it is not clear whether their simulations assumed that partial shots were being made. At the time these papers were published, the research team was addressing the determination of velocity, pressure, and temperature patterns during fill.

The latest paper by Frayce et al [21] introduces two FEM computer codes CASTFILL and CASTOL, that simulate fluid flow and solidification respectively. CASTFILL is able to simulate turbulent and laminar flow in cavities that can be approximated by thin shell elements. Flow directions,
velocity distributions and pressure fields are reported by this code. This software is perhaps a customized version of Prometheus-3D. CASTOL may be used to simulate solidification of castings as long as a mushy zone exists during this process. No mention is made about the possibility of simulating a non isothermal fill with CASTFILL. In the case of CASTOL, the authors specifically state that an initial condition must be defined for the temperature of the molten metal within the cavity. This probably confirms that a non-isothermal filling process is still beyond the capabilities of the filling code.

2.3.4 Research at General Motors

Perhaps the leader in study of thermally induced deflections in die casting dies is General Motors. Barone and Caulk [22, 23] summarize the theoretical work that led to the development of dieCAS, a computer code with customized utilities for the thermal analysis of the die casting process used by General Motors. In their formulation, three assumptions are made:

- the nominal thickness of the casting is small compared with its size;
- the thermal conductivity of the casting is much greater than the conductivity of the die steel;
- the casting cycle is short compared with the start up transient in the die.

Based on these assumptions, the transverse temperature gradient in the casting is neglected. Also, as shown in previous work [24], the periodic
temperature transients associated with the repeated injection cycle penetrate only a short distance into the die. The temperature of the bulk of the die maintains a steady pattern through time.

To predict the thermal patterns in the casting and the die, they utilize a boundary element solver which incorporates a special formulation to account for the cooling lines [22]. The initial temperature of the molten metal must be given as part of the description of the model (i.e. they assume "instant fill").

It is claimed that an experienced user can create a fairly complicated model in about four weeks. The average thermal patterns in the die and part, the fraction of the thermal load borne by the different die components, the cavity temperature before injection and the maximum and minimum temperature patterns can be directly displayed by this software [25].

In some of their latest work, Barone & Kock [26] studied the effect of flow on heat transfer during die casting. Their objective is to overcome the limitations of the instant fill assumption while maintaining the essential computational advantages of the original layer-based methodology. The most important additions to the modeling process are the use of spatially-varying initial casting temperature. Because of the interdependence between the amount of heat released by the casting and the die temperatures, the new methodology involves computing the die temperatures using the instant fill
assumption and then updating these values as a function of the heat released during fill. This process is repeated repeatedly until convergence is obtained at each time step. Their formulation does not predict the actual flow field. Instead, the metal velocities are averaged across the cavity thickness. They conclude that the effect of flow becomes more important as the conductance of the die lubricant or the fill time increases, or whenever there is a reduction in casting thickness or density.

Kim and Ruhlandt [27], while working for GM, performed a two-dimensional thermal simulation of a runner using a finite element code. Among other things, their results confirms that proper preheat of the die, a uniform lubricating layer and avoiding certain geometric designs will result in smaller thermal stresses around the runner region.

Other work carried out at General Motors includes the determination of stresses and strains in castings due to the solidification process, and FEM analysis of thermal stresses in die casting dies [28, 29].

2.3.5 Other relevant research

There are further examples of two-dimensional analyses of the die casting process. Anzai and Uchida [30] carried out numerical simulations as well as experimental verification of filling patterns in flat plate die castings. Their main goal was to examine the influence of the gate thickness upon the filling patterns. Although they begin their analysis with a three dimensional
formulation, they limited their study to a two dimensional flow, by assuming that the fluid keeps its thickness when it goes from a thinner to a thicker section. They subdivide the domain into cells that can be full cells (those that span the full thickness of the cavity) and free cells (those that do not). The flow is assumed to move in a plane, but its thickness is modified as it flows by applying mass conservation principles. Entrapped air was then predicted to reside in the locations that filled last. The experimental work consisted on finding entrapped air in the plate castings by performing X-ray testing on them. The results of the numerical and experimental work were then compared. According to the authors of the paper, the results matched well, although the experimental data is not included in the paper.

Chen et al [31] describe the results of applying the surface marker (SMU) method to the analysis of filling of dies with cores. Their method can be applied to the simulation of two dimensional, incompressible, isothermal, transient, free surface fluid flow. In their own words, this method is suited for simulating two dimensional water analogy experiments. Iwata et al [32] performed two dimensional simulation of filling flows with two different formulations. In the first one, they utilize a formulation similar to Chen's. In the second formulation they assumed a two dimensional Poiseuille flow, i.e. a three dimensional body flowing in two dimensions with a prescribed thickness shape. By comparing their results with experimental tests, they
were able to determine the kinematic viscosity of the molten metal. No details are given about the experimental setup used in their work.

Davey and Hinduja [33, 34] modeled the heat transfer mechanisms in die casting using the boundary element method. In general, they assumed that the overall solution to the temperature field is made up of two components: a steady state part and a time dependent perturbation. Although they recognize the existence of two temperature regions in the die, their model does not specifically deal with the solution in either one of them as separate entities, as Barone and Caulk did. Their analysis also assumes thin parts and requires an initial condition for the temperature field in the casting. When compared to experimental data, their results are only partially correct. They attribute the discrepancies to the lack of information regarding the heat transfer coefficients in die casting.

Hu et al [35] performed numerical simulations of the filling flows in die casting and compared them with water-analog experimental results. Their theoretical model was designed to predict the distributions of the mass fraction of the injected liquid, as opposed to determining the continuous free surfaces. The ultimate goal was to predict the formation of porosity within the casting. A good match is observed between the experimental patterns and the simulation results. Still, the obvious drawback is that no temperature effects are accounted for in the experimental work.
2.3.6 Trends in the simulation of heat and fluid flow

Future trends in the simulation of heat and flow of castings include the development of software that can help the designer by quickly predicting gross problems. Numerical programs can then be used to finalize a design. Brown and Spittle [36] present a solidification simulation software that uses a cellular automation technique where a part is represented by a 3-D grid of cubic elements. The status of each element (solid or liquid and temperature) is then established as a function of the six nearest neighbor elements according to a deterministic rule. The analysis predict the macro-freezing patterns, as well as macroshrinkage defect locations. However, actual temperatures or times may are not predicted by this approach. Supposedly, the user does not need to input any physical properties data. In addition to being an inspector for gross design flaws, this software may be used as an educational tool for foundry personnel.

2.4 Pressure effects in die casting

From the point of view of the mechanical loads upon the die, the most important characteristics of the die casting process are the short fill times (typically between 0.01 and 0.1 seconds), and the high cavity pressures (up to 20,000 psi). From the point of view of the hydraulic system of die casting machine, there are four stages in the cold chamber die casting process [5]:

39
• slow shot velocity: the plunger advances slowly to reduce the entrapped air in the shot sleeve until the sleeve is full.

• fast shot velocity: the metal is injected into the cavity.

• static pressure: the plunger reaches the end of its stroke as the cavity fills.

• intensification pressure: the pressure in the cavity is increased to reduce shrinkage and solidification problems in the finished part.

All of these stages result in a combination of static/kinematic pressures upon the structural components of the machine and the die. During the die casting operation, the process engineer can define specific values for each of these variables. One variable that Herman recognizes and which is not directly controlled by the process engineer is the impact pressure (or pressure spike) that occurs at the end of the filling stage. All the moving mechanisms responsible for delivering the fluid to the cavity, as well as the molten metal itself, possess a mass that must be stopped at the instant the cavity fills. Based on a conservation of energy approach, Herman claims that the magnitude of this pressure spike is proportional to the square of the velocity of the moving component.

Problems associated with the hydraulics of the die casting operation have been reported for a long time. Everett [37] describes a water hammer-type of phenomena within the injection system of a die casting machine, and
proposes the use of a surge arrestor to reduce the magnitude of the pressure related problems. He claims that careful control of this surge is needed to avoid flashing (i.e. die opening due to a very high injection pressure), or casting porosity (because of low static pressure). Later on, Kawaguchi and Tanaka [38] presented a hydraulic system design that reduces the magnitude of the impact pressure upon the cavity and some of its associated problems, such as flashing. This systems basically reduces the plunger speed right before the cavity is filled. Furthermore, they claim that there is no relationship between the pressure surge in the injection system and the pressure spike in the die cavity. Ube Industries Ltd., Japan, a builder of die casting machines, has offered the pressure spike arrestant designed by this team of investigators ever since.

Mickowski and Teufert [39] state that, in their own experience, peak pressures in the cavity may reach higher values than what Herman proposed. Further, they observed that the pressure spike did not occur at a fixed moment of the pressure history for all their casting experiments, but that it varied depending upon the geometry of the product. They describe some of the different methods that could be used to reduce the pressure spike within the cavity, such as increasing the plunger diameter or reducing filling velocity, and propose an instrumentation system that provides real time closed loop control of the injection velocity. This system would be
insensitive to variations in the amount of metal delivered during each shot (in general the actual amount of molten metal delivered to the shot sleeve differs slightly from a target value from shot to shot). Based on these same principles and objectives, Bühler Ltd., Switzerland, another die casting machine builder, designed a closed loop control system to reduce pressure spikes which has been shown to reduce flashing in field tests.

2.5 Stresses in die casting dies

Several investigations have been conducted to predict stresses in die casting dies. However, only thermal loads have been considered in the published research. Furthermore, the simulation of the effects of the clamping force or the injection pressure upon the die remain an open question. For example, Samuels and Draper [40] developed an uncoupled quasi-static, thermoelastic model that transforms the thermal results of a one-dimensional heat transfer model into a stress pattern in a die casting die. Because they assume that the pressure within the cavity does not affect the thermal growth of the die, their analysis is better suited to low pressure die casting. Their main goal was to predict thermal fatigue of the die face. In view of the simplicity of their model, their major problem became the lack of information regarding the die material properties.

Hattel et al [41] introduce a Finite Difference formulation to predict thermal stresses in die casting dies. Their work uses the latest thermal stress
analysis module of MAGMAsoft. The stresses during filling and solidification are predicted by this software, with the inherent limitations imposed by the formulation of MAGMAsoft which have already been described.

The work of Granchi et al [42], who performed a two dimensional heat transfer analysis of a die using a finite difference scheme, deserves special mention. They simplified their work by assuming that the cavity fills up instantly, and the thermal coefficients do not depend upon position or temperature. Although they did not specifically address the thermal deformation of the die, their research focused on the mechanical design of the die as opposed to the analysis of the solidification of the casting. They also pointed out the controversy that surrounds the claim by Barton [43] that during the injection process the kinetic energy of the flow results in a temperature rise near the gate. Again, they basically avoided this issue in order to simplify their model.

2.6 Experimental work

Several references report the measurement of different parameters during the die casting operation. Hong et al [44] calculated the heat transfer coefficient at the die cavity/ molten metal interface. In order to compute this
parameter, they assumed that the die fills instantly. They report a value in the range of $7.9 \times 10^4$ to $8.7 \times 10^4$ W/m$^2$K for the heat transfer coefficient.

Mochiku et al [45] proposed a concept that they labeled "intelligent die casting". Their idea was to control the quality of the die casting process by constantly monitoring the temperatures and deformations throughout the die and the machine. For this purpose, they propose the use of a variety of pressure, temperature and displacement sensors that were developed in previous work. At the time their paper was published, they had only instrumented the cavity of an experimental die, and were in the process of instrumenting the rest of the machine. Closely related to this work is the research by Chijiwa and Shirahige [46], and Hatamura et al [47]. The former discusses one of the early attempts to develop temperature and pressure sensors for die casting. The latter is an extension of the report by Mochiku, and presents a detailed explanation of the use of such sensors. Their data reveals an important influence of the injection velocity upon the pressure and temperature patterns within the die cavity. Based on this data, they proposed two qualitative models that intend to describe the metal flow at the two velocity levels with which they experimented. They visualize a two phase, lava-like flow at low velocities, and a spray-like flow at higher velocities. No further description of these models was presented.
Nelson [48] placed 5 thermocouples at different depths from the surface of a die for a magnesium casting. Based on his measurements, he was able to determine the heat transfer coefficients between the melt and the die at different moments of the casting cycle: when the casting still has superheat, during solidification and after the casting solidifies. No mention is made of whether the value of the heat transfer coefficient changes in space.

Lindberg et al [49] filmed the flow of an aluminum alloy into an experimental die. They defined a formula based on the gate geometry to identify three flow regimes: continuous jet flow, course particle jet flow and atomized jet flow. In spite of the limitation imposed by the filming speed, they were able to visualize the different regimes as well as some of the effects of the entrapped air upon the flow. Among other things, they conclude that a film recording rate of over 10,000 pictures per second is necessary to capture the flow fields for velocities over 20 m/s.

2.7 Work at Ohio State

Since its conception, the Engineering Research Center for Net Shape Manufacturing (ERC/NSM) at the Ohio State University has maintained an active research program in the area of die casting. Published work ranges from the theoretical and experimental analysis of wear and soldering in die casting dies [50-52], to the analysis of process parameters upon the quality of a die cast part [53]. Particularly related to this research, Brennan [54] simulated
the thermal fatigue of the cavity surface using ABAQUS, a commercial FEM
package, while Paliani and Brevick [55] simulated the thermoelastic
deformation of shot sleeves in cold chamber die casting.

Of interest are the investigations by Papai and Mobley [56], who
measured temperature fields and heat fluxes in an experimental die. Their
work showed that the major temperature gradient occurs in regions very
close to the cavity, while the temperature of the bulk is fairly uniform and
changes gradually with time.

In general, the publications by the ERC/NSM in the area of die casting
are too numerous to list here. Nevertheless, the work by Padiyar and Miller
[57] and Ahuett-Garza et al [58] must be mentioned because the research
described in this document represents an extension of their work. Their work
has addressed some of the issues in the simulation of die casting die
deformation using ABAQUS, and has produced data regarding the parting
plane separation during the casting operation of simple open/close type dies.
In the following chapters, some of these issues as well as the way in which
they have been addressed will be described in more detail.

2.8 Remarks

Due to the extreme conditions encountered in die casting, the
numerical modeling of the process or even the measurement of these
conditions present a difficult problem. As a result, a variety of issues
surrounding the computation and prediction of the loads needed to evaluate the mechanical performance of a die casting die have not been addressed yet. There are at least four different loads on a die casting die:

- Heat released by the molten metal.
- Momentum exerted by the fluid flow on the different die components.
- Cavity pressure, including the intensification pressure.
- Clamping (locking) pressure.

Our literature review has shown that of all of these, only the calculation of the heat released by the molten metal has been given close attention. The models available to predict the heat load are more suited to gravity casting than to die casting. The accurate prediction of the heat released during fill is virtually an impossible task. In essence, an accurate description of the thermal loads generated during fill and solidification, would require one to solve the fluid flow equations (continuity, Navier Stokes) in a regime that frequently surpasses the onset of turbulence coupled with the energy equations that govern the thermal behavior of the casting, where phase change may be encountered, and in an arbitrary domain. To give an idea of the magnitude of such task, it will be mentioned that analytical solutions of the Navier-Stokes equations exist only for a handful of geometric arrangements, for fluids of constant properties in steady state.

47
As presented in the literature review, numerical work has produced some results in describing fluid flow and heat release for laminar flow regimes similar to what would be found in the gravity casting. In the case of die casting, work still needs to be done. Furthermore, in many cases the description of the heat released by the molten metal is not the main goal of commercial casting software, but it is rather a byproduct of the solidification analysis. Generally, transferring the results of the solidification analysis from a commercial system into a die stress analysis model is not a straight forward task. Furthermore, there is no indication of how "accurate" our description of the thermal loads needs to be in order to predict the mechanical behavior of our die with some degree of confidence. In other words, an accurate description of the thermal loads upon the die may not need an accurate description of the solidification patterns in the casting. As our review has shown, only in one case [23] has an effort been made to systematically avoid completing solidification analysis to determine thermal patterns in the die.

Industry [59, 60] has constantly reported the deflection of moving elements in dies for transmission casings. Depending upon whose account one listens to, these deflections may be caused by the momentum exerted by the flowing metal as it impinges upon particular locations of the die, by the application of the intensification pressure, by the temperature differential during fill and solidification or by their combined effects. The reality is, there
is no formal study that can help us define the contributions of each type of load to the deflection of a particular component of a die. The complications associated with predicting flow and solidification combined with other mechanical loads are partially to blame for the poor understanding of the loading picture. Another reason may be the fact that, in general, thermally induced problems have captured the attention of most researchers.

To the best of our knowledge, deflections due to pressure in the cavity and clamping forces have not been simulated outside of Ohio State. Again, a lack of knowledge as to the time-space characteristics of the cavity pressure is partially to blame for this fact. The effect of the clamping force upon the die structure presents another case that has not been looked at from the point of view of simulation except at Ohio State. This situation exists in spite of the fact that the simulation tools for this type of analysis are commercially available (although their use may need to be explored further).
Chapter 3. Characterizing the Loads in a Die Casting Operation

3.1 Introduction

In this chapter, the loads that act on the structure of a die in a typical die casting operation are introduced. Whenever possible, a mathematical description of the phenomena in question has been prepared. The loads are presented in the same sequence as they would occur during the actual casting operation. Based on this description, simplifications and assumptions to generate boundary conditions in the Finite Element model are made. As will be shown, the first and perhaps most important obstacle in predicting the behavior of the die is the existence of three different time scales and three size scales. The time scales are due respectively to:

- fill time, on the order of hundredths of a second,
- solidification time, on the order of seconds, and
- casting operation time, on the order of hours

The size scales are:

- size of the features in the cavity, millimeters,
• size of the thermal layer subject to transients within each cycle, centimeters

• size of the region where temperatures fluctuate significantly only in the long run. Same order of magnitude as the size of the die (meters).

As will be explained throughout this chapter, there is a sequence of events that interact to produce a deflection pattern. In essence, the maximum deflections in the die are a function of the stiffness of its structure. In turn, this stiffness depends upon three factors:

• fundamental design of the die

• clamping force

• temperature of the die

In general, a die deflects in response to the application of pressure. Throughout the cycle, the die deflects in regions close to the cavity in response to the effects of the heat released by solidification. The next paragraphs provide a description of the loads and the time and dimension scales in which they have an effect. Figure 3.1 illustrates the loads that act upon the structure of a die casting die.
Figure 3.1. Different stages and corresponding loads in a die casting operation.
Stage 3. Holding
- Intensification Pressure
- Heat Released during Solidification

Figure 3.1. (Continued).
3.2 Locking action

For all practical purposes, a die casting cycle begins with the closing of the dies. At the instant of closure, the machine presses the ejector and cover sides of the die together. The locking force is exerted by a toggle mechanism. Figure 3.2 presents a macroscopic model of the clamping action of the machine upon the die. Once the toggle is engaged, the locking action essentially pre-loads the die in preparation for the application of pressure generated by the injection stage. The magnitude of this pre-stress is limited by the maximum locking force of the machine. This macroscopic model of the locking action of a die casting machine predicts a behavior which is analogous to the behavior of a bolted joint (see Figure 3.2), and it is useful for the purpose of computing the shutoff force (Fs) needed to hold a die together. Clearly, dies will be closed as long as Fs is positive. This model however cannot predict the spatial nature of Fs (or Ps, the shutoff pressure) at the parting surface, which in the end will determine the likelihood of losing shutoff area, and which is in reality the design variable that determines the ability of the die to be free of flashing.

A simulation to predict the magnitude of the deflections of the die needs to account for the loads generated by the locking action of the machine, particularly if the subject of study is the parting plane of the die. It is not clear why other die casting die deflection studies neglect the effects of the clamping
Figure 3.2. Schematic model of the locking action of the die casting machine. All forces are computed by multiplying the corresponding pressures by the area upon which they act.

1. Ejector Cover Die
   Die in open position

2. Ejector Die
   Fo
   Fo
   Dies locked

3. Dies Locked
   Pt.-> Distributed clamping force

4. Fo = Clamping force
   Pt. = Fo / A
   A = Area of ejector support

5. Forces:
   - Fo
   - Pc
   - Ps

6. Cavity Force : Fc

7. Graph:
   - Fc = Force on machine linkage
   - Ps = Shutoff force

55
action of the machine. At first sight, an order of magnitude analysis shows that the thermal stresses in a die are given by:

\[ \sigma = E \cdot \alpha \cdot \Delta T \]

In particular, \( \Delta T \) represents the rise in temperature of the region around the cavity that defines the thermal interaction of the die and casting. Under typical conditions:

\[ E = 2.1 \times 10^{11} \text{ (Pa)} \] (Modulus of Elasticity)
\[ \alpha = 15 \times 10^{-6} \text{ (°C}^{-1}) \] (Coefficient of Thermal Expansion)
\[ \Delta T = 250 \text{ (°C)} \]

therefore,

\[ \sigma = 7875 \times 10^5 \equiv 1 \times 10^9 \text{ Pa} \]

By comparison and based on typical die/machine arrangement settings\(^1\), stresses generated at the parting surface are of the order of \( 2 \times 10^6 \text{ Pa} \). This load is almost 2 orders of magnitude smaller.

A more careful look will show that this is true only if one neglects the true interaction between both loads. For the thermally induced loads to reach such high values, the die must be constrained from growing in the direction

\(^1\) Based on clamping force and die size data published on the Bühler Cold Chamber Die Casting Machine Catalog.
of the application of the locking force. This will necessarily be accompanied by an increase in the locking force.

As will be shown in the next chapter, a unique feature of the current work is the manner in which the effects of the clamping action have been accounted for. As a first approximation, a distributed pressure behind the ejector die may be used (similar to what is displayed in Figure 3.2). This model introduces the effect of the clamping stress and gives an idea of the magnitude of the stresses in the die under balanced loads (a balanced load would be the ideal condition in which clamping force is kept at the desired value, and in practice would be attained by setting the travel of the movable platen to a distance that compensates for the growth of the die from room temperature, while still providing full clamping force). On the other hand, this particular model is unable to account for the nature of the support provided by the foundation. Furthermore, because the pressure is applied directly on the surface of the model, predicted stresses and deflections may be unreasonably high in regions close to where this load is applied. More refined FE models of the die should include spring elements to account for the support of the machine. This would allow a better representation of the environment around the die, while eliminating the need to model explicitly the machine. These issues will be addressed in more detail in the next chapter.
In summary, it is necessary to account for the effects of the clamping force when simulating die casting die deflections. This is particularly important if the goal is to establish the behavior of the die at the parting plane, or the effectiveness of an inboard lock. In cases where there is no preload, such as in typical horn pin lock arrangements, clamping force is not critical for the analysis. Typical lock designs are introduced in Chapter 5.

3.3 Filling Stage

During fill, molten metal is injected into a die cavity in a very short time. In aluminum die casting for example, liquid metal may be ladled at 650 °C and forced to fill the cavity in less than 0.05 sec. In general, these conditions result in flow regimes within the die cavity that surpass the onset of turbulence. From the perspective of our analysis, the filling process is important to the degree in which a die is loaded. In other words, the mechanisms of fill need to be described with a resolution that is significant from the point of view of their effect upon the magnitude of die deflections. From this same point of view, the characteristics that define molten metal flow are the hydrodynamic pressure and momentum transfer generated during the filling stage, plus the heat released by the metal as it solidifies.

The definition of the flow characteristics is not a straightforward task. Traditionally, researchers have implemented solutions of the Navier Stokes equations, coupled with the mass conservation and energy equations to
describe the filling mechanisms. A flow problem is defined by the following set of equations:

**Momentum Equations**

\[
\begin{align*}
\rho \left( \frac{\partial u}{\partial t} + u \frac{\partial u}{\partial x} + v \frac{\partial u}{\partial y} + w \frac{\partial u}{\partial z} \right) &= -\frac{\partial p}{\partial x} + \mu \left( \frac{\partial^2 u}{\partial x^2} + \frac{\partial^2 u}{\partial y^2} + \frac{\partial^2 u}{\partial z^2} \right) \quad 3.1 \text{ (a)} \\
\rho \left( \frac{\partial v}{\partial t} + u \frac{\partial v}{\partial x} + v \frac{\partial v}{\partial y} + w \frac{\partial v}{\partial z} \right) &= -\frac{\partial p}{\partial y} + \mu \left( \frac{\partial^2 v}{\partial x^2} + \frac{\partial^2 v}{\partial y^2} + \frac{\partial^2 v}{\partial z^2} \right) \quad 3.1 \text{ (b)} \\
\rho \left( \frac{\partial w}{\partial t} + u \frac{\partial w}{\partial x} + v \frac{\partial w}{\partial y} + w \frac{\partial w}{\partial z} \right) &= -\frac{\partial p}{\partial z} + \mu \left( \frac{\partial^2 w}{\partial x^2} + \frac{\partial^2 w}{\partial y^2} + \frac{\partial^2 w}{\partial z^2} \right) \quad 3.1 \text{ (c)} 
\end{align*}
\]

**Mass Conservation**

\[
\frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} + \frac{\partial w}{\partial z} = 0 \quad 3.2
\]

Subject to the boundary conditions \( u = v = w = 0 \) where the fluid is in contact with the die walls, plus

\[
\frac{\zeta}{r_1 + r_2} = p \quad 3.3
\]

where \( \zeta = \text{surface tension} \) and \( r_{1,2} \) are the principal radii of curvature of the free surface. This boundary condition is needed to establish the shape of the fill front. Additionally, in the region corresponding to the location of the biscuit:

\[ p(t) = \text{prescribed} \]
This system is coupled with:

Energy equation within the fluid

\[
\rho c_p \left( \frac{\partial T}{\partial t} + u \frac{\partial T}{\partial x} + v \frac{\partial T}{\partial y} + w \frac{\partial T}{\partial z} \right) = k \left( \frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} \right)
\]

Energy equation within the die

\[
\rho c_p \frac{\partial T}{\partial t} = k \left( \frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} \right)
\]

(internal heat generation and other second order terms have been neglected).

Energy balance at the interface solid-liquid within the fluid

\[
\rho H \frac{\partial S}{\partial t} = k \frac{\partial}{\partial n} (T_i - T_*)
\]

with initial conditions

\[ T_i = T_i \text{ where } T_i > T_{\text{liquidus}} \]

and boundary conditions

\[ \frac{\partial T}{\partial n} = 0 \quad \text{at liquid - air and solid - air interfaces} \]

and

\[ -k \frac{dT}{dn} = h(T_d - T_i) \quad \text{at liquid - die interface} \]
This system constitutes the complete problem statement during die fill. Equations (3.1) are commonly known as the Navier Stokes Equations. Except for a few cases, no analytical solution exists for these equations. In the case of die casting, the presence of jet or atomized flow requires that the boundaries of the fluid front in the vicinity of obstacles be computed as part of the solution, thus complicating the problem immensely.

As discussed in Chapter 2, implementation of numerical solutions for this system in arbitrary domains is extremely complicated unless simplifying assumptions are made. The use of these equations implicitly carries the assumption that molten metal behaves like a Newtonian fluid, in spite of the fact that in a die casting operation molten metal carries a considerable amount of solid. An argument may be made that the system is in fact a slurry. Nevertheless, these equations seem to provide reasonable results when predicting the characteristics of flow for gravity casting operations. Still, the validity of using this formulation when predicting the characteristics of high pressure die casting still needs to be investigated.

Characterization of the loads during fill consists in estimating the relative contributions of each load to the overall deflections of the die. Under certain conditions, a scale analysis similar to the one presented by Eckert [61]

---

2 Equations 4.1 are a particular form of the Navier Stokes Equation. The general form contains terms that account for effects of gravity and fluid compressibility.

61
can be helpful. The objective in those cases is to establish the order of magnitude of the loads. To differentiate between the contributions of two different loads of the same order of magnitude, other techniques must be used. For example, the above system of equations (3.1 through 3.5) may be solved under certain conditions using a numerical simulation based on a finite difference scheme.

There are general purpose computer codes that are designed to handle this type of problem. Venkatesan et al [52] have shown the type of work needed to customize one of these systems, Flow 3D, for the analysis of flow during injection in a die casting operation. Validation data is extremely hard to find, however. Furthermore, even for simple problems the computational power required to obtain a solution prohibits the use of these systems as everyday design tools.

Given the difficulties in solving the equations necessary to predict the filling regime and, hopefully, the pressures, temperatures and forces exerted upon the die, two questions must be addressed first:

- based on these equations, but without solving them explicitly, what kind of statements can be made about the nature of the loads upon the structure of the die? and
• how accurate does the description of the loads during fill (or, for that matter, at any stage) need to be in order to allow one to model die deflections with some degree of confidence?

These questions will be addressed throughout the rest of this document.

3.3.1 Heat released during fill

The simplest way to analyze the filling stage is by assuming that this process takes place under isothermal conditions. In essence, Equation 3.4 and those that follow are eliminated from the problem formulation. The flow regime can then be established by solving the Navier Stokes Equations (3.1). This is still a formidable task given the conditions found in a typical die casting operation.

As has been mentioned, Eckert [61] tried to develop a generic description of the mechanisms that take place during the filling stage. With a scale analysis, he concluded that the ratio of $t_s$, the time scale for solidification, to $t_c$, the time scale for conduction within the die, is given by:

$$\frac{t_s}{t_c} = \frac{H_{Al}}{c_{p-Die} \Delta T_{Al-Die}}$$

Typical values of these parameters in a typical die casting operation are:
\[ H_{AI} = 390,000 \text{ J/kg} \]
\[ c_p = 590 \text{ J/kg}^\circ \text{C} \]
\[ \Delta T = 350 \text{ } ^\circ \text{C} \]

where \( \Delta T \) represents the typical temperature difference between die and molten metal. From this equation:

\[ \frac{t_s}{t_c} = 1.9 \]

or the time scale for conduction is of about the same order of magnitude as the time scale for solidification. This fact illustrates the point that the heat transfer characteristics of the die do not hinder the solidification process. Die casting belongs to the class of processes whose solidification time is governed by the characteristics of the interface between the molten metal and the die. Referring to Figure 3.3, the heat released by the casting is absorbed by the die through the finite conductance imposed by the interface. Assuming the molten metal is exactly at its liquidus temperature (i.e. no superheat) and based on Equation 3.5, the total heat transferred into the die per unit area is given by:

\[ \rho H \frac{dS}{dt} = h(T_{AI} - T_{Die}) = h\Delta T \]

where \( S \) is the length of solidified metal measured from the casting-die interface, \( H \) is the latent heat of the solidifying material and \( h \) is the thermal resistance of the interface. Solving for \( S \):
\[ S = \frac{h \Delta T}{\rho H} \int_0^t dt \]

Assuming that the time scale for solidification is defined as the time in which the solidification front reaches the middle of the casting (refer to Figure 3.3), then:

\[ S = \ell \]

Where \( \ell \) is half the thickness of the part\(^3\)

\[ t_s = \frac{\rho H \ell}{h \Delta T} \]

(3.7)

the time scale for fill is given by

\[ t_f = \frac{L}{V} \]

and finally

\[ \frac{t_s}{t_f} = \frac{\rho H \ell V}{L h \Delta T} \]

(3.8)

Typical values for die casting are:

\(^3\) Given that this is a scale analysis, this parameter may also refer to the average thickness of the part, or the smallest thickness of the part, when there are significant differences between the smallest and largest thickness.
\[ \rho_{Al} = 2570 \text{ kg/m}^3 \]
\[ h = 40,000 \text{ W/m}^2 \cdot \text{°C} \]
\[ H = 390000 \text{ J/kg} \]
\[ \Delta T = 350 \text{ °C} \]

or

\[ \frac{t_s}{t_f} \equiv \ell \cdot \frac{70}{t_f} \]

It would be expected that heat released during fill would not be significant as long as \( \frac{t_s}{t_f} \geq 10 \), which requires \( \frac{\ell}{t_f} \geq \frac{1}{7} \) (where \( \ell \) is in meters and \( t_f \) is in seconds). In this case, the fill time is at least an order of magnitude faster than the time required for solidification. Heat released during fill would not play a role in the magnitude of the deflections of the die under these conditions. In other words, from the point of view of the magnitudes of deflections, the analysis can assume that the molten metal starts at a uniform temperature within the cavity. As a corollary, fill should proceed without thermally induced problems (such as cold shuts) whenever \( \frac{t_s}{t_f} \geq 2 \).

This approximation is based on the knowledge that under typical conditions molten aluminum will carry a significant fraction of solid during fill (40\%-
60%). The assumption is that solidification time is about twice the time it takes to fill the cavity.

Clearly, the last statement stretches the validity of Equation 3.7, given all the assumptions made before arriving to this equation. Nevertheless, this number is consistent with the field observations reported by Chen [62] reproduced in Figure 3.4. This figure illustrates how a majority of castings in a typical plant are produced under conditions that the previous formulas indicate will not have fill related problems. By the same token, the figure also shows that most castings will fill in a regime in which heat released is not insignificant.

It is important to establish that the degree of significance depends on the effect that is being analyzed. As it will be shown, even in the case in which large thermal gradients exist within the molten metal once the cavity is full, thermal fields and deflections are affected only in a small scale. On the other hand, it is unlikely that the chemical composition of the casting will be unaffected by a thermal imbalance.

An issue arises in those cases in which there is a significant amount of heat released during fill (i.e. when $\frac{t_s}{t_f} = 2$): how are the deflection patterns affected by this deviation from the instant fill condition? A significant amount of heat released during fill may affect die deflections in two ways:
Figure 3.3. Schematic model for definition of simple solidification front in die casting.

Figure 3.4. Testing the validity of results of scale analysis of heat released during fill. Data taken from [62]. Reprinted with permission from NADCA.
• within the time frame of a single cycle, the temperature fields, and possibly the deflection patterns in regions close to the cavity may differ significantly from those found under "instant fill" conditions.

• within the time frame of the whole casting operation, the quasi-steady state-temperature fields in the bulk of the die may be affected, resulting in a different stiffness of the die.

The existence of the different time and size scales is depicted by the previous two statements. Given the variation in part geometry, die design and process conditions found in die casting, it is difficult to make broad statements about the effect of heat released during fill.

Barone and Caulk [26] have studied this problem from the point of view of its effect upon the thermal patterns in the die structure. Consistent with their previous work, an exact description of filling or solidification patterns within the cavity is not developed. In their original work, a computer model was developed to estimate die temperatures during quasi steady state without the need to simulate multiple cycles. That work assumed instant fill. More recently, they have added to their model the capability of computing the liquid metal velocities during fill. These velocities are averaged across the cavity thickness. In their paper, there is no description of how the flow field is computed. However, it is clear that this is not done in a rigorous manner (i.e. solving the Navier-Stokes equations). Instead, it
appears that the casting is developed into a surface, from which a flow field is established based on a constant average velocity, which in turn may be computed by the ratio of distance to the farthest point from the gate to fill time. Temperatures are assumed constant throughout the thickness of the casting. A full thermal analysis consists of two steady conduction analyses, with a transient heat conduction analysis for thermal patterns during fill in between. This analysis provides the correction from the instant fill solution that is used to establish thermal fields in the first heat transfer analysis. Barone and Caulk also present a two dimensional case to illustrate the effect of heat released during fill. Their results indicate that only under extreme circumstances do the thermal fields in the die deviate significantly from those obtained when instant fill is assumed. Their work however reports only average temperatures in the die, without any reference as to how average die deflections are affected. Furthermore, their simulations do not seem to include the effects of coolant spray upon the die.

3.3.2 The effects of heat released during fill

A simulation with a geometry similar to the one used by Barone and Caulk was set up to establish the effect of heat released during fill, while taking into account the effect of coolant spray. The geometry and casting conditions are shown in Figure 3.5. The transient problem being solved is essentially:
Figure 3.5. Testing the effect of instant fill vs. non instant fill conditions.

Description of geometry and conditions.

Notes:
Cooling lines' cross sections are .01 x .01
Casting thickness = 0.005
All dimensions in meters

Simulation conditions:

Casting material: A1380
Liquidus temperature: 580 °C
Solidus temperature: 520 °C
Latent Heat: 390 KJ/Kg
Specific Heat: 963 J/Kg*K
Heat removed by coolant spray:
  • 625 E +03 J/m²*sec for 3 seconds
    (20% of heat given up by casting)
Initial temperatures:
  • Instant fill case: 630 °C
  • Non instant fill case:
    T_A = 830 (50% of latent heat more
    heat content than at 630 °C)
    T_B = 561 (50% of latent heat less
    heat content than at 630 °C)
    Linear distribution in between
\[ \rho c_p \frac{\partial T_{n,\alpha}}{\partial t} = k \left( \frac{\partial^2 T_{n,\alpha}}{\partial x^2} + \frac{\partial^2 T_{n,\alpha}}{\partial y^2} \right) \]

with convection boundary conditions:

\[ -k \frac{\partial T_{n,\alpha}}{\partial x_i} = h(T_a - T_i) \]

where:

\( n = \) cycle number
\( \alpha = 1,2 \) for die and casting respectively
\( i = \) coefficient that identifies the coordinate of the normal direction at interface

Because a change of phase is involved, the specific heat \( (c_p) \) is given by:

\[
c_p = \begin{cases} 
  c_0 & T_{n,2} > T_{\text{liquidus}} \\
  c_0 + \frac{H}{T_{\text{liquidus}} - T_{\text{solidus}}} & T_{\text{liquidus}} > T_{n,2} > T_{\text{solidus}} \\
  c_0 & T_{\text{solidus}} > T_{n,2} 
\end{cases}
\]

The two problems are coupled by the term:

\[ -k_1 \frac{\partial T_{n,1}}{\partial x_i} = h(T_{n,1} - T_{n,2}) \]

at the die-cavity interface. For each cycle \( n \), \( T_{n,1} \) (initial) equals \( T_{n-1,1} \) (final) from the previous cycle. In addition, \( T_{n,2} \) (initial) is prescribed as 630 °C for the instant fill case.
In the non-instant fill case there was no explicit flow simulation to compute thermal patterns in the casting or die. Instead, an unbalanced initial temperature distribution $T_{n,2}^{(\text{initial})}$ was used to account for heat released during fill. This unbalance is computed by assuming that molten metal loses heat uniformly as it travels from the entrance to the end of the cavity. Consequently, a volume pack of liquid metal at the entrance to the cavity accumulates heat from all the volume packs that have passed through it. In this case, it was assumed that the first volume package to enter the cavity loses the equivalent of 50% of its latent heat by the time it reaches the end of the cavity. Therefore, the volume packages at the end of the cavity will have less heat by a margin of 50% latent heat, while those packages that are close to the gate will have more heat by the same margin. Volumes at the symmetry plane are unchanged with respect to the instant fill conditions.

While this heat loss may seem extreme, it is known that under normal conditions there is a significant amount of solid (40% +) carried within the molten metal. Although in practice it can be expected that 50% or more of latent heat may be lost by the molten metal as it is taken from the furnace into the cavity, it would be extremely difficult to reach an unbalance in the temperature of the casting equivalent to the redistribution of heat produced by the fill process that is being assumed in this exercise. At any given point the temperature is given by:
\[ T_{n,2} = 630 \pm \frac{F}{c_p} \]

where \( F \) is the fraction of heat to be added or removed from the corresponding volume. The range of temperature between the coldest and the hottest region of the casting exceeded 250 °C. To put this gradient in perspective, a simulation with MAGMA for the three dimensional version of this geometry (a plate) will report a maximum gradient of about 80 °C between the coldest and the warmest region (the biscuit) for a case in which fill time is more than 2 times as long as would be considered typical of the application.

Returning to the 2 dimensional model, for each case, instant fill and non instant fill, 15 cycles were simulated. ABAQUS was used to carry out the simulations. Assumptions regarding the casting conditions are also shown in Figure 3.5.

Figures 3.6 and 3.7 illustrate the rate at which quasi-steady (periodic) conditions are reached for each case. These figures show that at the end of 15 cycles, the maximum temperature change by any node from the conditions at the end of the 14th cycle is less than 5 °C.

Figure 3.8 shows the temperature fields and deflection patterns for instant fill conditions at the end of 15 cycles. Figure 3.9 does the same for
Figure 3.6. a) Maximum temperature difference for any node between consecutive cycles, for instant fill conditions.

b) Projected number of cycles to reach periodic conditions.
Figure 3.7. a) Maximum temperature difference for any node between consecutive cycles, for non instant fill conditions. b) Projected number of cycles to reach periodic conditions.
Figure 3.8. (a) Temperature fields and (b) deflection patterns at end of 15 casting cycles. Instant fill conditions.
Figure 3.9. (a) Temperature fields and (b) deflection patterns at end of 15 casting cycles. Non-instant fill conditions.
non-instant fill. As would be expected, the instant fill case shows an almost perfectly symmetrical thermal field when compared to the non-instant fill case (slight deviations are found primarily due to the fact that the mesh is not perfectly symmetric). The deflection fields are also slightly different. Figure 3.10 illustrates the differences between the two cases. The nodal temperatures were obtained by subtracting the results of the instant fill case from those of the non-instant fill case. The range between the coldest and the warmest spot in this plot (Figure 3.10 (a)) is more than 100 °C, or roughly a third of the maximum steady state temperature of the bulk of the die. This occurs in a region that spans approximately one third of the length of the parting plane. The resulting difference in the deflection fields (Figure 3.10 (b)) reaches 8 E-05 m (approximately 0.003 in). This value is within the range of the manufacturing tolerance for the surface in question. While this dimension is not insignificant with respect to the change in dimensions due to the rise in bulk temperature of the die, it is apparent that its effect lies within a range that belongs in a different degree of detail.

Two interesting facts are also evident from the previous analysis. First, as seen in Figures 3.8 and 3.9, the shape of the parting plane at operating conditions differs by up to 6 x E-04 m (0.024 in) from manufacturing dimensions. Clearly, the shutoff pressure of this arrangement is much different than would be expected if the parting plane were flat. In general, die
Figure 3.10. (a) Temperature fields and (b) deflection patterns illustrating the difference between instant fill and non instant fill conditions.

80
designers do not account for this factor in their design. Furthermore, manufacturing tolerances are much tighter than this value. This suggests that the performance of shutoff surfaces may be improved by compensating for this growth rather than by improving manufacturing or assembly tolerances. Second, a true quasi-steady state, in which periodic conditions are reached, requires a very large number of cycles. Figures 3.6 and 3.7 show the projected number of cycles to reach periodic conditions. In both cases it takes about 125 cycles for maximum temperature differences between consecutive cycles to drop under 1 °C. In their own exercise, Barone and Caulk report that it took up to 300 cycles to reach quasi-steady state. The implication is that true periodic conditions would be extremely difficult to achieve in practice. Consequently, dies should be designed to operate within a characteristic range of temperatures.

Geometric arrangements and process conditions found in die casting vary widely. Consequently, deviations in the thermal fields in actual cases from what this exercise has shown can be expected. Significant heat released during fill will occur only when a part is very large compared to its thickness. Based on Equations 3.6 and 3.7, conditions defined in this exercise correspond to:

\[ t_s = 2 \cdot t_f \]

which results in:
\[ t_f = \frac{pH\ell}{2h\Delta T} \equiv 35 \bullet \ell \]

where \( \ell \) is in meters. Despite the limitations of this exercise, several conclusions may be drawn:

- under certain conditions (long fill times), heat released during fill will become significant.
- the magnitude of its effects is nevertheless a small fraction of the overall contribution that temperature fields play on the definition of the clamping characteristics of the die and can be confounded with other sources of noise.

3.3.3 Momentum during fill

It has already been explained that the filling stage in die casting is characterized by turbulent and atomized flow regimes. Prediction of flow fields and parameters can be accomplished through a computer simulation under certain circumstances. A case is presented in the work of Venkatesan [52] with Flow 3D, which has already been described. As has been said, this type of work is very expensive even for simple cases. Solving the governing equations (Equations 3.1 through 3.5) in an arbitrary domain is a formidable task. Furthermore, an argument may be made that in die casting a slurry, not a Newtonian fluid, is actually flowing.
During fill, molten metal impinges on different regions of the cavity, losing speed and changing direction. Velocities and concentration of fluid reach their largest magnitudes at the gate, where the cross section of the travel path is the smallest. Given their geometry and location with respect to the gate, slides are prime examples of components exposed to momentum generated forces. In fact, it is difficult to visualize a component that could be more susceptible to momentum induced deflections than a slide tip placed directly in front of a gate.

In general, metal flowing into the cavity will not form a continuous stream. Air entrapped within the flowing metal, changes in the plunger acceleration during fill, and non uniform transitions in the cross section of the channels that the fluid follows are some of the factors that cause a discontinuous stream. Consequently, the slide tip will be intermittently loaded during a time so short that the slide may be unable to deflect in response to the short bursts of force induced by the flowing metal. The most critical case would be a situation in which a continuous stream impinges upon the tip. This case is illustrated in Figure 3.11. To estimate the force upon the tip, a volume can be defined in the vicinity of the gate, in which a momentum balance is performed, neglecting all external and drag forces:

\[
\frac{dM}{dt} = - \int_{A_1} (\rho v) v \cdot dA - \int_{A_2} (\rho v) v \cdot dA - \int_{A_1} p dA - \int_{A_2} p dA \quad \cdots \quad (3.9)
\]

83
Schematic of Slide Tip/Gate Arrangement Modeling the tip as a beam

b) Magnitudes of deflection, based on beam models.

\[
I = \frac{bh^3}{12}, P=70 \text{ MPa}, F=10,000 \text{ N}.
\]

<table>
<thead>
<tr>
<th>Model</th>
<th>Stiffness ( \cdot \cdot \cdot K ) (N/m)</th>
<th>Deflection ( \cdot \cdot \cdot m ) (in)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.</td>
<td>( \frac{48EI}{L^3} ) (1344 E+06)</td>
<td>7.44 E-06 (0.0003)</td>
</tr>
<tr>
<td>2.</td>
<td>( \frac{3EI}{L^3} ) (84 E+06)</td>
<td>119 E-06 (0.0047)</td>
</tr>
<tr>
<td>3.</td>
<td>( \frac{17.6EI}{L} ) (493 E+06)</td>
<td>20.3 E-06 (0.0008)</td>
</tr>
<tr>
<td>4.</td>
<td>( \frac{AE}{L} ) (8400 E+06)</td>
<td>333 E-06 (0.013)</td>
</tr>
</tbody>
</table>

Figure 3.11. Analysis of momentum related deflections. a) Model of tip / gate / fluid stream. b) Table describing magnitude of deflections for different slide tip arrangements.
where:

\[ M = \text{Momentum} \]
\[ p = \text{pressure} \]
\[ v = \text{fluid velocity} \]
\[ A_{1,2} = \text{Area at inlet, outlet of control volume} \]

Assuming that the tip is close enough for the continuous stream to maintain the cross sectional area defined at the gate, and assuming steady state, Equation 3.6 for the given geometry and coordinate system reduces to:

\[
\frac{dM}{dt} = \rho(v_1)^2 A_1 \cos \alpha - \sum_{i=1}^{2} (v_{2,i})^2 A_i \cos \alpha + p_1 A_1 \cos \alpha - p_2 A_2 \cos \alpha
\]

Assuming further that there is perfect venting in the cavity, the order of magnitude of the force due to momentum can be defined as:

\[ F = \rho(v_1)^2 A_1 \cos \alpha \]

as a function of the mass of the casting (m), fill time \((t_f)\) and area of the gate \((A_g)\), and for small angles \((\alpha)\):

\[ F = \rho \left(\frac{m}{\rho A_1 t_f^2}\right)^2 A_1 \left(\frac{m^2}{\rho A_1 t_f^2}\right) \]
It can be observed that the magnitude of the force is very sensitive to the ratio of mass vs. fill time. Typical values for large castings, such as an aluminum ($\rho = 2570 \text{ kg/m}^3$) transmission case are:

\[
\begin{align*}
m &= 20 \text{ kg} \\
A_1 &= 0.001 \text{ m}^2 \\
t_f &= 0.2 \text{ sec}
\end{align*}
\]

from which $F$ is approximately 4000 N. Typical values in small castings are:

\[
\begin{align*}
m &= 2 \text{ kg} \\
A_1 &= 0.0001 \text{ m}^2 \\
t_f &= 0.05 \text{ sec}
\end{align*}
\]

which results in $F= 6225$ N. Based on these two cases, the force due to momentum can be assumed to be in the order of 10,000 N. To determine the magnitude of the deflection of the slide tip, the stiffness of the slide/tip arrangement must be determined.

The order of magnitude of the stiffness can be estimated based on beam theory. In general, slide tips that are placed in front of a gate are provided with an extra support at their tip. Tips that are not impinged upon directly by the stream coming from the gate do not necessarily have an extra support. As
a first approximation, a cantilever beam can be used as model. The deflection at the tip of the cantilever is given by:

\[ \delta = \frac{FL^3}{3EI} \]  

(3.10)

from which the stiffness can be defined as:

\[ K = \frac{3EIl}{L^3} \]

Assuming a rectangular cross section of height (h) and base (b), Equation 3.14 becomes:

\[ \delta = \frac{F \cdot 4L^3}{E \cdot bh^3} \]

For typical applications, L is of the order of 1 m, while b and h are of the order of 0.2 m at the smaller end of the tip. Then, for a steel tip (E=210 E+09 Pa):

\[ \delta = \frac{10 E+03 \cdot 4 \cdot 1^4}{210 E+09 \cdot 0.2^4} = 120 E-06 \text{ m} = 0.0047 \text{ in} \]
This value is of approximately the same order of magnitude as the manufacturing tolerances for the details in the tip, and is well below the expected distortion due to the relaxation of thermal stresses once the part cools down to ambient temperature after ejection. Figure 3.11 shows different beam models and their corresponding stiffness for a particular case. Case 1 perhaps provides the most accurate description of the order of magnitude of the slide tip deflections under momentum loads. Interestingly, for the given geometry the contraction induced by a typical cavity pressure (assuming it were applied to the full area at the end of a cantilevered tip), would be higher than the largest deflection caused by momentum.

Based on these results it can be concluded that, in general, momentum induced deflections are relatively small. Only in poor designs would deflections due to momentum become significant. On the other hand, momentum forces may exceed the weight of some of the components upon which they act. While this force may not induce significant strains within the tip, the tip may move as a rigid body if there is clearance between the slide and the cover die.

3.3.4 Hydrodynamic pressure

Of all the loads in a die casting application that have been studied in this work, hydrodynamic pressure is the one that has received the least attention. Estimating the nature of this load presents exactly the same
complications as establishing the amount of heat released during fill. Strictly speaking, the same equations need to be solved. Given the arbitrary shapes and conditions found in die casting, specific cases will present different behavior. In fact, from the point of view of the die, the unbalanced load caused by a hydrodynamic pressure may not be more severe than the natural unbalance that arises from the combination of an arbitrary geometry and the fluid path that fills it. An estimation of the relative contributions of each one of these types of loads associated with the filing flow cannot be accomplished without looking at a variety of different cases. However, and without being rigorous, a few comments may be made about the characteristics and effects of hydrodynamic pressures during fill.

Contrary to the characteristic viscosity dominated flow found in gravity casting or injection molding, the filling regimen in die casting is a momentum dominated process. This is particularly true in the cavity region. In the runner region fluid velocities are significantly lower than at the gate or within the cavity. Ignoring the effect that the cavity geometry has upon the magnitude and direction of the force as seen by the die structure, the maximum pressure drop during fill would be caused by changes in part thickness. The largest such change occurs at the entrance to the cavity.

Runners are designed to blend into the gates smoothly, maintaining a constant cross section. Molten metal accelerates as it moves from the biscuit
region towards the gate. However, once metal flows past the gate, the area for flow increases dramatically. At this point the metal is traveling at such a high speed that under typical conditions it will break apart and bounce off the cavity surfaces. In practice, gates often present a discontinuity in the path of flow. It is conceivable then that the gate defines the boundary between two regions where pressures differ significantly from each other. It is very likely that pressure drops within other regions of the die would be smaller than at this region. In the limit, the maximum imbalance would result in a load of similar magnitude as that produced by momentum induced forces in front of the gate.

In establishing the importance of the hydrodynamic pressure, there are other factors to consider. As has been discussed, filling occurs in a very short time. Variations in the pressure during fill will take place in times that are shorter than the actual fill time. The different die components may not have enough time to respond to these variations. Furthermore, if a given component were able to move in response to hydrodynamic pressure, it is conceivable that it would be able to return to its equilibrium position once the unbalance is removed. It will be shown that a hydrostatic pressure distribution seems to explain the deflections reported in field data. Under these conditions, most components would be free of unbalance once the
cavity is full, and therefore should be able to return to their original conditions.

Based on the available evidence, it is difficult to justify the effort needed to prepare a more accurate description of the deflections caused by a hydrodynamic pressure. A full scale study could be justified if the study of other phenomena (for example, formation of porosity) were included as part of the objectives of such an analysis.

3.3.5 Pressure surge at end of fill

For many years, casters have witnessed deflections that seem to coincide with the end of fill. Casters associate this deflection with a pressure surge that can sometimes be measured in the hydraulic system of the machine. This pressure spike, as it is commonly called in industry, has been known to burst pressure gauges before the magnitude of the spike is actually measured.

Figure 3.12 shows a schematic of the hydraulic actuation of the plunger in a die casting machine. In essence, the plunger is a piston that moves under the application of pressure through the hydraulic fluid. During fill, the hydraulic system is capable of generating plunger velocities of 10 m/s or more. Given the short duration of the actual injection cycle, large accelerations and decelerations are usually experienced by the plunger.
Figure 3.12. Schematic diagram of hydraulic actuation of die casting machine.

Rs = Friction within system
Pressure transients are thus expected within the hydraulic system and, more importantly, within the die cavity.

One source of dynamic loads is the kinetic energy of the moving masses within the system. In the more basic die casting machine forms and casting operations, plungers are slowed down only by back pressures exerted by molten metal upon plunger tips as cavities fill up. Clearly, all masses that are in motion (i.e. plunger and hydraulic fluid) must come to rest at the end of fill (or shortly thereafter, given that a small oscillation within the plunger hydraulic fluid-molten metal may be expected). Eventually, all dynamic loads must be dissipated by the structure of the die and machine.

The other source of dynamic loads is precisely the casting machine’s hydraulic system. Sudden rises in pressure within the hydraulic system at the end of cavity fill have been documented and reported on numerous occasions [5, 37-39, 47, 63]. This pressure spike is a result of the sudden interruption of the flow of oil as the plunger comes to rest, and is of a similar nature to the water hammer phenomena in pipes. As early as 1972, systems for the suppression of this spike in the hydraulic system of the die casting machine were designed [37].

Of particular relevance to this research is the fact that, to this date, there is considerable confusion about the role that pressure surge within the machine’s hydraulic system plays in regards to the quality of a casting
operation. Many casters are under the impression that flashing is the result of this pressure spike. Several experimental studies [38, 39, 63] have confirmed the existence of a pressure spike within the cavity of the die just at about the instant that the cavity is completely full. However, these studies have also shown that the pressure rise within the cavity does not coincide with pressure spikes in the hydraulic system. Investigators associated with these studies argue that while the presence of shock absorbers (water hammer suppressers) reduce the amplitude and period of the hydraulic vibration after plunger arrest, their effect upon the pressure surge within the cavity is minimal [38]. The implication is that, from the perspective of die deflections, the largest load upon a die at the end of fill is caused by a sudden dissipation of kinetic energy as the plunger is brought to rest. This would support the observation that the existence of large pressure spikes in a machine's hydraulics does not necessarily result in large die deflections [64]. Today, many if not all of the die casting machine builders provide their machines with some type of plunger deceleration device that reduces the plunger speeds right before they come to a complete stop.

Referring to Figure 3.13, a considerable back pressure will not exist until the runner system begins to fill. This rise may not occur at a constant rate during fill time and, given the characteristics of the filling flow, it is not uniform (spatially) throughout the cavity. The full extent of pressure is not
Figure 3.13. a) Assuming no friction, plunger experiences negligible back pressure from cavity. b) Pressure at plunger tip begins to rise considerably as runner begins to fill.
felt by the die until the cavity has been filled completely. Conversely, the plunger is not subject to the maximum back pressure until this last instant.

Barton [43] argued that the kinetic energy associated with the moving masses, or at least a significant portion of it, is dissipated in the form of heat. According to his calculations, the temperature rise in the molten metal produced by this energy may exceed 50 °C. There is no solid experimental evidence to support his claims. There are two other ways in which this energy may be dissipated:

1) In the form of sharply localized, traveling disturbances, i.e. elastic waves, that are eventually damped out within the die and machine.
2) By inducing deflections within the structure of the die. Once all energy is dissipated, the die would return to an equilibrium state. The time it would take to reach this state would depend upon the damping characteristics of the system.

Case (1) would not produce a significant (macroscopic) deflection in the die. Other issues may arise though (for example, unwanted vibrations and noise). Case (2) clearly falls within the scope of this research. The key is being able to estimate the magnitude of the maximum pressure within the cavity.

In his doctoral dissertation, Sachs [65] analyzed the die casting process from the perspective of the hydraulic characteristics of the die casting machine and die. His work consisted of a-one dimensional analysis of the
characteristics (pressure and velocity) within the system. Clearly, this analysis is capable of predicting these characteristics only at a macroscopic level. Assuming fluids are non-elastic, Bernoulli's equation for unsteady flow is:

\[ f(\xi) \frac{dc}{dt} \int \frac{d\xi}{f(\xi)} + \frac{c^2}{2} + z + \frac{p}{g} = F(t) \]

where:

- \( \xi \) = coordinate along conduit
- \( f(\xi) \) = area of conduit (variable)
- \( c \) = velocity of fluid along conduit
- \( z \) = height of fluid particle with respect to reference
- \( p \) = pressure of fluid particle
- \( g \) = acceleration of gravity
- \( F(t) \) = integrating function, independent of location

A balance of forces at the time the cavity begins fill, when pressure upon the plunger starts to build up is:

\[ A_H p_H - (A_H - A_B)p_B - A_o p_o - R_s = M \frac{dv}{dt} \]

Referring to Figure 3.12, the subscript "H" refers to the locations behind the plunger, "b" refers to the area of the plunger extension, "o" refers to the plunger tip region, "M" and "v" are the mass and velocity of the plunger respectively. While it may be argued that this equation holds throughout the
injection cycle, the reality is that a significant $p_o$ will be encountered only when the runner system begins to fill up. This factor becomes even more pronounced once metal begins to flow into the cavity. The same may be said about Rs.

Any of the given pressures can be presented as a function of the accumulator pressure ($P$). For example, according to Sachs, pressure behind the plunger ($p_b$) has the form:

$$p_H = P - \rho \frac{v^2}{2} f(r,x) - \rho \frac{dv}{dt} g(r,x)$$

where $f(r, x)$ and $g(r, x)$ are functions that account for friction ($r$) within the system as a function of plunger displacement ($x$).

For the most part, these equations are not solvable analytically, and simplifications must be made. In his work, Sachs eliminated a variety of factors that were supposedly unimportant, and determined that the relationship between the accumulator pressure ($P$) and the air cavity pressure ($p$) was of the form:

$$K_1P - K_2P = K_3 \frac{dv}{dt} + K_4v^2$$

where $K_n$ ($n=1,2,3,4$) are constants that account for pressure losses in the system. Field data was available to Sachs, and he concluded after computing a numerical solution of the previous equations that the dependency upon plunger displacement proved to be negligible, and so terms affected by this
variable were dropped from the equation. Once the Kn constants are known, pressures at different locations within the system can be evaluated.

Clearly, these equations include a multiplicity of parameters that characterize a particular machine and die. While some of these factors may be available (especially those associated with commercial components such as valves and pipes), others are more difficult to find, specifically those associated with the cavity. For these equations to have any value as tools for predicting cavity pressures, significant experimental data needs to be generated regarding flow resistance factors within the cavity.

Sachs showed that this approach provides first order approximations to the actual pressures and velocities, which may vary from results generated by more rigorous methods (e.g. based on a wave formulation) by less than 3%. The limitation of this approach is that pressure spikes / water hammer mechanisms cannot be accounted for with this formulation.

Assuming that the pressure surge within the cavity is caused mainly by the dissipation of the kinetic energy of the moving masses, an alternate approach for computing the magnitude of the spike within the cavity can be developed around the actual profile of the plunger velocity. A case in point is presented in Figure 3.14. This chart presents the history of plunger velocity and pressure at the tip vs. plunger position. This trace was recorded during
the injection of a transmission casing. The capability of the casting machine to decelerate the plunger was not used in this case.

Obviously, the hydraulic drive of the die casting machine is designed to overcome all sources of friction and back pressure ($p_b$) within the piston cylinder in such a way that the plunger can be rapidly accelerated to injection speeds. As has been mentioned, the cavity pressure will eventually provide the force that brings the plunger down to full stop. Assuming that molten metal behaves inelastically, the magnitude of the pressure surge within the cavity can be computed as the force at the plunger end divided by the plunger area. Based on the assumption of non-elasticity of the fluid and given the ability of the molten metal to take the shape of the cavity, the pressure spike is perceived as an increase in the hydrostatic pressure within the cavity. Referring to the free body diagram in Figure 3.15, a summation of forces on the plunger gives:

$$ \sum F_x = m\ddot{x} $$

and

$$ F_H - F_C = m\ddot{x} $$
Figure 3.14. Field data: velocity and pressure vs. plunger position. Data provided by Exco, Engineering (Newmarket, Ontario, Canada).

Figure 3.15. Forces acting on plunger. Free body diagram.
Based on the arguments presented before, the change in kinetic energy equals the work that the cavity pressure exerts upon the plunger in order to stop it:

\[
\text{Spike energy} = \Delta \text{K.E.} = \int F_{\text{spike}} \, dx \\
\frac{mv^2}{2} = \int (F_c - F_H) \, dx \\
= -\int mx \, dx
\]

and consequently

\[
F_{\text{spike}} = -m\ddot{x} \quad \text{................................. (3.11)}
\]

and

\[
P_{\text{c-spike}} = \frac{F_{\text{spike}}}{A_{\text{plunger}}} \quad \text{................................. (3.12)}
\]

The plot in Figure 3.14 displays only velocity vs. position. In order to extract acceleration from this chart, one can use the following form:

\[
\frac{dx}{dt} = \frac{dx}{dx} \cdot \frac{dx}{dt}
\]

or as a first order approximation

\[
\ddot{x}_i = (\text{slope at point } i) \cdot (\text{velocity at point } i)
\]

where (i) refers to a specific point along the abscissa.
The effects of force generated by the resistance to flow within the cavity have not been included in this analysis. Consequently, spike forces computed by this method will be an upper bound or conservative value.

As can be seen in the model, the force needed to bring the plunger down to a complete stop equals its rate of change of momentum. The pressure spike is then computed as this force divided by the cross sectional area of the plunger. Figure 3.16 shows the approximated values of velocity and cavity pressure as a function of time. These plots were based on Figure 3.14 and were developed using Equations 3.10 and 3.11. In this particular case there is not a clear spike. Instead, the plunger seems to come to rest rather smoothly in a very short period of time after a series of small spikes. It is known that plungers vibrate as cavities fill. The smaller spikes are an indication of these vibrations, and may be caused by sudden increases in back pressure upon the plunger tip as the molten metal flows through the gate. In the particular case presented in Figures 3.14 and 3.16, the magnitude of these spikes may be accentuated by the fact that line segments were used to fit plunger velocity data, and consequently there is no continuity in the slope between adjacent line segments (i.e. there is no continuity in the acceleration).

From the perspective of the definition of a load in a finite element model, the questions that needs to be answered is which load is larger:
Figure 3.16. Traces of plunger velocity, force on tip of plunger and estimated cavity pressure vs time. True cavity pressure would be represented only by the last segment. Force and pressure are computed by applying a scale factor to the acceleration curve (not shown).
injection pressure _plus_ the pressure surge within the cavity at the end of fill or intensification pressure? (As has been explained before, other studies have shown that pressure surges in the hydraulic system of the machine do not affect the cavity pressure). In the majority of the traces that have been observed, plungers tend to decelerate rather smoothly. Under these conditions, intensification pressures that are at least 35% higher than injection pressures will cause the largest deflections.

Abrupt changes in the slope of the plunger velocity trace indicate the presence of discontinuities in the acceleration profile. These changes may indicate the presence of large pressure spikes. It would be very expensive to try to eliminate the effects of these spikes by increasing the size of the die. As a consequence, pressure spikes must be dealt with in a different manner (such as with the use of plunger deceleration devices).

Clearly, it would be desirable to have a tool that can, analytically and a priori, estimate the magnitude of the pressure spike at the end of fill. At first look, Sach's approach seems to provide an answer. In reality, values of resistance coefficients that characterize the behavior of the machine hydraulics would have to be determined experimentally. Furthermore, resistance coefficients for the cavity geometry would have to be developed.

Given that collecting velocity profiles is a rather straightforward activity, the use of velocity traces for the purpose of estimating dynamic loads
upon the cavity is useful for diagnosis or troubleshooting. On the other hand, the application of this approach for design purposes requires a knowledge of the nature of this trace before the die is built.

Because of the lack of data in the literature, a study would have to be conducted to first identify the factors that affect the velocity profile, and second to determine equations based on these factors for the prediction of velocity profiles in die that have not been built yet. More important than the velocity profile would be the estimation of the maximum plunger deceleration as the cavity fills up, since this parameter will determine the maximum in pressure cavity at the end of fill.

3.4 Dynamic response of die and platen

The importance of the pressure spike depends upon the magnitude of the deflection it causes. With the objective of describing how a typical system die / platen responds to a load of characteristics that are similar to what would expected from the pressure surge at the end of fill, two independent structures were simulated.

Figure 3.17 illustrates the geometry and dimensions for each model. IDEAS was used to model the response of the structures to a pressure spike, using the Lanczos method to extract the natural frequencies. Figures 3.18 and 3.19 present the three lower natural frequencies for each one of them.
Figure 3.17. Ejector and cover die/platen arrangement used for dynamic analysis.

\[ \Sigma F = P_c \times A_c \]

- \( P_c \): Cavity pressure = 70 E06 Pa (~ 10,000 psi)
- \( A_c \): Cavity area = 0.2032 \times 0.23876 m^2 (8\" x 9.4\")
Figure 3.18. Lower natural frequencies and mode shapes, cover side. a) 537 Hz. b) 640 Hz. c) 647 Hz.
Figure 3.18. (Continued).
Figure 3.19. Lower natural frequencies and mode shapes, ejector side. a) 563 Hz. b) 758 Hz. c) 851 Hz.
Figure 3.19. (Continued).
Figures 3.20 and 3.21 present a description of the excitation and the response of the structure recorded in 3 points. The excitation is basically a force that is applied during \( \tau \) seconds, and requires \( \tau/2 \) seconds to reach its peak value. For each structure, \( \tau \) and the damping coefficient were varied, while the peak force on the structure was kept constant for all cases. Peak deflection was recorded for each case. \( \tau \) for Figures 3.20 and 3.21 is 0.001 sec. These figures also show the response under a static load (that is, a load that is applied very slowly) of the same peak magnitude. The response to the static load can also be interpreted as the steady state response of the structure.

The results of this analysis are summarized in Figures 3.22 and 3.23. The plots show the ratio of the peak magnitude of deflection under dynamic conditions to the deflection under static load as a function of the spike duration. In essence, these plots show how dynamic a certain excitation is. For example, Figure 3.22 shows that for a spike that lasts 0.001 seconds and assuming the structure is completely undamped, the deflection caused by this dynamic load is 40% higher (\( \delta_{\text{max}}/\delta_{\text{sta}} = 1.4 \)) than the deflection that the same load would produce if it were applied very slowly.

In general, simulation results show that the die / platen system responds to the load in a quasi-static manner when the time to peak value is approximately 0.005 sec. or longer (note that the time to peak under our simulation conditions is half the spike duration). It is thus clear that typical
Figure 3.20. Cover side: excitation and response under two different conditions.
Figure 3.21. Ejector side: excitation and response under two different conditions.
Figure 3.22. Shock response curve for cover side.

Figure 3.23. Shock response curve for ejector side.
intensification pressures will not result in significant dynamic effects (intensification pressure rise takes more than 50 milliseconds). For lightly damped structures (damping factors less than 0.5 of critical damping), pressure may have important effects when the time to peak lies between 0.4 and 2.5 milliseconds. Loads associated with a pressure spike at the end of fill are more likely to lie in this region, and therefore would produce important dynamic effects. Clearly, a structure that is capable of responding to such short burst of energy would also be able to recover very rapidly once the load is removed. From the charts spikes that last between 0.008 and 0.005 seconds would have potentially the most important dynamic effects (i.e. flashing may be worse under such conditions).

In their current state, the shock response curves introduced in this section are not suitable for use as design tools mainly because of the lack of experimental data to validate them. Nevertheless, there is evidence that these curves do meet their goal, which is to provide a first order approximation to estimate dynamic effects of pressure spikes in die casting. From an experimental work whose goal was to measure adverse effects associated with the pressure surge, Everwin [63] reported pressure spikes that took about 0.0015 seconds to reach their peak values. The in-cavity pressure spikes that he reports are very similar in shape to the pressure spikes used in this section. This would translate into a spike whose duration is
approximately 0.003 seconds. The shock response curves report a (conservative - no damping) overshoot anywhere between 20 and 35% for this spike duration. This would be consistent with what is commonly observed in industry, which is illustrated by the fact that die casting machine builders [66] usually recommend that clamping forces be 20% higher than a normal analysis based on injection pressures alone would report.

3.5 Characterization of the loads in a die casting operation

The conclusions reached in each section of this chapter can be summarized to provide a picture of the characteristics of the loads and die behavior in a die casting operation:

- Die casting is characterized by an extremely violent filling process.

- From the perspective of their effects upon the magnitude of die deflections, momentum and hydrodynamic loads are negligible under typical casting conditions. Only poorly designed structures or uncharacteristic filling conditions would make these loads important.

- Injection pressure may induce a dynamic response in the die structure only under very specific circumstances. For example, a typical die / 800 ton machine arrangement will likely suffer dynamic effects when pressure build up within the cavity takes less

117
than 0.005 sec. Pressure spikes within the cavity that fall in this category will likely result in an overshoot from steady state response for the same magnitude of loads.

- Typical patterns of intensification pressure build up clearly result in quasi-static loads. In the majority of cases, maximum deflections are reached with intensification.

Compared to instant fill conditions, heat released during fill may produce variations in the thermal fields of the casting and die that are not necessarily insignificant. However, its contributions are probably not more significant than other sources of noise (for example, manufacturing and assembly tolerances).

The analysis presented in this chapter reduces to the following recommendations:

- Intensification pressure and heat released from solidification have to be accounted for.
- The effects of heat released during fill can be ignored.
- Dynamic effects have to be addressed in a different manner than by increasing the stiffness of the die.

It is important to emphasize that the objective of this chapter is to provide an idea of the scale of the effects of each load to a first order of magnitude. In other words, statements about the relative importance of the
different loads are based on the relative size of the deflection they produce. As an example, one can say with some degree of confidence that intensification pressure is more important than momentum because our results show that, in general, deflections due to pressure are at least an order of magnitude larger. While all the experimental data to validate every single statement made here is not available, what this analysis predicts seems to correlate well with what is commonly observed in the field.
Chapter 4. Modeling Die Casting Die Deflections

4.1 Introduction

In this chapter, the steps that are followed to simulate die casting die deflections are introduced. These steps will be explained through a specific example: a die used to produce a flat plate casting. Concepts that were introduced and developed in the previous chapter regarding the characteristics of loads upon dies will be used for the definition of boundary conditions. The objectives of this chapter are:

- to present a methodology for simulating die casting die deflections, that can be used for the purposes of design or diagnosis
- to describe the issues that need to be addressed by a reliable model
- to present alternatives to address these issues.

Based on the analysis of the results of the exercise in question, i.e. the flat plate die, and with the help of field data, the behavior of a die casting die will be characterized. As will be shown, this characterization is consistent with what is commonly observed in the field, which gives a certain degree of
confidence in the ability of the modeling procedure to simulate die deflections accurately. The procedure will be tested further by the analysis of slide blowback, which is introduced in the next chapter.

4.2 Steps in simulation

Figure 4.1 shows a flowchart for a typical simulation session. As the chart shows, preparation of a model involves the definition of geometry, mesh, boundary conditions, load history and simulation control parameters. The centerpiece of this chart are the numerical computations that result in the description of the state of stress/strain of the problem at hand. The numerical methods that are commonly applied to the analysis of uncoupled theromelasticity problems (Finite Element, Boundary Element) are relatively reliable tools that have been tested in other fields of study. It is the other steps in the chart that still present challenges for the application of such numerical methods for the analysis and simulation of die casting die deflections. In particular, interpretation of results and subsequent design modifications are currently beyond the expertise of the industry as a whole.

Throughout this research, ABAQUS (from Hibbit, Karlsson & Sorensen, Inc.), a commercial finite element analysis package, was used to carry out numerical computations. Most models were prepared in IDEAS (SDRC, Milstead, Ohio). In spite of the flexibility and power of these two
Figure 4.1. Flowchart for a typical simulation session.
systems, the quality of the simulation depends totally on the decisions made at the moment the model is prepared for simulation. In the case of die casting, there is a lack of understanding about the characteristics of this manufacturing process for an accurate model to be prepared (a basic premise of this work).

In the next sections, the manner in which the issues that arise in preparing a model for simulation and the manner in which they can be addressed will be illustrated through a specific example. The bulk of the work that follows was conducted with funding and supervision from the North American Die Casting Association's Computer Modeling Task Group.

4.3 A specific example: geometry and casting conditions

Figure 4.2 presents the geometry of a die and flat plate casting, along with their general dimensions. This geometry is a scaled up version of an experimental die available at the Ohio State University. The dimensions of the original die were modified based on the recommendations made by the monitoring committee, because the original dimensions would not be representative of a die that fits in an 800 ton (English) die casting machine.

This is the predominant size of die casting machine in the North American market. A die of the size presented in the figure would fit in a machine of such characteristics. Two versions of this die were simulated. In one of these, the insert on the ejector die was modeled explicitly. In the other
Figure 4.2. Schematic of the geometry used for simulations. Dimensions are given in inches.
case, the ejector die was assumed to be a monolithic entity, that is, it was assumed that the cavity was machined directly on the ejector base and therefore there was no need for an insert.

Tables 4.1 and 4.2 illustrate the process conditions and characteristics of the casting cycle that were used in this exercise, respectively. Heat transfer coefficients were collected from the literature [23, 24, 26, 56].

A simulation consists of two steps. In the first step, a heat transfer analysis is conducted. In this step, only heat transfer boundary conditions and loads are included in the model. In a second step, temperature fields from the previous analysis are added to a stress analysis model, in which all mechanical loads and corresponding boundary conditions are included.

4.4 Model geometry and its effect on mesh size

Preparation of an FEM model starts with the creation of model geometry. As is the case with many CAD/CAE systems, IDEAS has the capability of semi-automatically generating a mesh of finite elements out of a volume. Nevertheless, complex geometry may cause meshing problems, such as unacceptable degrees of distortion in the finite elements, or extremely large mesh sizes. Accuracy of results is greatly affected by these two factors.

When compared to other manufacturing process, such as forging or sheet forming, the most important characteristic of die casting is perhaps the intricate shapes of the parts it can produce. Because of the need to facilitate
Table 4.1: Assumed casting conditions

<table>
<thead>
<tr>
<th></th>
<th>Final Simulations (Full Die)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Casting Material</td>
<td>Al380</td>
</tr>
<tr>
<td>Melt Temperature</td>
<td>600 °C</td>
</tr>
<tr>
<td>Maximum Cavity Pressure</td>
<td>70 MPa (10,000 psi)</td>
</tr>
<tr>
<td>Cycle Time Analyzed</td>
<td>36 sec</td>
</tr>
<tr>
<td>Clamping Force</td>
<td>7250 KN (815 tons)</td>
</tr>
<tr>
<td>Ambient temperature</td>
<td>20 °C</td>
</tr>
<tr>
<td>Heat transfer coefficient steel-air</td>
<td>20 J/°K</td>
</tr>
<tr>
<td>Heat transfer coefficient steel-steel</td>
<td>2000 J/°K</td>
</tr>
<tr>
<td>Heat transfer coefficient steel-cooling line water</td>
<td>10000 J/°K</td>
</tr>
<tr>
<td>Temperature of platens</td>
<td>80 °C</td>
</tr>
<tr>
<td>Water temperature</td>
<td>40 °C</td>
</tr>
</tbody>
</table>

Table 4.2: Assumed steps in a single casting cycle

<table>
<thead>
<tr>
<th>Time (sec)</th>
<th>Step</th>
</tr>
</thead>
<tbody>
<tr>
<td>0-1</td>
<td>dies are clamped</td>
</tr>
<tr>
<td>1-17</td>
<td>heat and pressure applied to cavity</td>
</tr>
<tr>
<td>17-32</td>
<td>die opens, part is knocked out</td>
</tr>
<tr>
<td>32-35</td>
<td>lubricant spray</td>
</tr>
<tr>
<td>35-36</td>
<td>dies clamped</td>
</tr>
</tbody>
</table>
metal flow during injection and to reduce large stress concentrations in die cavities, most edges in castings are rounded. It may be seen in Figure 4.2 that the sizes of the smallest features of the part (rounded edges in overflows) are in the order of millimeters (0.040"). On the other hand, largest features in the die (see long outside edges) may exceed a meter in length. The wide range of size scales puts a severe strain in the ability of the CAD system to generate a mesh that is fine enough to produce accurate results economically.

The most important mesh control parameter is the characteristic length of the element. In general, fine meshes (i.e. small elements) can reproduce part geometry better and are less distorted than coarse meshes. Consequently, simulation results are more accurate. On the other hand, fine meshes require more computational power. Therefore, a compromise must be made between accuracy of simulation results and the cost of generating them.

A technique to reduce meshing problems involves the elimination of small rounded edges in the model of the casting. In the case of the flat plate die, elimination of these features resulted in a geometry whose smallest edges were approximately 0.01 m long. Consequently, the range of feature sizes was reduced by about an order of magnitude. Figure 4.3 (a) and (b) show the original casting and the modified version that was used for simulation purposes, while Figure 4.3 (c) illustrates the model for the die. Because of the
Figure 4.3. Wireframes of a) actual casting, b) modified geometry and c) die used in FEM work.
symmetry of the problem, only half of the geometry was actually modeled. The size of the mesh needed to reproduce this geometry was about 1,800 parabolic elements (tetrahedrons), versus more than 30,000 elements that would have been needed to model the original geometry. The final mesh was made up of about 3000 parabolic elements (i.e. sensitivity of results to an increase in the number of elements was negligible when more than 3000 elements were used).

It is important to understand the effect that the elimination of rounded edges can have upon simulation results. By replacing smooth features with sharp edges, maximum stresses will be overestimated in regions close to these geometric features. Deflection results, on the other hand, are less sensitive to slight modifications in the geometry. While strains may be overestimated, displacements will not be greatly affected as long as these strains remain localized.

When more complex geometry is being studied, and if results are sought at locations far enough from the cavity, complete features may be ignored. This technique will be illustrated by the exercise in slide blowback presented in the following chapter.

Another issue that affects the characteristics of the mesh is the type of finite element used. In the case of IDEAS, only tetrahedrons can be used to mesh free-form geometry. For a mesh of the same size, elements that use
parabolic interpolation will match curved geometry more closely than linear type elements. Results will also be more accurate. In this particular case, a mesh of about 15,000 linear elements was needed to reproduce approximately the same results, although solutions were obtained in slightly less time. In the case of ABAQUS, parabolic elements often produced spurious results (i.e. oscillations) that could be controlled only after extensive manipulation of the mesh and simulation control parameters. Frequently, these problems could not be eliminated when multiple cycles were simulated. Eventually, all simulation work was done with linear elements.

It should be pointed out that creating the geometric model and mesh is generally a time consuming procedure. If the goal is to have a tool that can be used at the design stage, simulation / analysis systems whose architecture is not open to accept geometry from CAD systems are seriously handicapped.

4.5 Load history and boundary conditions

As already explained, die casting dies are subject to several different types of loads: momentum from flowing metal, heat from solidification, clamping force, cavity pressure and dynamic loads due to sudden stoppage of moving masses. In the previous chapter it was concluded that momentum and heat released during fill will not result in significant deflections and consequently their effects can be ignored. On the other hand, heat from solidification, clamping force and cavity pressure need to be accounted for.
Clearly, the first question to address is the extent of the model, that is, where should the subject of analysis end? At this location, the rest of the world needs to be modeled. While seemingly trivial, the quality of the results will be greatly affected by the choices made at this point. To a degree, the answer to this question depends upon how much is known about the environment around the subject.

For example, assuming that the final objectives of the deflection analysis of the flat plate die are to predict the casting dimensions and to predict the likelihood of operational problems in the die, such as flashing, one possibility could be to constrain the finite element analysis to the insert alone. The advantage of this approach would be that the model would be small, which reduces meshing problems and allows the analysis of fine details. However, this model requires that the interaction of the insert with the die base be modeled. In other words, the rest of the die needs to be modeled from the perspective of the insert.

Figures 4.4 (b) and (c) intend to illustrate the uncertainty about the boundary conditions when modeling the insert alone. For example, a force distribution in the back of the insert would have to be defined. This distribution would very likely be affected by the loads upon the cavity, since a load on the cavity will necessarily result in a deflection of the die base, which will affect the contact area between the insert and the base.
Figure 4.4. Issues when modeling isolated inserts. (a) Insert mounted on die. (b) Schematic of mechanical boundary conditions. (c) Schematic of heat transfer boundary conditions.
A heat transfer analysis would present a similar problem. In the case of an isolated insert, its interaction with its environment would have to be represented by a convection boundary condition. However, because it is likely that the rest of the die will change its temperature as the casting cycle progresses, the temperature that represents the die would be an unknown function in time.

In the balance, the benefits that isolating the insert brings (better resolution and simplicity in the construction of the geometric model) are probably overwhelmed by the inaccuracies that would be caused by the approximations done when accounting for the rest of the world. In the flat plate exercise, it was decided to model the full die, because it was felt that a model of the die casting machine would be less sensitive to inaccuracies in our assumptions.

In the sections that follow, other issues regarding loads and boundary conditions as well as some possible approaches to address them will be introduced.

4.5.1 Heat released during solidification

The stiffness of the die is affected by the thermal fields in two ways:

- material properties such as Young's Modulus and Yield Strength depend upon temperature.
• by modifying die dimensions to the point that stress patterns at lock up will be significantly affected. This is particularly important in the case of stresses at the parting plane, close to the cavity.

There are three possible ways to compute temperature fields in die casting dies. The obvious approach would be to run a solidification simulation with a specialized system (i.e. MAGMAsoph⁴, ProCast⁵). Because most of these systems are not designed to address die stress analysis, temperature data must be exported from this system into a model for stress analysis based on another system (such as ABAQUS). There are several problems with this approach:

• Most commercial solidification systems are not designed to address conditions encountered in high pressure die casting.

• In most cases, heat transferred to the die is only a byproduct of a solidification analysis. As a result, other phenomena are being computed simultaneously (microstructure, chemical composition gradients, etc.). Consequently, a simulation in which the only goal is to determine die stress / strain fields would be extremely expensive in terms of time and computational power needed to obtain results.

⁴ MAGMA Foundry Technologies, Inc. Arlington Heights, Ill
The need to export data between two different systems requires that the architectures of both systems be open and compatible, or that an external tool that provides a suitable interface be available.

Typically, this approach is too expensive to be used as an every day design tool.

An alternate approach would be to use a general purpose code to generate the thermal fields. ABAQUS has the capability of performing transient heat transfer analysis in which one component solidifies. The manner in which ABAQUS conducts this type of analysis was illustrated in the previous chapter when the effects of heat released during fill were studied. In this type of analysis, ABAQUS will basically account for the release of latent heat. No effort is made to predict solidification fronts, microstructures or other mechanisms associated with the solidification process.

The use of this option has several drawbacks. First of all, general purpose codes do not have the database to support casting analysis. Consequently, material properties and parameter values for boundary conditions must be either collected from the literature or generated experimentally. Secondly, as the heat-released-during-fill exercise showed, this feature of ABAQUS is not designed for multiple-cycle simulations.

UES Inc. Dayton Ohio

5 UES Inc. Dayton Ohio

135
When working with ABAQUS, the user must transfer manually all nodal temperatures from the end of one cycle into the input file for subsequent cycles to be analyzed. This action is time consuming and subject to errors. Input files can become extremely large and difficult to handle. Furthermore, only one full cycle can be analyzed after a given simulation has been completed. This means that an input file must be prepared for each cycle. In this file, initial conditions are the temperatures from the last instant of the previous cycle. Finally, running this type of analysis in a system like ABAQUS requires that the cavity interior be meshed. Given that the scale of cavity thickness may be as small as a millimeter, even after modifications to the geometry are made, problems with meshing as described in the previous sections may arise again.

The simplest approach to calculate die temperatures consists of carrying out a heat transfer analysis for which the magnitude of the heat from solidification is prescribed. Numerous experimental studies [17, 44, 46, 56] have shown that heat released by the casting into the die proceeds approximately at an exponentially decaying rate, irrespective of location. In his work, Padyiar [57] took advantage of this behavior to model the thermal load upon the die. His basic assumption was that heat escaping from the part flows in a direction perpendicular to the cavity surface only, and that the part is very thin.
In this model, the constraint is that the total heat released by the part be governed by the following equation:

\[ H = C_l(T_m - T_l) + \text{Heat of Fusion} + C_s(T_s - T_e) \] \hspace{1cm} (4.1)

where:

- \( H \) = Total heat released by the part
- \( C_{ls} \) = Specific heat for liquid, solid casting material
- \( T_m \) = Melt temperature
- \( T_l \) = Liquidus temperature
- \( T_s \) = Solidus temperature
- \( T_e \) = Temperature at time of ejection

It is assumed that the rate of heat release may be approximated by an exponentially decaying function, where the initial rate is several times larger than the rate at the end of the thermal cycle. The shape of the heat release is given by:

\[ Q = K^*b^mt \] \hspace{1cm} (4.2)

where:

- \( Q \) = heat rate
- \( K \) = arbitrary constant
b = base, in our case set as 10
m = parameter that defines the rate of release
t = time of heat release

This distribution must meet the constraint (4.1), and therefore:

\[ H = \int_{t=to}^{t=ti} K b^m dt \]  

where:

- \( t_o \) = time when heat release starts
- \( t_i \) = time at which heat from solidification is stopped

As an initial approximation, one can assume:

\[ \frac{Q(t_o)}{Q(t_i)} = 100 \] 

Given a total heat release (H) for a given region, constants K and m in Equation (4.2) can be computed based on the constraints imposed by equations (4.3) and (4.4). In essence, the accuracy of the approximation depends upon the values used for K and m. The trick of course is being able to match the part temperature at ejection (Te) after a time (t_i - t_o).
This model may also be used to represent the heat extracted by the lubrication spray. It has been reported that, in general, the heat extracted by lubricant spraying ranges from 15 to 30% of the heat delivered by the solidification process. The previous equations (4.1 through 4.4) apply once again, with the following considerations:

- \( H_s = 0.15H \), \( H_s \) = Heat removed by spray
- \( t_o = \) time at which spray starts
- \( t_i = \) time at which spray is stopped

The major problem which prevents the application of this approach to castings in general is the fact that the values of constants \( K \) and \( m \) will definitely not be the same for all regions of the cavity. This is due mainly to the intricate shape of typical castings, which results in regions of varying volumes. Consequently regions such as runners and biscuits will necessarily have different heat release rates than the product.

4.5.2 Heat flux applied to flat plate die

From our perspective and with the tools available, there were two practical ways to define thermal field history the die. The first one involves running transient heat transfer analysis with ABAQUS. These results are then used in a stress analysis model. The problems with this approach have already been explained.
The second practical alternative involves running a simulation with MAGMA and obtaining temperature data at selected locations. Due to the closed architecture of MAGMA, thermal fields cannot be imported into a simulation with ABAQUS.

Based on the temperature results, the total amount of heat released by the part can be estimated, from which values can be assigned for K and m in different regions. These values are used in a heat transfer simulation with ABAQUS. Thermal patterns can then be compared with the results of MAGMA, and the values of K and m can be modified if significant differences exist. This process is repeated until temperature fields in both simulations are similar. Figure 4.5 summarizes this procedure.

At the time this exercise was conducted, it was felt that the second alternative would result in less computational expense and time, and therefore this was the selected option. In particular, transferring nodal temperatures between different input files as would be required by the first alternative seemed a formidable obstacle.

In all, only a couple of iterations (i.e. ABAQUS simulations) were needed to compute the values of K and m that resulted in thermal fields that matched approximately those reported by MAGMA. Table 4.3 presents the values used for the different regions which are shown in Figure 4.6. With these values, thermal patterns within the die structure were found to differ by
Figure 4.5. Flowchart to determine constants $K, m$. 

141
Figure 4.6. Regions into which part was divided for the purpose of modeling the heat released during solidification. Results from a simulation with MAGMA.
Table 4.3. Thermal zones and respective K and m values

<table>
<thead>
<tr>
<th>Thermal zone</th>
<th>During Solidification</th>
<th>Coolant Spray</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>K (W/m²)</td>
<td>m (s⁻¹)</td>
</tr>
<tr>
<td>Overflow</td>
<td>2.019 E+06</td>
<td>-2/15</td>
</tr>
<tr>
<td>Fan</td>
<td>2.538 E+06</td>
<td>&quot;</td>
</tr>
<tr>
<td>Plate</td>
<td>2.168 E+06</td>
<td>&quot;</td>
</tr>
<tr>
<td>Runner</td>
<td>3.225 E+06</td>
<td>&quot;</td>
</tr>
<tr>
<td>Biscuit</td>
<td>3.332 E+06</td>
<td>&quot;</td>
</tr>
</tbody>
</table>

less than 70 °C in the cavity surface, and less than 30 °C in the bulk of the die, or about 10% of the temperatures reported by MAGMA at the given locations.

Contrary to what is done in the case of the heat given up by the casting, the heat removed from the cavity surface during lubricant spraying was modeled as a negative heat flux and applied on the free element faces in the finite element model. In our flat plate exercise, a value of 15% was used. In this exercise it was assumed that lubricant was applied for 3 seconds (i.e., ti - to = 3).

4.5.3. Other heat transfer boundary conditions

The heat transfer model for the flat plate die was completed by adding parameters to account for convection / conduction between the different interfaces. It was assumed that convective heat transfer takes place at the cooling channels and the exterior of the die during the die holding time,
lubricant spraying time and die open time. It is important to note that when
the die is closed, the parting plane surfaces are in contact and their interface is
represented by a finite conductance in the heat transfer model. When the die
opens up, these two surfaces are exposed to the ambient, and therefore a
convection heat transfer boundary condition is added.

Table 4.2 presents the values used for the heat transfer coefficients, as
suggested by Barone [23], and the assumed boundary temperatures.

4.5.4. Cavity pressure and clamping force

Earlier in this chapter it was stated that momentum and hydrodynamic
pressure distributions are not significant mechanical loads. Cavity pressure
and clamping force are then the only other sources of mechanical loading.
Given the speed with which these loads are applied, intensification pressures
and clamping forces can be regarded as quasi-static loads.

As a first approximation, clamping forces can be assumed to be
distributed evenly at the back of the dies, in the form of a uniform pressure.
Clearly, the accuracy of this assumption depends on factors such as the
geometry and deflection of the ejector platen.

Cavity pressure as a load exists only for as long as liquid metal within
the cavity communicates with the plunger tip. As discussed in the previous
chapter, several mechanisms result in cavity pressure. Towards the end of the

144
injection cycle, pressure within the cavity builds up very rapidly. This buildup is the result of two components:

- hydraulic pressure build up behind the plunger as it slows down
- forces at the plunger tip (i.e. on the cavity side) needed to dissipate the kinetic energy (if no plunger deceleration control is used)

When defining the magnitude of the cavity pressure, the question that needs to be answered is which pressure is higher? The injection pressure plus whatever pressure spike occurs at the end of fill, or the intensification pressure?

It has already been explained that the pressure spike in the hydraulic system, which is caused by the sudden stoppage of the hydraulic fluid masses, will probably not cause a raise in cavity pressure of sufficient magnitude and duration to cause significant deflections. Assuming that all moving masses come to rest due only to the force exerted by the molten metal upon the plunger, the magnitude of the cavity pressure will be greatly affected by the nature of the velocity history of the plunger. Sudden changes in the slope of the velocity profile, or seen from a different perspective, discontinuities in the acceleration trace, might indicate a significant pressure spike in the cavity of the die. Pressure spikes are likely more significant in smaller dies, since small parts require faster fill velocities. Based on the same premise, it may be expected that larger dies will be less susceptible to pressure spikes.
Because the amount of molten metal is limited and given that the cavity expands under the influence of the pressure surge, the duration of the in-cavity pressure spike will be very limited. Indeed, the duration of the pressure spikes reported in the literature are within the range where significant dynamic effects may be expected. Under favorable conditions for its occurrence, the magnitude of a pressure spike would be too large to be controlled by making the die stiffer (i.e. increasing the dimensions of the die). For this reason, only flashing associated with intensification pressures could be eliminated by modifying the design of the die.

In general, the analysis should use intensification pressure as the maximum load that will be applied within the cavity. The reason for this is that under most circumstances the maximum load that the die will see is precisely this. Even in the presence of short lived pressure spikes, their magnitudes are so large that dies cannot be designed to remain unchanged by these loads (although the clamping should be able to keep the dies from opening completely). Pressure spikes should be dealt with in a different manner. From the point of view of the design of the die, one of the few alternatives would be to make modifications to the runner system to produce an increased back pressure as the cavity fills up, thus producing a favorable deceleration trace. Machine builders already provide their machines with plunger deceleration devices.
In this particular case, it was assumed that the maximum cavity pressure (70 MPa), regardless of its source, would be reached while metal within the cavity was still in the liquid state. Therefore, a hydrostatic pressure distribution was assumed. Given that there is no information as to how the cavity pressure changes as the part solidifies, it was assumed that the maximum pressure would hold for as long as the part remained in the cavity (16 seconds). In reality, for the cavity pressure to have this behavior it would be required that the part remained liquid throughout this time, which is clearly not the case (that is, this reasoning would be contrary to the nature and objective of the die casting process). Therefore, deflection results should be accurate for the first few instants following part injection. After this period, the effects of cavity pressure will be overestimated by the model.

4.5.5. Other boundary conditions in the stress analysis model

Die casting dies are not monolithic entities. To the contrary, they are complex assemblies whose individual components are tightly fit. The most obvious boundary is the parting plane. Die builders make a great effort in making sure that there is an accurate match between the two die halves. As a general rule, parting and shutoff surfaces are ground to very tight tolerances.

ABAQUS is an example of an FEM code that is capable of simulating interfaces among die elements with the use of "gap" and "interface" elements. This type of finite element is capable of representing different types
of surface interactions such as solid contact and friction. In the latest versions of ABAQUS, three dimensional surfaces can be defined as contact surfaces without the need for creating gap or interface elements.

The presence of joints in the die assembly presents other modeling problems. For example, when compared to a homogeneous structure, a bolted joint produces a variation in the resistance to flow in the path of heat. Also, the transmission of mechanical loads is clearly a function of the type of joint in question. In the case of the die, a large number of bolted joints are found. It is very difficult to economically model each joint explicitly in the type of simulation we are running.

Before the final model of the flat plate die was developed, the effects of the joints were tested. The results of these tests were reported by Hegde [67]. This work showed that the deflections of the cavity surface were fairly insensitive to the presence (or absence) of bolts at the ejector shoe/support block interface. Clearly, stress / strain fields in the immediate vicinity of bolted are indeed affected. Due to their proximity to the region being analyzed (i.e. the cavity), bolts that hold the insert within the pocket of the ejector holder block were specifically modeled. Obviously, this was done only in the case in which a die with an insert was modeled.

Figure 4.7 presents free body diagrams of the environment around the die with the components that affect die deflections. To constrain the model
Figure 4.7. (a) Schematic arrangement of die casting machine. (b) Relevant free body diagrams.
fully, the cover die was assumed to be clamped to the platen, which was assumed to be a rigid foundation. In reality, this boundary condition may be too stiff, given that the cover platen will exhibit some flexibility. This boundary condition will be addressed again in a future article within this chapter.

Figure 4.8 illustrates schematically the characteristics of the models developed for heat transfer and stress analysis of the flat plate die. In these sections that follow, simulation results will be discussed with the goal of characterizing the behavior of the die.

4.6 Failure modes and resolution of results sought

Die casting dies can fail due to several different mechanisms, such as thermal fatigue or wear. However, from the perspective of the structural design of the die, elastic deflections can be of sufficient magnitude that the performance of the die can be seriously handicapped. For example, jamming of a mechanism prevents the casting operation to continue. Jamming is usually caused by parts that stick to the die or large deflections of moving components. Consequently, jamming usually involves large deflections.

Flashing is another type of failure. The presence of flashing requires the removal of excess material from the part. If overlooked, the build up of flashing can result in indentation of the shutoff surface in the casting. Flashing can be caused by gaps as little as 0.004"-0.007" [68], if the area that
Figure 4.8. Characteristics of models. (a) Stress analysis. (b) Heat transfer.
separates is large enough. It is with this resolution that gaps in the die structure should be defined.

As the results of the next chapter will show and based on our experience, a model based on the recommendations made in this chapter can predict gaps within the structure with a degree of resolution of about 0.004"-0.005". In other words, gaps of a smaller magnitude may not be accurately predicted with the approximations that are recommended here. This range is of the same order of magnitude of die manufacture/assembly tolerances. In an open-close die, parting plane separation is perhaps the only type of gap that will be found in the structure of the die, and therefore this factor was selected for analysis.

It is important to emphasize that only the likelihood of flashing can be predicted. Flashing is produced by a combination of factors, including the size of the gap, as well as the fluidity and local pressure. It is for this reason that the gap that results in flashing is given in such a wide range (0.004"-0.007").

4.7 Simulation results

Table 4.4 summarizes the cases that were simulated in this study. The reader should refer to [69] for a full description of the results and raw data of this study. In this table, \( w \) refers to a die with an insert and \( wo \) refers to a die without an insert.
Table 4.4: Process conditions for the different simulation trials

<table>
<thead>
<tr>
<th>Load conditions</th>
<th>Ejector Die thickness (t)</th>
<th>Dimensions in inches</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>4.875</td>
<td>5.875</td>
</tr>
<tr>
<td>70 MPa (10,000 psi)</td>
<td>w/wo</td>
<td>w/wo</td>
</tr>
<tr>
<td>70 MPa (10,000 psi) +</td>
<td>w/wo</td>
<td>w/wo</td>
</tr>
<tr>
<td>solidification heat</td>
<td>w/wo</td>
<td>w/wo</td>
</tr>
</tbody>
</table>

4.7.1 The first cycle

Deflection results have the characteristic that, under certain conditions, they can be observed in the field without the need for sophisticated instrumentation. Still, not all deflections are relevant to the performance of the die. In the case of the flat plate die, deflections at the parting plane were chosen for analysis. More specifically, the parting plane separation at the edge of the cavity was observed for different cases. Separations at such locations may result in flashing, a condition in which metal spills out of the cavity. This excess material must be removed from either the part, the die, or both. Removing thick flashing from the casting may require a special trimming die during the casting operation. Flashing in the die must be removed manually by the operator, which results in delays in the casting operation.

Figure 4.9 shows a comparison of the maximum parting plane separation as a function of ejector holder thickness. As can be observed, the
Case = Ejector Half Die
Ejector Half Die Dimensions
Height = 35 $^{1/2}$ in
Width = 23 $^{3/4}$ in
Thickness (t) = variable
Cavity Pressure = 10,000 psi
First casting cycle

Maximum parting plane separation vs. ejector die thickness

Figure 4.9. As the plate thickness increases, the maximum parting plane separation decreases.
maximum separation between die halves at the parting surface decreases as the thickness of the ejector holder shoe is increased. Also, the parting plane separations of the die with insert are larger than the corresponding deflections of the monolithic die. This separation occurs between the overflows of the casting. Simulation results show that, typically, the maximum separation starts at the instant the cavity pressure is applied. Figure 4.10 illustrates this phenomena. After a short time, the heat load takes effect and causes the gap to collapse.

Figure 4.11 shows the variability of the cavity dimension. Before pressure is applied, the die has grown almost uniformly. This fact is the result of assuming that the casting cycle start after the die has been heated up uniformly to 100 °C. The cavity space, measured in the direction of the thickness of the die half, has been reduced only slightly. As the pressure is applied, the size of the cavity is increased initially and, as the heat takes effect, the space is once again reduced. This recovery in the dimension can occur only if the part within the cavity can deflect under the pressure produced by this recovery.

Figure 4.12 illustrates the magnitude of the overall die growth after one cycle. This particular plot presents deflection data at a point on the back of the ejector holder. While the overall growth of the die is clearly dominated by the thermally induced strains, the magnitude of the instantaneous peak
Case = Ejector Die Half (Monolithic)

Ejector Die Dimensions
Height = 35 1/2 in
Width = 23 3/4 in
Thickness (t) = 4 7/8 in
Cavity Pressure = 10,000 psi
First casting cycle

Approximate location of nodes

Figure 4.10. Chart showing the parting plane separation. The maximum occurs at the moment the pressure is applied.
Case = Ejector Die Half (Monolithic)

**Ejector Die Dimensions**
- Height = 35 $\frac{1}{2}$ in
- Width = 23 $\frac{3}{4}$ in
- Thickness (t) = 4 $\frac{7}{8}$ in

**Cavity Pressure** = 10,000 psi

First casting cycle

Approximate location of nodes

---

**Magnitude of node displacements at opposite sides of the cavity surface**

- 17.72E-03
- 15.75E-03
- 13.78E-03
- 11.81E-03
- 9.84E-03
- 7.87E-03

**Cavity pressure is applied**

**Pressure is removed**

---

Figure 4.11. Prior to the application of pressure, the cavity space measured across the thickness of the plate has shrunk slightly. As the pressure is applied the cavity space grows. Once the pressure is released, the space shrinks again.
**Case** = Ejector Die Half (Monolithic)

**Ejector Die Dimensions**
- Height = 35 \( \frac{1}{2} \) in
- Width = 23 \( \frac{3}{4} \) in
- Thickness (t) = 4 \( \frac{7}{8} \) in

**Cavity Pressure** = 10,000 psi

**First casting cycle**

Approximate location of node (Back of plate)

---

**Figure 4.12.** The overall growth of the die is illustrated by the displacement of a node located in the back of the die half.
deflection depends upon the pressure being applied. In general, maximum
deflections occur near the middle of the die, away from the pillar support.
Additional supports in the middle may reduce the magnitude of the
deflections.

4.7.2 Contributions by the different loads

From the perspective of the analysis of the behavior of the die, an
important outcome of this exercise is the ability to visualize the relative
importance of the different loads. Figure 4.13 shows the contributions of heat
and pressure to the deflections of two points of the die. The deflections of the
cavity can be used to explain the effects of each load.

Before pressure is applied, the cavity is displaced from its original
position by a significant amount due to the raise in temperature. The
magnitude of this displacement is not presented in the figure because the size
of the cavity remains almost unchanged. As pressure is applied within the
cavity, the dimension measured across the thickness of the cavity grows
instantaneously. This is illustrated in the figure by the fact that the pressure
induced deflection is larger than and opposite to the heat induced deflection.
After a few casting cycles, the temperature of the die increases, resulting in a
larger contribution of heat to the overall deflection.
**Case** = Ejector Die Half (Monolithic)

**Ejector Die Dimensions**
- Height = 35 1/2 in
- Width = 23 3/4 in
- Thickness (t) = 4 7/8 in
- Cavity Pressure = 10,000 psi

**Approximate locations**

### Contribution to maximum deflection by each type of load

<table>
<thead>
<tr>
<th>Deflection Magnitude (in)</th>
<th>1st cycle</th>
<th>4th cycle</th>
<th>1st cycle</th>
<th>4th cycle</th>
</tr>
</thead>
<tbody>
<tr>
<td>-15.75E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>-11.81E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>-7.87E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>-3.93E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>0.00E+00</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.93E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>7.87E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>11.81E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>15.75E-03</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

- **Deflection measured across cavity thickness**
- **Deflection measured at back of die**

Figure 4.13. Contribution to overall deflection by each type of load at selected locations.
A better picture of the contributions by each load is presented in Figure 4.14. This figure shows displacement fields at a plane that passes through the symmetry plane of the die.

Figure 4.14 (a) shows displacement fields after clamping force has been applied and prior to metal injection into the cavity. Maximum deflections are of the order of 0.00005 m (0.002") and occur at regions close to the pillar supports.

Figure 4.14 (b) presents displacement patterns due to the combination of clamping force and cavity pressure. No thermal effects have been included in the model in this case. Maximum deflections under these conditions are approximately 0.0003 m (0.012") and occur approximately in the middle of the cavity. A gap is created at the parting plane in the immediate region of the cavity. This gap is not seen in the proximity of the pillar supports.

Figure 4.14 (c) illustrates the effect of the heat load a few instants after heat is applied. In this case clamping force is applied, but no injection pressure has been introduced. The die tends to "bulge" in response to the stress caused by the temperature fields. If there were no mechanical stops behind the platen (i.e. if we were dealing with a purely hydraulic press), the non uniform growth of the die would actually force the platen to move back until the forces stresses by the clamping force balance themselves with the stresses generated by the heat load. This phenomena is shown by the fact that
Figure 4.14. Effects of different loads. (a) Clamping force. (b) Clamping force and cavity pressure. (c) Clamping force and heat from solidification. (d) All loads combined.
the maximum deflection (approximately 0.00035 m or 0.0015") is again found around the pillar support region, except that its direction occurs against the action of the clamping force. The gap at the parting plane is now in the periphery of the die, instead of in the middle of the cavity as in the cases in which only cavity pressure was applied.

Figure 4.14 (d) shows the combined effects of all loads: clamping force, injection pressure and heat from solidification. Maximum deflection occurs once again at the center of the die in a direction that tends to separate the die halves. The magnitude of this deflection is approximately 0.00051 m (0.020"). In this region, pressure (with 0.00026 m) and heat (with 0.00025 m) combine to produce the maximum deflection. The contribution of the clamping force is negligible in this region of the die. This figure also shows that a gap along the parting plane runs all the way from the cavity to the atmosphere. The likelihood of metal spitting out of the die is high in this region. Contrary to common belief, results indicate that heat and pressure do not necessarily offset each other’s effects.

4.8 Multiple cycles

The behavior of a die without insert during 4 cycles was simulated. In general, results show that the heat load tends to dominate deflections as the operation approaches quasi-steady state.
In this case, the heat load model was modified by reducing the amount of heat being released after each subsequent cycle. This is done to account for the fact that, as the temperature of the die increases, the heat that can be transferred from the part to the die is actually reduced for the same holding time. As a side note our experience indicates that the rate of heat release has a more important effect upon the magnitude of the instantaneous deflections than that magnitude of the total heat transferred (up to a certain point). Also, our models seemed to be fairly insensitive to the heat convection with the environment.

Figure 4.15 shows how the maximum parting plane separation keeps getting smaller as time progresses. In essence, the heat load begins to create a pre-stress at the parting plane as the die components try to grow due to the increased temperature. The location where the maximum deflection is found shifts from a region close to the overflows, to a point close to the middle of the cavity.

4.9 Characterizing the long term behavior of a die and correlation with field observations

The picture that the flat plate results present is consistent with what is commonly observed in the field. In general, it is known that early in the casting operation dies tend to move a lot and in patterns that might be unpredictable. After a few thermal cycles, deflections seem to converge to a
Figure 4.15. Parting plane separation at midpoint of plate. After the second cycle this point shows the maximum separation. For each cycle, only the deflection that occurs between the instants of metal injection and part ejection has been recorded. Estimated values were extrapolated based on the results of a single cycle.
certain pattern. It is for this reason that casters will bring the die up to operating conditions rather slowly, by heating up the dies with partial shots, heaters or hot oil and applying cavity pressures that are smaller than nominal values. Throughout the casting operation product dimensions will differ from their target values in a range that tends to be stable.

At the beginning of each casting cycle the stiffness of the die is defined by the design of the structure of the die, the temperature of the bulk, and the clamping force applied upon the die. Clearly, the fundamental design of the die is fixed for a given casting operation. Die temperature interacts with the die casting machine settings to define the clamping force. At some point during the casting operation, initial die temperatures for each casting cycle will be approximately the same between consecutive cycles. It is said that quasi steady state, or periodic conditions, have been reached at this point. A characteristic thermal distribution is reached within the structure of the die. This distribution combines with the fundamental design of the die and the properties of the die material to define the stiffness of the die, and therefore its susceptibility to deflect. This behavior is illustrated by the results shown in Figure 4.15. Keep in mind that this figure tracks the z displacement of the two nodes located at a similar position on either side of the parting plane (i.e. one node is on the cover side while the other is on the ejector side).
At the beginning of each cycle, both nodes are coincident (dies are closed). As pressure in the cavity is applied the nodes separate, indicating that the die halves move apart. Once heat from solidification takes effect the dies tend to grow. As a result both die halves try to come together again. This sequence repeats itself in each cycle.

There are two important facts that can be seen from Figure 4.15. The plot clearly shows how the starting position for each cycle keeps shifting. Similarly, the maximum separation between consecutive cycles keeps decreasing. The most plausible explanation for this behavior is that as the casting operation progresses, the temperature of the bulk of the die increases, resulting in variations to the size and material properties of the die. The stiffness of the die changes as a result of both, the change in material properties and the change in the stiffness of the die structure as fits between mating components change.

Field observations confirm this behavior. Figure 4.16 shows a schematic diagram of the placement of displacement probes on the structure of a die for a transmission casing. Figure 4.17 (a) shows how the maximum parting plane separation at an outside corner of the die changes slightly over a period of 40 cycles (which in the field were collected during a period that was considered to present quasi steady state conditions). Process conditions

---

Data courtesy of Matrix Technologies, Muncie In.

167
Figure 4.16. Schematic view of location of displacement sensors on structure of die for a transmission casing. Die has three slides that use a horn pin type lock. Reproduced with permission from Matrix Technologies, Inc.
Figure 4.17. (a) Behavior of parting plane separation as reported by Matrix.
(b) Process conditions (scaled for ease of display).
remain fairly constant during this period of the casting operation. In Figure 4.18 (c), the separation at an outside corner of the flat plate die presents the same characteristic behavior. This is an indication of how the stiffness of the die changes with time.

4.10 Relevance of simulation control parameters

The choice of simulation control parameters has a very important effect upon the quality of the results and the cost of the simulation exercise. The role that the simulation control parameters play is illustrated by the difference in results between a single cycle and a multiple cycle simulation.

When compared to a single cycle simulation, simulating multiple cycles requires the modification of certain simulation control parameters. For example, because more time is being simulated, the user is forced to decide the frequency with which data is to be collected and filed during the numerical computations. This has an important effect upon the values of the output data, because peak values may be missed by not storing data frequently enough. A conflicting constraint is presented by the fact that more frequent storage increases the size of the results files.

In the flat plate exercise, maximum deflections reported during the first cycle of the multiple cycle simulation differ from those reported by the single cycle simulation. This fact probably indicates that the time when the real maximum separation occurred was not sampled in the multiple cycle
simulation. This raises the issue that, as the time scale is increased, the resolution and accuracy of results is reduced. Figure 4.15 also presents an estimation of what the actual magnitude should be. The estimation is made based on the results observed in a single cycle simulation of the same geometry.

4.11 Sensitivity and accuracy of results

The accuracy of simulation results is affected by the magnitudes of loads, the choice of boundary conditions and the quality of the mesh. It is important to emphasize that given that the final goal of a simulation is to reduce operational problems and provide for dimensional control of the part, results need to be accurate to a scale that is significant for these purposes. In general, allowances in the 0.0001 m (0.005") range are considered very tight for die casting. The reason for this is that although individual components can be manufactured to tighter tolerances, assembly constraints make it difficult to keep tighter tolerances than 0.005".

Based on the approach described in this chapter, accuracy of simulation results will fall within this range. For example, experience with the flat plate die indicates that deflection results are fairly insensitive to variation in the cavity temperature up to about 70 °C. The effect of variations of up to 30 °C in
the bulk of the die has also negligible effects. This is rather fortunate, given
the inaccuracies inherent to the process of computing thermal fields.

Interestingly, significant variations to the geometry produce results
that fall also within this dimensional range, as illustrated by the
modifications to the thickness of the die presented in Figure 4.9. Although
results show that a variation of about 40% in the thickness brings a change of
about 33% in the maximum parting plane separation, in absolute terms the
difference is about 0.00006 m (0.002"), well within the range of what could be
considered accurate in die casting.

As a further indication of the fact that the simulation procedure seems
to correlate well with field observations one can look at the practice of
manufacturing proud inserts. By allowing the surface of the insert to emerge
from the surface of the pocket in the die by a few thousandths of an inch,
surfaces in the immediate vicinity of the cavity will come in contact before
the rest of the parting surfaces touch. This ensures that the periphery of the
cavity is sealed during operation, thus preventing material from escaping the
cavity.

For the die geometry of the flat plate die, the common practice in
industry is to allow inserts to protrude between 0.003" and 0.005" from the
parting plane surface on both die halves. Experience indicates that, under
normal loading conditions, flashing is prevented when inserts are designed
in this manner. Referring to Figure 4.9, it can be seen that the maximum separation reported by the simulation is 0.0071", which falls around the middle of this range.

Finally, a clear source of inaccuracy in the model for the flat plate die is the manner in which the support from the die casting machine is modeled. As has been mentioned, by fully clamping one side (cover die) and applying a uniform pressure on the other die half (ejector), the elasticity of the support that the machine provides is totally ignored. A different model that accounts for this factor may be built using spring elements to model the platen support behind the dies. This model is schematically illustrated in Figure 4.18 (b). The key issue is to determine the stiffness of the springs. The equivalent stiffness of the springs can be computed by calculating the stiffness of different components and then calculating an overall stiffness for the series / parallel arrangement. Deflection results for a model that was built using this approach show that in general, maximum deflections remained similar, although the locations at which they occur vary with respect to what the original model indicated. Figure 4.18 (c) shows parting plane separation for this case at the same corner. This figure shows that the behavior at this point is similar to the previous model, although magnitudes differ with respect to the original case.
\[ K_1 = \text{Cover platen stiffness} \]
\[ K_2 = \text{Ejector platen stiffness} \]
\[ K_3 = \text{Toggle / Tie Bar stiffness} \]

Parting plane separation. Upper outside corner, open - close die design. Simulation results.

Figure 4.18. Schematic comparison of die support. (a) Original model.
(b) Modified model that accounts for elasticity of support.
(c) Comparison of simulation results, outside corner of die.
4.12 Concluding comments

Given the nature of the loads and the type of support provided, die casting dies cannot be made to operate deflection-free. Deflections can be minimized though. Furthermore, dies should be designed to deflect in a predictable, consistent manner.

The analysis presented in this chapter indicates that the stiffness of the die is fixed at the moment the clamping force is applied. The key for minimizing die deflections is to be able to produce favorable stress patterns to oppose pressure induced deflections.

It has been shown that die builders try to accomplish this by rising the insert in such a way that a favorable pre-stress is created at the parting plane prior to metal injection (see Figure 4.19). However, to this date very little is known about the role of other structural design parameters, or even temperatures, on this stress. That is, die designers hope that in operation a favorable stress pattern will still be found when using this type of design.

In this chapter, a methodology for the simulation of die casting die deflections was introduced. The issues in this process and recommendations for addressing them were also explained. It was stated that results seem to be in the range of what is commonly observed in the field. In the next chapter, these procedures and recommendations will be applied to the analysis of slide blowback.
Figure 4.19. Approximate nature of compressive stress patterns at parting plane.
(a) Flat parting surface. (b) Effect of proud inserts. Case (b) intends to oppose deflection due to cavity pressure more efficiently.

5.1 Dies for transmission casings and other power train components

The automobile industry is perhaps the largest user of die casting products. Many of the large structural components of the power train are routinely die cast. Transmission casings, engine blocks and clutch housings are some of the examples. Figure 5.1 shows some of these components. Deflections of the die casting dies that make these types of components have been reported and documented by automakers.

This work will focus on studying the deflection of the slides for shapes similar to the transmission case extension. There are several reasons for selecting it:

- when prompted for dies that show the most deflection related problems, die casters refer to this type of dies with great frequency.
- because of the geometry of the part and the corresponding die construction, this geometry is subject to most of the loads possible
Figure 5.1. Some power train components typically produced by die casting.
in a die casting operation, from momentum due to impingement of flowing metal upon the slide surface, to heat release during solidification.

- dies for these parts present a specific structural component: the slide lock. The design of this mechanism plays a crucial role in the performance of the die.

Once a clear picture of the characteristics of loads upon the slide is obtained, this description can be used to determine the behavior of the full structure of the die. This chapter describes some of the details of the structural design of dies for transmission-case like parts.

5.2 Structural characteristics of transmission case dies

In addition to the components found in every die (inserts, holder blocks, ejector plates, etc.), dies for transmission casings and similar geometry present undercuts that require the introduction of sliding components to produce internal features without obstructing part ejection.

Figure 5.2 presents a schematic of this type of arrangement. During the casting operation, the core resides inside the cavity, thus producing the internal features of the part. Once solidification is complete, the core slides out to allow the ejection of the part (hence the denomination "slider").
Figure 5.2. Typical die construction with a sliding core. From Exco Engineering's product catalog (Newmarket, Ontario).
A slider has two components: the slide tip and the slide carrier, shown also in Figure 5.2. The slide tip is the element that produces the geometric features in the inside of the part. Except for the portion of the tip that is attached to the carrier and whose design is standard, the geometric design of the tip depends upon the product. During the casting operation, the tip resides within and forms part of the cavity.

The design of the slide carrier allows this component to hold, orient, and guide the slide tip. During operation, the slide carrier resides outside the cavity. It provides a shutoff (metal seal area) to stop the molten metal from escaping to the atmosphere. It also provides a surface for the lock to apply the locking force. The design of the carrier follows standard rules and dimensions, drawn from experience and which may vary from caster to caster.

There are several actuation mechanisms for the slider, including the rack and pinion pull, the pin actuated core pull, and the hydraulic core pull. They are shown in Figure 5.3. These actuators are normally fastened to the outside structure of the die and have only two functions: to move the core into position before the metal is cast, and to remove the core from within the cavity before the part is ejected.

During the actual casting operation, the slider is kept in place by a locking mechanism whose lone function is to accurately position and hold
Figure 5.3. Different slide pull activation mechanisms. From Doehler's "Die Casting" [4].
the slider within the cavity during the casting cycle. Because the pressure within the cavity may reach large values (~$10^4$ psi), a considerable force is needed to hold the slider in place. For this reason the lock is designed in such a way that the locking action and force of the casting machine is used to keep the slide in place.

5.3 Slide deflection

During operation, the slide tip is subject to a variety of loads. These loads are schematically represented in Figure 5.4. The slide will deflect under these loads, resulting in parallelism, concentricity and perpendicularity problems. The wall thickness of the part also depends upon the deflection of the slide. To illustrate the effect that the lock performance has upon the deflection of the slide, a crude cantilever-beam like model can be used (Figure 5.5). In such a model, the slide tip represents the beam, while the slide carrier and lock represent the support. Clearly, the type of support plays an important role in the magnitude of the deflection of the beam.

5.4 Lock design

There are at least three types of lock designs: the outboard design, the inboard design, and the pillar-horn design. Because of its construction, the outboard design is the cheapest to manufacture. It is also the least reliable design, due to the presence of the bolts. As can be seen in Figure 5.6, the
Types of loads

H = Heat from solidification
F = Momentum due to metal impingement during cavity fill
P(0), P(x) = Pressure on slide tip, whose nature depends upon filling and solidification patterns

Figure 5.4. Loads upon slide during die casting operation.

Figure 5.5. The model of the slide support depends upon the performance of the lock.
**Outboard Lock:** Independent component usually bolted to die block, *outside* the die structure.

In locked position, core is supported by the pressure generated at the interface between locking surfaces.

Figure 5.6. Outboard lock construction. From Doehler's "Die Casting" [4].
locking action produces a moment upon the lock which is opposed only by the tensile stiffness of the bolts that attach the lock to the upper (cover) die. In general, this design is used only when the conditions are not too stringent, e.g. in small dies that are not expected to last more than 50,000 cycles.

The inboard lock provides a stiffer construction than the outboard design. Contrary to what happens in the case of the outboard lock, the inboard lock is built within the envelope of the ejector and holder blocks. The compressive force applied by the locking action of the machine opposes directly the flexural moment generated at the angled face. Figure 5.7 shows this type of lock, along with some of its design parameters. It is interesting to point out the type of preload used in this design. In general, the angled surfaces are designed to make contact before the parting plane surfaces on the cover and ejector blocks meet. When the dies are closed during operation, a stress in the angled surfaces is generated once the dies come into contact. The gap determines the amount of preload designed into the lock. The nominal size of the gap is defined for the cold die, e.g. the actual size of the gap may be different during the actual casting operation, when thermal deflections of the die are important.

The pillar-horn type lock is used in the large dies. This type of lock is shown in Figure 5.8. A typical slide-lock arrangement includes two pillar-horns, one on either side of the slide carrier.
Figure 5.7. Inboard lock construction (DCD Technologies, Cleveland, Ohio).
Figure 5.8. Pillar-horn pin lock construction. From Exco Engineering's product catalog (Newmarket, Ontario).
The performance of the lock may be measured in terms of the magnitude of the deflections of the slide. Figure 5.9 shows actual field data for the deflection of a slide for a large transmission casing die in which a pillar-horn pin type lock was used. The data was collected by placing an LVDT in the back of the slide.

The plot shows the magnitude of the "blow back" of the slide at the moment of application of the cavity pressure. This deflection is measured along the "sliding" axis, in a direction which the lock action would oppose.

5.5 Comments on the lock design

In general, die casting die designers have developed lock design standards based mostly on experience. These standards may vary among the different die design / manufacture companies. To the knowledge of the author, there is no published work that reports an analysis of the mechanical design of locks in die casting dies, in spite of the impact that the lock design has upon the deflection of the slide.

Finite element simulations of different lock geometry may shed some light into the performance of alternate lock designs. Any study of this sort must include the effect that the thermal loads have upon the designed preload.
Figure 5.9. Slide blowback trace. Data courtesy of Warren Bishenden, Exco Engineering, Newmarket, Ontario.
5.6 Simulating slide blowback

To summarize the previous discussion, slide blowback is the displacement that a sliding core experiences once pressure is applied in the cavity. Blowback occurs against the action of the slide lock (see Figure 5.10).

To compensate for blowback, slides are often moved into the cavity in such a way that nominal dimensions are obtained at the "blown back" position. The distance that slides are moved into the cavity is fixed once the die has been built. This distance is determined based on prior experience. The uncertainty regarding the definition and interaction among the different loads is perhaps the main reason why numerical simulations or other types of analytical tools are not used for the design of such components.

A description of the process that resulted in the definition of an FEM model of slide blowback that can be used for design purposes is now presented. The experimental setup that produced the data that backs up this model will be introduced first, followed by a description of the modeling procedure. The model is then applied to a different slide for which blowback data is also available.

5.7 Methodology for developing an FEM model to predict slide blowback

The development of an FEM model that can be used to predict blowback involves at least four activities:
Figure 5.10. Schematic slide / lock arrangement showing blowback direction.
• Define the desired output.

• Determine the variables that may have an effect on output. In our case, this step consists essentially of defining the geometry, boundary conditions, and the parameters that will be varied.

• Determine the sensitivity of the model response to the variation in the values of parameters and variables.

• Validate model with field data.

A certain degree of recursion may be expected in the process described above. Clearly, developing a model without having a clear idea of what is expected from it may result in a waste of time and resources. Similarly, the sensitivity of the model cannot be tested until it has been defined and built. On the other hand, the choice of the desired output or even the values of the parameters that will be varied will necessarily be influenced by the characteristics of field data, if available. Frequently, field data may not specifically describe the phenomena of interest. Even in those cases field data can be used to gain confidence about the model's quality.

In this research, blowback data was available before the models were designed. Consequently, this data was used as reference when testing the sensitivity of the model to variations in the input parameters. In the paragraphs that follow, the procedure to develop the models is described. This description begins with an explanation of the case for which blowback
data was available. After the model is explained, its application to the simulation of blowback for a similar design for which data was also available is presented. Finally, this model is used as a design tool in which potential design modifications to the original horn pin are analyzed.

5.8 Slide blowback: field tests

Field data of slide blowback was collected for a transmission casing die. A schematic of the die and casting (a transmission casing) is presented in Figure 5.11. There are a few characteristics of the slide / lock design that need to be mentioned:

- the hydraulic cylinders that move slides into position are de-energized during the injection and solidification process
- during injection and solidification, slides are kept in place by the locking mechanism, which in this case is solely the horn pin
- in the locked position and at room temperature, there is no interference between wear plates located behind the lock and the front of the pin

Figure 5.12 illustrates the location of the displacement probes with which blowback data was collected. Linear Variable Differential Transformers (LVDT's) were placed behind the top slide (labeled "pan slide") because
Figure 5.11. Schematic illustrating the most important characteristics of the die and casting for which data was made available. Slide tips and details of cavity are not shown.
Figure 5.12. Schematic placement of LVDT's behind top slide.
traditionally this slide presents the largest amount of blowback. Dies were slowly brought up to operating conditions by the following sequence:

- The shot sleeve was loaded with molten aluminum several times (5-7) without actually injecting metal into the cavity.
- Several partial shots were made (5-7) after the shot sleeve was brought up to temperature.
- Five more shots were made before actual data collection began.

The only parameter that was varied during this experimental run was the timing for intensification, that is, the time delay between the end of injection and the application of intensification pressure. At each level of timing of intensification, blowback for two shots was recorded.

Typical plunger velocity and shot pressure vs. position traces for the experimental run were shown in Figure 3.14. Injection pressure measured in the hydraulic end reached 1900 psi, while intensification pressure reached 3200 psi. Consistently, intensification pressure dropped to about 3000 psi approximately 0.2 seconds after its peak value had been reached. Figures 5.13 through 5.17 present the averaged value of the response of the slide for each case.
Figure 5.13. Field data: slide blowback, timing of intensification = 0.1 seconds.

Figure 5.14. Field data: slide blowback, timing of intensification = 0.2 seconds.
Figure 5.15. Field data: slide blowback, timing of intensification = 0.5 seconds.

Figure 5.16. Field data: slide blowback, timing of intensification = 0.6 seconds.
Trace of average slide blowback
Channels 1 & 3, Timing of Intensification = 1.5 sec.

Figure 5.17. Slide blowback trace: timing of intensification=1.5 seconds.
5.9 Comments on experimental data

As reported by industry [64, 70], all other variables being equal, slides with the largest in-cavity projected tip area suffer the largest blowback. This fact seems to support the hypothesis that a hydrostatic state of pressure exists within the cavity at some point during the casting operation.

The field data presents a very detailed description of the response of the slide during and immediately after fill. In general, there is only a slight overshoot at the end of fill. Blowback at the end of fill reaches approximately 7.11 E-04 m (0.028 in). The maximum magnitude of blowback for timing of intensification of less than 1 second is approximately 9.4 E-04 m (0.038 in).

For timings higher than 1 second, intensification results in a very small increase in blowback. This is clearly a result of the inability of the casting to transmit pressure onto the surfaces of the cavity once solidification has reached a certain level.

From the perspective of the displacement probes, the slide seems to move back in a rather uniform fashion. For all cases, the maximum blowback recorded in Channel 1 is slightly larger than blowback in Channel 3. This may be the result of gaps in the structure of the die caused by looseness in assembly fits, imbalance on the load upon the horn pins due to the shape of the projected area of the slide tip, differences in the stiffness of the two pins
that lock the slide, inaccuracies in the calibration of the LVDTs or a combination of all of these factors.

From the available data, it is difficult to match accurately the response of the slide to the injection process, that is, the slide blowback trace and the plunger velocity trace. Nevertheless, the fact that the slide takes about 0.03 seconds from the moment it starts to move to the moment it reaches its end of travel seems to indicate that there is no response until a significant amount of pressure has built up within the cavity.

Based on the plunger velocity trace, a state of hydrostatic pressure may be developed within the cavity in a very short time (0.01”). The sluggish response of the slide may be caused by damping within the system. In the current design, damping will be provided mainly by friction between mating components, with some contribution being made by the de-energized hydraulic cylinder behind the slide carrier.

5.10 An FEM model to predict slide blowback

For an FEM model to be used as a design tool, the model needs to be simple enough for computational expenses to remain low. Under such conditions, multiple simulations may be performed to analyze alternate designs. At the same time, the model should be detailed enough to reproduce the phenomena being measured in the field tests for validation purposes and to provide pertinent design data. Despite the extended use of this type of
locking mechanism, there is no clear understanding as to which are the characteristics that define its behavior.

Based on the conclusions that were detailed in Chapter 3 about the characteristics of the loads and the recommendations of Chapter 4 the following assumptions were made:

- A state of hydrostatic pressure within the cavity is reached shortly after fill.
- Maximum cavity pressure is reached at the moment of intensification. Dynamic effects at the end of fill are negligible.
- Temperature effects can be ignored for the most part. Because of its distance to the cavity, fits among mating components are not affected significantly by temperature. The most important effect due to temperature is on Young's Modulus.
- The slide / lock arrangement is symmetric with respect to the mid plane between the horn pins
- The model must be able to display slide blowback as a function of cavity pressure
- Besides slide blowback, only one more output is being sought from this model: force transmitted to the foundation. While the role of this variable is not clear in terms of its effect upon the locking action, the force transmitted to the foundation will affect the
clamping force needed to hold the dies together and consequently, the size of the machine.

As will be shown, after a model was developed under these assumptions, it was found that there are only two variables of importance in the behavior of the model:

- Friction coefficient between contact surfaces
- Boundary condition behind the horn pin

Figure 5.18 shows the geometry upon which the finite element model of the slide / horn pin was developed. Boundary conditions and typical results are illustrated in Figure 5.19. IDEAS was used for preprocessing and some post processing. ABAQUS was used for computing the numerical solutions. Contact surfaces were defined to represent the interfaces between wear plates. As a result there are three independent meshes within the model, all interacting through their mating surfaces.

In this model, blowback, as measured in the field, is represented by the displacement of the back of the slide carrier. The force transmitted to the cover die is computed by multiplying the stress in the vertical direction by the cross sectional area of the pin. The transmitted force represents the resultant force, and as such is more of a measure of the average load in the region, rather than a measure of how severely the structure is loaded.
Figure 5.18. Overall dimensions of Exco’s slide carrier and pin.
Figure 5.19. a) Model description for computation of slide blowback (Exco's Design).
   b) Typical simulation results.
Clearly, within the top surface of the pin, some regions are in tension while some others are loaded in compression.

The assumptions made, along with the capability of modeling boundaries between different components simplify the modeling procedure to the point that the most important factor becomes the boundary condition behind the pin. The major problem associated with this boundary condition is the fact that there is a finite clearance (of about 0.005") between the pin and the wall of the pocket where the pin lies. Consequently, the pin will be free to deflect up to the point that it comes in contact with this wall.

Table 5.1 in Illustration 5.1 illustrates the effect that the variations in the friction factor coefficient have on slide blowback for two levels of cavity pressure. When a section of the back of the pin (spanning 4 inches from the top of the pin) is constrained from moving in $x$, the simulation results vary in a range that lies within 10% of measured blowback. When no support behind the pin is provided in the model, simulated blowback is approximately 20% higher when compared with the results of a partially supported pin. The small block at the bottom of the pin helps account for two factors:

- the finite stiffness of the support at the bottom of the pin, and
- the effect of the angle of this surface with respect to the vertical.
Boundary condition at back of pin:
P: Part of surface is restrained from moving in x (approximately 0.1 m from top)
NS: Surface is unrestrained

Table 5.1 Sensitivity of deflection to variation in friction factor and boundary conditions. Exco's design.

<table>
<thead>
<tr>
<th>Cavity Pressure Mpa (ksi)</th>
<th>Friction factor</th>
<th>Boundary condition behind pin</th>
<th>E (Mpa)</th>
<th>Blowback x E-04 m (in)</th>
</tr>
</thead>
<tbody>
<tr>
<td>95 (13.778)</td>
<td>0.1</td>
<td>P</td>
<td>2.1 E+11</td>
<td>11.3 (0.044)</td>
</tr>
<tr>
<td></td>
<td>0.2</td>
<td>P</td>
<td></td>
<td>10.9 (0.043)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>P</td>
<td></td>
<td>9.88 (0.039)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>P</td>
<td>1.86 E+11</td>
<td>1.09 (0.042)</td>
</tr>
<tr>
<td>50 (7.252)</td>
<td>0.1</td>
<td>P</td>
<td>2.1 E+11</td>
<td>6.1 (0.024)</td>
</tr>
<tr>
<td></td>
<td>0.2</td>
<td>P</td>
<td></td>
<td>5.84 (0.023)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>P</td>
<td></td>
<td>5.37 (0.021)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>NS</td>
<td></td>
<td>6.43 (0.025)</td>
</tr>
</tbody>
</table>

Illustration 5.1. Details of final model and test results.
Results from this model indicate that a higher friction results in smaller blowback. This reduction is accompanied by an increase in stresses at the contact surfaces. A case was run in which the Modulus of elasticity of steel was modified to account for a rise in temperature that would be considered typical for the bulk of the die in a casting temperature).

For example, Young's Modulus of AISI 4140 drops about 10% as a temperature of 300 °C is reached [71]. In this case, the maximum blowback is increased by about the same factor when compared with the conditions that were considered to best match the field data. As the results show, the model behaves rather linearly with a change in the value of the Modulus of Elasticity. Consequently, results can with scaled this factor whenever a significant temperature rise is detected. It must be pointed out that 300 °C would be considered high for the bulk of the die to reach during the casting operation.

Figure 5.19 (b) presented results computed using the final model. The conditions of friction factor = 0.3, partial support behind pin, and a block support at bottom of pin seem to provide the best match with field measurements for the given cavity pressure (95 MPa).

The final model evolved from a simpler model, shown in Illustration 5.2. Table 5.2 in Illustration 5.2 summarizes the conditions under which the original model was tested along with blowback reported by each trial.
XY is plane of symmetry

Clamped Surface

Back of Pin

Horn Pin

Slide Carrier

Blowback

Cavity Pressure

Nodes on edge are restrained in every degree of freedom, except for rotation around z

Boundary condition at back of pin:
P : Part of surface is restrained from moving in x (approximately 0.1 m from top)
NS : Surface is unrestrained

Table 5.2 Sensitivity of deflection to variation in friction factor and boundary conditions. Exco's design.

<table>
<thead>
<tr>
<th>Cavity Pressure Mpa (ksi)</th>
<th>Friction factor</th>
<th>Boundary condition behind pin</th>
<th>Blowback x E-04 m (in)</th>
</tr>
</thead>
<tbody>
<tr>
<td>95 (13.778)</td>
<td>0.1</td>
<td>P</td>
<td>9.12 (0.036)</td>
</tr>
<tr>
<td></td>
<td>0.2</td>
<td>P</td>
<td>8.88 (0.035)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>P</td>
<td>8.61 (0.034)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>NS</td>
<td>9.66 (0.038)</td>
</tr>
<tr>
<td>50 (7.252)</td>
<td>0.1</td>
<td>P</td>
<td>4.80 (0.019)</td>
</tr>
<tr>
<td></td>
<td>0.2</td>
<td>P</td>
<td>4.67 (0.018)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>P</td>
<td>4.54 (0.018)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>NS</td>
<td>5.11 (0.020)</td>
</tr>
<tr>
<td></td>
<td>0.4</td>
<td>NS</td>
<td>4.9 (0.019)</td>
</tr>
</tbody>
</table>

Illustration 5.2. Details of original model and test results.

210
The relevance of the behavior of the contact surfaces was evident when testing this model. Coulomb friction was used to model the interaction between contact surfaces. The value of the friction factor had an important effect upon convergence of the solution. In most cases, convergence was not achieved for a friction equal to or greater than 0.4.

The model in Illustration 5.2 proved to be stiffer and less sensitive to variations in the friction factor than the final model. As crude as the original model was, blowback results matched the field data well enough that only slight modifications were necessary. The most important shortcoming of this model was its inability to account for the effect of the angle at the bottom of the pin. This first approximation did not simulate explicitly the support at the bottom of the horn pin.

Figure 5.20 presents a comparison of CPU time vs. number of elements in the FEM model, and it also illustrates how the magnitude of the reported blowback is affected by the number of elements.

ABAQUS has the capability of modeling surface contact in different ways. Different models may be used to facilitate numerical solution at the expense of accuracy. Figure 5.21 illustrates the types of modifications to the surface behavior that may be made. In the default model, labeled "hard" contact, full contact pressures are developed at the moment that contact is detected, regardless of how localized or intermittent this contact may be.
Effect of increasing the number of elements on magnitude of 
blowback
Reference: 5750 linear elements, 7.785E-4 m (0.0307") blowback

Effect of increasing the number of elements on CPU time
Reference: 5750 linear elements, 262.88 sec. cpu time

Figure 5.20. Effect of varying the number of elements upon computational 
expense and results.

212
Figure 5.21. Models for defining contact surface behavior in ABAQUS.
When the clearance between contact surfaces is positive, there is no contact and therefore no pressure being transmitted. In the over closed model, no contact is reached until contact surfaces overlap by a distance "c". Similarly, surfaces do not separate until a normal tensile stress of magnitude $p^o$ is generated between the surfaces. The last model presents a softened condition, in which contact pressure increases exponentially as the mating surfaces approach each other. This model is particularly suitable for those cases in which a thin coating on one or both of the contacting surfaces exists.

Padyiar [57] suggested that the use of such capability might ease some of the numerical problems. Our tests indicate that convergence is not necessarily facilitated by the use of these options. Tables 5.3 and 5.4 present the results of applying these surface contact behavior models in those cases in which convergence had already been achieved. As the tables shows, convergence was not reached in some cases. Furthermore, in those cases where a solution was found, results varied as much as 42%. In general, deflections seemed insensitive to the values of the parameters that defined the surface behavior ($c$ and $p^o$).

5.11 Application of model to the analysis of an alternate slide lock design

The model presented in the previous sections can used to analyze blowback in slides of similar design. Figure 5.22 presents a modified version of the horn pin lock design. In this new version, additional support is
Table 5.3 Comparison of results, over closed surface contact

<table>
<thead>
<tr>
<th>Cavity Pressure MPa (ksi)</th>
<th>( c \times 10^{-3} ) (in)</th>
<th>( p^o ) MPa (ksi)</th>
<th>Slide blowback ( m \times 10^{-3} ) (in)</th>
<th>% Difference with respect to working model</th>
</tr>
</thead>
<tbody>
<tr>
<td>95 (13.778)</td>
<td>3.9 (0.0001)</td>
<td>10^5</td>
<td>1.21 (0.048)</td>
<td>(+) 20</td>
</tr>
<tr>
<td>95 (13.778)</td>
<td>3.9 (0.0001)</td>
<td>10^6</td>
<td>&quot;</td>
<td>&quot;</td>
</tr>
<tr>
<td>95 (13.778)</td>
<td>7.8 (0.0002)</td>
<td>10^5</td>
<td>&quot;</td>
<td>&quot;</td>
</tr>
<tr>
<td>95 (13.778)</td>
<td>7.8 (0.0002)</td>
<td>10^6</td>
<td>&quot;</td>
<td>&quot;</td>
</tr>
</tbody>
</table>

Table 5.4 Comparison of results, softened surface contact

<table>
<thead>
<tr>
<th>Cavity Pressure MPa (ksi)</th>
<th>( c \times 10^{-3} ) (in)</th>
<th>( p^o ) MPa (ksi)</th>
<th>Slide blowback ( m \times 10^{-3} ) (in)</th>
<th>% Difference with respect to working model</th>
</tr>
</thead>
<tbody>
<tr>
<td>95 (13.778)</td>
<td>3.9 (0.0001)</td>
<td>10^5</td>
<td>(did not converge)</td>
<td>-</td>
</tr>
<tr>
<td>95 (13.778)</td>
<td>3.9 (0.0001)</td>
<td>10^6</td>
<td>(did not converge)</td>
<td>-</td>
</tr>
<tr>
<td>95 (13.778)</td>
<td>7.8 (0.0002)</td>
<td>10^5</td>
<td>(did not converge)</td>
<td>-</td>
</tr>
<tr>
<td>95 (13.778)</td>
<td>7.8 (0.0002)</td>
<td>10^6</td>
<td>1.35 (0.053)</td>
<td>(+) 34</td>
</tr>
<tr>
<td>50 (7.252)</td>
<td>3.9 (0.0001)</td>
<td>10^5</td>
<td>0.76 (0.030)</td>
<td>(+) 42</td>
</tr>
<tr>
<td>50 (7.252)</td>
<td>7.8 (0.0002)</td>
<td>10^6</td>
<td>0.76 (0.030)</td>
<td>(+) 42</td>
</tr>
</tbody>
</table>
Figure 5.22. Modified lock design. Schematic of shape and dimensions.
Dimensions in inches.
provided by the cover die behind the slide carrier. In essence, this lock is a combination horn pin/inboard lock. This particular die was instrumented with a deflection measuring system from Matrix Technologies.

Blowback data at periodic conditions for this design is presented in Figure 5.23. Deflections reached 0.035" approximately. This figure also presents the pressure measured in the hydraulic system of the machine, and its adjusted value to describe cavity pressure (71.8 MPa, or 10,400 psi).

Figure 5.24 illustrates typical simulation results for this case. Except for the block that represents the support provided by the cover die directly behind the slide carrier's centerline, this model is the same as the one used to simulate blowback in the horn-pin lock with which the modeling procedure was originally defined. Given the nature of the pressure rise within the cavity, loads were assumed to be quasi-static. This assumption is further supported by the response of the slide displayed in the field data (little to no overshoot).

Table 5.5 in Illustration 5.3 shows the behavior of this model as friction is varied. Simulation results indicate that this slide will blowback as much as 6.205 E-04 m (0.024"), approximately 2.69 E-04 m (0.011") less than actually measured. Part of this difference is due to the presence of a gap between the slide carrier and the lock, which by design is 1.02 E-04 m (.004") This gap exists because the structure of the die is prepared to take replacement pins and
Figure 5.23. Slide blowback, combination lock.
Figure 5.24. a) Schematic model of Delaware slide pin with boundary conditions for computation of slide blowback.  
b) Typical simulation results.
Table 5.5 Blowback data Delaware die - carrier supported by cover die

<table>
<thead>
<tr>
<th>Cavity Pressure Mpa (ksi)</th>
<th>Friction factor</th>
<th>Boundary condition behind pin</th>
<th>Blowback x E-04 m (in)</th>
</tr>
</thead>
<tbody>
<tr>
<td>71.8 (10.4)</td>
<td>0.0</td>
<td>P</td>
<td>6.2 (0.024)</td>
</tr>
<tr>
<td></td>
<td>0.1</td>
<td>P</td>
<td>6.0 (0.023)</td>
</tr>
<tr>
<td></td>
<td>0.2</td>
<td>P</td>
<td>5.6 (0.022)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>P</td>
<td>5.3 (0.021)</td>
</tr>
</tbody>
</table>

Illustration 5.3. Schematic of (a) combination lock and (b) test results.
slides, and consequently an assembly allowance has been provided. As Table 5.5 shows, a variation of the friction alone will not bring the simulated blowback to the point that the field data is matched.

Figure 5.25 illustrates blowback for the same design, but without the extra support that the cover die presumably provides. Table 5.6 in Illustration 5.4 presents results under different values of friction for the same design. Maximum blowback in this case reaches 8.13 E-04 m (0.032"). With the addition of the gap distance (0.004"), this result is close enough to the field data that any discrepancy would be attributable to errors inherent in the modeling process. The implication is then that the extra support provided by the cover is not as effective as would be expected.

5.12 Variations to lock design

The model for slide blowback can be used as a design tool. As an example, the dimensions of the lock design provided by Exco Engineering were varied, and their effect upon the magnitude of slide blowback and force transmitted to the cover die recorded.

---

Based on the variability of the sensitivity of the results to the input data as shown in Tables 5.1 through 5.5, the error can be expected to run as high as +/- 10 %
Figure 5.25. a) Schematic model of Delaware slide pin with boundary conditions for computation of slide blowback. b) Typical simulation results.
Table 5.6 Blowback data Delaware die - carrier supported only by pin

<table>
<thead>
<tr>
<th>Cavity Pressure Mpa (ksi)</th>
<th>Friction factor</th>
<th>Boundary condition behind pin</th>
<th>Blowback x E-04 m (in)</th>
</tr>
</thead>
<tbody>
<tr>
<td>71.8 (10.4)</td>
<td>0.1</td>
<td>P</td>
<td>8.17 (0.032)</td>
</tr>
<tr>
<td></td>
<td>0.2</td>
<td>P</td>
<td>7.78 (0.031)</td>
</tr>
<tr>
<td></td>
<td>0.3</td>
<td>P</td>
<td>7.44 (0.029)</td>
</tr>
<tr>
<td></td>
<td>0.0</td>
<td>NS</td>
<td>8.77 (0.035)</td>
</tr>
</tbody>
</table>

Illustration 5.4. Modified model and simulation results for combination lock.
Illustration 5.5 presents the horn pin geometry and the dimensions that were varied in the parametric study. An FEM model was built for each case described in Table 5.7 in this illustration. An effort was made to keep the size of the slide carrier constant in all cases.

For each case, the magnitude of blowback was recorded for three different cavity pressures: 20, 30 and 40 MPa or 2900, 4350 and 5800 psi respectively. The highest of these pressures is about half of typical intensification pressures for this type of the die. The reason for keeping pressures at this level is that some models presented numerical problems at higher pressures.

Results were prepared in a format similar to the plot shown in Figure 5.26, which displays cavity force vs. blowback for a particular case (5). The slope of this curve represents the stiffness of the lock. Clearly, a larger stiffness results in smaller blowback. In all cases, the stiffness proved to be constant for the range of loads selected. This fact made the stiffness a suitable design parameter. Force transmitted to the cover die was computed by multiplying the stress in the upward direction at the top of the pin by the cross sectional area of the pin (which was kept constant for all cases). A larger force into the cover die would result in the need for a larger clamping force. It is important to emphasize that force transmitted to the foundation is only a measure of the average load produced by the pin upon the cover die.
Table 5.7 Cases for parametric study.

<table>
<thead>
<tr>
<th>Case</th>
<th>t/w</th>
<th>α</th>
<th>β</th>
</tr>
</thead>
<tbody>
<tr>
<td>Original</td>
<td>O</td>
<td>O</td>
<td>O</td>
</tr>
<tr>
<td>1</td>
<td>H</td>
<td>H</td>
<td>H</td>
</tr>
<tr>
<td>2</td>
<td>H</td>
<td>H</td>
<td>L</td>
</tr>
<tr>
<td>3</td>
<td>H</td>
<td>L</td>
<td>H</td>
</tr>
<tr>
<td>4</td>
<td>H</td>
<td>L</td>
<td>L</td>
</tr>
<tr>
<td>5</td>
<td>L</td>
<td>H</td>
<td>H</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>H</td>
<td>L</td>
</tr>
<tr>
<td>7</td>
<td>L</td>
<td>L</td>
<td>H</td>
</tr>
<tr>
<td>8</td>
<td>L</td>
<td>L</td>
<td>L</td>
</tr>
<tr>
<td>OHH</td>
<td>O</td>
<td>H</td>
<td>H</td>
</tr>
<tr>
<td>OHL</td>
<td>O</td>
<td>H</td>
<td>L</td>
</tr>
</tbody>
</table>

Where:

\[
t/w (O) = 1.964
\]
\[
t/w (H) = 1.3 \times t/w (O)
\]
\[
t/w (L) = 0.7 \times t/w (O)
\]

\[
a (O) = 3.5 \text{ deg}
\]
\[
a (H) = 1.3 \times a (O)
\]
\[
a (L) = 0.7 \times a (O)
\]

\[
b (O) = 10 \text{ deg}
\]
\[
b (H) = 1.3 \times b (O)
\]
\[
b (L) = 0.7 \times b (O)
\]

Constraint:

Area of top of pin

\[
t^*w (O) = t^*w (H) = t^*w (L)
\]

Illustration 5.5. Parametric study of Exco's horn pin design. Parameters and variations. (O) refers to original dimensions, (H) refers to higher value and (L) refers to lower value.
Figure 5.26. Force vs. displacement plot used to compute stiffness of lock.
Figure 5.27 summarizes the characteristic behavior for each trial case compared with the original case. In the graph, the value of the stiffness and force transmitted to the foundation of the original case was used as reference, A value of 1 was assigned to each one of them. As an example of how to interpret the results, a stiffness of 1.43 represents a 43% increase over the original case. The third parameter, ratio of stiffness to force is also included in the chart.

As the chart shows, the original design ranks very high when compared to the rest of the cases. This fact is not at all surprising, because this type of lock design was originally developed during the 1970's. The design of this lock has been optimized on the basis of experience alone. Simulation results show that this lock combines a large stiffness (ranked 2nd overall) with a relatively small force transmitted to the foundation (ranking 4th overall).

Cases 1,2,3,7, OHH and OHL are clearly inferior. Cases 5, 6 and 8 are within the range of error of the modeling procedure. Case 5 results in an increased stiffness, at the expense of an increased force into the foundation (although there still seems to be a slight improvement in the design). Cases 4 and 6 are less stiff, yet the force that they transfer to the cover die is reduced by a larger factor than the stiffness is reduced. Case 4 is particularly interesting because it falls outside the range of error of the simulation results.
Parametric Study: Comparison of Results

Where:

\( K_n = \text{Stiffness for Case n} \)
\( F_n = \text{Force Transmitted to Cover Die in Case n} \)
\( F_o = 0.279 \times \text{Cavity Pressure} \times \text{Area of Slide Tip} \)
\( K_o = 6200 \ \text{E}+06 \ \text{N/m} \)

Subscript "o" indicates data was taken from original case

Figure 5.27. Comparison of characteristic behavior of slide / lock arrangements for different cases in parametric study.
Furthermore, given the significant reduction in the force transmitted to the foundation, this case would be ideally suitable for providing clues as to the role of the force transmitted to the foundation.

Clearly, an increase in the stiffness will result in a reduction of blowback. Therefore, an increase in stiffness is an outcome sought. On the other hand, the relevance of force transmitted to the foundation is not totally clear. The magnitude of force transmitted to the cover die will play a role in the size of the machine needed to hold the dies. However, given the fact that machines change in size in discrete steps (i.e. 800 ton, 1500 ton, etc.) changes in the force transmitted to the cover die will not necessarily result in the need to mount the die on a different machine.

From the point of view of lock design, investigating the role that force into the cover die plays in tightness of the locking action is perhaps a worthwhile exercise. For example, lower forces will probably result in less deflections in regions close to the pin, and consequently in tighter assemblies in such regions. It is feasible that tighter fits will result in a more stable operation of the lock.

5.13 Further applications: effect of projected area of die on die deflections

The role of the match between die and die casting machine has already been discussed and quantified to a certain point by practitioners [72]. From the perspective of the support provided by the die casting machine, an increase in
the size of the parting surface of the die results in a larger portion of the platen surface being engaged by the die. As a consequence, cover platen deflection is reduced and more uniform support behind the cover die is provided. Beyond this, very little is known about the effects of the magnitude of the parting plane area of the die.

In Chapter 4, an open close die was used to illustrate the application of the principles regarding the nature of the loads in die casting that were developed in Chapter 3. As a result of this exercise, the sensitivity of the parting plane separation to variations of the thickness of the die was presented.

As a further illustration of the type of analysis that can be conducted using the techniques introduced in this work, a pilot study of the effect of varying the projected area of the die is presented here. This study was intended to complement the results that were presented in Chapter 4. The die geometry for the flat plate die was modified by increasing and decreasing certain dimensions of the die. The dimensions of a volume around the cavity were kept constant. These dimensions are the same as those of the volume that represented the insert.

Figure 5.28 presents the dimensions used in this study. In general, a factor of +/− 25% was applied to the original dimensions. Casting conditions remained the same as those used in Chapter 4. Paring plane separation data
Figure 5.28. Comparison of dimensions for analysis of effect of projected area.
at two different locations resulting from this exercise is presented in Figures 5.29 and 5.30. What these figures show is that by increasing the size of the die area, parting plane separation was virtually eliminated after the second cycle.

This type of response by the die comes as a total surprise to most die designers and casters\(^8\) (although some studies of late have reported this behavior \([72]\), validation is clearly needed, specially in view of the limitations of the model used to generate this data). Most die manufacturers try to reduce deflection related problems by increasing the thickness of the die bases.

The relevance of this study lies in the fact that it illustrates the poor understanding of the mechanisms that allow a die to lock properly. For example, the previous results show that a large die will reduce parting plane separation more dramatically than increasing the thickness of the die. On the other hand, the smaller die also produced deflection results that were slightly smaller than in the case of the nominal die. A possible explanation for this behavior is that, when compared with the nominal die, the support rails are closer to the cavity profile in the case of the smaller die (in practice rails can be placed only as close to the die as the ejector pin plate will allow them to be).

\(^8\) During the summer of 1996, these results were presented at a series of seminars sponsored by NADCA throughout the U.S. The audience was generally a mixture of die casters and designers. Of the 200+ attendants to these meetings, only one die designer anticipated that an increase in the area of the die would have a more dramatic influence than an increase of the thickness of the die.
Ejector Die Dimensions
Height (h) = 33.24, 35 \( \frac{1}{2} \), 37.78 in
Width (w) = 20.66, 23 \( \frac{3}{4} \), 26.64 in
Thickness (t) = 4.875 in
Cavity Pressure = 10,000 psi
Clamping Force = 815 ton

Approximate location of nodes

Maximum Separation at Parting Plane: Multiple Cycles

Figure 5.29. Maximum parting plane separation at corner of cavity. Multiple cycles.
Ejector Die Dimensions

Height \( h \) = 33.24, 35 \( \frac{1}{2} \), 37.78 in

Width \( w \) = 20.66, 23 \( \frac{3}{4} \), 26.64 in

Thickness \( t \) = 4.875 in

Cavity Pressure = 10,000 psi

Clamping Force = 815 ton

Approximate location of nodes

Maximum Separation at Parting Plane: Multiple Cycles

![Graph showing maximum separation at parting plane.](image)

Figure 5.30. Maximum separation at a point close to middle of the cavity.
These results clearly indicate that the ability of a die to remain closed is affected by the interaction of the different design parameters: die thickness, surface area, and location of pillar supports (or rails).

5.14 Shutoff pressure profile

The results of the previous exercise are meaningful for a couple of reasons. First of all, these results illustrate the need to investigate the role of the different die design parameters more carefully (i.e. not only the thickness of the die). Second, and perhaps more important, the need to identify a parameter that can help in the evaluation of the effects of the different design parameters is apparent.

Our discussion so far has indicated that one of the issues associated with the analysis of die casting die deflections is being able to collect the data at meaningful moments. The longer the cycle, the more difficult it is to manage the volume of information that a simulation produces. Clearly, the ability to predict the final shape and dimensions of the casting is affected by the degree of detail with which a description of the time history of the deflection patterns within the cavity is predicted. For this reason alone, time deflection data as presented in Chapter 4 is important.

While parting plane separation is a good indicator of the response of the die, it has been shown that the magnitude that our simulation reports depends on the instant at which data is collected. Furthermore, parting plane
separation data in itself is incapable of describing what is actually happening within the die. Therefore, it is difficult to predict the effect that a certain design parameter produces.

The bolted joint analogy that was used earlier in this document to characterize the behavior of the open-close die suggests that a parameter that can be help to evaluate the ability of a die to withstand the initial blow that tends to separate it is the shutoff pressure profile. This parameter is basically the magnitude of the compressive stresses at the parting plane along the periphery of the cavity. Regions of low shutoff pressure are more likely to deflect than regions of high pressure. The shutoff pressure depends on the long term thermoelastic response of the die. For this reason, there are less restrictions on how this parameter can be predicted and analyzed than in the case of the instantaneous maximum parting plane separation. The interaction between die design parameters such as die thickness, surface area and placement of rails supports will affect the nature of the stresses in the die structure, which in the end determine the ability of the die to withstand deflections. The effect of varying the design parameters can be evaluated in terms of the shutoff pressure profile.
5.15 Correlation with process conditions

Based on the analysis presented up to this point, it can be seen that the behavior of the die, namely the parting plane separation, can be directly correlated with certain process conditions.

The use of hydrostatic pressure to account for the load upon the cavity produced simulation results that closely matched field data. The field data showed that this assumption was correct as long as the timing of intensification was short. In the case of the blowback data, significant variations in the maximum blowback were not observed until application of intensification was delayed approximately 5 times the fill time. This suggests that even with a significant amount of solid the assumption for hydrostatic in cavity loads holds. Furthermore, the observed behavior suggests that timing of intensification is a variable that can be used to influence the magnitude of deflection. However, this variable is likely to have effects upon the quality of the part. As a consequence, the effect of timing of intensification upon part quality needs to be investigated before it can be used as a parameter to control deflections.

A gap at the parting plane can be caused by too large an injection or intensification pressure. Clearly, reducing injection or intensification pressure will reduce the magnitude of the separation. Another cause of parting plane separation is the pressure spike at the end of fill. A large
pressure spike can be identified with the help of plunger velocity profile (irrespective of the injection pressure setting). Corrective measures consist basically of modifying this profile (by using plunger deceleration, reducing plunger velocity, etc.)

A gap in the shutoff surface that occurs prior to the application of pressure would be caused by the combination of the thermal deflections within the die structure and the clamping pressure patterns. In this case, the only way to eliminate this gap (short of increasing the clamping pressure) would be to modify the thermal patterns in the structure of the die, which can be accomplished by modifying flow in the cooling lines or redirecting lubricant spray (this is the type of modification that the work of Barone at General Motors [25, 73] can address).

5.16 Concluding remarks

Earlier in this Chapter, a comment was made about the role of field data in developing an FEM model. Given the fact that blowback data was available, a certain degree of confidence was obtained about the capacity of this model to reproduce slide blowback in a horn pin lock design. To some degree, the model was validated by applying it to a similar lock design, the combination horn pin-inboard lock. It is important to note however that further validation would be required before solid design recommendations are made.
Chapter 6. Conclusions and Future Work

6.1 Introduction

This research was motivated by the need to model and predict die casting die deflections. The theoretical problem is that of determining the thermoelastic deflections of a body under periodic thermo mechanical boundary conditions.

The problem is complicated by the existence of several different time and dimension scales. From the perspective of the loads, there are two different time scales:

- Cavity pressures due to the processes of filling and intensification are applied in a fraction of a second
- Clamping forces are applied throughout the duration of the casting cycle

The thermal response of the die is characterized also by at least two time scales:

- Time for solidification (seconds)
• Time to reach quasi-steady state conditions (hours)

For results to be of practical use, deflections must be computed with a resolution of fractions of a millimeter in models whose size is in the order of a meter. Because of the wide range of conditions, a very important task in this work was that of sorting out the interaction among the different loads, and clarifying what loads are relevant.

A modeling procedure was developed using ABAQUS, a commercial FEM system. The die behavior predicted by this procedure was consistent with what is commonly observed in the field. The procedure was also tested on a limited basis against field data. As a result, values of certain parameters and boundary conditions were defined.

Development and testing of this procedure was based on the cold chamber process. However, the same approach can be applied to the simulation and analysis of die deflections in the hot chamber die casting process.

The use of this procedure opens up the possibility of developing deflection data as presented throughout this document.

6.2 Research contributions

A global perspective of the characteristics of loads in die casting has been prepared. To this date, there is no other formal study of such
characteristics. Furthermore, this characterization has allowed us to correlate the behavior of the die with certain die design parameters (in those cases that have been analyzed). Perhaps more important, specific process conditions such as injection and intensification pressure, or plunger deceleration characteristics may be correlated with the behavior of the die. Rarely is the correlation of die casting die behavior with process conditions addressed in the literature.

The procedures that have been described here are general enough to be applied to any die geometry. Details about the scales of resolution of models and results have been addressed in this work. At least one failure mode from the perspective of die deflections has been established: parting plane separation (i.e. elastic deflection). With this failure mode in mind, the manner in which deflection data can be displayed for analysis and design purposes has been defined. Finally, a picture of the response of a die casting die has been introduced.

6.3 Conclusions

In this research, a basis for the characterization of the loads upon the tooling in a typical die casting operation has been provided. As mentioned already, this characterization opens up the possibility of correlating die performance with certain process conditions such as magnitude of injection
and intensification pressures, and plunger velocity. The results of this work indicate that:

- Momentum and hydrodynamic loads are negligible under typical casting conditions. Only poorly designed structures or uncharacteristic filling conditions would make these loads important.

- Cavity pressures can be modeled by hydrostatic loads. Field data confirms that this assumption holds except in those cases in which the application of intensification pressure is delayed.

- Injection pressure may induce a dynamic response on the die structure under very specific circumstances. Builders of die casting machines provide systems to suppress pressure surges. These systems are designed to reduce their adverse effects.

- Typical patterns of intensification pressure build up result in quasi-static loads.

- It may be expected that maximum deflections will be reached at the moment of intensification.

- Heat released during fill may produce deviations from instant fill conditions that are not necessarily insignificant. However, the contributions to die deflections of this deviation (when compared to the conditions predicted under the assumption of instant fill) are
generally not more significant than other sources of noise, such as manufacturing and assembly tolerances.

- Pressure and thermally induced deflections do not necessarily offset each other's effects. The implication is that the assumption that dies "tighten up" at operating temperature will not necessarily result in favorable lock up stresses.

- In a typical die casting operation, quasi-steady state conditions result when the die has reached a certain periodicity in its bulk temperature. A very large number of cycles are needed to reach quasi-steady state conditions. Therefore, dies should be designed to operate within a range of characteristic temperatures.

The response of the die casting die can be characterized as follows:

- The stiffness of the die ultimately determines the magnitude of the maximum deflections. This stiffness is a function of the design of the die, temperature of the bulk of the die, and clamping force.

- The shape of the characteristics shutoff pressure, i.e. the pressure that is built up at the surfaces where die halves meet, is affected by the temperature patterns within the die.

- Die design should be driven by the need to reduce deflections at the moment of intensification. Reducing the effects of the pressure spike through the modification of the structure of the die is not a
recommended practice. Pressure spike phenomena should be addressed in a different manner (i.e. plunger deceleration).

A finite element model to predict slide blowback was developed. The model was tested against field data. It is estimated that simulation results will lie within +/- 10% of actual blowback. This model can also predict force transmitted to the cover die. As illustrated by the design exercise, the horn pin design used as reference seems to have evolved into a design that combines a high stiffness (to reduce blowback) with low force transmitted into the cover die (which at minimum results in the need for less clamping force).

Simulation results seem to support the approximations being made regarding the role of temperature in the stiffness of the lock. Essentially, fits within the locking mechanism are not significantly affected by the change in temperature. A reduced Young’s Modulus caused by the raise in temperature results in a reduction in both the stiffness of the pin and the slide carrier, which in turn causes blowback magnitudes to increase. The response of the structure is rather linear, and therefore small variations in the Modulus of Elasticity, which would be expected given the range of temperatures at which the lock will work, will result in small deviations from predicted values.

Results indicate that a moderate value for the friction factor of 0.3 will produce blowback results that match field data. This factor would be applied
in the case in which full contact between the pin and the slide carrier is made at lock up.

6.4 Failure modes and resolution of results

Die casting dies can fail due to several different mechanisms, such as thermal fatigue or wear. However, from the perspective of the structural design of the die, elastic deflections can be of sufficient magnitude that the performance of the die can be handicapped. Jamming and flashing are two problems caused by the elastic deflections within the die structure of the die. Flashing can be caused by gaps as small as 0.004". One of the contributions of this work is that, by applying the modeling procedure presented here, gaps within the structure can be established with a degree of resolution of about 0.004"-0.005".

6.5 Future work

From the beginning, all efforts of this work were aimed towards predicting die deflections. At this point, the foundations for carrying out simulations that can predict deflections have been laid out. The limited simulation work that has been carried out so far indicates that other parameters have to be studied. Our field tests showed that by delaying the moment of intensification, the maximum slide blowback was reduced. This fact thus presents the possibility
of using timing of intensification as a process parameter to control deflections. On the other hand, delaying intensification can result in quality problems in the dimensions of the casting. For this reason, the effect of intensification delay upon the quality of the casting needs to be investigated before this variable can be used as a mechanism to reduce deflections.

The analysis of parting plane separation is an important issue in die casting die design. The term "shutoff pressure" was introduced in Chapter 5, and has been used constantly in this document to characterize the stress pattern at the parting surface of the die. It is clear that the ability of a die to remain closed will depend heavily on its closing characteristics, and consequently the shutoff pressure. Furthermore, the susceptibility of the die to operational problems such as flashing are affected by this characteristic. Future work should go in the direction of establishing the role that the shutoff pressure patterns play on the locking characteristics of a die. It is important to establish the types of patterns that will result in the best closing characteristics, in such that way operational problems are reduced. For this reason temperature and clamping pressure fields must be determined. Furthermore, it is important to establish the characteristics of the structural design of the die and process conditions that result in such shutoff patterns.

As shown in Chapter 4, more refined models are needed to predicted shutoff pressure patterns. For example, there is a need to establish the effect
of the size and stiffness of the die casting machine on the performance of the die.

In general, horn lock designs have been refined over a period of 40 years. Quite a bit of empirical knowledge is built into the actual design used as reference and for which field data is available. It may be feasible that some of the combinations that were tried out in the design exercise have actually been built. Assuming that this is the case, and supported by the results of the design exercise of Chapter 5, it can be said that stiffness may not be the only factor that defines the quality of the lock. In order to remove all speculations, it is necessary to build and test some of the variations that were analyzed in the design exercise.

Our results suggest that stiffness alone is probably not the reason for differentiating among the different designs. In other words, the evolution of the locking mechanism which has resulted in reduced blowback is not necessarily a function of the stiffness of the lock. A factor that needs to be measured is the force transmitted to the foundation. It is not known how this factor correlates with the quality of the lock performance. However, our simulation results indicate that this parameter is much more susceptible to variations in the lock design. It is possible then that the performance of the lock is also more susceptible to this parameter.
The way they currently stand, the recommendations and modeling procedure that were introduced in this work are good for the purposes of diagnosis. For example, given a condition in which the die flashes, the phenomena that causes it can be traced back to either a gap that appears in the parting plane at lock up, deflection due to intensification pressure or pressure spike phenomena. Corrective measures can be taken based on the results of this analysis.

For the purposes of design, the capability of predicting conditions that result in pressure spikes would be desirable. A study as suggested in Chapter 3 in which the plunger deceleration characteristics are correlated with the characteristics of the die (specifically, the runner-feeder-venting system) could provide some insights. Clearly, there is a need for more information before accurate predictions can be made.

Finally, it is important to emphasize that the ultimate goal of any research in this field should be that of improving the quality of the die casting process. The results of the current work can be used to reduce operational problems such as flashing. A natural extension of this work is the prediction of the final dimensions of the casting. The current work makes only a small contribution in this direction: it offers the possibility of predicting the dimensions of the cavity. An accurate prediction of the casting dimensions involves much more however, from the prediction of residual stress build up
of the casting as it solidifies under the influence of pressure within the cavity, to the prediction of the thermal distortion of the casting as it cools down after ejection.
LIST OF REFERENCES


at NADCA International Die Casting Congress & Exposition, Cleveland, Ohio, 1993.