Investigation of Formability and Fracture in Advanced Metal Forming Process

- Bulk Forging and Sheet Metal Forming

DISSERTATION

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By

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The dissertation focuses on fracture induced formability in advanced metal forming techniques. The purpose of this study is to fundamentally understand the fracture mechanics in metal forming processes and propose innovative alternative/optimized solutions to produce high quality part without fracture.

The dissertation is divided into two major parts: bulk forging and sheet metal forming.

In bulk forging part, three case studies were presented:

1. Precision forging of engine valves: the complete valve forging process (extrusion and coining) was investigated and the cracking at the “blade” area of the valve was predicted by Finite Element Simulations. Modified tooling design was proposed to reduce the cracking in forging.

2. Open die forging: comprehensive literature survey was conducted to explore the forming conditions that could influence part quality in open die forging. 3D FE simulation was conducted in order to emulate the actual forging process. Initial billet temperature was concluded to be the major factor to cause fracture. Adjustment of the initial temperature is one of the solutions to avoid fracture.

3. Bi-metal forging: an innovative gear forging concept for lightweight vehicle design was proposed and the forging process was validated by Finite Element simulations. Bi-metallic billet was designed and manufactured for test purpose. A new approach of utilizing induction heating method was studied and adopted to heat up the billet to achieve the required temperature gradients. Closed forging die design with special
modification to prevent cracking was developed in the actual forging experiment to forge “pancake” shape parts.

In sheet metal forming, the results of investigation on blanking and hole flanging of Advanced High Strength Steel (AHSS) were presented:

1. Blanking: the physical nature of blanking operation and the characters of a blanked edge were thoroughly investigated, using FEM and comparison of results with published data. The factors that influence that part quality including punch-die clearance, punch shape, punch and die corner radii were evaluated. Optimization of blanking conditions to provide better edge quality and enhanced tool life was proposed.

2. Hole flanging: issues and problems of the existing industrial standard that cause the unreliable experimental results were investigated. Finite Element simulations were conducted to predict hole expansion ratio with a suitable selection of fracture criterion (empirical and theoretical). The correlation of blanking and hole flanging was investigated to study the influence of blanking operation on hole expansion ratio. A new hole flanging tooling design was proposed to provide robust experimental results.
Dedication

This document is dedicated to my family.
Acknowledgments

I would like to express my deepest gratitude to my advisor, Dr. Taylan Altan, for his excellent guidance for my research.

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I would like to thank all the people that I love and all the people who love me.

Finally, I would like to thank my parents. Without their support, I would never be able to have enough encouragement to finish my PhD work. I would like to say thank you for your support.
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Fields of Study

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CHAPTER 1 Introduction

Formability is a term to describe the ability of a given metal workpiece to undergo plastic deformation without surface or internal failure. Typically, failure of the material refers to fracture initiation from either surface or the inside of the workpiece and even cracking of the whole part. As manufacturing has been emphasized by the government a few times in order to help United State to recover from economic recession, automotive companies and the suppliers are facing both opportunities and challenges in developing new metal forming technology. The recent technical trend is to build the metal part with higher strength and light weight. In order to achieve that goal, new materials such as Advanced High Strength Steel and Al alloy start to catch enormous attention and the formability and the manufacturing method of these new introduced materials are being investigated by more and more academic research associations and companies.

However, by bringing in the new materials and new structures, numerous challenges emerged during the manufacturing process of these new products. Material failed or fractured if conventional manufacturing processes are used due to limit of the formability. The new designed structure may request completely new manufacturing method to produce. The challenges exist in both bulk forging and sheet metal forming area. The research will be divided into these two major parts: forging and sheet metal forming.

1.1 Precision Valve Forging

The typical engine valve forging could be illustrated in Figure 1.1. In hot forging of engine valves, the billets are first sheared from bar stock to the desired sizes. These billets are then tumbled to remove any sharp edges left from shearing. The sheared billets are then heated in an
induction furnace to temperatures up to 1200°C. Then the heated billets are automatically fed to the first forming operation (extrusion) on the mechanical press.

Figure 1.1 Forging tooling schematic [Painter et al, 1996]

This first forming operation extrudes the billet to form the valve stem, as illustrated in Figure 1.1. Extrusion ratio is about 3:1 of the initial diameter of the billet. Lubricant is used to control heat transfer from the billet to the dies, to cool the punch/die and reduce interface friction. After extrusion, the part is ejected and transferred to the second forming station on the same press. In the second operation, the valve head is formed by the coining die, as shown in Figure 1.1. There is little or no change in the dimensions of the valve stem during the coining operation. Again, a lubricant is used to moderate the heat transfer and reduce friction. After coining, the valve is ejected from the press to cool. The extrusion and coining operations occur simultaneously during each cycle of the press.

All valves are manufactured on mechanical presses with load capacity of about 700 tons and idle stroking rate of about 60-90 strokes per min. These machines can operate under intermittent (after each stroke the ram stops for automatic part transfer) and continuous (the automatic part transfer
is synchronized with the press stroking rate i.e. slowed down to allow for part transfer) modes of 
operation. Intermittent mode of operates at a range of 60 to 90 strokes per min and continuous 
mode operates upto 30 strokes per min. The machines can produce about 25-30 parts per min.

1.2 Open Die Forging Technology

Hot open die forging is a well-known technology to form the large generally cast ingot into 
certain shape to be subjected to various following processes. The forging process uses dies with 
simple geometries and precise control of the press stroke and billet position to form the desired 
part geometry. A typical open die forging process is shown schematically in Figure 1.2.
1.3 Bi-metallic Forging Technology

The basic concept of bi-metallic forging is to forge a bi-metallic compound billet into a gear shape, rather than the traditional solid billet forging (Figure 1.3). The bi-metallic billet is made with one light weight material as the core/filling, and the outside casing is made of steel. Magnesium, Titanium and Aluminum are good candidates for the core material. Titanium alloy is too expensive for the truck application although it could the ideal selection from the functional point of view. Magnesium alloy is generally not as strong as Al alloy. Since the core material is also designed to carry a portion of the torque, the stronger material is selected as the core material.

The advantages of the weight reduction of gears in a transmission box can be summarized as:

1) Increase the sub-system efficiencies
2) Lower the power requirements
3) Increase fuel economy/decrease fuel consumption
4) Increase payload capacity

![Current gear and Bi-material gear comparison](image)

Figure 1.3 Comparison of the concepts of conventional gear and bi-metallic gear
Another advantage of bi-metallic forging is the possibility of near net-shape forging of the gear teeth. Traditional, the gear teeth on most of the spur gears and helical gears are cut from the forged gear blank. The difficulties in forging gear teeth include the high die stresses and severe die wear. In bi-metallic forging, the core material, e.g. Al, is softer and easier to forge. Only a portion of the steel – the casing – will be forged as in the traditional practice. Thus, there is a chance that the die stress and die wear could be reduced. Figure 1.4 shows a possible 2 blow procedure to forge the gear teeth with bi-metallic billet. In the first blow, the bi-metallic billet is forged into a “pancake” shape gear blank. Then the gear teeth are near net-shape forged in the second blow. The center hole of the gear could also be forged with very thin web left.

In forging area, there are two requests from the market: one is to develop innovative technology to meet the increasing demand of the light weight vehicles; secondly, improvement of the existing
technology to lower the cost and reduce the production failure. My research at ERC/NSM is conducted exactly by considering these two observations.

The research in valve forging area is intended to reduce the fracture issue and improve productivity in mass production by modification of the existing punch and die design.

Open die forging is a multi-hit forging process where material is formed using very simple dies to control the final shape without totally enclosing the workpiece. Generally, the workpiece in open die forging is hot and the metal flow is not constrained. Fracture could occur during the forging process which would waste a fortune in the view of fact that the ingot size is often fairly large. However, there is only small amount of research done on the fracture formation during open die forging. Part of the reason is that the die design and forging process in open die forging itself are relatively simple and adjustable with ease. The operators could possibly reduce the fracture by reducing the bite depth. Most software used in industry for pass schedule design is based on empirical equations. Another reason for the lack of literature about such topic is the complex situation in hot forging. A large number of fracture criteria were developed and proved successful in cold forming condition. However, in hot forging, since the temperature and strain rate play important roles on the forgeability of the material, a large portion of existing fracture criteria are not capable to predict the fracture.

The bi-metallic gear design is a blended concept of using alternative materials and new structures to save the weight of the vehicles. The potential benefits of the bi-metallic gear design are: 1) light weight; 2) save scratches; 3) reduce greenhouse emission (reduce forging load, etc.). A fundamental exploration will be conducted to investigate the feasibility from both manufacturing and application aspects.

In sheet metal forming area, blanking is the most important operation in material preparation before the further forming process. The quality of the blanked part directly affects the formability
of the material. Hole expansion or hole flanging test is commonly used to evaluate the stretchability and edge cracking in sheet metal forming. As AHSS are more widely used in automotive industry, difficulties in blanking AHSS draws great attention since AHSS is featured with much higher strength and more brittle than conventional high strength steels. Non-suitable blanking setup may result in bad edge quality and easily cause fracture in the next forming operation.

The current industry “standard” stretchability test – hole flanging test – is unreliable enough to evaluate the formability of the material due to the fact that a 10mm diameter hole is far away from the actual size of the part in industrial application. Any small system error may bring in noise to the experimental results and jeopardize the accuracy of the test. Also, many of the experimental conditions that will affect the results are not even specified in the “standard”. Therefore, in order to obtain robust and reliable experimental results, the hole expansion test procedure has to be revised and re-designed if necessary, especially a large hole size is essentially required.

1.4 Blanking Technology

Figure 1.5 shows the schematic of the blanking process. During blanking, the sheet material is held by the stripper plate (which is also the blank holder) by applying a certain stripper force, \( f_b \). The sheet material in between the punch and die undergoes very high deformation and is sheared as the punch penetrates the sheet material with velocity \( v_p \). The stripper plate strips the slug off the punch during the upwards motion of the punch.

The shear edge is made of different zones based on the method of material deformation that has occurred. The ratio of the different zones is influenced by different parameters like the punch-die clearance, material properties, and punch corner radius to name a few. In general, it is preferred to have a large shear zone and burr is not favorable.
The zones and deformation modes of a blanked part edge are given below and are shown in Figure 1.6

- Rollover ($Z_r$): caused by plastic material deformation
- Shear zone ($Z_s$): smooth and shiny area, created during material shearing
- Fracture/Rupture zone ($Z_f$): rough surface, results after the material cracks
- Burr ($Z_b$): caused by plastic deformation
- Penetration depth ($D_{cp}$): angle of fracture zone, depends mainly on clearance
- Secondary shear: created if cracks do not run towards each other and material is sheared again

![Schematic of blanking tooling setup](image.png)

Figure 1.5 Schematic of blanking tooling setup [Wiedenmann, Sartkulvanich et al. 2009].
Hole expansion or hole flanging test is commonly used to evaluate the stretchability and edge cracking in sheet metal forming. The hole expansion test is demonstrated in Figure 1.7.

Figure 1.7 Schematics of hole expansion operation.[Wiedenmann, Sartkulvanich et al. 2009]
The expansion of the hole can be illustrated in Figure 1.8. Hole expansion ratio (HER) is defined as:

\[ \lambda = \frac{d - d_0}{d_0} \times 100\% \]

Figure 1.8 Schematic of initial and final diameter of the hole in hole expansion test.

Where \( d_0 \) is the initial diameter of the hole, \( d \) is the final diameter of the hole after expansion. A higher HER indicates better stretchability of the material or edge quality.

There are many factors that will affect the hole expansion ratio. Some important factors are summarized as:

1) Edge quality of the hole
2) Positioning of burr with respect to punch
3) Sheet material and thickness
4) The shape of the punch used for hole expansion
5) Die and blank holder geometry (die corner radius, die /blank holder diameter)

The traditional method to evaluate the sheared edge formability - forming limit diagram (FLD) – is partially effective in stamping of conventional high strength low alloy steels (HSLA). As advanced high strength steels (AHSS) have been increasingly utilized in the automotive industry worldwide due to their superior crash energy absorption ability and vehicle weight reduction.
potential, the evaluation of the edge formability of AHSS draws more attention. Traditional FLD is not sufficient to predict the potential failure in stamping AHSS since cracking occurs sooner than predicted by conventional FLD, [Konieczny and Henderson 2007]. Another drawback of FLD is the edge quality from blanking or trimming is not considered in forming limit curve (FLC) if finite element method is used for failure prediction.

<table>
<thead>
<tr>
<th></th>
<th>Punch velocity (Vp)</th>
<th>Not specified</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch diameter (dp)</td>
<td>10mm</td>
<td></td>
</tr>
<tr>
<td>Inside die diameter (dd)</td>
<td>Not specified</td>
<td></td>
</tr>
<tr>
<td>Die corner radius (R)</td>
<td>Not specified</td>
<td></td>
</tr>
<tr>
<td>Punch corner radius (rp)</td>
<td>Not specified</td>
<td></td>
</tr>
<tr>
<td>Diameter of the blankholder (db)</td>
<td>Not specified</td>
<td></td>
</tr>
<tr>
<td>Die clearance (C)</td>
<td>12±2 % (t&lt;2)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>12±1 % (t&gt;=2)</td>
<td></td>
</tr>
<tr>
<td>distance from hole to edge of the blank</td>
<td>&gt;=45mm</td>
<td></td>
</tr>
<tr>
<td>Blanking holder force</td>
<td>Not specified</td>
<td></td>
</tr>
</tbody>
</table>

|                | Punch velocity (Vp) | <= 1mm/s      |
| Punch geometry | 60° conical shape   |               |
| Punch diameter (Dp) | sufficiently large to crack the hole edge-not specified |               |
| Die corner radius (R) | 2mm < R < 20mm, recommended 5mm |               |
| Diameter of the blankholder (db) | >=40mm |               |
| Blanking holder force | large enough to prevent draw in |               |

Table 1.1 Summary of hole expansion standard [ISO/TS 16630]

Another issue of hole expansion test is the industrial “standard” test is not reliable enough to provide robust and repeatable test HER, due to the fact that the existing “standard” does not
provide all the test conditions. The standard hole expansion test from industry is summarized in Table 1.1.

It can be seen that the standard hole expansion test is based on a 10mm diameter hole. But many of the blanking conditions are not specified. It is known that the edge quality of the hole by blanking will change the HER significantly. Thus, the hole expansion ratio of one certain material has to be related to the quality of the edge or the blanking method.

Also, the 10mm hole size is extremely small compare to the real hole size in an actual part in industrial application. Blanking operation of a larger hole might change the hole edge quality and result in different HER. Another consideration is in blanking a very small hole (10mm), plenty of potential experimental errors, e.g. eccentricity of the punch, will jeopardize the accuracy of the test. Therefore, in order to obtain robust and reliable experimental results, the hole expansion test procedure has to be revised and re-designed if necessary, especially a large hole size is essentially required.
CHAPTER 2  Objectives

The overall objective of this study is to develop a fundamental understanding of the formability of the material in developing advanced metal forming process including bulk forging and sheet metal forming, in order to optimize manufacturing process to provide good quality parts, i.e. high mechanical properties and low residual stresses, tight tolerances, etc.

To achieve these objectives, it is necessary to:

Part I. FORGING

a) In producing precision valve forging:
   i. Understand the complete forging process of engine valves.
   ii. Investigate the fracture formation during forging and explore the factors that cause the initiation of the fracture.
   iii. Simulate fracture formation using Finite Element Method with the combination of fracture mechanics (fracture criteria).
      i. Provide solutions and modifications of the existing procedure to reduce the fracture.

b) In producing open die forging
   ii. Understand the fundamentals and design factors and the major concerns and common failure mechanics in open die forging
   iii. Evaluate various fracture criteria and select the suitable candidate to apply in hot open die forging in order to predict and eliminate fracture.
   iv. Understand the common forging die design modifications and process adjustment methods to optimize the open die forging process.

c) In producing bi-metallic forging:
i. Understand the difficulties and challenges in forging a bi-metal part/gear, compared to traditional forging.

ii. Validate the possibility of replacing the traditional forging with bi-metal forging for specific applications and weight savings.

v. Evaluate the feasibility of traditional manufacturing method to forge a bi-metal gear and re-design the forging operation specifically for bi-metal forging.

**Part II. SHEET METAL FORMING**

d) In blanking

i. Completely understand the physical nature of blanking operation and characters of a blanked part.

ii. Simulate the blanking operation with suitable Finite Element model and fracture criterion.

iii. Investigate the factors that influence the part quality/sheared edge quality in blanking Advanced High Strength Steel (AHSS).

iv. Optimize punch shape and other blanking conditions to provide blanked hole quality of AHSS and enhance tool life.

e) In hole flanging:

i. Understand the issues and problems of the existing industrial standard that cause the un-reliable experimental results.

ii. Simulate the hole flanging process and predict hole expansion ratio with a suitable selection of fracture criterion (empirical and theoretical).

iii. Explore the correlation of blanking and hole flanging and investigate the influence of blanking operation on hole expansion ratio.
iv. Design hole flanging tooling to provide robust experimental result in hole flanging test.

v. Develop a hole expansion test standard/guideline for industrial application.
CHAPTER 3  State-of-the-art Review

3.1 Fracture Criteria

Fracture in forging is a lengthy and difficult problem, which many researchers have spent decades try to solve. Ductile fracture of the material generally goes through a progressive deterioration process, namely void nucleation, growth and coalescence. A term - Formability (forming limit) of a metal - can be defined as its capability to undergo deformation by forging without cracking. Considerable fracture theories which based on different aspects, such as continuum mechanics, material grain boundary evolution, etc. were developed. Some of the fracture models are proved effective in cold forging operation. However in hot forging, the complex situation where strain rate and temperature effects are involved makes the fracture prediction extremely challenging. [Soyarslan, Tekkaya et al. 2008] roughly divided the fracture models into three categories, namely, fracture mechanics (FM), micro-based damage mechanics (MDM) and meso-based mechanics (CDM).

1) FM model: this model takes the integral of the plastic work over the strain history. Once the damage value reaches a critical value, which is the threshold of the material failure, fracture initiates. Example: [Freudenthal 1950; Cockcroft and Latham 1968; Rice and Tracey 1969; Brozzo, Deluca et al. 1972; Oyane 1972].

2) MDM model: this model are derived from analysis on isolated unit cells involving idealized defects such as cracks, voids, or second phase particles. Example:[Gurson 1977; Tveergaard and Needleman 1984; Rousselier 1987; Gologanu, Leblond et al. 1997; Nahshon and Hutchinson 2008].
3) CDM model: this model implies that at increasing level of material damage, the material resistance to deformation decreases, since the effective resistant area reduces [Bariani, 2012]. Example: [Lemaitre 1971; Ju 1989; Steinmann, Miche et al. 1994; Lammer and Tsakmakis 2000; Menzel and Steinmann 2001; Voyiadjis, Al-Rub et al. 2004; Brunig and Ricci 2005; Engelen 2005; Mediavilla, Peerlings et al. 2006]

Generally, many fracture criteria could predict ductile fracture initiation in cold working precisely. [CLIFT, HARTLEY et al. 1990; Kim, Yamanaka et al. 1995] made a comprehensive study by comparing different FM fracture criteria in predicting ductile fracture in cold working. [Gouveia, Rodrigues et al. 2000] used FM criteria to predict fracture in various applications such as radial extrusion and open die forging, and concluded that in tri-axial and tension-compression state of stress, the fracture can be predicted successfully.[de Souza Neto 2002; Andrade Pires, César de Sá et al. 2003; Gupta, Venkata Reddy et al. 2003; Mashayekhi and Ziaei-Rad 2006; Bouchard, Bourgeon et al. 2010; Haji Aboutalebi, Farzin et al. 2010] used either standard Lemaitre’s model or modified enhanced Lemaitre’s model to predict ductile fracture in different cold working applications.

The study in application of fracture criteria in hot working is relatively limited. [Semiatin, Goetz et al. 1999] conducted a study of the cavitation and failure during hot compression of Ti-6Al-4V. The Ti alloy cylinder was heated to different temperatures and forged to certain height reduction using two different ram speeds. The depth of crack and the cavitation was measured after cutting the samples. FE simulations were conducted with C&L and Rice&Tracey fracture model. The critical damage value of the material is determined using the hot compression test at elevated temperature. The conclusion is the fracture model could capture the location of the fracture precisely. Also, it could predict the depth of the cracking approximately. Figure 3.1 is an example
of the damage value calculation from FEM compared with critical damage values to predict fracture initiation.

Figure 3.1 Ductile fracture prediction using Cockroft and Latham criterion [S.L. Semiatin, R.L. Goetz et al. 1999]

[Ruf, Sommitsch et al. 2006] conducted a study to compare the capability of many fracture criteria using cylinder compression test and collar test. After determining the critical values of different criteria, the variation was compared for different models. An ideal fracture criterion should have the critical damage value only material dependent. In another word, the critical damage value should be a constant in different forging conditions. However, the measured critical values for all the damage model studies showed variation for different forging conditions. The C&L, method of effective stress and maximum principle stress/UTS exhibit a relative constant critical damage value, Figure 3.2.
Figure 3.2 Critical values of damage models for various testing conditions [Ruf, Sommitsch et al. 2006]

[Behrens and Just 2002; Behrens and Just 2002] used Lemaitre’s model to predict fracture in warm forming of complicate part such as journal bearings and hexagon socket screw. They proved that with enough material parameters measurements and proper calibration, method of effective stress could predict the fracture in hot forging with great accuracy. 

[Sommitsch, Pölt et al. 2006] used Lemaitre’s model combined with a semi-empirical grain structure model to predict the fracture initiation in hot compressing alloy 80A. With consideration of dynamic recrystallization of material during hot working, the fracture initiation zone predicted from FE simulation matches the experiments.

3.2 Open die forging technology

Open die forging could increase the part strength and fatigue life in following ways: a) improves microstructure and refines the coarse grain size; b) closes the internal pores and void and reduce the defects; c) produces continuous flow lines. Many research have been conducted in open die
forging area [SHIAU and KOBAYASHI 1988; Kiefer and Shah 1990; Brand, Kalz et al. 1996; Chuna, Yia et al. 2002; Neumann, Aretz et al. 2003; Barton, Li et al. 2007; Groche, Fritsche et al. 2007]

3.2.1 Effect of forging die (anvil) design and operation conditions

[Tamura and Tajima 2003] investigated the effect of the die geometry and feed length on the dimensional precision and grain size uniformity. In the analysis, equivalent plastic strain was adopted as the indicator to evaluate the quality of the forged part.

1) Effect of feed length: the feed length does not change the magnitude of the equivalent strain at the center of the part. However, larger feed length will result in more inhomogeneous strain distribution.

2) Effect of stroke: magnitude of the strain is proportional to the stroke depth. Larger stroke will generate higher plastic strain at the center of the part.

3) Effect of the arc/die corner radius (Figure 3.3): the smaller radius of the die corner, the higher strain values can be obtained at the center of the part. Also, smaller radius is desirable for uniformity of the strain distribution.

4) Effect of the temperature drop: the strain at the center of the part becomes larger if the temperature gradient along the radial direction is considered. In contrast, the strain at the surface decreases in the same condition.
3.2.2 Effect of the part cross section and forging direction

[Tamura and Tajima 2004] studied the effect of the part cross section geometry on the strain uniformity and then applied the result on developing a pass schedule. The study indicated the strain uniformity in one cross section area is considerably affected by the cross section geometry before it is forged. As a result, it is suggested that the part should be always forged along the diagonal direction instead of in the flat direction, Figure 3.4.

![Figure 3.4 Schematic of forging die (anvil) geometry [Tamura, 2004]](image)

3.2.3 Effect of operation parameters on round shape forging

[Choi, Chun et al. 2006] conducted a study to evaluate the controlling parameters to ensure the roundness of the bar after forging. Some conclusions from the study are: a) radius uniformity is affected by the height reduction, especially the stroke of previous step; b) the variation of the radius profile in longitudinal direction is influenced by the feed length. The larger the feed length
used, the radius shows more variation; c) Rotation angle is also a factor that affects the roundness of the bar. In pre-round forging, 90° is suggested. In finish forging, either 45° or 120° is recommended in order to obtain the better roundness with less pass sequence. One example is shown in Figure 3.5.

![Figure 3.5 Deformation shape with rotation angle](image)

Figure 3.5 Deformation shape with rotation angle: (a) 0° open; (b) 0° forging; (c) 120° open; (d) 120° forging

### 3.2.4 Effect of die shapes on metal flow

[Dudra and Im 1990] studied the effect of die shapes on the equivalent strain distribution in open die forging. The die shapes considered in the study include conventional flat die, V-shaped die with different angles, and FML (free of Mannesmann effect at lower press load) die with 98cm width top die. The conclusions are a pair of V-shaped die could generate best strain state.
However, it requires the highest press load. On the other hand, FML die could produce a good strain distribution with least press load.

3.2.5 Grain size prediction in open die forging

Grain size refinement is another important consideration for optimizing open die forging operation, which draws great attention of many researchers.

[Dandre, Walsh et al. 2000; Huang, Wu et al. 2000; Sommitsch and Wieser 2001] tested the capability of finite element simulations to estimate the grain size evolution using different software, considering dynamic recrystallization, metadynamic recrystallization, static recrystallization and grain growth.

[Recker, Franzke et al. 2010] introduced a method to speed up the FE simulation, which calculates the grain growth in a multiple passes by considering the grain size in the first stroke. Due to the fast calculation speed, the author claimed that an assistant system for open die forging processes could be established, which could predict the microstructural evolution and strain uniformity before or at the first few stages during the forging.

3.2.6 Pass schedule

Pass schedule is the controlling of the operation parameters, e.g. stroke, feed length, rotation angle, to obtain good quality of the part in open die forging. Many pass schedule model were developed based on empirical experience and without mathematical fundamentals. Until recently, some researchers started to investigate the entire open die forging process with mathematical methods.

[Kim, Chun et al. 2002] developed a pass schedule using functional neural network, on the basis of cross section area reduction and press capacity.

[Tamura and Tajima 2004] proposed a pass schedule optimization method based on the requirement of the uniform strain distribution.
[Recker, Franzke et al. 2011] developed a fast model for online optimization during open die forging. In his model, the equivalent strain in the forged part is assumed to be related to the global geometrical change, and then it can be approximated by simple equation. The temperature model is also expressed with simple function. The model was validated using FE simulations with good accuracy. Also, the grain size could be estimated using the similar concept. As a result, an online assistant system could be developed for the press operator. In this concept, instead of using the fixed die stroke and feed length, etc. the system could calculate the ultimate microstructure or other parameters at the first several steps of the forging. In case of deviations from the target parameters, the forging strategy can be adjusted and optimized online and thus a more accurate process in regards to the quality of the workpiece can be realized.

3.3 Bi-metallic Forging Technology

3.3.1 Open top and bottom billet design

[Behrens and Kosch 2011] proposed a similar concept of bi-metallic forging, Figure 3.6. In their billet design, the aluminum core was fitted into a steel casing, but the top and bottom were left open. Then the assembled billet was heated up in an induction coil. Since billet cannot be moved after heating due to the bottom is left open, the heating process took place on the press. Finally, the billet was forged between two flat dies. Figure 3.7 shows the procedure of the bi-metallic forging experiment [Behrens and Kosch 2011]. Notice in their billet assembly, Al core is a little bit higher than the steel casing. That is probably due to the buckling issue of the billet during forging. This problem will be discussed later. Figure 3.8 is the bi-metallic part before and after forging.
Figure 3.6 Billet preparation, assembly, heating and forging process of bi-metallic part. [Behrens and Kosch 2011]

Figure 3.7 Bi-metallic forging experiment by experiment. [Behrens and Kosch 2011]

Figure 3.8 Bi-metallic billet after assembly and after forging
3.3.2 Challenges in bi-metallic forging

In Behrens and his co-researchers’ study [Behrens and Kosch 2011; Behrens and Kosch 2011; Behrens, Bouguecha et al. 2012; Behrens, Kosch et al. 2012; Kosch and Behrens 2012], they pointed out a number of the technical challenges in bi-metallic forging. These challenges can be summarized as:

1) Heating of bi-metallic billet
2) Avoiding of shrinking gap
3) Producing strong Fe-Al intermetallic bonding
4) Preventing buckling of the steel wall

These challenges will be discussed in more detail in this section.

3.3.2.1 Heating strategy of bi-metallic billet

According to Behrens’ study, there are two major difficulties in heating the bi-metallic billet, differing from the traditional heating method:

1) The traditional heating strategy is to heat up the billet uniformly so that the forged material could have better metal flow. The general forging temperature of steel is about 1200°C. However, the melting temperature range of Al alloy is about 477-635°C. Thus, in order to prevent melting of Al, the billet has to be heated up in a way that the steel case is closed to forging temperature (1200°C) while keeping the Al in a low temperature to avoid its melting.

2) The thermal expansion of the Al is larger than (approximately twice) steel. After heating and forging, the forged part will be cooled down to room temperature from high forging temperature. Right after forging, the OD of Al and ID of steel will be the same. However during cooling, the Al will shrink more than steel. This will result in a shrinking gap between Al and steel at the interface. Such defects have to be avoided.
Induction heating is most common heating method used in industrial application. The physical principle of induction heating is that a time varying electro-magnetic field of a coil induces eddy currents and leads to the heating of the work piece due to the Joule effect. The heat concentrates at the surface of the billet which is called skin effect. Figure 3.9 illustrates the principle of the induction heating of bi-metallic billet [Behrens, 2012]. Such inhomogeneous heating strategy meets the requirement perfectly. Due to the skin effect of the induction heating, the energy will concentrate at the surface of the billet and heat up the outside first. Since the energy generated at the center of the billet is less, it will be heated up slowly. Such heating approach indicates that the steel, which is at the outside of the billet, could be heated up to a high temperature first. Al core, which will be heated up slower, could remain at lower temperature. The higher frequency used in the induction coil, the more energy concentration generated at the surface, which means the steel will be heated up faster.

**Figure 3.9 Principle of induction heating in bi-metallic forging.** [Behrens, 2012]
During heating, if the steel wall is heated to much higher temperature than Al core, the heat will flow from steel wall into Al core through conduction or radiation, until they reach a uniform state. Therefore, the heating process has to be the heating has to be finished in a short period of time so that the temperature gradient in the bi-metallic billet is retained. As a result, a high power induction heating device is requested.

3.3.2.2 Shrinking gap avoidance

Shrinking gap between Al core and steel casing is due to the coefficient of thermal expansion mismatch. Figure 3.10 plots the comparison of the CTE of Al 7075 and steel. In [Mitchell 2004], CTE of Al 7075 were provided till about solidus point (~475C). According to [Kaushish 2010], pure Al will expand about 6.6% volumetrically. Al alloy was assumed to have similar volumetric expansion during melting. Therefore, linear extrapolation was added to the CTE curve. [Narender 2013] measured the density of Al 7075 using Gamma Ray Attenuation Method. CTE of the material were calculated based on the density values in function of temperature. It shows clearly that CTE of Al alloy is much higher than common steel alloy.

In order to avoid the shrinking gap, steel should be heated up to high temperature while keep Al core in a low temperature. The simple empirical Equation 3.1 gives a rough estimation of the temperature of Al core and steel casing that could result in same amount of shrinkage after cooling, which means no shrinking gap. [Behrens, 2011] defined the temperature process window plotted in Figure 3.11.

\[
\frac{d_{0,\text{st}} \cdot \alpha_{\text{st}}(T_{\text{st}}) \cdot (T_{1,\text{st}} - T_{0,\text{st}})}{d_{0,\text{Al}} \cdot \alpha_{\text{Al}}(T_{\text{Al}}) \cdot (T_{1,\text{Al}} - T_{0,\text{Al}})} = 1 \quad \text{Equation 3.1}
\]

However in this assumption, the temperature of Al and steel is the initial temperature right after induction heating. During billet transfer, forging and cooling, there will be heat transfer between Al and steel, and the steep temperature gradient tends to reach to a more uniform state. As a
result, shrinking gap may not be disappeared as we expected here even though the calculated temperature distribution could be achieved.

Figure 3.10 Comparison of coefficient of thermal expansion of Al 7075 and steel alloy

![Coefficient of thermal expansion comparison](image)

Figure 3.11 Temperature window to avoid shrinking gaps [Behrens, 2011]

![Temperature window diagram](image)

Probably due to the limitation of the facility, Behrens, etc. proposed an alternative method to achieve the required temperature distribution in the billet, instead of increasing the frequency and
power of the induction heater. The idea is to leave an air gap between Al and steel, without any direct contact, to prevent heat conduction between the two materials, Figure 3.12 [Behrens, 2011]. Since heat radiation is much slower than conduction, Al core could remain at much lower temperature for longer period of time. Figure 3.13 shows the time frame that the two materials reach the uniform temperature state. The temperature gradient between Al and steel will disappear and they will cool down together within less than 20s without air gap. However, if the air gap is added, not only a steeper temperature gradient can be obtained, the temperature difference can keep for much longer time. The air gap method can help the heating process to fit in the temperature window in Figure 3.11, and it also provide longer processing time for billet transfer and forging.

Figure 3.12 The air gap idea to improve heating process [Behrens, 2011]

Figure 3.13 Influence of the air gap on the temperature gradient [Behrens, 2011]
3.3.2.3 Formation and the properties of the intermetallic layer

During the hot forging the bi-metallic billet, Al will touch the hot steel casing and the surface of the Al core will melt. As a result, an intermetallic phase of type of Fe$_x$Al$_y$ will be formed at the Al-steel interface. Generally, such type of compound shows high hardness and brittle property, which is easily to crack [Bouche and Barbier 1998; Agudo and Jank 2008]. Dilthey and Yamamoto show that intermetallic phases with a phase seam thickness of 5–10 mm cause the strength of the connection to decrease [Dilthey, Brandenburg et al. 2005; Yamamoto and Takahashi 2007]. At thicknesses below 3mm, the intermetallic phases have the same strength as the aluminum material [Dilthey, Brandenburg et al. 2005].

Behrens, etc. summarized the factors that affect the forming of the intermetallic phases as follows:

1) Material preparation method
2) Processing parameters
3) Strain level (true strain)
4) Alloying elements

3.3.2.4 Buckling issue of the steel wall

Buckling is an interesting phenomenon in bi-metallic forging that will not occur in traditional forging practice. During upsetting of the assembled billet in axial direction, the two materials may not deform uniformly. In some cases the tube material may bulge out in larger amount and then gets separated from the core material; in other cases, the tube material may buckle instead of bulging from the center.

[Essa, Kacmarcik et al. 2012] studied the factors that could affect the buckling by conducting bi-metallic upsetting tests. The parametric study includes the billet height, OD/ID ratio, friction and the inner/outer material. Both forging test and finite element simulations were conducted to
compare the results. Figure 3.14 is an illustration of the billet geometries that had been studied. Table 3.1 is the summary of the parametric study. Figure 3.15 is the demonstration of the FEM simulations. It can be concluded that the separation or buckling is pure geometrical relation due to the non-uniform deformation. It is also confirmed by switching the inner and outer material, which gives an almost identical result. The friction at the interface does not have a great influence on the buckling formation. By carefully designing the billet geometry, especially the OD/ID ratio, the bi-metallic billet could deform uniformly as a piece of material.

![Diagrams of billet geometries](image)

Figure 3.14 Initial (a) and final (b) dimensions of a billet.

<table>
<thead>
<tr>
<th>Model (equivalent exp.)</th>
<th>Height, H (mm)</th>
<th>Outer ring dia., D_o (mm)</th>
<th>Inner ring dia., D_i (mm)</th>
<th>H_i/D_i</th>
<th>D_o/D_i</th>
<th>FE contact length, l_c (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>60</td>
<td>40</td>
<td>15</td>
<td>1.50</td>
<td>0.4</td>
<td>15.7^e Outer C15E</td>
</tr>
<tr>
<td>B</td>
<td>60</td>
<td>40</td>
<td>20</td>
<td>1.50</td>
<td>0.5</td>
<td>13.8 Outer C15E</td>
</tr>
<tr>
<td>C (1b)</td>
<td>60</td>
<td>40</td>
<td>24</td>
<td>1.50</td>
<td>0.6</td>
<td>9.46 Outer C15E</td>
</tr>
<tr>
<td>D</td>
<td>60</td>
<td>40</td>
<td>28</td>
<td>1.50</td>
<td>0.7</td>
<td>7.6 Outer C15E</td>
</tr>
<tr>
<td>E</td>
<td>60</td>
<td>40</td>
<td>32</td>
<td>1.50</td>
<td>0.8</td>
<td>4.1 Outer C15E</td>
</tr>
<tr>
<td>F (2)</td>
<td>40</td>
<td>40</td>
<td>24</td>
<td>1.00</td>
<td>0.6</td>
<td>7.3 Outer C15E</td>
</tr>
<tr>
<td>G (3)</td>
<td>14</td>
<td>40</td>
<td>24</td>
<td>0.35</td>
<td>0.6</td>
<td>3.7^e Outer C15E</td>
</tr>
<tr>
<td>H</td>
<td>20</td>
<td>40</td>
<td>24</td>
<td>0.50</td>
<td>0.6</td>
<td>5.2^e Outer C15E</td>
</tr>
</tbody>
</table>

Table 3.1 Parametric study of the influence of the billet geometry

[Behrens and Kosch 2013] also studied this effect by conducting both bi-metallic forging and hollow tube forging at high temperature. Two parameters, namely, buckle ratio and buckling
distance were defined in order to describe the buckling behavior quantitatively, Figure 3.16. The
effect of billet geometry, forging temperature and strain rate were studied. The conclusions can be
summarized as:

1) Billet geometry has a great influence on the buckling occurrence, which corresponds to
Essa’s study.

2) Forming temperature has a great impact on the buckling formation. This is because if
forging the bi-metallic billet at high temperature, the different forming temperature may
result one of the material too soft or too hard. In that case, buckling could be prevented or
triggered. Figure 3.17 shows the effect of both temperature and wall thickness. At
different forming temperature, 2mm wall billet never shows buckling. However, 1mm
billet has unstable deformation and buckles in a different way as temperature varies.

3) Strain rate has no influence on buckling.

Figure 3.15 Finite Element simulations of the forging billet.
3.3.3 Material preparation method

Three different types of sample preparation methods were examined by Behrens, etc., namely, untreated, ultrasonic bath and metal blasted followed by ultrasonic bath. With the same material combination and processing parameters, the results are shown in Figure 3.18 to Figure 3.20. The conclusion is the ultrasonic bath has a positive effect on the joining of two materials. However, the surface cleaning method is not the major influence on forming the intermetallic joining.
Figure 3.18 Joining zone of untreated samples [Behrens, 2011]

Figure 3.19 Joining zone of ultrasonic bath cleaned samples [Behrens, 2011]
3.3.4 Process parameters

The process parameters studied by Behrens are the forging temperature of Al core and the strain rate. Two Al temperatures (450°C and 600°C) are selected and strain rates (0.11/s and 30.1/s) were used in the tests. The results can be seen in Figure 3.21. It is concluded that high forming temperature is beneficial in forming of intermetallic bonding due to the high diffusion rate and also the better contact if Al melts. High strain rate also shows a positive effect since the forging process is completed in a short period of time, otherwise the extremely brittle layer would become too thick and would like to initiate cracking.
Figure 3.21 The effect of different process parameters on the forming of phase seam thickness [Behrens, 2011]

Strain level (true train)

The reason for investigating the strain level at joining zone is to crack the aluminum oxide layer during forming, suggested by Behrens, since the thermal stable property of oxide layer will prevent the direct contact between the two base materials. With increasing the height of the Al core, the true strain level at the interface increases. The results are shown in Figure 3.22. By
observing the 3 pictures, it clearly indicates that a higher level of true strain help to form a firm 
 bonding between the two materials.

![Image](image_url) 

Figure 3.22 The effect of strain levels at Al-Fe interface [Behrens, 2011]

3.3.5 Alloying elements

The influence of the different alloying elements of the base material on forming of intermetallic 
phase was also studied by Behrens. The high hardness intermetallic phase Fe$_x$Al$_y$ is very brittle 
and likely to initiate cracking. Therefore, the phase seam thickness should be as thin as possible.

Table 3.2 is the different material combinations that have been studied. Although it is suggested 
that the phase seam thickness is greatly influenced by alloying elements magnesium and silicon, 
the exact effect how there two elements will affect the phase seam thickness is still unclear.

<table>
<thead>
<tr>
<th>Material combination</th>
<th>Mg [w-%]</th>
<th>Si [w-%]</th>
<th>Hardness [HV/0.01]</th>
<th>Intermetallic phase</th>
<th>Phase seam thickness [µm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>S235JR/AW6082</td>
<td>0.58</td>
<td>2.8</td>
<td>730</td>
<td>Fe$_3$Al$_7$</td>
<td>7–12</td>
</tr>
<tr>
<td>S235JR/AW5754</td>
<td>0.49</td>
<td>0</td>
<td>820</td>
<td>Fe$_2$Al$_5$/FeAl$_3$</td>
<td>10–14</td>
</tr>
<tr>
<td>X-4CrNi8-10/AW6082</td>
<td>0.28</td>
<td>2.25</td>
<td>511</td>
<td>FeAl</td>
<td>15–25</td>
</tr>
<tr>
<td>X-4CrNi8-10/AW5754</td>
<td>0.81</td>
<td>0.47</td>
<td>1,100</td>
<td>FeAl$_2$/Fe$_2$Al$_5$</td>
<td>5–10</td>
</tr>
</tbody>
</table>

Table 3.2 The influence of magnesium and silicon on the phase seam thickness [Behrens, 2011]
3.4 Research Development in Blanking Technology

The objective of the literature review was to gain knowledge in the blanking process, blanking simulations and also look into other studies involving improving hole flangability. [Subramonian, 2013] conducted a comprehensive state-of-the-art review on blanking. The author has compiled and analyzed information on the fundamentals in blanking including effect of various process parameters on blanked edge quality, material models and fracture models used in simulations. [Maeno et al., 2013] conducted a study on semi-punching in hot stamping. In this process, the punch penetrates the sheet only about half the thickness. The authors developed semi-punching process by designing the hot stamping die to simultaneously form as well as partly punch the heated sheet. Later, at room temperature, the blanking is completed (Cold scrap removal). The authors concluded that the load requirement for semi-punching during hot stamping and then cold scrap removal is lesser than the load requirement for cold punching only. This process also improved the quality of the sheared edge according to the authors. [Osakada et al., 2011] suggest using a servo-press for piercing operations to obtain a long smooth shear surface and prevent fracture.

A lot of studies have been conducted on improving the simulation conditions of blanking operations. The procedure for a methodology of simulations conducted to predict edge cracking has been outlined in [Wiedenmann et al., 2009]. This report also details how fracture criterion is used to simulate shear. [Sartkulvanich et al., 2010] have evaluated various ductile fracture criteria in the blanking process and have concluded that the adapted Rice & Tracey’s criterion is the best criterion for modeling of blanking. [Goijaerts et al., 2001] have shown how the adapted Rice & Tracey criterion can be implemented in blanking simulations. [Husson et. al, 2008] have proposed a new viscoelastic hardening model and a new damage model to assess the influence of punch/die clearance, tool wear and friction on sheared edge
quality by comparing simulations with experiments. The authors conclude that friction is not important whereas punch/die clearance and tool wear play a significant role in having a good edge quality. Also, as punch/die clearance increases, the influence of tool wear increases. [Chiriac, 2010] studied the plastic deformation of sheared edge of DP 980 steels and showed that the hole expansion ratio (HER) represents an average value of the major strains along the hole edge by conducting a surface strain analysis.

There have also been studies to improve the edge quality by various methods. [Hance et. al, 2013] have studied the influence of edge preparation method on the hole expansion performance of automotive steel sheets and have observed laser cutting to have a productivity advantage over water-jet cutting. [Chiriac et. al., 2012] studied the effect of pre-strain on the sheared edge flangability of DP 780 steels and have concluded that uniaxial or biaxial prestraining before hole punching does not change the sheared edge flanging limit. [Mori et al., 2011] have developed a smoothing process of rough fracture surface of a sheared edge with a conical punch to improve the edge quality and thus the stretch flangability of ultra-high strength steels. They noted that the hole expansion ratio was improved by the smoothing even though the hardness at the edge increased due to smoothing.

There have been studies on the effect of punch geometry on edge quality/flangability as well. [Mori et al., 2013] have studied the effect of a gradually contacting punch on stretch flangability of UHSS and have concluded that the gradually contacting punch is effective in improving the stretch flangibility. [Takahashi et al., 2013] have suggested using a humped bottom punch to improve the stretch flangability of high tensile strength steel sheets.

3.5 Hole Flanging Technology

[Konieczny and Henderson, 2007] conducted a study to investigate the effect of blanking operation, sheared edge quality of AHSS on the HER, with 10mm hole size. Die clearance in
blanking has great influence on the rollover zone length (rollover depth) and shear zone length (burnish depth). Figure 3.23 gives an example of the burnish depth vs die clearance for different materials. The hole expansion tests with different blanking method shows that burr orientation, die clearance and punch shape will affect the HER of the material. Figure 3.24 shows an example of the edge quality on HER for different materials. The study concluded that AHSS exhibits rather low HER compared with HSLA. Also, edge quality has a great influence on the HER. Reaming and laser cutting produce better edge quality and yield high hole expansion ratio in forming AHSS. However, the conventional blanking would lead to significantly lower edge stretchability.

Figure 3.23 Effect of die clearance on burnish depth (shear zone length). [Konieczny and Henderson, 2007]
Figure 3.24 Impact of die clearance and sheared edge finishing on HER values for different materials, (burr up and conical punch) [Konieczny and Henderson, 2007]

[Levy and Tyne 2008] studied the failure of sheared edge during stretching the both HSLA and AHSS using forming limit curves. They concluded that the strain path in the sheared affected zone adjacent to the sheared edge has a dominant effect when establishing the forming limit.

[Kim, Lee et al. 2010] conducted a qualitative formability analysis for dual phase (DP) steels by employing the realistic microstructure-based finite element (FE) model. It was found that the localized plastic deformation in the ferritic phase may be closely related to the macroscopic formability of DP steels.

[Huang and Chien 2001] established the limitation of formability of low carbon steel by detecting the thinning of the material at the hole edge. The cracking starts when the thickness of the material at the hole edge goes below the critical fracture thickness.

[Sartkulvanich, et al, 2010] established a methodology to predict the HER based on the sheared edge quality using finite element analysis. Rice&Tracey fracture criterion was selected to predict
the fracture in both blanking and hole expansion simulations based on the lowest deviation of critical damage values. Blanking simulation was first conducted to estimate the sheared zone quality and the effect of different die clearance. Then in the hole expansion simulation, the pre-strain and damage values from blanking simulation was carried to the new simulation. In such way, the effect of shear edge quality is also considered in the hole expansion simulation. Figure 3.25 shows an example of FE simulations of HER of DP590 with spherical punch.

![HER prediction using FE considering the sheared edge quality](image)

Figure 3.25 HER prediction using FE considering the sheared edge quality [Sartkulvanich, et al, 2010].
CHAPTER 4 Investigation of Fracture in Hot Forging of Engine Valves

4.1 Description of the problem

The precision forging of engine valves is a well-established procedure that has been utilized by many manufacturers. A common manufacturing procedure includes the following steps: 1) billet shearing and heating; 2) extrusion to obtain an “onion” shape preform; 3) coining to form the valve head. The schematic of common valve forging process has been discussed in section 1.1. In order to improve the high temperature corrosion resistance and increase the part life in the severe operating environment of the combustion engine, high Chrome-Nickel based stainless steel 23-8N is introduced to replace the previous 21-2N as the valve material. However, due to the high percentage of the Chrome and Nickel addition, the formability of the steel at elevated temperature decreases. As a result, the material is likely to fracture during the forging operation. Experiment has shown that if the new material (23-8N) is processed with the previous die set and procedure, which is designed to form 21-2N, the material is likely to crack during coining. Fracture is observed at the “blade” (periphery) area, at the top of the valve head, as indicated in Figure 4.1. Another experimental observation is, if the billet is extruded in the extrusion die with a larger diameter (Figure 4.2) and then coined in the same coining die, cracking can be reduced.
4.2 Introduction of the Valve Forging Process

4.2.1 Tooling and forging procedure

The forging process in one of our sponsor’s plant is introduced below. Both the extrusion and coining operation take place on the same press. The induction heater is adjacent to the press. The
billet is transferred from the induction heater to the extrusion station by an automatic feed mechanism. During extrusion, the valve stem is extruded. This intermediate form (onion form) is lifted out of the extrusion assembly by the turret arm. The turret then transfers the part to the coining die. After the coining operation, the forged valve is lifted by the turret arm and moved out of the press. The forging tooling schematic is as shown in Figure 4.3. The schematic of the overall press operation is seen in Figure 4.4. In this specific plant, to maintain close thickness tolerances on the coined valve head, coining operation is done using “kissing” surfaces, as seen in Figure 4.3 (right). There is also “no” or very little flash so that the coining operation uses basically a trapped die.
Figure 4.3 Forging tooling schematic

Extrusion Die assembly

- EP: Extrusion Punch
- ED: Extrusion Die
- EB: Extrusion Bushing
- EDH: Extrusion Die Holder
- SGH_E: Stem Guide Holder (Extrusion)
- GB_E: Guide Bushing
- SGI: Stem Guide Insert
- IH: Insert Holder

Coining Die Assembly

- CP: Coining Punch
- CD: Coining Die
- CDH: Coining Die Holder
- GB_C: Guide Bushing (Coining)
- SGH_C: Stem Guide Holder (Coining)
4.2.2 Forging press and forging cycles

The forging presses used in our sponsor’s plant are AJAX mechanical presses. The presses have a capacity of 700 tons, length of stroke is 10 inches, connecting rod length of is 25 inches and an idle press speed of 80 RPM. (With these values it is possible to calculate the exact velocity of the ram during the stroke, i.e. for extrusion or coining.)

Both extrusion and coining die assemblies are mounted on a single press. The distance between the extrusion and coining die centers depends on the feeding mechanism. For automatic feed presses it is 12 inches and manual feed presses have 7 inches. The presses operate by intermittent
mode in which the press speed is in the range of 60 to 90 strokes per minute. The press ram movement is interrupted (the ram stops at the Top Dead Center (TDC)) after each stroke for 15 removal of the forged part from the die). There are load cells attached on the press frame to measure the total press load (extrusion + coining).

4.2.2.1 Process cycle – Extrusion

![Diagram of extrusion process sequence](image)

Figure 4.5 Valve extrusion process sequence

The extrusion process sequence is illustrated in Figure 4.5 and consists of:

- **Billet Transfer** ($t_a - t_0$): The time for which the billet is exposed to the environment, i.e. from the time it exits the induction heater until it is placed inside the extrusion bushing.
- **Clutch activation time** ($t_b - t_a$): It is the time taken by the press driving mechanism to start the ram movement.
• Forging Stroke \((t_e - t_b)\): It is the time taken by the press to move from the Top Dead Center (TDC) to Bottom Dead Center (BDC) and back to Top Dead Center.

• Extrusion/Deformation time \((t_2 - t_1)\): It is the time from when the punch touches the billet until it reaches BDC. The deformation time is determined from press kinematics. 16

• Knockout \((t_d - t_e)\): The knockout punch starts to move upwards to push the forged part out of the extrusion die. The knockout starts 40 ms after the ram starts to move upwards from BDC.

• Part removal \((t_f - t_d)\): The time required for the turret to transfer the extruded part from the extrusion die to the coining die.

• Lubrication \((t_g - t_f)\): The time during which the lubricant is pumped into the die. The lubrication, in the extrusion process, is applied by pumping (flooding) the die with a thick oil based lubricant through the lube ring that is permanently fixed to the top of the extrusion die assembly.

• Post Lubrication dwell \((t_h - t_g)\): It is the time from when the lubricant pumping is stopped until the start of the next forging cycle.

• The total cycle time for extrusion is \((t_h - t_a)\).

4.2.2.2 Process cycle – coining

The process sequence for the coining operation (Figure 4.6) is similar to that for extrusion, although there are some differences in the lubrication. The coining process consists of:

• Part location from extrusion to coining die \((t_a - t_0)\): The time for which the billet is exposed to environment, i.e. from the time the knockout of the extruded part starts until it is finally placed on the coining die.
• Post lube air blow (t_b – t_a): The air is blown (after lubrication of previous cycle) onto the surface of the coining die and punch from the nozzles located on the periphery of the coining tooling.

• Clutch activation time (t_c – t_b): It is the time taken by the press driving mechanism to start the ram movement.

• Forging Stroke (t_e – t_c): It is the time taken by the press to move from the Top Dead Center (TDC) to Bottom Dead Center (BDC) and back to TDC.

• Coining/Deformation time (t_2 – t_1): It is time from when the punch touches the extruded preform until it reaches the BDC. The deformation time is determined from press kinematics.

• Knockout (t_e – t_d): The knockout punch starts to move upwards to push the forged part out of the coining die. The knockout starts 40 ms after the ram starts to move upwards from BDC.

• Part removal (t_g – t_e): The time required for the turret to transfer the forged valve out of the coining die.

• Pre Lube Air Blow (t_h – t_g): Air is blown onto the coining punch and die surface from the nozzles.

• Lube Spray (t_i – t_h): The lubricant is sprayed onto the coining die and punch from the nozzles.

• The total cycle time for coining is (t_i – t_a). (which is same for Extrusion)
4.3 Methodology

4.3.1 Fracture Criterion

In order to understand the fracture formation, the forging process of both extrusion and coining needs to be simulated using finite element method to emulate the material behavior. A suitable fracture criterion has to be selected to predict the cracking during hot forging. According to [Ruf, Sommitsch et al. 2006], Cockcroft and Latham, maximum principle stress/UTS and method of effective stress could be the suitable fracture models to emulate hot forging process. Typically, method of effective stress requires certain calibration procedure to determine the material’s constant. Thus, the method of effective stress will be investigated in the future work. In current study, the other two fracture criteria - Cockcroft and Latham, maximum principle stress/UTS were utilized to estimate the fracture formation.

Maximum principle stress/UTS criterion:
\[ C = \frac{\sigma_{\max}^*}{UTS(T, \dot{\varepsilon})} \]  
Equation 4.1

Where \( C \) = instantaneous damage value

\( \sigma_{\max}^* \) = maximum principle stress

\( UTS \) = ultimate tensile strength

\( T \) = temperature

\( \dot{\varepsilon} \) = strain rate

Cockcroft and Latham criterion:

\[ C = \int_0^{\bar{\varepsilon}} \left( \frac{\sigma_{\max}^*}{\bar{\sigma}} \right) d\bar{\varepsilon} \]  
Equation 4.2

Where \( C \) = instantaneous damage value

\( \sigma_{\max}^* \) = maximum principle stress

\( \bar{\sigma} \) = effective stress

\( \bar{\varepsilon} \) = effective strain

In both of these criteria, a damage value \( C \) is defined, which is an instantaneous value that changes with stress-strain state. A larger damage value indicates the material is more likely to crack. If we can find a critical damage value \( C_{\text{critical}} \) for the specific material and forging conditions by experiment, we can predict the moment when cracking occurs, i.e. once the instantaneous damage value is larger than the critical value, the material will crack. Typically, the exact critical damage value is very difficult to determine.

4.3.2 Material properties

Table 4.1 shows the material’s properties of 23-8N obtained from Eaton Corp. The forging temperature of 23-8N is 1120°C - 1170°C. The highest temperature available from the data sheet is 870°C. To estimate the material’s behavior at elevated temperature till about 1200°C, the
properties (thermal expansion, thermal conductivity, UTS, yield stress, etc.) have to be extrapolated from the available data.

To conduct the finite element simulation, the flow stress of the material at elevated temperature (or at least within the forging temperature range) has to be input to the FE software. However, these data are not available for 23-8N. To estimate the flow stress of this material, another stainless steel, 21-2N, whose flow stress data and other properties are available in the commercial software DEFORM database, was used as a reference. The chemical composition of 23-8N and 21-2N are similar, while 23-8N has slightly higher percentage of Nickel (8%) and Chromium (23%), compared to 21-2N (2% Nickel and 21% Chromium). A higher percentage of Nickel and Chromium composition in 23-8N will result in a higher ultimate tensile strength and flow stress. The ultimate tensile strength of both materials plotted in Figure 4.7 illustrates this assumption. It can be seen from the plot that when the temperature is higher 600°C, the ratio of the UTS (23-8N to 21-2N) is almost constant (≈1.2). Since the forging process is conducted at a much higher temperature (≈1150°C), the small variation at lower temperature can be ignored. We then assumed that the flow stress of two materials follows the same ratio at elevated temperature, that is the flow stress of 23-8N is 1.2 times larger than 21-2N. Since the flow stress of 21-2N is

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Thermal Expansion (ppm/°C)</th>
<th>Thermal Conductivity (W/cm K)</th>
<th>UTS (MPa)</th>
<th>0.2% YS (MPa)</th>
<th>Elongation (%)</th>
<th>RA (%)</th>
<th>Elastic Modulus (GPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>RT</td>
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<td>0.1302</td>
<td>959</td>
<td>358</td>
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<td>20</td>
<td>206</td>
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<td>430</td>
<td>17.6</td>
<td>0.1955</td>
<td>696.5</td>
<td>324</td>
<td>27</td>
<td>27</td>
<td>153</td>
</tr>
<tr>
<td>540</td>
<td>18</td>
<td>0.2142</td>
<td>629</td>
<td>290</td>
<td>21</td>
<td>28</td>
<td>144</td>
</tr>
<tr>
<td>650</td>
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<td>0.237</td>
<td>545</td>
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<td>33</td>
<td>38</td>
<td>153</td>
</tr>
<tr>
<td>760</td>
<td>18.8</td>
<td>0.2501</td>
<td>405</td>
<td>239</td>
<td>36</td>
<td>44</td>
<td>144</td>
</tr>
<tr>
<td>870</td>
<td>19.2</td>
<td>0.228</td>
<td>159</td>
<td>26</td>
<td>71</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 4.1 Material Properties of 23-8N (Eaton Corp)
available, flow stress of 23-8N at elevated temperature can be estimated. Figure 4.8 plots the flow stress of 21-2N and 23-8N at 1037°C as an example.

Figure 4.7 Ultimate tensile strength of 23-8N and 21-2N at elevated temperature
Figure 4.8 Flow stress of 23-8N and 21-2N at 1037°C, the strain rate is 2.5s⁻¹

4.3.3 Finite Element Simulation Procedure

Finite element method was used to simulate the extrusion operation with both small and large extrusion dies and the following coining operation. The simulations were conducted with a commercial FE code DEFORM2D. Table 4.2 is the summary of the important simulation parameters used in this study.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Billet material</td>
<td>23-8N</td>
</tr>
<tr>
<td>Flow stress</td>
<td>DEFORM database with modification</td>
</tr>
<tr>
<td>Billet temperature</td>
<td>1150°C</td>
</tr>
<tr>
<td>Heat transfer coefficient</td>
<td>15kW/m²K</td>
</tr>
<tr>
<td>friction (shear factor)</td>
<td>0.3</td>
</tr>
</tbody>
</table>

Table 4.2 Simulation parameters
After extrusion simulation, the “onion” shaped valve preform from the large die has a larger diameter and smaller thickness “head”, while the one from the smaller die has a smaller diameter “head” but the thickness is larger. Then in the coining simulation, the two different shaped valve preform were forged in the same coining die. The shape of the extruded and coined part was illustrated in Figure 4.9.

Figure 4.9 FE simulation procedure of the extrusion and coining operation

4.4 FE simulation Results and Discussion

It is observed from the fractured samples (Figure 4.1) that the cracks only originated from the top surface of the valve head. This indicates that the fracture initiated at the very late stage of the forging operation, when the stroke is near the bottom dead center (BDC) and before the top and bottom die touches each other. In the precision forging die design, flash is added to prevent the extremely high die stress concentration. Thus, the maximum damage values just before the flash
is formed (assumed at such moment fracture initiates) were extracted from the simulations and plotted in Figure 4.10 and Figure 4.11.

Figure 4.10 Maximum damage values with maximum principle stress/UTS criterion: a) smaller extrusion die; b) larger extrusion die.

Figure 4.11 Maximum damage values with Cockcroft and Latham (C&L) criterion: a) smaller extrusion die; b) larger extrusion die.
It can be seen from the results that both fracture criteria predicted the maximum damage value occurs at the top surface of the part, which matches the experimental observation very well. However, C&L criterion predicted another location which has the same damage level. This results agree with the study conducted by [Semiatin, Goetz et al. 1999], which indicates that the maximum damage occurs at the maximum bulge radius area. This is because C&L criterion calculates the integral of the plastic deformation over strain history. The maximum bulge radius area is generally another area where strain is the maximum. By carefully observing the fractured samples, it is noticed that the fracture is aligned along the radial direction of the valve head. This is the evidence that the fracture is formed due to the tensile stress in circumferential direction. So, the maximum principle stress and the hoop stress distribution were also plotted for both cases, Figure 4.12 and Figure 4.13. It shows that the maximum principle stress and hoop stress at top surface are much larger than the side of the part. This indicates that the part will crack at the top surface of the valve.

Figure 4.12 Maximum principle stress distribution: a) smaller extrusion die; b) larger extrusion die (Die stroke: 253.8mm/Total stroke:254mm)
Figure 4.13 Hoop stress distribution: a) smaller extrusion die; b) larger extrusion die (Die stroke: 253.8mm/Total stroke:254mm)

The strain rate comparison in Figure 4.9 also supports the damage prediction. Since the punch speed is almost Zero when it gets close to the BDC, the strain rate distribution is very similar for the two cases. In both cases, the maximum strain rate occurs at two locations: the top surface and the side of the part, which matches the maximum damage area predicted by C&L criterion. This is because in hot forging, the flow stress of the material is sensitive to strain rate. The flow stress value will be higher if the material undergoes higher strain rate, resulting in higher damage values.

By comparing the results in Figure 4.10 and Figure 4.11, both fracture criteria calculated smaller damage values when a larger extrusion die is used, which means the material is less likely to crack. In another word, both fracture criteria have a good estimation of the fracture initiation potential in hot forging of engine valves.

Figure 4.14 plotted the maximum damage values predicted from FEM with different fracture models. It can be seen that the maximum principle stress/UTS criterion estimated 4.5% decrease in the damage value calculation when a large extrusion die is used. Considering the simulation
errors, the extrapolation of the UTS values at forging temperature (as explained in Section 3.2) and some other variables in the actual test, such trivial difference is not a strong argument to back up that the fracture is less likely to occur. So in the next section, we selected C&L criterion as our primary model to emulate fracture behavior.

![Bar Chart](image1)

**Figure 4.14** Maximum damage values predicted with two different fracture criteria

### 4.5 Modified Punch Design for Fracture Reduction

To further reduce the risk of cracking, some modified extrusion punch shapes were proposed. One solution is to add a convex bump at the top of the extrusion punch. The protrusion can be either like Figure 4.15a or Figure 4.15b, which were named as “convex” punch or “half convex” punch. The extruded preforms with the punch shape are shown in Figure 4.16a and Figure 4.16b. The idea behind this is to further reduce the material’s deformation during the coining process to lower the cracking risk, since the “onion” head is forged into a shape closer to the end shape of the valve head. Another proposed idea is to use a half concave extrusion punch (Figure 4.15c),
and the shape of the extruded preform is shown in Figure 4.16c. The reason for this design is that at the starting point of the coining operation, the coining punch will first touch the apex of the preform head. Then the material in the head area will be forced to flow toward the outside to increase diameter by itself, instead of squeezed by the coining punch.

Figure 4.15 Schematic of the modified punch shapes a) convex punch; b) half convex punch; c) concave punch

Figure 4.16 The shape of the preforms extruded with modified punches: a) convex punch; b) half convex punch; c) concave punch
Extrusion simulation with the modified punch shapes were conducted for both larger extrusion die and smaller extrusion die. The actual punch diameter was adjusted according to the diameter of the extrusion die. Then, the extruded preform with different head shapes were coined in the same coining die. The maximum damage values with C&L criterion were plotted in Figure 4.17. Some conclusions can be made by comparing the results from FE simulations.

1) The larger extrusion die will still result in smaller damage values in average, which means the part is less likely to crack if extrude with larger die. Therefore, a larger extrusion die is the optimized option to avoid cracking.

2) Convex punch actually increases the damage values, which indicates the part is more likely to crack. The sharp corner of the preform head might contribute to the damage increase.

3) Half convex punch is the optimized punch shape to lower the cracking risk. The maximum damage value it produces with smaller extrusion die is less than that with flat punch. In the case of larger extrusion die, the damage values are the same with flat and half convex punch. This is because the diameter of the die has more significant influence on the material flow than the punch shape. Also, the profile of the punch shape is not optimized. More iteration steps are necessary in order to optimize the punch convex profile to reduce the damage risk further.

4) For the concave punch concept, the damage values actually increased, which is the opposite result of our assumption. This indicates that the part extruded with concave punch design will be in more danger to fracture.
Figure 4.17 Maximum damage values with different punch designs

4.6 Conclusions

1) The valve cracking phenomenon can be estimated using finite element method with the help of suitable fracture criterion. Based on the literature search, two fracture criteria, maximum principle stress/UTS and C&L were selected to estimate the fracture in hot forging.

2) Both fracture criteria predicted a decreased damage value when a larger diameter extrusion die is used. The results agree with the experimental observation that a larger extrusion die could reduce the fracture of the material.

3) Both fracture criteria predicted the correct fracture location. However for C&L criterion, the maximum principle stress or hoop stress distribution is also required to estimate the fracture location.
4) Due to the trivial variation of the damage values produced by maximum principle stress/UTS criterion, C&L criterion was used to the optimization study in hot forging punch shape design.

5) Half convex punch shape in extrusion can further decrease the maximum damage values in coining, which means such design could help to reduce the cracking of the material in actual forging operation. The convex profile requires further optimization in order to be used the actual production line.

6) The convex punch shape will produce a high damage value due to the sharp corner generated on the preform.

7) The concave punch shape will result in much higher damage values, which means it will increase the danger of cracking.
5.1 Description of the problem

The initial billet is made from Al 7175 material. It is cast to a size of 23.5” (596.9 mm) diameter × 38” (965.2 mm) length. This billet is heated to a temperature of 805 F (430 C). This section describes the operations that eventually lead to failure. A description of the sequence of operations is as follows. The axes of the forging should be noted very carefully to understand the process:

Step 1: Upsetting:

The cast billet is upset using flat dies to a height of 22.8” (579.1 mm), Figure 5.1.

Figure 5.1 Step 1: Upsetting (schematic)

Step 2: Cross-forging:

This step is made up of 2 sub-steps as described below:

Step 2A: In this step, the forging is rotated about the X-axis, as seen in Figure 5.2 and compressed to a square cross section. Six 90° rotations about the X-axis help to achieve this. The
first 2 to 3 strokes (bites) compress the forging to within 2 inches of the final dimensions and the last 3-4 strokes (bites) make the forging cross-section square.

Figure 5.2 Step 2A: Cross forging: Converting the forging to have a square cross section (Schematic)

A comprehensive schematic to help understand this step is shown in Figure 5.3:
Figure 5.3 Complete figurative description of step 2A (schematic)
Step 2B: In this step, the corners of the square forging are broken by slowly rotating it about X-axis and taking small bites to get a circular cross section. The forging is rotated by a random amount varying between 5 to 15 degrees. Note that the axis of symmetry of the cast billet was Y-axis, and the operations have changed the axis of symmetry of the forging to X axis in the original co-ordinate system.

Figure 5.4 Step 2B: Cross forging: Converting square cross section back to circular by breaking corners (schematic)

Step 3: Upsetting:
The forging after step 2 is similar to the initial cast billet, with the axis changed. This upsetting step is similar to step 1 described above and the upset height is also the same.

Step 4: Cross forging:
This step is similar to step 2 described above. Note that this step again changes the axis of the billet as shown previously to Z-axis in the original co-ordinate system.

Step 5: Upsetting:
During this step, the forging is upset to a height of 36” (914.4 mm) as shown in Figure 5.5:

![Figure 5.5: Step 5: Upsetting (schematic)](image)

Figure 5.5 Step 5: Upsetting (schematic)

Step 6: Cross-forging:

The final cross forging is different from the previous cross-forgings because the forging is not brought down to a square cross section before making it circular, but it is done directly by cross-forging it about the axis of symmetry of the forging. During this stage, cracking is observed on the part. This is shown in Figure 5.6:
Figure 5.6 Step 6: Final cross forging without converting it into a rectangular cross section (schematic)

The picture of the fractured part is also shown in Figure 5.7:

Figure 5.7 Fractured specimen provided by Company A.
5.2 Validation Study (Forging of Ti-6Al-4V)

After literature review, a validation study was conducted based on “Cavitation and Failure during Hot Forging of Ti-6Al-4V” by Semiatin et al. The purpose of this study is to compare the accuracy of FEM simulations with the actual experiment. Notice here that in the simulation, two set of flow stress data of Ti-6Al-4V were used. One is from DEFORM database, the second is from Semiatin’s original data. Thus, the influence of the flow stress can also be compared in this study.

5.2.1 Forging of Ti-6Al-4V at 815°C and ram speed of 101.6 mm/s

<table>
<thead>
<tr>
<th>Forging #</th>
<th>Height Reduction (%)</th>
<th>Visual Fracture Observations</th>
<th>Max Local Damage (Max absolute damage)</th>
<th>Max. Local strain rate/(s)</th>
<th>Local Hoop stress (Mpa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>12</td>
<td>15</td>
<td>No free surface cracks</td>
<td>0.09 0.06 0.068</td>
<td>0.15 0.15 0.09 0.09</td>
<td>118 153</td>
</tr>
<tr>
<td>6</td>
<td>25</td>
<td>No free surface cracks</td>
<td>0.15 0.12 0.14</td>
<td>0.24 0.23 0.08 0.08</td>
<td>192 203</td>
</tr>
<tr>
<td>2</td>
<td>35</td>
<td>Free surface cracks</td>
<td>0.27 0.21 0.234</td>
<td>0.32 0.32 0.09 0.1</td>
<td>243 222</td>
</tr>
<tr>
<td>1</td>
<td>50</td>
<td>Many free surface cracks</td>
<td>0.45 0.4 0.43</td>
<td>0.49 0.5 0.13 0.14</td>
<td>250 192</td>
</tr>
<tr>
<td>7</td>
<td>70</td>
<td>Many free surface cracks</td>
<td>0.7 0.73 0.78 (0.93)</td>
<td>0.79 0.83 0.19 0.2</td>
<td>264 167</td>
</tr>
</tbody>
</table>

Table 5.1 Forging of Ti-6Al-4V at 815°C and ram speed of 101.6 mm/s

- FS is flow stress
- Global strain is instantaneous ram speed/Instantaneous sample height
- Local properties refer to the outer surface at mid height of the billet and absolute damage refers to the maximum damage anywhere in the billet.
- Semiatin refers to the results of simulations from [Semiatin et al., 1998].
Figure 5.8 Comparison of damage at 815°C and ram speed 101.6 mm/s and 63.5% Height reduction. The maximum damage value of (b) occurs at the die-workpiece interface. (a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]

Figure 5.9 Comparison of hoop stress at 815°C and ram speed 101.6 mm/s and 63.5% Height reduction a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]
Figure 5.10 Comparison of effective strain at 815°C and ram speed 101.6 mm/s and 63.5% Height reduction a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]

5.2.2 Forging of Ti-6Al-4V at 900°C and ram speed of 7.6 mm/s

<table>
<thead>
<tr>
<th>Forging #</th>
<th>Height Reduction (%)</th>
<th>Visual Fracture Observations</th>
<th>Max Local Damage (Max absolute damage)</th>
<th>Max. Local strain</th>
<th>Max. Local strain rate/(s)</th>
<th>Local Hoop stress (Mpa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>14</td>
<td>25</td>
<td>No free surface cracks</td>
<td>0.12 (Old FS) 0.09 (New FS)</td>
<td>0.24 Old FS</td>
<td>0.224 New FS</td>
<td>1.11 Old FS 1.02 New FS</td>
</tr>
<tr>
<td>3</td>
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<td>Free surface cracks</td>
<td>0.34 (Old FS) 0.29 (New FS)</td>
<td>0.45 Old FS</td>
<td>0.43 New FS</td>
<td>1.3 Old FS 1.28 New FS</td>
</tr>
<tr>
<td>8</td>
<td>63.5</td>
<td>Many free surface cracks</td>
<td>0.52 (Old FS) 0.45 (New FS)</td>
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<td>0.58 New FS</td>
<td>1.74 Old FS 1.70 New FS</td>
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<tr>
<td>9</td>
<td>75</td>
<td>Many free surface cracks</td>
<td>0.62 (Old FS) 0.63 (New FS)</td>
<td>0.78 Old FS</td>
<td>0.76 New FS</td>
<td>2.61 Old FS 2.64 New FS</td>
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</table>

Table 5.2 Forging of Ti-6Al-4V at 900°C and ram speed of 7.6 mm/s

- FS is flow stress
- Global strain is instantaneous ram speed/Instantaneous sample height
- Local properties refer to the end tips of the billet and absolute damage refers to the maximum damage anywhere in the billet.
- Semiatin refers to the results of simulations from [Semiatin et al., 1999].

Figure 5.11 Comparison of damage at 900C and ram speed 7.6 mm/s and 50% Height reduction
a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]
Figure 5.12 Comparison of hoop stress at 900C and ram speed 7.6 mm/s and 50% Height reduction a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]

Figure 5.13 Comparison of effective strain at 900C and ram speed 7.6 mm/s and 50% Height reduction a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]
5.2.3 Forging of Ti-6Al-4V at 815°C and ram speed of 7.6 mm/s

<table>
<thead>
<tr>
<th>Forging #</th>
<th>Height Reduction (%)</th>
<th>Visual Fracture Observations</th>
<th>Max Local Damage (Max absolute damage)</th>
<th>Max. Local strain</th>
<th>Max. Local strain rate/(s)</th>
<th>Local Hoop stress (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Semiatin</td>
<td>Old FS</td>
<td>New FS</td>
<td>Old FS</td>
</tr>
<tr>
<td>13</td>
<td>25</td>
<td>No free surface cracks</td>
<td>0.19</td>
<td>0.14</td>
<td>0.145</td>
<td>0.26</td>
</tr>
<tr>
<td>4</td>
<td>50</td>
<td>Many free surface cracks</td>
<td>0.5</td>
<td>0.5</td>
<td>0.48 (0.52)</td>
<td>0.6</td>
</tr>
<tr>
<td>10</td>
<td>80</td>
<td>Many free surface cracks</td>
<td>0.95</td>
<td>1.12</td>
<td>1.15 (1.46)</td>
<td>1.26</td>
</tr>
</tbody>
</table>

Table 5.3 Forging of Ti-6Al-4V at 815°C and ram speed of 7.6 mm/s

Figure 5.14 Comparison of damage at 815°C and ram speed 7.6 mm/s and 70% Height reduction
(a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]
Figure 5.15 Comparison of hoop stress at 815C and ram speed 7.6 mm/s and 70% Height reduction a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]

Figure 5.16 Comparison of effective strain at 815C and ram speed 7.6 mm/s and 70% Height reduction a) Using flow stress from DEFORM database (b) Using flow stress from [Semiatin et al., 2001]
5.2.4 Conclusions and recommendations

1) With the new flow stress used in simulations, the results are closer to the original prediction by Dr. Semiatin [Semiatin et al., 1999], compared to the previous simulations using the flow stress from DEFORM database. There are some other factors that might result in the errors between Dr. Semiatin’s prediction and ERC’s prediction, e.g. software version update, neglect of the press idle time. Still, the ERC’s prediction successfully duplicated the Author’s work and proved the capability of the FE simulation in predicting the fracture formation in hot forging.

2) From the table 1-3, we can conclude the local stress (hoop stress as well as maximum principal stress) cannot be used as fracture criterion in the view of fact that they exhibited uncertain trend during forging process, especially in the case which the material has work softening at elevated temperature.

3) Local strains at the fracture zone have exhibited the same trend as the damage values. Generally strain values have larger variations and it is not suitable to be used as a criterion. However in certain cases, it is still a good indicator of fracture.

4) By comparing the local damage values with the experimental measurement [Semiatin et al., 1998; Monthly report to FIA 1_16_13], it is concluded that the damage value prediction using Cockcroft&Latham criterion can predict the fracture initiation within certain errors.

5) One drawback of Cockcroft&Latham criterion is the critical damage value varies for different temperature and possibly strain rate. The temperature effect was shown in [Semiatin et al., 1998]. The strain rate effect requires further investigation.
5.3 Material properties and process parameters

The FE simulations of the forging were conducted using the implicit FE code DEFORM – 3D™. Since flow stress data was not available for Al 7175 alloy, flow stress data for Al 7075 was taken from DEFORM database since both the alloys have a similar mechanical response.

<table>
<thead>
<tr>
<th>Material Properties</th>
<th>Value</th>
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</thead>
<tbody>
<tr>
<td>Young’s Modulus</td>
<td>68.9 GPa</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>0.3</td>
</tr>
<tr>
<td>Friction coefficient (Shear)</td>
<td>0.7 (From literature, for hot forging without lubricant)</td>
</tr>
</tbody>
</table>

Table 5.4 Material properties and friction for Al7075

Contact boundary conditions in the simulation were modeled as shear friction, i.e. $\tau=\mu p$

Where $\tau$ = Shear Stress, $\mu$ = coefficient of friction and $p$ = normal pressure at the die/material interface.

<table>
<thead>
<tr>
<th>Process Parameters</th>
<th>Value/Details</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial temperature of billet</td>
<td>805 F (430 C) - Given by Company A</td>
</tr>
<tr>
<td>Initial temperature of dies</td>
<td>250 C – Given by Company A</td>
</tr>
<tr>
<td>Press speed (averaged during process)</td>
<td>30 mm/s – Given by Company A</td>
</tr>
<tr>
<td>Interface heat transfer coefficient</td>
<td>5000 W/m²-K (Assumed, default value from DEFORM for hot forging)</td>
</tr>
<tr>
<td>(between forging and dies)</td>
<td></td>
</tr>
<tr>
<td>Convective Heat Transfer Coefficient-</td>
<td>20 W/m²-K (Default value from DEFORM database)</td>
</tr>
<tr>
<td>HTC (between forging and air)</td>
<td></td>
</tr>
</tbody>
</table>

Table 5.5 Process parameters
5.4 Finite element simulations (3D)

5.4.1 Construction of the FE model

The FE model was constructed based on the inputs from Company A. Since the modeling involves a series of operations where the object is rotated and forged in different orientations, it was not possible to use a 2D model or even a symmetric 3D model. The billet was modeled as a plastic object and the dies were assumed to be rigid. The geometry of the dies is flat as can be seen in the model, Figure 5.17 Initial input geometric model. All dimensions shown are in mm.

![Initial input geometric model](image)

Figure 5.17 Initial input geometric model. All dimensions shown are in mm.

5.4.2 Simulation steps and procedure

Modeling of the process involves modeling of a series of operations starting from the first step of upsetting. The output at the end of a step is used as the input for the next step. Modeling of the cross forging operation is explained in more detail below:
5.4.2.1  Modeling of the cross forging step 2A and 4A

The output from the upsetting operation is rotated and oriented for cross forging. The step 2A itself is made of 6 bites and the output of each bite is rotated and used as the input for the next bite, Figure 5.18.

The object is rotated 900 about the Y axis after each bite. The forging operations reduce the initial cross section dimensions of 32” X 22.8” (811.2mm X 579.2 mm) to 22” X 22.4” (558.8mm X 569.1mm)

5.4.2.2  Modeling of the cross forging step 2B and 4B

The square forging obtained at the end of the previous operation is converted into a circular cross section by squeezing it between the dies and rotating it by small angles and repeating the process. A total of about 20 bites (that is 20 models, each by using the previous model as input) were implemented to simulate each of the steps 2B and 4B. The modeling of step 2B is shown below in Figure 5.19. Step 4B is similar to step 2B.
Figure 5.19 Modeling of cross forging step 2B. (a) At the beginning of step 2B  (b) At the end of step 2B. The corners of the square forging are broken by slowly rotating it about X-axis and taking small bites to get a circular cross section. The forging is rotated by a random amount varying between 5 to 15 degrees after each bite. It can be seen that the forging operations result in getting a circular cross-section having a diameter of about 23.5” (596.9mm).

5.5 Preliminary results and heat transfer considerations

After modeling the first 2 steps of the operation, it was observed that the maximum temperature in the forging reaches 475 C (at the core) as shown in Figure 5.20.

The temperature at the core (475 C) is greater than the solidus temperature of the material which is 473 C. Since there were 4 more steps to be simulated after the 2nd step, it would cause a greater temperature rise and more melting in the simulations. Because the material reaches close to melting temperatures in the operations, the assumptions made in the simulations were looked into closer. One of the assumptions was that the heat transfer between operations (to transfer the billet to the next station or the time between bites) would be insignificant because the time between operations is less and also due to the low convective heat transfer coefficient, less heat would be
lost to the air. This assumption was examined carefully by simulating the heat transfer between operations and the heat transfer between the bites of an operation.

Figure 5.20 Temperature distribution at the end of step 2 (before considering heat transfer between operations (a) At the outer surface (b) At the center cross section.

Figure 5.21 Simulation of heat transfer between operations (20s) (a) At the end of step 2 before heat transfer with air (b) After heat transfer
In consultation with Company A, it was decided to estimate the heat transfer between each step (steps discussed in chapter 1) as 20 s. From Figure 5.21, it can be seen that the core temperature reduces by 5-7 C (6 C in this case) in the 20 s though the temperature at the surface is more or less a constant. Thus, even though little heat is lost to the environment, the heat transfer within the part is significant because of the relatively high conductivity of the material (Al 7175 alloy).

Figure 5.22 Simulation of heat transfer between cross forging bites (Step 2B) (5s) (a) At the end of bite 12 of step 3 before heat transfer with air (b) After heat transfer.

In consultation with Company A, it was decided to estimate the time for heat transfer between each bite as 5 s. From Figure 5.22, it can be seen that the core temperature reduces by 1-3 C (1 C in this case) in the 5 s.

The simulations were re-run and the heat transfer between operations and bites were predicted, a little more accurately.
5.6 Observations

The damage was calculated using normalized Cockroft & Latham (C&L) criterion. The damage distribution after step 2 is shown in Figure 5.23.

It can be seen that a greater damage occurs at the surface than the center which is contrary to the observation made during actual forging. Hence, the normalized C&L criterion fails to predict damage in this hot forging.

Since we know that the crack originates from the center cross section (as observed during forging tests), and the temperature is maximum at the center, we tried to use a damage criterion that is strongly dependent on temperature (stress/UTS). However, this criterion also failed to show expected results (i.e. Indicate fracture at the center of forging.) Only upsetting results are shown here in Figure 5.24.

Figure 5.23 Normalized Cockroft-Latham damage at the end of step 2 (before considering heat transfer between operations) (a) At the outer surface  (b) At the center cross section
Figure 5.24 Stress/UTS damage criterion at the end of step 1 (before considering heat transfer between operations) (a) At the outer surface (b) At the center cross section.

Because the temperature reaches a value close to the solidus temperature, it is one of the most important parameters. As a result, the temperature is plotted as shown in Figure 5.25 to show schematically how the estimated temperatures change as the operation proceeds.
Figure 5.25 Variation of maximum temperature (occurs at the core in each case) over the process (schematic).

Figure 5.26 Temperature distribution at the center cross section at: (a) End of step 2A (cross forging) (b) End of step 2B (Cross forging – roll on OD) (c) End of step 5 (Upsetting).
From Figure 5.25, it can be seen that the maximum temperature of the process exceeds the solidus temperature in step 4A (cross forging). This indicates a possibility of local melting at this stage. Even if local melting does not occur, it can safely be said that the material becomes soft at this stage since it reaches a high temperature that is very close to the solidus temperature. This softening of material could lead to initiation of cracks during one of the forging steps (bites).

The temperature distribution at the cross section is plotted at the end of certain operations as shown in Figure 5.26. It can be seen that the maximum temperature of the process exceeds the solidus temperature.

The temperature during the cross forging step 4A is also plotted below in Figure 5.27. The figure is plotted at the bites where the local temperature exceeds the solidus temperature of the material.

Figure 5.27 Temperature distribution at the center cross section when local melting occurs (Local temperature > 477 C): (a) End of bite 3 of step 4A (cross forging) (b) End of bite 4 of step 4B (Cross forging – roll on OD)
5.7 Mannesmann effect on the forging

Mannesmann effect refers to the formation of a cavity along the longitudinal axis in bars subjected to radial compression in metalworking operations like transverse rolling, cogging etc. Cavities form due to secondary tensile stresses that are induced at the center of the bar by a highly inhomogeneous deformation in the bar cross section. To check for Mannesmann effect on the forging, the stresses in the radial directions were plotted at certain stages of cogging (step 6) and cross forging (step 4A) operations.

Figure 5.28 Distribution of stress in X-direction: (a) End of 2 rotations of step 6 (cogging) (b) End of step 4A (cross forging)
It can be seen in Figure 5.28 that tensile stresses are generated in the X-direction at the center cross section during step 6. These secondary tensile stresses may result in cavitation by the Mannesmann effect, thus weakening the material further at the center. The stress distribution in the X-direction has been plotted in Figure 5.29 for the line AA shown in Figure 5.28. Figure 5.29 illustrates that there are tensile stresses generated near the center during rolling on OD (cogging). In the cross forging and upsetting operations, no tensile stresses are generated. Only compressive stresses are present. This shows why the cracking occurs in the cogging operation and not in the cross forging or upsetting.

5.8 Effect of heat transfer coefficient at the die/forging interface and thermal stresses
Since the exact heat transfer co-efficient can be found only by experiments, it was decided to look into the effect of heat transfer co-efficients on the operations.
As seen in Figure 5.30, the heat transfer co-efficient at the die/forging interface does not make much difference on the temperature distribution, especially at the center where fracture occurs. It was also decided to investigate the effect of thermal stresses on the simulations. To save computer time and overall simulation effort, the elastic-plastic model was used to simulate the heat transfer after step 4A (i.e. where the local melting possibly occurs).

The simulation shows thermal gradients have 0 or negligible effect on the simulations. This is because the thermal gradients that are developed in the forging are not very large to cause large thermal stresses.

5.9 Conclusions and recommendations

1) Cockcroft - Latham (C&L) criterion fails to predict damage in this case. Ultimate tensile strength criterion also fails to predict damage.
2) Calculated temperatures are higher at the center of the billet, where the crack is initiated. Thus, there seems to be a correlation between maximum temperature and fracture location. Hence, a damage criterion that has a stronger dependence on temperature must be used in hot forging.

3) During step 4A (Cross forging), the temperature at the core increases to reach a value above the solidus temperature. Even if the material does not melt, we can say that the material becomes “soft” at the center at this stage due to the temperature being very close to the solidus temperature of the material. Secondary tensile stresses occur in cogging operations leading to cavitation. These tensile stresses may also contribute to the cracking during the cogging (rolling on OD) operation.

4) Within the realistic range of values considered, the heat transfer co-efficient and thermal gradients have insignificant effect on the fracture in forging.

5) It is recommended to start off the forging process with a temperature about 10 C to 20 C lower to successfully forge a component, in this specific application.
CHAPTER 6  Investigation and Process Design of Bi-metallic Forging

6.1 Significance of the Bi-metallic Forging

Weight reduction is a new technical request by all truck and automotive OEMs. The weight reduction could be achieved by introduction of new lightweight materials, e.g. polymers, nanofibers, or through new structure design, or the mixture of both ideas.

The sponsor of this project, Eaton Corporation, is one of the major heavy duty truck transmission suppliers globally, whose transmission sales per year is over 200M. The weight of the heavy duty transmission provided by Eaton is about 1000lb, while that of medium duty is about 400lb. Gears account for about 20% weight of transmission. If we could reduce the weight of the gear by 50%, the total weight reduction 100lb for heavy duty transmission and 40lb for medium duty transmission. Table 6.1 shows the fuel economy improvement percentage per 100lb weight reduction[Ricardo 2011]:

<table>
<thead>
<tr>
<th></th>
<th>City</th>
<th>Hwy</th>
<th>Combined</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline Engine</td>
<td>0.35%</td>
<td>0.25%</td>
<td>0.3%</td>
</tr>
<tr>
<td>Downsized Engine</td>
<td>0.45%</td>
<td>0.3%</td>
<td>0.37%</td>
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</tbody>
</table>

Table 6.1 Fuel Economy Improvement of Heavy Duty Trucks with Light Weigh Design (Gasoline Engine)

OEMs will pay $1.5 to $2 per pound weight reduction. Considering the production volume of our sponsor, 50% of gear weight reduction is about $46M market size.

The advantages of the weight reduction of gears in a transmission box can be summarized as:

- Increase the sub-system efficiencies
- Lower the power requirements
- Increase fuel economy/decrease fuel consumption
- Increase payload capacity

In the traditional gear tooth machining and center hole drilling process, approximately 40-50% of the starting materials will be wasted. If the gear teeth and the center hole could be net-shape or near net-shape forged, a large amount of material could be saved. Figure 6.1 shows the comparison of solid steel gear and bi-metallic gear forged from 0.75” wall and 0.25” wall billet. Figure 6.2 shows the weight reduction percentage of bi-metallic gear and the waste saving if the teeth could be forged. Superficially, the weight reduction is much higher if the steel wall of billet is thinner. Also, if the gear teeth are forged instead of machined, over 60% of starting material could be saved.

![Figure 6.1 Comparison of the weight of the gear and material waste.](image)

Figure 6.1 Comparison of the weight of the gear and material waste.
Figure 6.2 Comparison of weight reduction of 0.75” and 0.25” wall billet and waste saving of machined teeth and forged teeth

Based on the lightweight feature of bi-metallic gear and the possible smaller press load required for forging operation, the greenhouse gas emission and the cumulative energy demand to produce different types of gears were calculated and plotted in Figure 6.3 and Figure 6.4. For comparison purpose, a new filling material – clay – is also used in the calculation as a representative of a really low cost core material. Necessary assumptions and approximations were made in such calculation. It can be seen that carbon footprint of Al core gear is about 21% lower than solid steel gear, and the cumulative energy demand to produce the Al core gear with forged teeth is about 28.4% lower. If the core material is clay, the carbon footprint and cumulative energy demand is extremely lower than both solid steel gear and Al core gear. That indicates the cost and the weight of filling material have great influence on the fuel efficiency and cost of the production, since clay costs barely nothing and is lighter than most of the light weight material, e.g. Al, Mg.
Figure 6.3 Carbon footprint of different types of gears. Clay core gear is used as a comparison.

Figure 6.4 Cumulative energy demand to produce different types of gears. Clay core gear is used as a comparison.
6.2 Gear Candidates Selection

A suitable selection of the gear model is the good starting point of this study to demonstrate the bi-metallic forging concept. The bi-metallic gear was proposed to be used in heavy duty truck transmission. One simple solution is to forge a same gear configuration that has been used currently in the transmission box, using bi-metallic forging method, to prove the concept as well as validate the capability of the bi-metallic gear as a replacement of the conventional gear component. However, since the first forging trial will be a proof of concept, some other criteria can be used for the initial gear selection:

1) The gear should have a simple geometry, without complicated structural design, such as thin web, keyway, splines, inner teeth, etc., for the ease of forging.

2) The volume/weight of the gear should be large enough. A very small gear cannot be used as a proof of concept since the bi-metallic gear cannot be scaled down as a conventional due to the fact that the steel wall has to remain certain thickness, in order to maintain the strength of the part. Considering a minimum steel wall thickness that could meet the gear strength requirement, a larger gear (large gear diameter and height) could be “filled” with more light weight material, resulting in a higher the weight saving percentage.

3) Initially, the gear candidates are suggested to have larger teeth/module, considering the ease of forging. Also, the light weight material, which is Al in this study, is expected to flow into tooth area to form a “wavy” shape following the shape of the tooth. A larger and thicker tooth on the gear will be easier to allow Al to flow into the tooth area.

The first gear model selected in this study is an Eaton forged with the outside diameter OD = 129mm, ID=63mm and height h = 60.3mm. The application of this gear is unknown. The geometry of the gear is shown in Figure 6.5. The gear model meets all the three requirements mentioned above, with simple, bulky shape and large diameter. In the actual forging operation,
the 100% steel billet was forged into a “ring” shape with only a thin web left at the center hole and no teeth will be forged. The gear teeth were fine machined after forging. The die set is shown in Figure 6.6 a) and the geometry of the forged part is shown in Figure 6.6 b).

Figure 6.5 Geometry of the selected gear model

Figure 6.6 a) Die configuration for the “pancake” forging; b) forged part geometry.
6.3 Billet Design and Preparation

6.3.1 Billet Design

1) Volume of the billet

The bi-metallic billet design is based on the selected gear geometry. To simplify the forging process, the center hole will not be forged. Only a “pancake” shape will be forged. Thus, the volume of the bi-metallic billet is calculated as the volume of the gear plus the center hole with forging tolerance. Based on that calculation, the volume of the bi-metallic billet $V = 66.35\text{in}^3$.

2) H/D ratio

After the volume of the billet is fixed, the next consideration is the H/D ratio of the billet. According to the practical experience from Walker Forge, the H/D ratio of a billet is generally between 1.5 and 2. A “short and fat” (small H/D ratio) billet is not convenient for induction heating and handling; while a “tall and thin” (large H/D ratio) billet will cause buckling during forging. In our study, we follow the same rule and select H/D ratio = 2 in the first billet design, although this empirical experience may not be able to apply to the new forging method.

3) Material selection

It is a great importance to select the suitable material for the bi-metallic billet since it will affect both manufacturing process and performance of the part.

Very high strength steel is preferred for the casing since the steel casing/gear shell will carry the major portion of the torque in application. The commonly used gear material AISI 8620 is selected as the steel casing material.

Ideally, steel casing will carry most of the torque during transmission and the Al core will also carry a small portion of the load. Thus, the core material should also have a high strength feature. Alloy 7075 sheet products have application throughout aircraft and aerospace structures where a combination of high strength with moderate toughness and corrosion resistance are required. It is
also a light weight material that can meet our weight reduction requirement. Thus, Al 7075 is selected as the core material in the bi-metallic billet. Table 6.2 lists the chemical composition of both materials and Table 6.3 shows some of the physical and mechanical properties of the two materials. It can be seen that the density of Al 7075 is about 1/3 of 8620 steel, showing its great potential in light weight structural design. While the modulus of elasticity of Al 7075 is also about 1/3 of 8620, which means the strength of the bi-metallic part will be reduced, although 7××× serious have the highest strength in Al alloys. Another interesting observation from Table 6.3 is that the melting point of Al 7075 is about 635°C at most. However, the general forging temperature of 8620 is above 1000°C. This will cause a dilemma that if forging the billet at 1000°C, Al core will be fully melted; if forging the billet below 400°C, the formability of steel may not be good enough. Thus, the actual forging temperature of bi-metallic billet requires more investigation. This issue will be discussed later.

<table>
<thead>
<tr>
<th></th>
<th>AISI 8620 steel Wt. %</th>
<th>Al 7075 Wt. %</th>
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</thead>
<tbody>
<tr>
<td>Al</td>
<td>81.7-91.4</td>
<td></td>
</tr>
<tr>
<td>C</td>
<td>0.18-0.23</td>
<td></td>
</tr>
<tr>
<td>Cr</td>
<td>0.40-0.60</td>
<td>0.18-0.28</td>
</tr>
<tr>
<td>Cu</td>
<td>1.2-2</td>
<td></td>
</tr>
<tr>
<td>Fe</td>
<td>96.89-98.02</td>
<td>&lt;0.5</td>
</tr>
<tr>
<td>Mg</td>
<td>2.1-2.9</td>
<td></td>
</tr>
<tr>
<td>Mn</td>
<td>0.7-0.9</td>
<td>&lt;0.3</td>
</tr>
<tr>
<td>Mo</td>
<td>0.15-0.25</td>
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</tr>
<tr>
<td>Ni</td>
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<tr>
<td>Si</td>
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</tr>
<tr>
<td>S</td>
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<tr>
<td>Ti</td>
<td>&lt;0.2</td>
<td></td>
</tr>
<tr>
<td>Zn</td>
<td>5.1-6.1</td>
<td></td>
</tr>
</tbody>
</table>

Table 6.2 Chemical composition of AISI 8620 and Al 7075 [Matweb, 2013]
### Some physical and mechanical properties of AISI 8620 and Al 7075

<table>
<thead>
<tr>
<th></th>
<th>AISI 8620 steel</th>
<th>Al 7075</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (g/cc)</td>
<td>7.85</td>
<td>2.81</td>
</tr>
<tr>
<td>Modulus of Elasticity (GPa)</td>
<td>205</td>
<td>71.7</td>
</tr>
<tr>
<td>Forging Temperature</td>
<td>1800-2200 F / 982-1024 C</td>
<td>250F / 121C</td>
</tr>
<tr>
<td>Melting Temperature</td>
<td>2600 F / 1426 C</td>
<td>477-635C / 890-1175F</td>
</tr>
</tbody>
</table>

Table 6.3 Some physical and mechanical properties of AISI 8620 and Al 7075

### 4) Structure

There are many possibilities to design the structure of the bi-metallic billet. The first two models are 1) the easiest structure with a hollow steel casing filled with Al core; 2) a slender steel cylinder is added at the center of the billet to ensure the center hole is pierced thought steel instead of Al. The two models are demonstrated in Figure 6.7. Obviously the preparation of the second model is much more complicated than the first one. Although the center steel cylinder added in the second model is protect the center hole machining, it is a rough idea without considering the deformation during forging. The metal flow might be so unpredictable and the steel cylinder may not remain at the center. Therefore, the second model was abandoned due to its complicity. In the following study, all the billets will have the similar structure as shown in Figure 6.7a.)
5) Steel wall thickness

Obviously, thinner steel wall thickness will yield high weight reduction percentage since the volume of the billet is already fixed. However, considering the strength of the part, the steel wall has to remain certain thickness and the strength of the bi-metallic part has to be verified. Two wall thickness 0.25” and 0.5” were used in the billet design. The actual structure of the billet is shown in Figure 6.8.

6) Welding

The billet structure shown in Figure 6.8 also considers the preparation method of the sample. First, the steel tubes of the specific diameter and thickness and the Al cylinder of designed diameter can be ordered. Second, the steel end caps are machined out. Then, the Al cylinder is fitted into the steel tube. Both press fit and loose fit can be tried. Before assembly, the surface of Al cylinder is cleaned to remove the oxidized layer. Finally, the steel end cap is welded on both end of the tube to completely enclose the Al core inside the billet. The short step on the end caps
are designed for a) locating the end cap for welding; b) protect the Al core from contacting the welding arc or laser.

![Figure 6.8 Bi-metallic billet design a): 0.5” wall thickness; b) 0.25” wall thickness](image)

6.3.2 Billet Preparation

Totally 80 billets (40 of 0.25” wall and 40 0.5” wall) were made based on the billet design in Figure 6.8. The manufacturing process is explained above. One assembly process of a 0.5” billet sample is shown in Figure 4.9. Notice here that when the actual samples were prepared, the steel tube ordered is ASTM A513, which is equivalent to the AISI 1018. In previous section, we mentioned that the suggested steel material should be AISI 8620. However, due to the cost consideration and ease of forging, A513 is used to make the first stack of billet samples for the proof of concept study.
Figure 6.9 Demonstration of billet assembly of a 0.5” wall sample: a) steel tube, Al core and end cap before assembly; b) press fitted or loose fitted the Al core into steel tube; c) electron beam welding the end cap onto the steel tube. Material: tube: ASTM A513, Core: Al 7075.

Figure 4.10 shows a cross-section view of the electron beam welded zone. It can be seen that the cap and the steel tube are perfectly welded together and the heat affected zone are clearly shown.
The grain size of the steel in heat affected zone is generally larger than the base material, resulting in lower mechanical strength. An un-welded zone adjacent to the Al core is left due to the stepped end cap design. The step successfully protected the Al core from melting by the electron beam weld as designed, since the EB weld only penetrated into the steel cap. However, such un-welded zone will bring more troubles than expected in the following test. This will be discussed in the next section.

![Cross section of the electron beam weld zone](image)

Figure 6.10 Cross section of the electron beam weld zone

6.4 Heating strategy and temperature control

6.4.1 Finite Element Simulation of Slow Heating Process

Before the forging simulation, the heating process of the bi-metallic billet was conducted. Normally in a hot forging simulation, a heating simulation is not necessary. The billet can be set to the requested forging temperature, i.e. 1150°C for steel. However, in bi-metallic forging, due to
the difference of the thermal expansion of steel and Al as well as the possible melting of Al, as explained in section 3.4, FE simulation has to be conducted to analyze the risk of heating process.

One furnace heating simulations was conducted using 0.25” billet model. The dimensions are shown in Figure 6.8. The simulation matrix is shown in Table 6.4. The billet was heated up from room temperature to about 400°C for both steel casing and Al core. The shape of the billet after heating is shown in Figure 6.11

<table>
<thead>
<tr>
<th>Simulation parameters</th>
<th>Descriptions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core material</td>
<td>AL 7075 (flow stress from DEFORM 2D™ database)</td>
</tr>
<tr>
<td>Welded steel case material</td>
<td>AISI 8620/ASM513 (flow stress from DEFORM 2D™ database)</td>
</tr>
<tr>
<td>Die material</td>
<td>AISI H13</td>
</tr>
<tr>
<td>Thermal expansion (m/°C) (a function of temperature)</td>
<td>AL7075 - Figure 11</td>
</tr>
<tr>
<td></td>
<td>AISI 8620 - 1.5 ×10^-5 @ 20C (assumed as constant)</td>
</tr>
<tr>
<td>Convection coefficient between steel and environment (kW/m²K)</td>
<td>0.02 (obtained from DEFORM 2D™)</td>
</tr>
<tr>
<td>Material type</td>
<td>Aluminum core and welded steel case–elastic-plastic</td>
</tr>
<tr>
<td>Coefficient of friction, m (between Al core and welded steel case)</td>
<td>0.25 (assumed)</td>
</tr>
<tr>
<td>Initial billet temperature</td>
<td>20°C/68°F</td>
</tr>
<tr>
<td>Environment temperature</td>
<td>400°C/752°F</td>
</tr>
</tbody>
</table>

Table 6.4 Parameters used in heating simulation
It can be seen from Figure 4.11 that due to the larger coefficient of thermal expansion of Al, the volume of Al core expanded much more than steel casing after heated to 400C, resulting in the top and bottom cap bulging out. A close look at the corner of the weld zone shows that the unwelded tip opens up because of the bulging of the end cap. Al will be squeezed into the open corner area. The formation of the un-welded zone was explained in section 6.3.2. Such un-weld zone behaves as a pre-existing “crack” and introduces extensively high stress concentration at the tip of the weld zone, which is the junction area of the end cap and tube. The high stress concentration might crack the weld zone and break the billet from the cap. In simulation, the cracking is not shown since the actual strength of the weld and the weld affected zone is unable to describe. However, based on the theory of fracture mechanics, a pre cracked tip will weaken the strength of the material by 2/3. As a result, there is a high risk that the billet will crack at the weld zone.
6.4.2 Furnace heating test

Furnace heating test were conducted with both 0.25” billet and 0.5” billet. 315C/600F and 600C/1112F were used as the test temperature. The test procedure was simple: place the billet into the furnace for 2 hours until the inside Al and steel casing reach the target temperature; then take the billet out of the furnace and cool it down to room temperature.

In the test with the furnace temperature 315C, the shape of billet did not shown significant change other than little bulging of the end cap. However in the test with furnace temperature 600C, after certain heating while the billet is still the billet, the billets cracked from the top cap and liquid Al leaks out from the weld zone, Figure 6.12, Figure 6.13.

Figure 6.12 Cracking and leaking of the Al after heating to 600C, 0.25” billet.
Several summaries can be made from the furnace heating test:

1) CTE of Al is much larger than steel. Although the billets did not crack at 315°C, cracking occurred once the furnace temperature is 600°C, due to the larger difference of volume expansion.

2) At 600°C, most Al has turned into liquid state. According to Figure 3.10, the volume expansion of Al during the change of state is extremely high. Table 6.5 calculates the volume expansion percentage if the Al is heated to fully molten. As a result, the Al pushed cap from inside and then cracked the billet and leaked out.

Figure 6.13 Cracking and leaking of the Al after heating to 600°C, 0.5” billet.
Table 6.5 Volume expansion of Al from room temperature till fully molten state.

<table>
<thead>
<tr>
<th>Temperature range</th>
<th>Volume expansion percentage</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>20°C-635°C</td>
<td>10%</td>
<td>[Mitchell, 2004]</td>
</tr>
<tr>
<td>27°C-577°C</td>
<td>19%</td>
<td>[Narender, 2013]</td>
</tr>
</tbody>
</table>

3) The un-welded area acts as a “pre-cracking” and weakens the strength of the weld. Consequently, cracking initiated from the tip of the weld zone.

4) After the crack initiated, it propagated along the weld affected zone, where the strength of the material is lower due to grain grown during welding process. Figure 6.14 shows this trend clearly. The hardness measurement shown in Figure 6.15 and Figure 6.16 demonstrates that the material’s strength at heat affected zone is lower than the weld zone. That explains the crack would propagate along heat affected zone and then penetrate through the steel wall and break the end cap entirely.

Figure 6.14 Cross section view of a fracture area in 0.25” billet.
6.4.3 Modification of the billet design

In order to avoid the cracking of billet during heating, there are several methods proposed. One of them is to modify the initial billet design.

Figure 6.15 Hardness measurement at weld zone, 0.25” billet.

Figure 6.16 Hardness measurement at weld zone, 0.25” billet.
6.4.3.1 Removal of the Al

This is the simplest modification that can be made for the current billet to avoid cracking issue. The idea is to cut off a portion of the Al and leave an empty space at the inside of the billet. As a result, the Al core can expand freely inside the billet without pushing the steel casing during heating, since the air is easily to be compressed relatively. Figure 6.17 exhibits several possible ways of cutting part of the Al core from the inside of the billet. Since all the billets were already made and welded from both ends, they have to be cut open from one end and then the Al can be removed. After that, the billet should be welded together again. Although this sounds like a “clumsy” work, it is the most economic and effective way to solve the cracking issue in heating.

Figure 6.17 Several ways of cutting the Al. (a) and (b) machining the Al from the top of the billet; (c) machining the Al core into a smaller cylinder in diameter.

If the Al removal method (a) from Figure 6.17 is used, the height of the empty space has to be determined to ensure the crack will not happen during heating. Such height or the amount of Al to be removed can be calculated using FE simulations.

6.4.3.2 Redesign the billet and the forging method

A second solution of the problem is to entirely re-think about the forging method. Instead of making a simple bi-metallic billet and hot forge it into certain shape, the steel casing can be hot or
cold forged into some desired shape, then the Al core can be cold forged into the steel casing to fully fill the cavity. The original objective of this project is to reduce the weight of the gear or similar components by replacing the steel parts with bi-metallic parts. Thus, if the cold forging method can be used to make the part, the difficulties in hot forging will be avoided. Also, the cost of manufacturing would be further lower down. One important concern is the bonding strength between Al and steel, since Al is designed to carry a small portion of torque during transmission. In hot forging, it is expected to have a metallurgical bonding formed at Al-steel interface. While in cold forging, the Al-steel interface could be designed with mechanical interlock so that the torque could be transmitted. Such mechanical interlock could be much stronger and more reliable than metallurgical bonding. Figure 6.18 One example of billet design for cold forging. shows a possible bi-metallic billet design for cold forging. The steel casing should be forged into the designed shape with inner “teeth”. Then the Al core could be cold forged into the steel casing to form a strong mechanical interlock.

Figure 6.18 One example of billet design for cold forging.
6.4.4 Induction heating

Another proposed solution is avoid cracking of the billet is to use induction heating. Indeed, most of the industrial applications use induction heating method to heat up the billet in hot forging. However, the technical requirement of the heating method in bi-metallic forging is quite different from conventional heating. In traditional forging application, the billet is recommended to be heated to the forging temperature uniformly, i.e. the temperature gradient within the billet – from center to surface or from the two ends to the middle – is as small as possible. However, as we discussion in section 3.4.2, induction heating was used in bi-metallic forging to avoid shrinking gap by heating up the billet with a steep temperature gradient from Al core to steel casing. A sharp temperature gradient could be achieved due to the skin effect of induction heating which has been explained in the literature review. Here in this section, the sharp temperature gradient in the billet provides another advantage that be utilized to prevent cracking of the billet other than the avoidance of the shrinking gap.

In slow furnace heating test, Al core is always at the same temperature level as steel casing due to the heat transfer at Al-steel interface. As a result, the volume of Al core expands much more than steel casing due to the difference of CTE. If the billet could be heated up by induction coil in a way that steel casing reaches to the forging temperature (1150°C) very fast while the Al core remains at low temperature (lower than 400°C), the amount of thermal expansion of steel casing could be equivalent or larger than Al core. Therefore, there will be enough free space inside the billet to allow Al core to expand instead of pushing the end cap to cracking.

6.4.4.1 Heating Experiments at Contour Hardening

Equation 3.1 in section 3.4.2 provides a simple way to estimate the temperature range that will ensure same amount of thermal expansion of Al and steel. Behrens’ study suggested the temperature gradient to avoid shrinking gap during cooling. Our study is more or less like a
reverse process of Behrens’ study. In both studies, it is expected to make the Al core and steel casing have the same amount of volume expansion by controlling the temperature gradient.

To verify the effectiveness of the induction heating method, some preliminary induction heating tests were conducted at Contour Hardening Inc. Both 0.25” and 0.5” billets were tested using the induction coil which is designed by Contour Hardening. Some parameters of the induction heating power generator are:

- 600 kW
- 10 kHz
- Heat Station Design – Caps with Transformer on secondary
- Voltage control

The observations from the test are:

1) **Test 1. 0.5” wall billet**

Heat 438Kw @ 7.5Khz for 30 seconds, dwell for 10 seconds without heat then cool down.

Observations: Surface (sub-surface) of the part hit curie temperature around 22 seconds into the cycle, confirmation was by the drop in current and spike in frequency/voltage. Visually the curie temp appeared to happen around 28 seconds.

Heat was shut off and allowed to air cool for 10 seconds. The heat was sucked out of the casing after about 3-5 seconds. Part was cooled down with water. Neither of the caps separated and no Aluminum was expelled.

2) **Test 2. 0.5” wall billet**

Heat 438Kw @ 7.5Khz. for 45 seconds, dwell for 10 seconds without heat then cool down.

Observations; Surface(sub-surface) of the part hit curie temperature around 22 seconds into the cycle, confirmation was by the drop in current and spike in frequency/voltage. Visually the curie temp appeared to happen around 28 seconds.
Heat was shut off and allowed to air cool for 10 seconds. The heat was sucked out of the casing after about 8 seconds. Part was cooled down with water. Neither of the caps separated and no Aluminum was expelled. However during the cooling we heard cracking more than likely in the weld area.

3) Test 3. 0.25” wall billet

Heat 438Kw @ 7.5 Khz for 30 seconds, then air cool (No dwell and water cool down).

Observations; Surface (sub-surface) of the part hit curie temperature around 15 seconds in the cycle, confirmation was by the drop in current and spike in frequency/voltage. Visually the curie temp appeared to happen around 18 seconds. The heat was shut off and the part was allowed to air cool. The part stayed really red/orange for about 15 seconds then the heat was quickly sucked out of the outer casing. No separation of the caps was seen and not Aluminum was expelled.

4) Test 4. 0.25” wall billet

Heat 438Kw @7.5Khz for 40 seconds then air cool (No dwell and water cool down).

Observations: surface (sub-surface) of the part hit curie temperature around 15 seconds in the cycle, confirmation was by the drop in current and spike in frequency/voltage. Visually the curie temperature appeared to happen around 18 seconds. The heat was shut off and the part was allowed to air cool. During the heat cycle, approximately 30 seconds into the heat cycle the outer casing formed a metal slag and began to deform. The billet stayed red/orange for about 20 seconds then the heat was gradually sucked out of the casing and the entire billet had a dull color for several seconds. Billet continued to hold the heat for some time. Once cooled the welded caps appeared to be intact with no Al leakage. After removal and completely cooled, the slag on the OD of the billet was able to be chipped off. It appeared initially the outer casing was melting however after removal of the slag the casing looks to be intact. The surface almost appears
blistered. After cutting the sample, it appears the Al only melted on the surface near the steel casing. The near surface Al appears to be oxidized with the steel case.

The test results shows that by carefully design the induction heating process, frequency, power, heating time, etc., the bi-metallic billet can be heated up to the desired temperature gradient without cracking the cap.

6.4.4.2 Induction Heating Simulations

In order to effectively design the induction heating method, numerical calculation of induction heating the bi-metallic billet has to be conducted. The FEM software DEFORM™ includes the induction heating simulation function. However, such feature is a new developed function which still needs additional refinement. The software is able to simulate the induction heating process of a conventional solid steel billet. But it is unable to calculate the temperature change once both Al and steel are present in the model. Thus, all the induction heating simulations and calculations were conducted by an outside company Fluxtrol Inc. Fluxtrol is expertise in calculating the temperature variation in different induction heating application with their own developed software ELTA™ and developing the induction heating method including coil design and production process design.

The first simulation strategy prepared by Fluxtrol is:

i. Make initial calculations on central area using ELTA (1-D coupled EM + Thermal)

ii. Run Calculations for what could be done on Contour Hardening’s machine for testing

iii. Run Calculations for what is possible with equipment in South Bend

iv. Send preliminary Temperature and Power Density curves for South Bend to OSU for forming Simulation

v. Based upon feedback from OSU, adjust power/time profiles to converge upon the best possible process with existing equipment
vi. Work with OSU to determine best possible process with other equipment that could be purchased at a later date

The temperature calculation results with Test 1 condition on 0.5” wall are shown in Figure 6.19 and Figure 6.20.

Some notes from the initial study by Fluxtrol are:

- The thermal cycles modeled agree well with experimental observations except for the power value
- The thermal cycles are slow and there is only a small gradient between the steel and aluminum temperatures
- This limits maximum temperature of the steel after 10 second delay to 650 – 700 C maximum

After the preliminary study, more simulations were conducted by Fluxtrol to determine a better heating procedure to achieve the desired temperature gradient. The simulation strategy is:

- Using 600 kW, 10 kHz Power Supply
  - Steel wall thickness ½”
    - Max steel temperature < 1200 C, Max Al temperature < 600 C
    - Max steel temperature < 1350 C, Max Al temperature < 600 C
  - Steel wall thickness ¼”
    - Max steel temperature < 1200 C, Max Al temperature < 600 C
    - Max steel temperature < 1350 C, Max Al temperature < 600 C
- 10s transfer time (assumed based on the experience from Eaton’s South Bend plant)

Some simulations results are shown in Figure 6.21 and Figure 6.22.
Figure 6.19 Temperature Color Map: Test 1 – 0.5” Wall.

Figure 6.20 Temperature vs Time: Test 1 – 0.5” Wall.
Figure 6.21 Temperature Color Map. ½” Steel Wall Thickness. 8.8 second heating time, 10 second transfer time, maximum steel temperature < 1200 C.

Figure 6.22 Temperature vs. Time for Different Radii. ½” Steel Wall Thickness. 8.8 second heating time, 10 second transfer time, maximum steel temperature < 1200 C.
Summary of the preliminary runs by Fluxtrol can be concluded as:

i. For all cases, during the 10 s transfer a significant amount of heat is lost from the steel.

ii. For ¼” thick steel, with 600 kW, 10 kHz power supply the temperature after 10 s of the steel will be around 600 C if the temperature of the aluminum is kept below 600 C during the process.

iii. For ½” thick steel, with the proper cycle the surface of the steel will be 750-800 C after 10 s transfer.

iv. The simulation is done with the assumption that there is ideal thermal contact between the aluminum and steel. If the steel separates from the aluminum, temperature rise will be much more rapid, steel cool down greatly reduced and aluminum temperatures will be much lower.

v. A review of the coil matching at Contour Hardening should be made to increase the real power output of the generator and experiment with faster, higher power tests.

Based on Fluxtrol’s simulation results, it can be concluded that although the sharp temperature gradient between Al and steel could be achieved, it can only remain for only a few seconds. If such heating process is utilized in practice, after the billet is transferred into the press, the temperature of Al and steel will reach a uniform state. Also, after about 20s, the Al temperature will go up to about 600C, where melting will occur. Then the billet may crack again even before the forging operation starts.

An assumption made by Fluxtrol in their simulation is the Al and steel are in ideal contact during the entire heating cycle. As a result, the heat transfer at Al-steel interface is severe through conduction and results in rapid temperature increasing in Al. However, if the high frequency and power induction coil is used, the steel casing will be heated up first and expands quickly before Al core, and then get separated from Al core. Consequently, the heat transfer between Al and
steel will become radiation, which is much slower than the assumed conduction. In this way, the steel casing could be heated up to the forging temperature faster while the Al could remain at a much lower temperature. Since Fluxtrol’s software ELTA cannot simulate the elastic deflection of the billet, such study was investigated using DEFORM.

The simulation strategy to predict Al-steel separation is:

i. The temperature variation vs radical distance in bi-metallic billet is provided by Fluxtrol, with time interval of 3 seconds.

ii. The bi-metallic billet is modeling is DEFORM with elastic-plastic model, which can predict the elastic deflection with temperature change.

iii. Based on the temperature distribution from Fluxtrol, we will approximate the temperature in Al and steel by applying boundary conditions. The expansion of Al core and steel case will be simulated separately.

iv. The temperature variation data will be input into the DEFORM model for each time interval, from time 0sec to 30sec.

v. Two ways to approximate temperature distribution:
   - Linear temperature gradient. (time consuming)
   - Average temperature. (very small error)

The expansion by these two methods are approximately the same.

vi. Once the Al and steel are heated up, elastic deflection will be estimated by DEFORM at each time point.

vii. The criterion to determine the separation is: once the ID of the steel casing is larger than the OD of the Al core, separation occurs.

viii. After separation occurs, the temperature variation calculated by Fluxtrol is not accurate any more, since it is estimated by assuming perfect contact between Al and steel. The
temperature distribution after separation has to be re-calculated based on the new heat transfer coefficient (radiation) at Al-steel interface.

The temperature calculation results of Test 3 are used in this study. The temperature variation within the billet is shown in Figure 6.23.

![Temperature vs Radius at Different Times for Test 3](image)

Figure 6.23 Temperature vs Radius at Different Times for Test 3.

In order to simulate the temperature gradient in steel and Al, suitable boundary condition is used to approximate the temperature variation within the billet, Figure 6.24. Also, average temperature in Al and steel can be easily applied in the FEM model to simulate the thermal expansion Figure 6.25. The comparison in Table 6.6 shows that the elastic deflection predictions with the two methods are almost identical. So either way could be used in FE simulation to estimate the separation.
Figure 6.24 Assumed Boundary Conditions of Heating

Figure 6.25 Comparison of the two temperature approximation methods.

<table>
<thead>
<tr>
<th></th>
<th>average temp</th>
<th>linear temp</th>
<th>Error</th>
</tr>
</thead>
<tbody>
<tr>
<td>OD of Al(mm)</td>
<td>38.56</td>
<td>38.6</td>
<td>0.1%</td>
</tr>
<tr>
<td>ID of steel(mm)</td>
<td>38.34</td>
<td>38.33</td>
<td>0.2%</td>
</tr>
</tbody>
</table>

Table 6.6 Simulation errors in elastic deflection
<table>
<thead>
<tr>
<th>time (s)</th>
<th>OD of Al (mm)</th>
<th>ID of steel(mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3</td>
<td>38.13</td>
<td>&lt; 38.16</td>
</tr>
<tr>
<td>6</td>
<td>38.18</td>
<td>&lt; 38.20</td>
</tr>
<tr>
<td>9</td>
<td>38.26</td>
<td>&gt; 38.23</td>
</tr>
<tr>
<td>12</td>
<td>38.33</td>
<td>&gt; 38.25</td>
</tr>
<tr>
<td>15</td>
<td>38.4</td>
<td>&gt; 38.28</td>
</tr>
<tr>
<td>21</td>
<td>38.56</td>
<td>&gt; 38.34</td>
</tr>
<tr>
<td>30</td>
<td>38.86</td>
<td>&gt; 38.43</td>
</tr>
</tbody>
</table>

Table 6.7 Elastic deflection simulation results

The simulation results in Table 6.7 indicate that the ID of the steel expands and becomes larger than OD of Al within the first 6 second. The result shows that the steel casing expands so fast during heating and right after the heating starts, it gets separated from Al core. After 6 seconds, the temperature calculation is not accurate anymore since separation occurs.

Another induction heating test at Contour Hardening Inc. proves this phenomenon. In this test, the bi-metallic billet used is not welded from top and bottom. Only the Al core is press fitted into the steel tube. Figure shows the setup of the test. The billet was placed in vertical position and the fixture only supports the Al core. Steel tube is held by press fit. Once the coil is turned on, steel casing turned red immediately and then dropped down. Al core was not melted. This phenomenon indicates during heating, steel tube got heated up and expanded quickly, while Al core was not heated up yet. In a very short period of time, ID of steel become larger than OD of Al core and press fit was gone. As a result, steel tube dropped down. This test demonstrated that during induction heating, separation occurs between Al and steel.

With this new information, Fluxtrol conducted the simulation again to optimize the induction heating process. The induction heating coil and power supply parameters provided by Interpower Corporation are:

- Coil
10 Turn Coil – split into two parallel sections
- 1/2“ x 1/2” tubing
- Length 7.25”

**Power Supply**
- 500 kW
- 7,000 A
- 10 kHz
- 308.4 µF

The heating cycle used in the 2D simulations are:
- 350 kW for 4.8 s to the coil head (does not include losses in the heat station, which can be 20-30% of total power)
  - Current Range: 5,500 A to 7,000 A

- 100 kW for 4.2 s
  - Current: 4,200 A

- No power for 10 seconds

Specifically, there is a separation between the ID and OD surfaces. A 0.2 mm gap with k = 0.12 W/(m*K) is present to represent radiant heat transfer between the surfaces.

Figure 6.26 to Figure 6.29 shows the temperature calculation in a 0.5” wall billet.
Figure 6.26 Temperature Distribution in radial direction vs. Time. 0.5” billet.

Figure 6.27 Temperature Distribution at ID of steel casing in vertical direction vs. Time. 0.5” billet.
Figure 6.28 Temperature Distribution at OD of steel casing in vertical direction vs. Time. 0.5” billet.

Figure 6.29 Temperature contour of 0.5” billet at forging.
6.4.4.3 Summary of induction heating study

i. Induction heating should be used in bi-metallic forging operation to heat up the billet to prevent cracking of the end cap.

ii. The induction coil, frequency, power supply has to be optimized in order to achieve the desired temperature distribution.

iii. The temperature distribution in the bi-metallic billet has to be carefully controlled by induction heating process so that the steel could reach to its forging temperature while Al core still remains at lower temperature to avoid melting.

iv. With the help of Fluxtrol and Interpower, the induction heating coil could be designed to heat the bi-metallic billet successfully for the forging test at next stage.

6.5 Closed-die design

6.5.1 Issues in upsetting the billet

After the heating method is determined, the forging die needs to be designed for the forging experiment. The final objective of the bi-metallic forging project is to near net-shape forging the gear teeth. However for the first forging trial, the tooth forging will be neglected. Only a “pancake” shape is recommended to forge for a proof of concept. As mentioned in section 3.3, the initial plan for a “pancake” forging is a two stage forging: a) upsetting with a pair of flat dies; b) closed-die forging into a “pancake” shape with straight outer wall.

However, due to billet cracking and leaking of liquid Al in the furnace heating test, the initial plan has to be revised. In the furnace heating test, the un-weld zone performs as a pre-existing “cracking” and easily opens up to initialize the crack at the tip of the weld. Compared with the fracture mode in furnace heating test, such opening up of the un-weld zone will occur also in the upsetting of the bi-metallic billet, Figure 6.30. Simple FE simulation of upsetting test in Figure 6.31 shows that once the upsetting starts, the un-weld zone will open up due to the geometric...
relation. Thus, cracking will definitely occur at the corner area. So, an alternative forging method is needed to avoid cracking of the billet.

Figure 6.30 Schematic of the critical area that would like to crack during upsetting.

Figure 6.31 Demonstration of the un-weld zone opening during upsetting.
6.5.2 Alternative forging methods

Two solutions were proposed for the billet cracking issue: a) change the weld zone design to eliminate un-weld opening; b) change the die design to prevent cracking.

6.5.2.1 Modification of the weld zone design

Figure 6.32 shows one possible modification of the weld zone design. Instead of the original flat edge with a step on the cap, the new cap is featured with a tapered edge. The tapered edge can help to position the end cap onto the tube during welding, which functions the same as the small step in the original design. Also, the weld will penetrate into the end cap because of the angled direction, which can protect the Al from touch the weld arc, although at the tip of the weld zone at ID of steel tube, the contact of the Al and arc is not avoidable. So before we can test the effectiveness of such modification, the welding method of such design is already a challenge.

Figure 6.32 Modification of the weld zone design.
FE simulation in Figure 6.33 shows the deformation of the new weld zone in upsetting. Figure 6.34 shows the comparison of the weld zone opening at the tip area for both original design and the new proposed design. It can be seen that due to the geometric relation, the high tensile stress concentration is unavoidable. However in the tapered weld zone design, the maximum tensile stress concentration occurs away from the weld zone. As discussed in section 4.4.2, a pre-existing “crack” is more easily to crack and the strength of the material at the heat affected zone is weaker than the base material. If the maximum stress concentration occurs at the tip of the weld zone, the material would like to fracture with ease and propagate along the heat affected zone. In the new tapered weld zone design, the stress concentration is away from the tip of the weld zone and the heat affected zone. Therefore, the material is less likely to crack compared with the original design.

Figure 6.33 New tapered weld zone design and the deformation of the critical area in upsetting.
Figure 6.34 Comparison of the stress concentration and the opening of the “crack” for the original and new tapered weld zone design after upsetting.

Another simulation to verify the new weld zone design is to calculate the tensile stress or the normal pressure along the weld zone. Figure 6.35 and Figure 6.36 are the normal pressure along the weld zone line during upsetting for both old and new design. It can be seen that in the old design, the weld zone is under tensile stress in most of the time during the entire forging stroke; while with the new tapered weld zone design, the weld is under compression mostly. Superficially, cracking is triggered by the tensile stress, which indicates the old design would be more likely to crack. The new tapered design is relatively safer since the weld area is under compression most of the time.
6.5.2.2 Modification of the die design

Initially, the “pancake” shape was planned to be forge with two stage forging, upsetting and closed die forging. Since in the first stage, the billet is forged between two flat dies, cracking
would like to occur. The modification of the weld zone is also based on the two stage forging plan. Another way to prevent cracking is to change the forging plan and forge the billet completely in a closed die, without modifying the current billets. The concept is shown in Figure 6.37. The cavities on the top and bottom die are used to constrain the metal flow at the top and bottom portion of the billet. As a result, cracking will not occur since the un-weld corner will not open up during the entire forging stroke.

Figure 6.37 Schematic of the closed-die design for bi-metallic forging.

Comparing with the change of the weld zone, there are several reasons for the modification of the die design:

i. The bi-metallic billets have been made and welded already. The change of the weld zone means the disposal of all the current billets and preparation of the new billet. This is not economic method.
ii. Although FE simulations have shown the new taper weld zone could help to reduce the risk of cracking during upsetting, the end cap may still crack during upsetting. Considering the possibility of the melting of Al, liquid Al might shot out from the billet if cracking occurs since the forging press runs fast. There is a severe safety issue involved in this operation.

iii. If the billets can be forged in the closed die, the end cap and weld zone can be protected by the special designed die shape. Also in case of accident happening, i.e. liquid Al shooting out, the high temperature liquid Al will stay in the closed up without jeopardizing the operator.

Of course there are some cons in this modification of the die set:

i. Only the middle portion of the billet will be formed. The forged part will not be a “pancake” shape. Instead, there will be some portion at the top and bottom un-deformed after forging.

ii. The billet is designed based on the target gear geometry mention in section 4.2. If the top and bottom of the billet will not formed, the OD of the forged part will not be as large as the target gear model.

iii. Although in the first forging trail, the die set with top and bottom cavities can be used to prevent cracking, the billet still needs re-design in the next forging trial, since the shape of the forged part is not desired.

In the first forging trail, the closed-die design with cavities was adopted to forge the current billets.

6.5.3 Closed die configuration design for the first forging trial

After the overall closed-die forging plan is determined, it is necessary to design the detailed configuration and dimensions of the die. The die design strategy is:
i. Flash design. Selection of the vertical flash or horizontal flash.

ii. Design of the cavity.

iii. Die stress analysis.

iv. Finalizing the die design.

6.5.3.1 Flash design

Flash is necessary in this die design to prevent the possible failure of the die due to overloading, considering the many instabilities in heating and forging the bi-metallic billet. Either vertical flash or horizontal flash could be used in this design. The concepts are shown in Figure 6.38 and Figure 6.39. After consulting with the engineers at Walker Forge Inc., who will build the tooling and conduct the forging test, vertical flash design is preferred. This is because in the horizontal flash design, the upper die and middle ring will “kiss” at the end of the stroke, which will cause significant shock on the press. More than that, in the horizontal flash design, tilting of the upper die is likely to occur due to any non-symmetrical positioning of the billet or the elastic deflection of the press itself, resulting in over-filling of the part corner at one side and non-filling at the other side. If the material flows over the flash area, it may also cause die failure. In vertical die design, the upper die will not touch the lower and or the ring. Also, the outer ring can also function as guidance for the upper die so that tilting could be reduced.
Figure 6.38 Vertical flash design.

Figure 6.39 Vertical flash design.
6.5.3.2  Design of the die cavity

The cavity is added to the top and bottom die to protect the end cap from cracking, which is the key idea for changing the die design. The design of the cavity will influence whether it will successfully prevent cracking or not. There are a few aspects to consider in designing the cavity shape:

i. Diameter of the cavity. Basically, the idea of the cavity is to constrain the metal flow at the end cap and prevent the opening of the un-weld zone. So ideally, the diameter of the cavity could be the same as the billet diameter so that the end cap of the billet does not open or bulge out at all. However, since the billet has to be placed in the bottom cavity and the top portion of the billet will slide into the top cavity during forging, the diameter of the cavity must not be too small. Also, taper angle must be added to the cavity so that the top portion of the billet can go into the top cavity easily. Also, a taper angle is necessary for the removal of the forged part from the die after forging. A demonstration of the top and bottom cavity design can be seen in Figure 6.40. Notice here that the 5° taper angle is selected randomly as a demonstration is the figure. At the bottom of the cavity, the diameter is slightly larger than the diameter of the billet to ensure minimum opening of the two ends of the billet. Gradually the diameter of the cavity expands due to the taper angle. A chamfered corner is added to the die to avoid stress concentration at sharp corner.

ii. Another consideration is the depth of the cavity. Figure 6.41 indicates that the depth of the cavity has to be large enough to prevent the corner opening. Especially for the 0.5” billet. If the cavity is shallow, the corner may still open up and trigger the cracking of the weld zone. Obviously, the depth of the cavity will be smaller for the 0.25” billet.
However, only one die set will be made in the first forging trial. Thus, all the cavity design will be based on the 0.5” wall billet.

Figure 6.40 A schematic of the top and bottom cavity design.

Figure 6.41 Influence of the depth of the cavity. 0.5” billet.
6.5.3.3 Die stress analysis

The first two die designs are shown in Figure 6.42. To estimate the die stress, forging operations were simulated. Table 6.8 shows the simulations parameters. The billet temperature used in the simulation is shown in Figure 6.43. The temperatures are assumed to be such that the steel case is heated up closed to 1000°C. The Al core remains at low temperature (350°C). Based on the calculations from Fluxtrol, steel casing could be heated to a higher temperature. However in reality, such temperature gradient may not be achieved considering the experimental variables. Thus, the temperature assumption in the simulation is a conservative estimation.

Figure 6.42 Draft of the initial die design: a) OD = 160mm; b) OD = 140mm.
<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al core temperature</td>
<td>Gradient (average about 400°C)</td>
</tr>
<tr>
<td>Steel case temperature</td>
<td>Gradient (average about 1000°C)</td>
</tr>
<tr>
<td>HTC (W/m²K) between steel and die</td>
<td>11 (general value for hot forging)</td>
</tr>
<tr>
<td>Friction shear factor</td>
<td>0.3</td>
</tr>
<tr>
<td>Initial die temperature</td>
<td>200°C</td>
</tr>
<tr>
<td>Part outside diameter</td>
<td>160mm (50% height reduction)</td>
</tr>
<tr>
<td></td>
<td>140mm (sample gear size/ provided by Eaton)</td>
</tr>
<tr>
<td>Press speed</td>
<td>50 strokes/min</td>
</tr>
<tr>
<td>Total stroke</td>
<td>300mm</td>
</tr>
</tbody>
</table>

Table 6.8 Parameters for forging simulations

![Temperature distribution in the billet for FEM simulations](image)

Figure 6.43 Temperature distribution in the billet for FEM simulations.

Forging load prediction and maximum die stress in top die are shown in Figure 6.44 to Figure 6.47.
Figure 6.44 Forging load prediction with OD=160mm die at different punch stroke.

Figure 6.45 Maximum effective stress in the top die with OD=160mm die.
Figure 6.46 Forging load prediction with OD=140mm die at different punch stroke.

Figure 6.47 Maximum effective stress in the top die with OD=140mm die.
The maximum die stress of OD=140mm die is much larger than the stress predicted in OD=160mm die. This is because in the simulation, flash was formed at the end of the punch with 140mm die, resulting in extremely high press load, compared with the 160mm die, when the flash was not formed. Generally, H-13 is used for the die material in hot forging due to its high strength at elevated temperature. The strength of H-13 as function of temperature is shown in Figure 6.48. For both cases, the die stress at corner is higher than the strength of the material. Therefore, some modifications are required to lower the die stresses to prevent the die failure during forging. One modification method is to add chamfer at the die corner where high stress concentration exhibits. An example modification is shown in Figure 6.49. The press load and the die stress prediction are shown in Figure 6.50 and Figure 6.51.

![Figure 6.48 Yield strength of H13 as a function of temperature. [Matweb, 2013]](image-url)
Figure 6.49 Adding chamfer at the die corner to reduce the stress concentration. Left: chamfered corner has to be machined on the billet to fit in the die; Right: the shape of the die with chamfer corner.

Figure 6.50 Press load prediction with the modified chamfered corner die.
It can be seen from the die stress prediction that although a large chamfer is added to the die corner, the die stress is still higher than the strength of the die material (H-13) (1860MPa>1100MPa). Such high stress occurs even before the press load reaches the maximum. It is not practical to keep increasing the chamfer corner although it can actually reduce the die stresses to some extent. Also, another machining process is required to cut the fillet on the billet in order to fit into the die.

Another way to reduce the die stress at the corner is to use split die instead of one piece. The concept is shown in Figure 6.52. Figure 6.53 shows two initial die set design with dimensions by Walker Forge, who will produce the die. Compare with the die design is Figure 6.53 a) and b), the advantage of design b) is the lower the top holder to prevent the elastic deflection of the bottom portion of the top middle die from bending outward. Therefore, all the next die designs are all follow this concept.
Figure 6.52 Split die design to release the stress concentration.

Figure 6.53 Two initial die design by Walker Forge.
As mentioned above, there are two OD selections: 160mm and 140mm. The only difference of these two designs is the OD of the part. With smaller OD of the part (140mm), less material will be formed, and more material at the top and bottom portion of the billet will be kept in the top and bottom cavity without any deformation. According to Eaton’s request, OD=140mm is more closed to the actual gear size in the truck transmission. Therefore, the die design will focus on the OD = 140mm. If the billet will be forged into a 140mm OD, most of the material will remain un-deformed. The forged portion (flange) can be at the middle height of the billet or near the top of the billet, Figure 6.54. According to Walker Forge’s suggestion, keeping most of the un-deformed material at bottom die is easy for spotting the billet as well as for the billet to slide into the upper die. Therefore, the plan in Figure 6.54 b) is accepted. Figure 6.55 shows the final draft of the closed-die design concept.

Figure 6.54 Position of the forged portion (flange): a) at the middle height; b) close to the top.
Die stress analysis was necessary with the final die design with both 0.25” and 0.5” billet. Before the bi-metallic forging experiment, a test-run of forging solid steel billet 4150 using the final die set was completed at Walker Forge. No obvious die failure was observed at die corner area. Thus a simulation with solid steel billet 4150 was conducted first. It can be seen from the die stress analysis that the stress concentration occurred in the previous one piece die design has been released completely, Figure 6.56. The maximum die stress occurs at the bottom corner of the middle die. The maximum stress is 2120MPa, which is still higher than the strength of the die material. However based on Walker Forge’s report, no obvious plastic deformation was observed on the tooling. Thus, it can be concluded that the FE software overestimated the die stresses.
Figure 6.56 Forging 4150 solid steel billet with final die set and the maximum die stress. Stroke: 405.9/406mm. Press load: 973ton. Filling corner: 1.5mm.

Based on the new information, die stress analysis was conducted with both 0.25” and 0.5” billet. Most simulation parameters are the same as shown in Table 6.8. However, since the desired forging temperature of the bi-metallic billet may not be achieved due to its complicity, three forging temperature combination with steel temperature at 1100C, 1000C and 900C and Al temperature 350C were used in the simulations. The results are shown in Table 6.9. Figure 6.57 and Figure 6.58 illustrates the definition of filling corner and the location of the maximum stress. Figure 6.59 and Figure 6.60 shows the comparison of the press load and maximum die stresses in different cases in bar chart.
<table>
<thead>
<tr>
<th>Sample#</th>
<th>Material</th>
<th>Steel temp (C)</th>
<th>Al temp (C)</th>
<th>Press Load (ton)</th>
<th>Filling corner (mm)</th>
<th>Max stress at die corner (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>solid steel</td>
<td>1100</td>
<td></td>
<td>1006</td>
<td>3.03</td>
<td>2230</td>
</tr>
<tr>
<td>2</td>
<td>solid steel</td>
<td>900</td>
<td></td>
<td>1578</td>
<td>3.14</td>
<td>3610</td>
</tr>
<tr>
<td>3</td>
<td>Al</td>
<td></td>
<td>350</td>
<td></td>
<td>2.8</td>
<td>2030</td>
</tr>
<tr>
<td>4</td>
<td>0.5&quot; steel wall</td>
<td>1100</td>
<td>350</td>
<td>867</td>
<td>3.18</td>
<td>1990</td>
</tr>
<tr>
<td>5</td>
<td>0.5&quot; steel wall</td>
<td>1000</td>
<td>350</td>
<td>1150</td>
<td>3.08</td>
<td>2630</td>
</tr>
<tr>
<td>6</td>
<td>0.5&quot; steel wall</td>
<td>900</td>
<td>350</td>
<td>1464</td>
<td>3.19</td>
<td>3320</td>
</tr>
<tr>
<td>7</td>
<td>0.25&quot; steel wall</td>
<td>1100</td>
<td>350</td>
<td>950</td>
<td>3.1</td>
<td>1930</td>
</tr>
<tr>
<td>8</td>
<td>0.25&quot; steel wall</td>
<td>1000</td>
<td>350</td>
<td>1221</td>
<td>3.2</td>
<td>2720</td>
</tr>
<tr>
<td>9</td>
<td>0.25&quot; steel wall</td>
<td>900</td>
<td>350</td>
<td>1639</td>
<td>3.2</td>
<td>3600</td>
</tr>
</tbody>
</table>

Table 6.9 Simulation results

Figure 6.57 Illustration of the filling corner.
Figure 6.58 Location of the maximum die stress at the end of the forging stroke.

Figure 6.59 Comparison of the press load.
Figure 6.60 Comparison of the die stresses.

Some conclusions can be made from the comparison:

i. Temperature of the steel influences the press load as well as die stresses significantly. In order to lower the die stress and prevent die failure, the steel casing must be heated above 1100°C.

ii. The absolute values predicted from FEM software are questionable. From the actual forging experience, the FEM software would like to overestimate the die stresses. Therefore, the maximum die stress value predicted in forging a solid steel billet (~2000 MPa) was used as the die stress limit.

iii. If the steel casing could be heated up to 1100°C or above, the die stresses predicted with either 0.25” or 0.5” wall billet are at the same level as the solid steel billet, which indicates the die would not fail. Still, it is suggested that the billets should be heated up to the desired temperature as discussed in section 4.4.4.
iv. It can be observed that the thickness of the billet does not have a significant influence on the press load prediction. The press load predicted from 0.25” and 0.5” billet are at the same level of solid steel billet at 1100C. This is due the fact that the flow stress of Al 7075 at 350C is at the same level of AISI 8620 at 1100C.

v. A conclusion can be made that the final die design could be used to forge bi-metallic billet in the actual forging test.
CHAPTER 7  Actual Bi-metallic Forging Test

After FE simulation of the entire bi-metallic forging procedure and the die design investigation, the actual forging test was conducted at Walker Forge.

7.1 Forging test report

Experiment Date: 06-06-2013

Location: Walker Forge, WI.

Participants: Bulent Chavdar, Xi Yang, Robert Goldstein (Fluxtrol), Jake Butkovtch (Walker Forge).

Bi-metallic samples:

<table>
<thead>
<tr>
<th></th>
<th>0.5&quot;</th>
<th>0.25&quot;</th>
</tr>
</thead>
<tbody>
<tr>
<td>seamless</td>
<td>8 (6 made by Bradley Thompson and 2 made by Eaton)</td>
<td>4 (BT made)</td>
</tr>
<tr>
<td>seam</td>
<td></td>
<td>10 (Eaton made)</td>
</tr>
</tbody>
</table>

The billets made by Bradley Thompson had 0.005” interference fit between steel tube and aluminum core while the billets made by Eaton had 0.003” interference fit.

Test Procedure:

1. Experiment starts from 8:00am.

2. Starting from 0.25” billet, due to thinner wall and easier heating process.
   a. Several test run were conducted to optimize cycle time.

   Heating requirement: steel is higher than 2000F/1100C; Al is lower than 750F/400C.

   No direct way to measure Al temperature. Fluxtrol estimated Al temperature is about 390~750F (200~250C).
b. The first 0.25” billet was induction heated. White material was spotted at billet attaching to the billet outer surface. The material was found at three locations which were evenly distributed around the billet perimeter (about 120 degree interval). Initial guess is the white material could be the leaking Al from inside the billet, or the melted ceramic powder from the induction coil. The billet was air cooled for a while and then water cooled back to room temperature. By visual inspection, there is no cracking at the welding zone or leaking Al. As a result, it is confirmed the while material found outside the billet is the melting ceramic from induction coil.

c. After the billet was induction heated and cooled down, we can hear the Al insert bouncing if shaking the billet hard. That indicates that the thermal expansion of the Al after heating deformed (push out) the steel case. After the billet cools down to room temperature, the inner volume of the steel case become larger than the original Al.

d. The first billet was successfully forged. To protect the tooling, the flange corner is not fully filled. Flange bottom corner filling OK, top corner un-filled.

e. The die position was adjusted down by 0.1”. Corner was filled better, still not fully filled.

f. The die was removed from the press to further adjust the position in order to bring down the die position to fill the corner.

g. With new die set up, flange corner was filled better. Two billet cracked consecutively at the die chamfer area.
h. One seamless 0.25” billet is forged to test the effect of the seam on the steel tube.

The billet cracked at the same location. The conclusion is the seam on the tube is not the trigger of cracking.

3. Forging tests with 0.25” billet stopped since the cracking issue cannot be solved. Totally 5 billets were tested. The first 2 were forged without cracking, but there was large unfilled corner. The later 3 billets were filled better at the corner, but cracking occurred.

4. Start the forging test with 0.5” billet.

a. Several test run were conducted to optimize cycle time.

   Heating requirement: steel is higher than 2000F/1100C; Al is lower than 750F/400C.

   Due to the thicker steel wall, the billet requires long heating time and the Al temperature will be higher compared to the 0.25” billet.

b. 0.5” billet was forged successfully without cracking. The flange corner is filled well. Totally five 0.5” billets were forged with the same heating procedure.

5. In order to solve the cracking issue of the 0.25” billet, more tests were conducted with new 0.25” billet.

   a. New heating cycle time was selected to have: lower steel temperature and warmer Al temperature.

   b. Steel temperature after induction heating dropped down to 2000F and 1800F, respectively.

   c. Billet still cracked.

   d. It is observed that the steel wall at the fracture zone gets thicker if steel is at lower temperature.
6. In order to validate the concept of hot “hydro-forging”, which means the Al is in liquid state, one more test was run with molten Al, with 0.5” billet.
   
a. Several test run were conducted to optimize cycle time. Noticeable longer heating time was applied to melt the Al in the billet.
   
b. No cracking was observed during heating and transferring.
   
c. Right after forging, leaking Al was observed immediately when the operator was trying to take the billet out of the die. Hot liquid Al was leaked out from the billet.
   
7. One last test is the open die forging with 0.5” billet.
   
a. The purpose of the test is to investigate how the crack would occur during open die “pancake” forging.
   
b. For safety consideration, the heating cycle was setup to keep Al in solid state.
   
c. The billet blew out during forging and liquid Al shot out. No people got hurt.
   
d. Al is in liquid state instead of solid state as it is required. This is different from Fluxtrol’s estimation.
   
8. Experiment ended for the day, at about 3:00pm.
### Summary of the load and temperature

<table>
<thead>
<tr>
<th>Billet wall thickness</th>
<th>Sample #</th>
<th>Load (ton)</th>
<th>Temperature (F)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.25</td>
<td>1</td>
<td>584</td>
<td>2143</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>609</td>
<td>2272</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>1237</td>
<td>2214</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>1294</td>
<td>2061</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>1148</td>
<td>1998</td>
</tr>
<tr>
<td></td>
<td>6</td>
<td>1315</td>
<td>1839</td>
</tr>
<tr>
<td></td>
<td>7</td>
<td>1303</td>
<td>1864</td>
</tr>
<tr>
<td>0.5</td>
<td>8</td>
<td>1471</td>
<td>2229</td>
</tr>
<tr>
<td></td>
<td>9</td>
<td>1400</td>
<td>2278</td>
</tr>
<tr>
<td></td>
<td>10</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td></td>
<td>11</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td></td>
<td>12</td>
<td>N/A</td>
<td>N/A</td>
</tr>
</tbody>
</table>

Note:

1. All the billet temperature in the table above are the peak temperature in the induction oil. The actual forging temperature will drop ~200F (approximate) during transferring.

2. # 1,2: part corner is not filled. Press load is low.

3. # 3,4: die set was removed from the press and adjusted to reduce the flange thickness, in order to fully fill the part corner.

4. # 5-7: low down the steel temperature in order to reduce cracking.

5. #10-12: the load and temperature are not recorded because the experimental setup are the same as sample 8 and 9. The load and temperature are similar to sample 8 and 9.

6. #2 and #8 were cross sectioned (cut in half) to observe the steel and aluminum interface development on the forged 0.25” and 0.5” billets respectively.

The entire forging experiment procedure is shown in Figure 7.1. Figure 7.2 shows the billet samples before forging and some forged parts. Figure 7.3 shows the induction heating system.
Figure 7.1 Bi-metallic experiment procedure. a) induction heating; b) billet transferred to the die; c) billet forged in the press; d) billet removed from the press; e) forged part, several seconds after forging.
Figure 7.2 a) billet samples before forging; b) forged parts.

Figure 7.3 Induction heating system.
7.2 Observations from experiment

7.2.1 0.25” wall billet

The shape of the 0.25” billet from first two trials is shown in Figure 7.4. Since the upper corner was not filled completely, the press stroke was adjusted to reduce the distance between the top and bottom die to reduce the flange thickness, in order to tightly fill the corner. After adjustment, the part shape is shown in Figure 7.5. However after the press was adjusted and the part corner is tightly filled, steel wall cracked at the flange corner area either at top or at the bottom, Figure 7.6. Although a few adjustments were made, cracking was not able to be prevented. The part corner cannot be filled completely.

Figure 7.4 Shape of the 0.25” billet with non-filled corner.
Figure 7.5 Shape of the 0.25” part after adjustment of the press. Better corner filling.

Figure 7.6 Steel wall cracks at the flange corner area in 0.25” billet.

All the forged samples were cut and sand blasted. Figure 7.12 shows the cross section of two 0.25” wall parts. Thickness at the corner area was measured if the wall is not cracked. It can be
seen that the thickness of the steel wall becomes extremely low at the corner area after deformation. This is due to the fact that during forging, steel wall flows into the die cavity to form the flange. The diameter of the steel wall was expanded and the material was stretched. Also, the die corner and the Al core squeezed the steel wall to form a thin layer at the corner. Such deformation can be related to cup drawing in sheet metal forming, where the over thinning of the material occurs at the die corner area sometimes. This can explain the cracking of the steel wall in the forging test. By comparing the shape of the flange area in Figure 7.7 a) and b), we can easily find that in Figure 7.7 a) the corner was not fully filled. Although the thickness of the steel wall is very small, fracture did not occur. In Figure 7.7 b), the press was adjusted in order to tightly fill the part corner. Consequently, the steel wall at the corner was stretched additionally to fill the part corner and the thickness of the steel wall decreased further, resulting in the fracture of the wall finally since the steel wall was as thin as a piece of paper.

Another observation from the cross section is that folding occurred at the flange area in steel wall, as indicated in Figure 7.7. The metal flow of the steel wall and Al core was not stable, resulting in the non-symmetrical shape of the flange. The fracture location was not the same for all the forged part. As shown in Figure 7.7 b), steel wall would crack in either top corner or bottom corner randomly. The non-uniform deformation of the steel wall and Al core can explain such phenomenon. This will be discussed in more detail in the next section.

Also, it can be seen that a shrinking gap is formed at Al-Fe interface. Al core can move freely and even taken out from the steel shell, Figure 7.8. No firm bonding was formed. The formation of the gap can be explained by the difference of the coefficient of thermal expansion between Al and steel. This was discussed in Chapter VI. Induction heating method was used to generate sharp temperature gradient between Al and steel to avoid cracking as well as shrinking gap, as
discussed in Behrens’ study. However the experiment shows that shrinking gap still occurs after forging. The thickness and gap size measurements are summarized in Table 7.1.

Figure 7.7 Cross section of two 0.25” wall parts after cutting and sand blasting.
Figure 7.8 Al core a) was removed from steel shell b), indicating no firm bonding was formed.

The cut Al core was macro-etched to examine the flow curve of the material. The pictures are shown in Figure 7.9 - Figure 7.10. The clear view of flow lines at flange area indicates that the Al core was not melted during forging. The recrystallization during solidification and grain grown at the surface of the Al core proves that the Al melts near the ID of the steel wall, due to the heat transfer from steel.

Figure 7.9 Macro-etched Al core in a 0.25” billet sample.
Figure 7.10 Flow lines are clearly shown at flange area.

Figure 7.11 Recrystallization at the surface of the Al core.

7.2.2 0.5” Wall billet

For 0.5” billet, no corner non-filling issues were observed. One example of the 0.5” forged parts is shown in Figure 7.12. The cross section view of a cut 0.5” part is shown in Figure 7.13. For a 0.5” billet, significant wall thinning issue was not observed. The part corner was tightly filled. However, it can be seen that steel wall fractured at ID especially in the flange area. The fracture at the top and bottom weld zone is due to the same reason as in slow furnace heating, which is the
open up of the un-weld step. The fracture at ID of the steel wall was not predicted before the test. Thus, it requires more investigation.

Also, a bubble is formed at the upper right corner in Al core. Fracture inside Al core was observed near the ID of the steel wall. This indicates that at least a portion of Al core near the Al-Fe interface was melted due to the heat transfer. During the solidification of the molten Al, bubble was formed and hot tearing occurred.

After the part was cooled down to room temperature, it is observed that the side wall at the flange outside is curved. Again, this is due to the difference of the CTE of the material. This phenomenon will be discussed in the next section.

Another test was run with 0.5” wall billet by increasing the heating time to melt the Al core on purpose. However the liquid Al leaks out from the billet once the billet was taken out of the die. Figure 7.14 shows the leaked Al sticks at the outside of the billet after it cools down and solidified. After the sample was cut, a large cavity was found at the top of the billet, indicating the liquid Al was leaked outside, Figure 7.15. The fracture was more severe than the billet in Figure 7.13 and penetrated through the steel wall, resulting in the leaking of the liquid Al, as indicated in Figure 7.15.

No shrinking gap was found in the leaked part. This is because the liquid Al shrinks from the top empty space during cooling, without pulling back from the rest area. Al and steel wall was bonded together.
Figure 7.12 0.5” wall part after forging.

Figure 7.13 Cross section of two 0.25” wall parts after cutting and sand blasting.
Figure 7.14 Leaking of the liquid Al from the bottom side of the 0.5” wall billet.

Figure 7.15 Cross section view of the cracked 0.5” wall sample.

Again, one 0.5” sample was macro-etched and examined carefully under microscope. The images are shown in Figure 7.16 and Figure 7.17. No flow lines were observed anywhere in Al core. This
indicates that the entire Al core was melted either during induction heating or during forging and cooling time.

Figure 7.16 Microstructure of the Al core at flange area.

Figure 7.17 Microstructure of Al at the center of the core.
<table>
<thead>
<tr>
<th>Sample #</th>
<th>Initial wall thickness</th>
<th>Flange thickness (mm)</th>
<th>Average wall thickness at corner (mm)</th>
<th>Average gap size (mm)</th>
<th>Fracture?</th>
</tr>
</thead>
<tbody>
<tr>
<td>6</td>
<td>0.25”/6.35mm</td>
<td>30.94</td>
<td>1.41</td>
<td>0.42</td>
<td>No</td>
</tr>
<tr>
<td>2</td>
<td>0.25”/6.35mm</td>
<td>32.39</td>
<td>1.75</td>
<td>0.53</td>
<td>No</td>
</tr>
<tr>
<td>7</td>
<td>0.25”/6.35mm</td>
<td>30.59</td>
<td>2.75</td>
<td>0.61</td>
<td>Yes</td>
</tr>
<tr>
<td>1</td>
<td>0.25”/6.35mm</td>
<td>29.95</td>
<td>0.95</td>
<td>0.44</td>
<td>Yes</td>
</tr>
<tr>
<td>9</td>
<td>0.5”/12.7mm</td>
<td>30.89</td>
<td>10.15</td>
<td>0.58</td>
<td>ID of steel wall</td>
</tr>
</tbody>
</table>

Table 7.1 Thickness and gap size measurements.

7.3 Finite Element Simulations of the Forging Process

7.3.1 0.25” Wall billet

Forging simulation was conducted for both wall thicknesses. The simulation parameters are the same as in Chapter VI. Figure 7.18 shows the temperature approximation of the billet before forging and the intermediate step and final step of the forging process. It is clearly shown that at intermediate step during forging (83% in Figure 7.18), the deformation of the steel wall and Al core are non-uniform. Steel wall was squeezed into the die cavity in a greater extent and got separated from the Al core, resulting in a large gap between steel wall and Al core. After separation, the metal flow of the steel wall was free motion and un-predictable. Buckling is highly prone to occur in steel wall since there is no support from inside of the wall. At the end of the stroke (99% in Figure 7.18), Al core was squeezed into the die cavity finally and got in contact with the steel wall again. However, folding is likely to occur due to the earlier separation and buckling. Also, due to the free motion of the steel wall during forging, the final shape of the flange is not symmetric.
The steel wall at the corner of the die was pressed by the die and the Al core. The material was stretched to fill the die cavity. Therefore, the thickness of the steel wall decreased dramatically at the corner area.

The simulation results explain the experimental observation very well. The unstable deformation and buckling of steel wall cause the folding and the non-symmetrical shape in flange area. This is observed in experiment as well. Also, since the metal flow of the steel is free motion and unpredictable, the final shape of the flange is random. Based on the cross section view of the cut samples, the final shape of the flange of different samples are not identical. Such results agree with the free motion prediction by FE simulation.

Although fracture of the steel wall was not predicted in FE simulation, extensive wall thinning at the die corner area was found. Due to the non-symmetrical deformation, the steel wall at one side of the flange (either top or bottom) got too thin and triggered fracture. Such behavior is difficult to calculate in FE simulation. From Table 7.1, the average thickness measurement of the steel wall at the die corner area (if not fractured) varies from 1.41mm to about 2.75mm. If the part fractured, the steel wall will be thicker at the other side. The minimum thickness calculated by FE simulation is 1.7mm, which is an accurate prediction.
Figure 7.18 FE simulation of the forging process with 0.25” billet.

As we discussed several times previously, the difference in CTE between the two materials brings many issues in heating and cooling. Suitable Induction heating procedure was adopted to generate sharp temperature gradient to avoid cracking of the billet as well as prevent shrinking gap. However, gap still forms in the forged samples, which indicates no firm metallurgical bonding was formed at Al-Fe interface.

Cooling simulation was also conducted for both billets. Figure 7.19 shows the temperature variation in the billet during cooling. Figure 7.20 shows the maximum temperature of the Al during cooling. It can be seen that right after forging, the temperature in steel wall is still as high as 1200C while that in Al is around 400C. However the temperature gradient remains for only a few seconds. The gap generated between Al core and steel casing in induction heating was gone after forging. Al core and steel casing were tightly in contact. Due to the heat transfer at Al-Fe interface, the temperature of Al increases while that of steel decreases. We use a term “post heating” to describe the temperature raise in Al during the cooling process, Figure 7.20. After
about 97s, Al and steel reached a uniform temperature state. After that, Al and steel cools down together to room temperature, as one piece of the material. As a result, the volumetric shrinkage of Al is greater than steel casing. Since no metallurgical bonding was observed in the forged samples, gap was formed between Al and steel. In FE simulation, no bonding condition was imposed at Al-Fe interface. The gap size predicted is about 0.52mm. The measured gap size is about 0.5mm from Table 7.1. Thus, the FE simulation yields an accurate prediction. It can be concluded that if the Al is at low temperature (below 400C), no metallurgical bonding will be formed at Al-Fe interface.

Figure 7.19 Cooling simulation of 0.25” billet. 0s is the time point right after forging.
7.3.2 0.5” wall billet

The same forging simulation was conducted with 0.5” billet. The simulation procedure is similar to 0.25” billet. The simulation result in Figure 7.21 shows that during the entire forging process, no separation or buckling occurred. By the end of the stroke, the part corner is tightly filled. Such results match the experiments very well.

By comparing the result with 0.25” billet, it is found that once the billet wall thickness is increased, buckling or non-uniform deformation can be avoided. Similar study was conducted by [Essa, Kacmanick et al. 2012] to investigate the buckling issue in upsetting bi-metallic ring billets. It is concluded from his study that the buckling issue is directly and only related to the geometry of the billet. The material of the billet, either core material or ring material, will not affect the buckling. Within a range of initial height/outer diameter ratio, contact at core-casing interface can be well maintained and no buckling will occur. This is a similar observation as we found from the forging test.
To verify this assumption, a similar study was conducted using the same geometry in [Essa, Kacmarcik et al. 2012] but with our interested materials, Al 7075 and 8620. The geometries used in simulation are listed in Table 7.2. Table 7.3 lists some simulation parameters. The simulation results are shown in Figure 7.22. Very similar results were found from the verification study that only the geometrical relation of the billet, i.e. height/OD ratio and ID/OD ratio, will affect the buckling and contact after forging. The material of the billet has no influence on such phenomenon. Thus, we can conclude that in the future process design, the ID/OD and height/OD ratio has to be carefully investigated in order to avoid the buckling of the part.

Figure 7.21 FE simulation of forging 0.5” billet.
<table>
<thead>
<tr>
<th>Model</th>
<th>Initial Height $H_0$ (mm)</th>
<th>Outer Dia. $D_0$ (mm)</th>
<th>Inner Dia. $D_i$ (mm)</th>
<th>Final Height $H_f$ (mm)</th>
<th>Height reduction percentage</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>60</td>
<td>40</td>
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<td>30</td>
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</tr>
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<tr>
<td>8</td>
<td>20</td>
<td>40</td>
<td>24</td>
<td>10</td>
<td>50%</td>
</tr>
</tbody>
</table>

Table 7.2 Geometries in FE simulation

<table>
<thead>
<tr>
<th></th>
<th>Inner cylinder</th>
<th>Ring</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>Al 7075</td>
<td>AISI 8620</td>
</tr>
<tr>
<td>Temperature</td>
<td>400°C</td>
<td>1100°C</td>
</tr>
<tr>
<td>Friction</td>
<td>0.11 (Al-steel)</td>
<td>0 (die-part)</td>
</tr>
<tr>
<td>Press speed</td>
<td>50 rpm</td>
<td></td>
</tr>
</tbody>
</table>

Table 7.3 Simulation parameters
Another significant defect in 0.5” part is the cracks at ID of steel wall. The effective stress calculation in the steel wall at an intermediate step is shown in Figure 7.23. It can be seen that the location of maximum stress corresponds to the fracture spots in the forged samples. The maximum stress calculated at ID is about 266MPa and the strain is 0.8. This is not a high stress and strain value in a typical forging part. However, the temperature at ID at such condition is about 720C. At this temperature, the formability of the steel is low. According to the material datasheet, a typical forging temperature of 1020 steel is 950C – 1200C. Especially, it is mentioned that 1020 steel should not be forged below 900C. Tensile tests at elevated temperature conducted by [Leslie 1981] shows that the elongation of 1020 carbon steel at low temperature (<600C) is around 5-10%. The tensile tests were conducted at very low strain rate (0.000175/s). In hot forging, the press runs very fast and the strain rate is much higher than the test condition. So the elongation results from the tensile test may not reflect the actual formability of the material in hot forging. However, it is not expected that at about 700C in higher strain rate, the elongation
of the material can be as high as 80%. The ultimate tensile test at elevated temperature in Figure 7.25 shows that at about 500°C, the UTS of 1020 is as low as 200MPa. At around 700°C, the UTS of the material will be even lower. As a result, the maximum stress predicted (266MPa) is higher than the UTS of the material, which is another explanation of the occurrence of the fracture at ID of the steel wall. So we can conclude that the forming of the fracture is due to the low temperature at ID of steel wall during forging, which causes the low formability of the material. Therefore, the solution of the issue is to increase the temperature of the steel wall.

![stress-effective diagram](image)

Figure 7.23 Effective stress calculation in steel wall. Initial steel temperature ~ 700°C – 1100°C from ID to OD.
Figure 7.24 Elongation of 1020 steel at elevated temperature. [Leslie 1981]

Figure 7.25 Ultimate tensile stress of 1020 carbon steel. Strain rate is not specified. [Material selection table from internet]
Another interesting observation of the 0.5” part is the curved side wall at the flange, Figure 7.12. The forming of the curved side wall can be explained by the difference of the thermal expansion. Cooling simulation was conducted for 0.5” part.

It is already mentioned in section 5.3.2 that the entire Al core was melted in 0.5” billet. As discussed before, the volumetric expansion of Al during phase change is dramatic. Therefore, in FE simulation, the thermal expansion of Al in phase change has to be taken into account. During the change of state, the material will absorb a huge amount of heat, which is called latent heat, while the temperature of the material will not increase much. In FE simulation, equivalent heat capacity model which is proposed by [Hsiao 1985] was used to approximate the melting and solidification of the Al. The equivalent heat capacity model superimposes the latent heat effect onto the specific heat of the system over a temperature interval across the fusion temperature [Hsiao 1985]. As shown in Figure 7.26, the specific heat capacity of Al alloy increases drastically within the melting range. The specific heat capacity was calculated based on the latent heat measurement of Al alloy A536, [Lewis and Ravindran 2000], since no Al 7075 data is available. Figure 7.27 shows the comparison of the maximum Al temperature calculated from FE simulation with and without including the latent heat model. It can be seen that with phase change model, the maximum temperature calculated is about 610°C, while the maximum temperature is 750°C if without including the phase transition model. Apparently, without considering the phase change, FE simulation overestimated the maximum temperature in Al. This is due to the fact that a great portion of heat absorbed from steel was used for phase change instead of temperature increase. Thus, in the actual cooling simulation, the equivalent heat capacity model was used to estimate the temperature of the Al.
Figure 7.26 Specific heat capacity of Al alloy for FE simulation.

Figure 7.27 Maximum Al temperature calculation with and without considering the phase transition.
Figure 7.28 shows the temperature variation of the 0.5” billet during cooling process. The cooling process is very similar to 0.25” billet. Al and steel casing reach a uniform state at about 180s. In 0.5” billet, the amount of Al is less compared with 0.25” billet while the volume of steel is more since the wall is thicker. As a result, more heat will be transferred into Al and the temperature of Al is higher compared with 0.25” billet. When Al and steel reach a uniform temperature state, the volume expansion of Al core is much larger than the steel casing and pushes the steel casing from top and bottom, resulting in the curved side wall at the flange edge. This prediction matched the experimental observation very well.

![Figure 7.28 Temperature variation of 0.5” billet during cooling.](image)

7.4 Bonding at Al-Fe interface

It is already mentioned above that shrinking gap is formed between Al and steel casing after the part is formed. There are two possibilities: 1) no metallurgical bonding is formed at all at the interface; 2) some weak bonding or intermetallic layer is formed but broken during cooling. For a 0.25” billet, it is observed that the Al core can move freely and removed from the steel shell after cutting, Figure 7.29. The rough surface on Al core indicates the surface melting during forging. The inner surface of steel casing is relatively smooth and sharp, although some rough
surface is observed at the inside of the flange area. Still, this result demonstrates that in forging 0.25” billet, no intermetallic bonding or very few bonding was generated. This is due to the fact that in forging 0.25” billet, the temperature of Al is very low. According to the calculation from Fluxtrol, the temperature of Al could be as low as 250°C, although in reality the temperature should be much higher than that. It can be concluded that if the Al is at low temperature, no metallurgical bonding can be expected at Al-Fe interface.

The Al-Fe interface of the molten Al core billet with 0.5” wall thickness was explored carefully also. The microscopic views of the interface area are shown in Figure 7.30. All the images were taken in the fracture area. It can be seen that if Al is molten, liquid Al fuses into steel and bonded with steel. The hot tearing of the Al during solidification proves that the shrinking did not break the bonding completely, which results in the fracture inside the Al core near the interface. The thin grey layer proves that an Al,Fe_x compound was formed at the interface. The study by Behrens’ suggested that such compound is brittle and would like to crack. In Figure 7.30 e), it can

Figure 7.29 Al core was removed from the steel shell after cutting.
be seen that the crack occurs along the grey layer, which proves the brittle property of the compound.

Figure 7.30 Microstructure of the Al-Fe interface at fracture area in 0.5” billet.

Based on these observations, we can conclude that
i. In order to form a bonding at Al-Fe interface, the temperature of both materials has to be high enough. The surface of Al needs to be melted at least.

ii. Intermetallic compound (Al\textsubscript{x}Fe\textsubscript{y}) will form at interface as a thin layer. Such layer is brittle and likely to crack. So the bonding strength is directly related to the forming of such layer.

iii. The cooling rate of the part needs to be controlled to avoid hot tearing in Al side if bonding is formed at interface.

7.5 Summary

1) Bi-metallic billet was heated using calculated induction heating cycles. The steel wall was heated to the forging temperature (~1200C). Al core was expected to remain below melting point of Al. In 0.25” billet, Al core did not melt. However in 0.5” billet, Al core got molten, which is unexpected.

2) Bi-metallic part was successfully forged with closed-die. No cracking occurs at the weld zone. However, cracking occurs at the inner wall of 0.5” billet. Finite element simulation indicated the occurrence of cracking is due to the extreme low temperature at the inner wall.

3) Non-filling corner was observed in 0.25” wall billet. This phenomenon is due to the non-uniform metal flow/buckling of the steel wall during forging. Finite element simulation accurately predicted this phenomenon and indicated it is solely related to the geometrical relations between steel wall and Al core.

4) Shrinking gap was observed in both 0.25” and 0.5” billet.

5) Intermetallic bonding was formed at the Al-Fe interface. The thin and brittle layer will jeopardize the mechanical properties of the part. The forming process has to be optimized further.
CHAPTER 8 Investigation of Blanking Advanced High Strength Steel (AHSS)

8.1 Problem Statement

The potential opportunities offered by advanced high-strength steels (AHSS) in comparison to commonly used mild steels in vehicle weight reduction and crash performance improvements have led to significantly increased use of AHSS in automotive body-in-white components, particularly for crash-sensitive components. Lightweight construction has become a key priority for improving fuel economy in automotive industry.

“Five Years ago, no vehicle produced in North-America contained AHSS, but today, almost every new vehicle coming on the market has applications of AHSS. In the next five years, the body-in-white is expected to be composed of up to 45% AHSS.” [Shi, 2007].

However, the higher strength of AHSS decreases the formability which is the main challenge for metal forming and thereby for blanking and flanging processes researched in this study. Forming of AHSS can present several problems, such as fracture in stretch bending or edge cracking. Hole flanging test is commonly used to evaluate edge cracking and flangeability. But a combined blanking and flanging process may result in fracture that cannot be predicted by the Forming Limit Curve (FLC). A number of studies have shown that the edge quality of a punched hole has a significant influence on hole flangeability.

It is useful to characterize the sheared edge quality, resulted from blanking, through experiments and computer simulations.

In general high-strength steels (HSS) exhibit a tensile strength from 270 to 700MPa, while steels with more than 700MPa tensile strength are known as Ultra-High-Strength Steels (UHSS). The principal difference between HSS and Advanced High-Strength Steels (AHSS) is their
microstructure. AHSS are multi-phase steels containing phases of ferrite, martensite, bainite and sometimes retained austenite in quantities sufficient to produce unique mechanical properties. Compared to conventional micro-alloyed steels, AHSS exhibit a superior combination of high strength with good formability. This combination arises primarily from their high strain hardening capacity as a result of their lower yield strength to ultimate tensile strength ratio. For conventional steels, reduced formability is one of the consequences when selecting steels with higher strength levels. Disadvantages in using AHSS are edge splitting, springback, tool wear and high press forces. Dual-phase (DP), transformation induced plasticity (TRIP), complex-phase (CP) and martensitic steels refer to AHSS. Examples of stress and strain curves of some selected steels included DP590 are shown in Figure 8.1.

![Stress-Strain Curves for some selected steels](image)

Figure 8.1 Stress-Strain Curves for some selected steels [Konieczny, 2007b]

DP steels, whose microstructure consists of a ferritic matrix containing a martensitic second phase (see Figure 8.2), are an excellent choice of applications where the combination of high-
strength and moderate ductility is required. Increasing the fraction of the second phase generally increases the strength but reduces the total elongation and ductility, see Figure 8.1.

![Figure 8.2 DP steel with ferritic matrix and martensitic second phase [Lenze, 1999]](image)

Due to the availability of experimental data the mainly researched material in this report is DP590. Chemical compositions and mechanical and thermal properties of DP590 are listed in Table 8.1. This material information is obtained from [Chao, 2005] and [Konieczny, 2007a].

<table>
<thead>
<tr>
<th>Chemical Composition</th>
<th>C [%]</th>
<th>Fe [%]</th>
<th>Mn [%]</th>
<th>P [%]</th>
<th>S [%]</th>
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<td></td>
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<td>97</td>
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<td>0.018</td>
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<table>
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<th>Mechanical Properties</th>
<th>YS [MPa]</th>
<th>UTS [MPa]</th>
<th>UE [%]</th>
<th>TE [%]</th>
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<th>r-value</th>
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<td></td>
<td>387</td>
<td>607</td>
<td>16</td>
<td>25.5</td>
<td>0.156</td>
<td>1.07</td>
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</tbody>
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</thead>
<tbody>
<tr>
<td></td>
<td>11</td>
<td>60.5</td>
<td>3.41</td>
<td>0.95</td>
</tr>
</tbody>
</table>

Table 8.1 Chemical composition and mechanical and thermal properties of AHSS DP590. [Chao, 2005] and [Konieczny, 2007a]

The overall objectives of the study are to:

1) Understand the factors that affect the quality of pierced/blanked edge as well as Hole Expansion Ratio (HER) and edge cracking.
2) Develop guidelines for optimum blanking conditions listed below for given sheet material, thickness and hole diameter (or curvature).

The objective will be achieved by studying and selecting the optimum parameters that influence blanked edge quality and hole flangeability/edge formability.

The important parameters that can affect the blanked edge quality and the hole expansion ratio, flangeability are:

i. Punch-die clearance
ii. Blank holder (Stripper) pressure
iii. Punch tip geometry
iv. Punch velocity
v. Hardness (distribution and average) at the blanked/pierced edge
vi. Surface quality of the blanked/sheared edge.

8.2 Background of case study I – [Takahashi et al, 2013]

From the literature review, a couple of experimental case studies were identified. The objective being to use the experimental data from the literature and conduct simulations based on these experiments. Once the simulations matched the experiments, similar simulation procedure could be adopted to investigate other important parameters. FE simulations of blanking were conducted using the implicit FE code DEFORM – 2D™.

8.2.1 Summary of [Takahashi et. al, 2013]

Dr. Takahashi et. al., conducted piercing and hole expansion experiments using 20 mm diameter punches of different geometries – flat and humped bottom. The humped bottom punch was a geometry introduced by the authors and the study aimed at comparing this new geometry with the conventional punch. Experiments were conducted in sheets of thickness 3.6 mm and 2.9 mm from AHSS (Similar to TRIP 780). The hardness at the sheared surface was measured for both punch
geometries to give a quantitative measure of the blanked edge quality. Also, the hole expansion ratio was measured for both cases. From their experiments, the authors observed that the hole expansion ratio given by the humped bottom punch was greater than the hole expansion ratio. Also, the hardness at the blanked edge was lower for the humped bottom punch compared to the flat punch.

8.2.2 Simulations to compare with [Takahashi et. al, 2013]

8.2.2.1 Punch shapes used in simulations

The 2 punch geometries from [Takahashi et. al, 2013] – flat bottom punch (Geometry-1) and humped bottom punch (Geometry-2) were used for simulations. Additionally, 2 more geometries – conical punch-flat (Geometry-3) and conical punch-pointed (Geometry-4) were also used for comparison. The punch geometries are shown in Figure 8.3.

![Punch shapes in simulations.](image)

**Figure 8.3** Punch shapes in simulations.

8.2.2.2 Simulation parameters

The simulation parameters are listed in

<table>
<thead>
<tr>
<th>Simulation Parameter</th>
<th>Value</th>
</tr>
</thead>
</table>

194
Table 8.2. Punch geometries are given in Figure 8.3.

<table>
<thead>
<tr>
<th>Simulation Parameter</th>
<th>Value</th>
<th>Note: Temperature increase is considered</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sheet</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Material</td>
<td>Similar to TRIP 780</td>
<td></td>
</tr>
<tr>
<td>Thickness, t</td>
<td>3.6mm</td>
<td></td>
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<tr>
<td>Flow Stress</td>
<td>bulge test for TRIP 780 [CPF 2.1/10/02] and with K=1351.5 and n=0.1538</td>
<td></td>
</tr>
<tr>
<td>Punch</td>
<td>Corner radius 0.1 mm unless otherwise mentioned)</td>
<td></td>
</tr>
<tr>
<td>Diameter, D</td>
<td>20mm (R=10mm)</td>
<td></td>
</tr>
<tr>
<td>Shapes</td>
<td>Humped, flat, conical-flat and conical-pointed, Figure 5-1</td>
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<tr>
<td>Hump height, h</td>
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<tr>
<td>Punch speed</td>
<td>300mm/sec</td>
<td></td>
</tr>
<tr>
<td>Punch-die clearance, c</td>
<td>0.45mm – 12.5% of sheet thickness</td>
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</tr>
<tr>
<td>Coefficient of friction</td>
<td>0.1 (assumed)</td>
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</tr>
<tr>
<td>Blank holder force, BHF/Blank holder pressure</td>
<td>10kN/1.03 MPa (assumed)</td>
<td></td>
</tr>
</tbody>
</table>
Simulation | Punch Geometry (Corner radius) | Purpose
---|---|---
1.1 | Geo-1 (0.1mm) | Effect of punch Geo-1 on temperature and strain
2.1 | Geo-2 (0.1mm) | Effect of punch Geo-2 on temperature and strain
3.1 | Geo-3 (0.1mm) | Effect of punch Geo-3 on temperature and strain
3.2 | Geo-3 (0.5mm) | Effect of punch corner radius
1.2 | Geo-1 (0.1mm) | Effect of mesh size with Geo-1
2.2 | Geo-2 (0.1mm) | Effect of mesh size with Geo-2
3.3 | Geo-3 (0.1mm) | Effect of mesh size with Geo-3
4.1 | Geo-4 (0.1mm) | Effect of punch Geo-4 on temperature and strain
3.4 | Geo-3 (0.1mm) | Effect of hump height (Height-0.4mm)
3.5 | Geo-3 (0.1mm) | Effect of hump height (Height-1mm)
2.3 | Geo-2 (0.1mm) | Effect of stripper pressure on temperature and strain (Pressure = 0.57MPa)
2.4 | Geo-2 (0.1mm) | Effect of stripper pressure on temperature and strain (Pressure = 0.25*yield strength)
2.5 | Geo-1 (0.1mm) | Effect of stripper pressure on temperature and strain (Pressure = 0.75*yield strength)

Table 8.3 List of simulations

8.3 Finite Element Simulations (2D)

8.3.1 Damage criterion and critical damage value (CDV)

[Goijaerts et. al, 2001] and [Sartkulvanich et. al, 2010] have concluded that the adapted Rice-Tracey damage criterion is the most suitable for blanking simulations. The different zones (rollover, shear and fracture) were measured from [Takahashi et al., 2013] for both punch geometries – flat and humped bottom. Details of damage criterion and CDV are explained in the appendix.

Initial simulations were conducted without a critical damage value as shown in Figure 8.4. The simulation has to be stopped when the shear zone height is equal to the shear zone height obtained from the blanked specimen in the experiment.
Figure 8.4 FEM blanking simulation without damage criterion (no fracture). Image from simulation 3.1 Table 8.3.

The step where the rollover zone in the simulation matched the rollover in the experiment was noted. The damage value at this step was taken to be the critical damage value and the simulation was re-run taking this as the critical damage value. In the second run of simulations, elements get deleted once the damage value reaches the critical damage value. The same procedure was adopted to get the critical damage values using both geometries. The critical damage value thus found was similar for both punch geometries, as expected (The critical damage value was expected to be the same because fracture depends only on the sheet material properties and sheet geometry which was same for both the cases used for comparison.) This value was also used for additional geometries.
8.3.2 Different steps in the simulation

Once the damage value has been incorporated in the model, the elements where the critical damage value is reached get deleted and thus shear is achieved.

Figure 8.5 DEFORM simulation results – (a) Stroke – 1.2 mm (b) Stroke – 1.25 mm for simulation 3.1 from Table 8.3.
To explain overall strategy and simulation, simulation # 3.1 is used. The other simulations proceed in the same manner. During deformation, initially the region of the sheet in contact with the bottom dies reaches peak strain as seen in Figure 8.5 (a).

The elements at the bottom of the sheet in contact with the bottom die reach critical damage value and get deleted. This is the beginning of shear. This is shown in Figure 8.5 (b).

The last region to get sheared is at the top of the sheet as shown in Figure 8.6 (a). This is the region where the punch comes in contact with the sheet.

As shown in Figure 8.6 (b), maximum strain at the sheared edge occurs at the region on the sheet which is the last to get sheared.

These steps are taken from the results of the simulations for conical-flat punch (Geometry 3, Figure 8.3) with punch corner radius 0.1 mm (Simulation 3.1). However, the observations for all simulations are similar, and hence, this gives a generalized step-by-step view of how the shearing proceeds.
8.4 Measurement of strain at the blanked edge

To evaluate the blanked edge quality, strain was “measured” at the blanked edge from the simulation results. A number of points were chosen at the blanked edge and the effective strain was measured at these points as shown in Figure 8.7. The points along the blanked edge where effective strain was measured are shown by black dots. These points give the graph of effective strain measure as a function of distance from the top of the sheet.

Figure 8.7 Points at the sheared edge where effective strain was measured for simulation 3.1 from Table 8.3

8.5 Aberrations in the simulation

There are outliers from the simulations due to folding, element shape, mesh etc. as shown in the Figure 8.8. Hence the maximum strain was “averaged out” to reduce this error. This averaging
was done over elements near the point of maximum strain where the strain was greater than the average strain at the edge.

Figure 8.8 Aberrations in the simulation taken from simulations 2.1 and 3.1 (Table 8.3)

8.6 Observations and results

8.6.1 Effect of different punch geometries – Comparison with [Takahashi et. al., 2013]

8.6.1.1 Strain distribution

The results of FE simulations conducted to observe the effect of punch geometry are shown in Figure 8.9 and Figure 8.10.
Figure 8.9 Strain measurements (from simulations) at various points on the sheared edge (a) shows points where the shear was noted and (b) shows the value of effective shear strain at these points (Simulations 1.1, 2.1 and 3.1 from Table 8.3).

<table>
<thead>
<tr>
<th>Punch geometry (Simulation #)</th>
<th>Average effective strain</th>
<th>Ave maximum strain</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flat – Geo-1 (1.1)</td>
<td>1.3</td>
<td>1.99</td>
</tr>
<tr>
<td>Humped – Geo-2 (2.1)</td>
<td>1.11</td>
<td>1.57</td>
</tr>
<tr>
<td>Conical-flat with punch corner radius 0.1 mm – Geo-3 (3.1)</td>
<td>0.91</td>
<td>0.85</td>
</tr>
</tbody>
</table>

Table 8.4 Average and maximum strains for different punch geometries
Figure 8.10 Strain distribution at the blanked edge for simulation 1.1, 2.1 and 3.1 from Table 8.3.

Figure 8.11 Plot of temperature distribution at the sheared edge for simulation # 1.1, 2.1 and 3.1 (Table 8.3).
8.6.1.2 Temperature distribution

The temperature distribution after complete shear has been plotted in Figure 8.12. However, the maximum temperature occurs in an intermediate step.

Figure 8.12 Temperature distribution for (a) Flat bottom punch (Geo-1) Max. Temperature = 140 C Avg. temperature = 115 C (b) Humped bottom punch (Geo-2) Max. Temperature = 186 C Avg. temperature = 149 C (c) Conical flat punch (Geo-3) Max. Temperature = 179 C Avg. temperature = 151 C – Simulation #1.1, 2.1 and 3.1 from Table 8.3.

8.6.1.3 Comparison with [Takahashi et. al., 2013]

The effective strain at the blanked edge obtained from simulations conducted by CPF has been compared with the hardness distribution at the blanked edge obtained from experimental results from [Takahashi et. al., 2013] as show in Figure 8.13. It can be seen that the both the experiments and simulations predict the humped punch to have a better sheared edge quality compared with the flat bottom punch based on strain/hardness distribution, i.e. humped punch predicts lower strain.
Figure 8.13 Comparison between (a) Simulations conducted at CPF – simulation 1.1 and 2.1 from Table 8.3 (b) Experimental results from [Takahashi et al., 2013] Note: Type 1 denotes flat bottom punch and type 2 denotes humped bottom punch.

8.6.2 Effect of mesh size

Simulations were run taking double the number of elements at the blanked edge to observe if the outliers could be reduced. Also, extra punch geometry, conical-pointed (Geometry-4) [Refer to Figure 8.3 for punch geometries] was added.

8.6.2.1 Strain distribution

The results of FE simulations are shown in Figure 8.14.
Figure 8.14 Strain distribution at the blanked edge for 4 punch geometries (with finer mesh), simulation 1.2, 2.2, 3.2 and 4.2 from Table 8.3

8.6.2.2 Comparison of mesh-1 (coarser) and mesh-2 (finer)

It can be seen from Figure 8.15 that the coarser (100 elements at the blanked edge) and finer (200 elements at the blanked edge) mesh yield similar results. The strains compare well, so the initial mesh is good. Also, from Figure 8.15, it can be seen that the conical-flat punch (Geometry 3) gave lower strains at the edge for both the coarser and the finer mesh.
Figure 8.15 Effect of mesh size—Comparison of mesh-1 (coarser) and mesh-2 (finer) from simulation 2.1 and 2.2 from Table 8.3

Table 8.5 Comparison of strains of (a) mesh-1 (Coarser) and (b) mesh-2 (finer)

8.6.3 Effect of hump height
Simulations were run changing the heights of the hump for a conical-flat punch (Geometry 3). The results are plotted in Figure 8.16. From Figure 8.16, it can be seen that changing the hump height has negligible effects on the strain at the blanked edge.
Figure 8.16 Effect of hump height on strain along the blanked edge – Simulations 3.1, 3.4 and 3.5 from Table 8.3

<table>
<thead>
<tr>
<th>Hump height (mm)</th>
<th>Average effective strain</th>
<th>Average max. strain</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.4 (3.4)</td>
<td>1.2</td>
<td>1.81</td>
</tr>
<tr>
<td>0.7 (3.1)</td>
<td>1.11</td>
<td>1.62</td>
</tr>
<tr>
<td>1 (3.5)</td>
<td>1.19</td>
<td>1.93</td>
</tr>
</tbody>
</table>

Table 8.6 Effect of hump height on strain along the blanked edge for Geo-3

8.6.4 Effect of blank holder pressures close to yield

Simulations were run to check the effect of stripper pressures that are close to the yield strength of the material for humped bottom punch (Geo-2).
Figure 8.17 Effect of stripper pressure close to yield strength – Simulation 2.1, 2.4 and 2.5 from Table 8.3

<table>
<thead>
<tr>
<th>Stripper pressure (Simulation #)</th>
<th>Average effective strain</th>
<th>Avg maximum strain</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.03 Mpa (2.1)</td>
<td>1.03</td>
<td>1.51</td>
</tr>
<tr>
<td>0.25*YS (2.4)</td>
<td>1.16</td>
<td>1.67</td>
</tr>
<tr>
<td>0.75*YS (2.5)</td>
<td>2.05</td>
<td>2.59</td>
</tr>
</tbody>
</table>

Table 8.7 Effect of blank holder pressure on strain along the blanked edge for Geo-3

It can be seen from Figure 8.17 and Table 8.7 that increasing the stripper pressure has negligible effects on the strain distribution at the edge. Also, it can be seen that using a stripper pressure close to the yield strength of the material has detrimental effects.
8.7 Punch Stress Analysis

8.7.1 Effect of punch geometries on the punch corner stress

The results in section 8.6 show that conical flat punch will generate lowest strain over the blanked edge compared with flat punch and humped punch. Based on [Takahashi et. al., 2013], lower strain at the hole edge ensures higher hole expansion ratio, or in another word, better stretchability of the sheet. Therefore, we can conclude that conical flat punch should be utilized in order to obtain good edge quality of the blanked hole.

One of our sponsors worried that the new punch shape will induce more die wear issue and reduce the tool life. Therefore, the die stress analysis was conducted to investigate the die stress level on the proposed punch geometries.

The different punch geometries studied here are shown in Figure 8.3. The previous study has demonstrated that conical-pointed punch will bring in negative effect on the strain evolution, so this punch shape was ignored in this study.

The simulation parameters are summarized in Table 8.8.

A numerical model of tool wear estimation generally requires many calibration and experimental measurements to utilize. It demands tremendous efforts to run the simulations and also many tests to verify the accuracy of the model. Therefore, it is not practical to estimate the tool life with a die wear model in the simulation.

The simple solution for a rough estimation of die life is the die stress concentration comparison. Superficially, if a punch shape generates higher stress concentration, the die life will be shortened. Thus, the stress concentration was used as a criterion to compare the die life between different punch shapes. Notice here that this study will not give quantitative estimation of the die life. It can only provide us the information that if the new punch shape will reduce the die life or not.
<table>
<thead>
<tr>
<th>Simulation Parameter</th>
<th>Value Note: Temperature increase is considered</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sheet</td>
<td></td>
</tr>
<tr>
<td>Material</td>
<td>Similar to TRIP 780</td>
</tr>
<tr>
<td>Thickness</td>
<td>3.6mm</td>
</tr>
<tr>
<td>Flow Stress</td>
<td>Obtained from biaxial bulge test [CPF 2.1/10/02] and extrapolated using Holloman's equation with K=1351.5 and n=0.1538</td>
</tr>
<tr>
<td>Punch</td>
<td>(All corner radii taken as 0.1 mm unless otherwise mentioned)</td>
</tr>
<tr>
<td>Diameter</td>
<td>20mm</td>
</tr>
<tr>
<td>Shapes</td>
<td>Humped, flat and conical</td>
</tr>
<tr>
<td>Hump height</td>
<td>0.7mm</td>
</tr>
<tr>
<td>Punch speed</td>
<td>300mm/sec</td>
</tr>
<tr>
<td>Punch-die clearance</td>
<td>0.45mm – 12.5% of sheet thickness</td>
</tr>
<tr>
<td>Coefficient of friction</td>
<td>0.1 (assumed)</td>
</tr>
<tr>
<td>Blank holder force</td>
<td>10kN – 1.03 MPa (assumed)</td>
</tr>
</tbody>
</table>

Table 8.8 Simulation parameters used in punch stress analysis

8.7.2 Flat punch

The maximum stress at the punch corner during blanking occurs when the punch stroke reaches the end of the shear zone, or at the tip of the fracture zone, Figure 1-4. For different punch shapes, the maximum stress occurs at different punch strokes since the length of the shear zone is different. The maximum stress on the flat punch and the stress evolution over time during blanking is plotted in Figure 8.18. The maximum value shown on the left figure (1892MPa) is the concentration of only one element. By taking the average value of adjacent 5-10 elements at the punch corner, the maximum stress value is about 1700MPa.
8.7.3 Humped Bottom Punch

The maximum punch stresses on humped punch are plotted in Figure 8.19. The stress variation versus time is plotted in Figure 8.20. Notice here that there are two stress concentration corners, namely non-shearing corner and shearing corner. The function of the hump is to push down the sheet, as explained in [Takahashi et. al., 2013]. Thus, the non-shearing corner is not involved in the shearing mechanism. Therefore, the stress generated at non-shearing corner is compressive stress. Typically the die failure is caused by rubbing due to friction or chip off due to tensile stress. Compressive stress will not bring negative effect and it will not cause the die to fail. Thus, the compressive stress at non-shearing corner will not decrease the tool life, even though the magnitude might be higher. It can be seen from Figure 8.19 that the stress level at non-shearing corner is indeed higher than shearing corner. But it will not contribute to the die failure. In the
view of that fact, we only need to compare the stress level at shearing corner when estimating the tool life.

![Image: Stress distribution in non-shearing and shearing corners.](image)

**Figure 8.19** a) Maximum die stress at non-shearing-corner. Die stroke = 0.49mm, shear thickness=3.6mm. Average stress = 2100MPa; b) Maximum die stress at shearing-corner. Die stroke = 1.14mm, shear thickness=3.6mm. Average stress = 1650MPa.
8.7.4 Conical Flat Punch

The same stress analysis was conducted with conical flat punch. In conical flat punch, there are also shearing and non-shearing corner. The stresses were plotted in Figure 8.21 and Figure 8.22.
Figure 8.21 a) Maximum die stress at non-shearing-corner. Die stroke = 0.3mm, shear thickness=3.6mm. Average stress = 1550MPa; b) Maximum die stress at shearing-corner. Die stroke = 1.19mm, shear thickness=3.6mm. Average stress = 1300MPa.

Figure 8.22 Stress variation over time at shearing and non-shearing corner on conical flat punch
8.8 Comparison of the stresses

The maximum punch stresses on different punch shapes are summarized in Table 8.9. It clearly shows that conical-flat punch has the lowest stress concentration at shearing corner. The stress variation over time comparison in Figure 8.23 shows that conical flat punch absorbs minimum energy in the blanking operation. As a result, we can conclude that the conical flat punch will produce not only good edge quality but enhanced tool life.

<table>
<thead>
<tr>
<th>Punch geometry</th>
<th>Maximum stress (non-shearing corner)</th>
<th>Maximum stress (shearing corner)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flat</td>
<td></td>
<td>1700 MPa</td>
</tr>
<tr>
<td>Conical-flat</td>
<td>1550 MPa</td>
<td>1300 MPa</td>
</tr>
<tr>
<td>Humped bottom</td>
<td>2100 MPa</td>
<td>1650 MPa</td>
</tr>
</tbody>
</table>

Table 8.9 Summary of the maximum stress concentration on different punch geometries

Figure 8.23 Stress variation over time for three punch shapes.
8.9 Conclusions

1) The new proposed punch shape will present a non-shear corner. The high stress concentration at non-shearing corner will not shorten tool life because of the compressive stress presented.

2) The presence of non-shearing corner reduces the time period for which high stresses are present at the shearing corner, thus reducing the total energy absorbed. This may possibly imply reduced wear and less probability of chipping at the shearing edge when a non-shearing corner is present.

3) Conical-flat punch has minimum corner stresses while flat punch has maximum corner stresses. Thus it seems like conical – flat punch and humped bottom punch will last longer than the flat punch, conical-flat being the best.
CHAPTER 9  Investigation of Hole Flanging

Based on the introduction in section 1.5, in this study, we need to:

1. Determine the relationship between edge quality (and the affecting tool parameters) and HER, for a given material and thickness.
2. Optimize the blanking conditions to obtain a max possible HER.
3. Design the hole flanging punch for the 6 in diameter die at EWI (Edison Welding Institute).
4. Develop a hole expansion test standard/guideline for industrial application.

9.1 Finite Element Simulation of Hole Expansion Test

In order to validate the accuracy of the FE simulations of hole expansion test, some preliminary simulations were conducted. Several criteria should be considered to evaluate the accuracy of hole expansion simulation:

1) Prediction of the fracture occurrence. When fracture is predicted in FE simulation, the hole expansion ratio limit is reached.
2) Thickness distribution of the sheet. Thinning of the sheet is one of the possible fracture criteria. Therefore, the calculation the thickness distribution is important.
3) Stress/strain calculation at sheared edge.

The fracture prediction in FEM requires a careful investigation of the fracture criterion, which is also one of the tasks in this study. Strain values or the hardness at the sheared edge is not available at current time. As a result, only thickness distribution was compared with experiments, in the preliminary simulations.
The hole expansion experimental results are provided by EWI-Honda. The material used in the test at JAC 780T, with thickness=1.65mm. The punched hole size is 10mm in diameter.

Finite element simulations were conducted using commercial FE package DEFORM™. The simulation parameters are shown in Table 9.1 and Figure 9.1. Notice here that in the simulation, the punch velocity, and blankholder force are assumed from [Sartkulvanich, et al, 2010] since no data are available. The punch velocity in the test should be low enough (< 1mm/s based on standard in Table 1.1 Summary of hole expansion standard [ISO/TS 16630]) and 0.4mm/s is a reasonable assumption. The blankholder force is applied to prevent the material from sliding. In the simulation, no sliding of the sheet was observed during the entire hole expansion process. Thus, FE simulations results should be good enough to compare with experimental data.

<table>
<thead>
<tr>
<th>Hole expansion</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Hole edge condition</td>
<td>No pre-strain</td>
</tr>
<tr>
<td>Hole dia. ($D_h$)</td>
<td>10mm</td>
</tr>
<tr>
<td>Punch velocity ($V_p$)</td>
<td>0.4mm/s (assumed)</td>
</tr>
<tr>
<td>Punch geometries ($\theta$)</td>
<td>60° conical</td>
</tr>
<tr>
<td>Die dia. ($D_d$)</td>
<td>50mm</td>
</tr>
<tr>
<td>Blankholder dia. ($D_b$)</td>
<td>50mm</td>
</tr>
<tr>
<td>Blankholder force</td>
<td>6672N (assumed)</td>
</tr>
<tr>
<td>Die corner radius ($R_d$)</td>
<td>2mm (assumed)</td>
</tr>
</tbody>
</table>

Table 9.1 Hole expansion simulation parameters
The comparison of FE simulations and experimental data are shown in Figure 9.2.

Figure 9.2 Thickness distribution in hole expansion experiment and FE simulation of sample #8. Hole expansion ratio = 14.8%. Punch stroke = 0.276in/7.01mm. Thinning at fracture = 18%.
Figure 9.3 Thickness distribution in hole expansion experiment and FE simulation of sample #11. Hole expansion ratio = 13.5%. Punch stroke = 0.271in/6.88mm. Thinning at fracture = 15%.

It can be seen from the results that FE simulation accurately predicts the thinning distribution of the sheets during hole expansion. Also, in both cases, the critical thinning at fracture is around 15% to 18%. The critical thinning is one of the important fracture criteria in our future study.

9.1.1 Fracture Criterion in Hole Expansion Simulation

Fracture prediction is one of the most difficult problems in sheet metal forming. Researchers have developed plenty of fracture criteria which can be utilized in different applications. However, no universal solution has been obtained that can be used in all kinds of applications. A practical way to predict fracture in sheet metal forming is to carefully compare and select one suitable candidate for one forming practice and also take into consideration of the experience of shop floor technicians.

9.1.2 Critical thinning

Critical thinning or thickness at hole edge is one fracture criterion proposed by sponsor companies based on shop floor experience. The idea is when the thickness at critical location –
here at hole edge – goes below the critical value, which is an experienced value, fracture will occur.

The similar idea was studied by [Huang and Chien 2001]. The material used in the hole expansion test is low carbon steel. The experimental setup is shown in Figure 9.4. Flat bottom punch was used in the hole expansion test. Different hole sizes were EDM machined. During the test, the punch will pierce all the way through the hole and result in the same final hole size. A smaller initial hole size will undergo higher hole expansion while larger initial hole size have small expansion. The material thickness is 1.18mm. The hole sizes used in the test are: 15mm, 16.5mm, 17mm, 18mm, 19mm, 20mm.

![Figure 9.4 Hole expansion tooling. [Huang and Chien 2001]](image)

FE simulations were conducted at CPF to compare with Huang’s study. The simulation parameters used are the same as in Figure 9.4. The results are shown in Figure 9.5 and Figure 9.6.
Figure 9.5 Thickness distribution, initial hole = 15mm, punch stroke = 25mm. Fracture occurs.

Figure 9.6 Thickness distribution, initial hole = 15mm, punch stroke = 25mm. No fracture.

The thickness distribution by ERC/CPF’s simulation agrees very well with Huang’s study. The fracture thickness of this material is measured from tensile test. It can been seen that if 15mm
hole size is used, once the thickness at the hole edge become less than the fracture thickness, fracture occurs in the sheet. This result agrees well with the experiment. If 19mm hole size is used, the thickness at the hole edge is greater than the fracture thickness, when punch stroke equals 25mm. No fracture was observed.

We can summarize that critical thinning can be used to predict fracture initialization in hole expansion test for low carbon steel. This criterion will be tested with Advanced High Strength steel in future work.

9.1.3 Critical strain

Another simple fracture criterion is when the effective strain overcomes the critical strain, fracture would occur. It can be expresses simply using Equation 9.1.

\[ \bar{\varepsilon} = \bar{\varepsilon}_f \]  

Equation 9.1

According to [Bao and Wierzbicki, 2004], critical strain \( \bar{\varepsilon}_f \) depends on the stress triaxiality.

Stress triaxiality can be expressed as:

\[ \text{triaxiality} = \left( \frac{\sigma_H}{\bar{\sigma}} \right) \]  

Equation 9.2

Where hydrostatic stress:

\[ \sigma_H = \frac{\sigma_1 + \sigma_2 + \sigma_3}{3} \]  

Equation 9.3

And effective stress:

\[ \bar{\sigma} = \sqrt{\frac{[(\sigma_1 - \sigma_2) + (\sigma_2 - \sigma_3) + (\sigma_1 - \sigma_3)]^2}{2}} \]  

Equation 9.4

An example of effective strain calibration as a function of stress triaxiality is shown in Figure 9.7. It can be seen that in different forming conditions, the fracture strain shows great variation. To apply this criterion in hole expansion simulation, the stress triaxiality at the hole edge during hole expansion has to be investigated and predicted.

FE simulation of hole expansion test with perfect edge condition (no pre-strain) was conducted. The simulation parameters are listed in Table 9.2 [Takahashi et al., 2013]. The stress triaxiality
calculation in FE simulation is shown in Figure 9.8. It can be seen from the result that, at the beginning of the hole expansion test, the stress triaxiality at the edge shows great variation. However right after a very short period of time, the stress triaxiality remains as a constant around 0.35. This triaxiality value is similar to the stress state in simple tensile test. So some conclusions we can draw are:

1) It is possible to use a fracture strain to predict fracture in hole expansion, due to the fact that the stress triaxiality at hole edge is almost a constant during the entire process.

2) The fracture could be measured from uniaxial tensile test. The stress triaxiality in hole expansion test is around 0.35, which is very close to simple tensile test condition.

Figure 9.7 Dependence of the equivalent strain to fracture on stress triaxiality [Bao and Wierzbicki, 2004]
<table>
<thead>
<tr>
<th>Material</th>
<th>TRIP 780</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specimen thickness</td>
<td>3.6mm</td>
</tr>
<tr>
<td>Punch diameter</td>
<td>20mm</td>
</tr>
<tr>
<td>punch type</td>
<td>conical punch (top angle=60°)</td>
</tr>
<tr>
<td>punch Dia.</td>
<td>40.8mm</td>
</tr>
<tr>
<td>Die and blankholder Dia.</td>
<td>48mm (40.8 + sheet thickness *2)</td>
</tr>
<tr>
<td>punch speed</td>
<td>0.4mm/sec</td>
</tr>
<tr>
<td>blank holder force</td>
<td>10kN</td>
</tr>
<tr>
<td>direction of punch motion</td>
<td>same as piercing punch</td>
</tr>
<tr>
<td>lubricant</td>
<td>used (oil)</td>
</tr>
</tbody>
</table>

Table 9.2 Hole expansion simulation parameters [Takahashi et al., 2013]

![Figure 9.8 Stress triaxiality at hole edge during hole expansion.](image)

9.2 Combined Effects of Blanking and Flanging – Strain Superposition Method

According to the discussion in Section Chapter VIII, different blanking operation will result in different sheared edge quality, which will significantly affect the hole expansion ratio. This is due to the fact the surface finish and residual stress/strain at the sheared edge are quite different with different hole piercing method. Experiments have shown generally that water jet cutting will give best hole expansion ratio among all the hole piercing method.
In order to simplify the FE simulation setup and reduce the computer time, the hole edge is assumed to be perfect in FE model, i.e. no pre-strain or surface roughness is considered in the simulation. From the discussion in Chapter VIII, it is possible to use the effective strain at the hole edge to predict fracture and calculate HER. Therefore, the residual strain in hole piercing operation has to be considered.

It is obvious that the total strain after hole expansion is generated from two operations: blanking and hole expansion. Ideally, the hole edge quality in the hole expansion simulation carry the pre-strain and surface roughness from blanking simulation. However such simulation will considerably increase the complexity of the FE model setup and increase the computer time. A proposed solution is to simulate the hole expansion with “perfect” edge condition, i.e. no pre-strain is considered in FE model. The final total strain after hole expansion, with a pre-strained edge, could be calculated by superposing the strain from blanking only and hole expansion only. The idea is explained in Equation 9.5.

\[
\text{Avg. Strain}_{\text{blanking}} + \text{Avg. Strain}_{\text{hole expansion “perfect” edge}} = \text{Avg. total strain}_{\text{expansion with blanked hole}}
\]

Equation 9.5

To investigate the final strain after hole expansion with blanked edge, we only need to simulate the hole expansion operation with “perfect” edge. The total strain can be later calculated by adding the two strains together. This method will effectively reduce the numerical simulation time.

In order to verify this idea, three simulations were conducted:

1) Blanking (the calculated residual strain will be carried on to the next simulation #2.)
2) Hole expansion with blanked model from simulation #1. Pre-strain is considered in this simulation
3) Hole expansion with “perfect” edge. No pre-strain is included.
The hole flanging simulation parameters are listed in Table 9.3. The simulation results are shown from Figure 9.9 to Figure 9.11. Using Equation 5, we added average strain from simulation “1” and simulation “2” together and compare with the average strain from simulation “3”. The result is shown in Figure 9.12.

<table>
<thead>
<tr>
<th>punch type</th>
<th>conical punch (top angle=60°)</th>
</tr>
</thead>
<tbody>
<tr>
<td>punch Dia.</td>
<td>40.8mm</td>
</tr>
<tr>
<td>Die and blankholder Dia.</td>
<td>48mm (40.8 + sheet thickness *2)</td>
</tr>
<tr>
<td>punch speed</td>
<td>0.4mm/sec</td>
</tr>
<tr>
<td>blank holder force</td>
<td>10kN</td>
</tr>
<tr>
<td>direction of punch motion</td>
<td>same as piercing punch</td>
</tr>
<tr>
<td>lubricant</td>
<td>used (oil)</td>
</tr>
</tbody>
</table>

Table 9.3 Simulation parameters in blanking and hole expansion. [Takahashi et al., 2013]

![Figure 9.9 Strain distribution from blanking (simulation “1”).](image-url)
Figure 9.10 Strain distribution after hole expansion. The blanked model from simulation “1” is used in hole expansion simulation. Pre-strain from blanking was carried to the new simulation.

Figure 9.11 Strain distribution after hole expansion with “perfect” edge condition (simulation “3”). No pre-strain is included in the FE model.
From the comparison, we can conclude that it is possible to run hole expansion simulation without considering the pre-strain from blanking. This will significantly reduce the simulation time. To estimate the total strain, superposition of the two strains from blanking and hole expansion is a good solution.

9.3 Fracture Strain vs Total Elongation

Based on the discussions above, we could use two possible methods to predict fracture using two criteria: critical thinning and the critical strain. The critical thinning criterion stems from shop floor experience and it is a practical solution for actual industrial application. The critical strain is another approach based on literature review. One issue of strain method is the calibration of the fracture strain values. In Chapter VIII, we have shown that during the entire hole expansion process, the stress triaxiality at the hole edge is almost constant. That means only one fracture strain value is enough for fracture prediction. The triaxiality value in hole expansion equals about 0.35, which indicates the fracture strain value could be measured from tensile test.
The total elongation is the fracture strain of a material in uniaxial tensile test. Therefore, the total elongation could be a possible fracture strain to predict fracture in hole expansion.

To verify this idea, hole expansion simulation of DP980 and TRIP 780 was simulated using DEFORM 2D. The simulation parameters of DP980 are listed in Table 9.4. The simulation parameters of TRIP 780 are listed in Table 9.1.

<table>
<thead>
<tr>
<th>Sheet</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>DP980</td>
</tr>
<tr>
<td>Thickness</td>
<td>1.0mm</td>
</tr>
<tr>
<td>Hole diameter</td>
<td>75mm, 100mm</td>
</tr>
<tr>
<td>Flow Stress</td>
<td>Obtained from biaxial bulge test [CPF-2.1/13/04] (VPB test conducted by EWI in June 2013)</td>
</tr>
<tr>
<td>Punch/ Die—Slides 2/3</td>
<td></td>
</tr>
<tr>
<td>Diameter</td>
<td>6in/152.4mm</td>
</tr>
<tr>
<td>Shape</td>
<td>60° conical</td>
</tr>
<tr>
<td>Coefficient of friction</td>
<td>0.12</td>
</tr>
<tr>
<td>Blank holder force</td>
<td>Assume no sliding (sticking)</td>
</tr>
<tr>
<td>Punch Speed</td>
<td>5mm/sec (assumed)</td>
</tr>
</tbody>
</table>

Table 9.4 Simulation parameters of DP980

For TRIP 780, experimental data from EWI-Honda are available. Simulation will stop at the same punch stroke when fracture was observed in experiment, which is about 17% HER here in this case.

Based on the shop floor experience, DP980 sheet will fracture at 6% of thinning, which means once the thickness goes below 0.94mm, fracture is triggered. So in FE simulations, the material is assumed fractured at 6% of thinning and the simulation will stop at that punch stroke.

Then, the circumferential strain, effective strain were measured at the hole edge at the punch stroke where fracture initiates. The results are compared with material’s total elongation value from tensile test. The total elongation values are obtained from AISI website.
The result shows that the strain level at fracture point is comparable with the total elongation of the material. This is a solid evidence that the fracture strain could be a good indicator of fracture and can be used in FE simulation to predict HER of the material.

9.4 Hole Expansion Tooling Design and Hole Size Selection

Hole expansion tooling design is another important task for this study. The available punch from EWI is 6in in diameter. The punch shape should be optimized. Either conical punch with different angles or spherical shape could be the candidates.

Before the punch shape design, the hole size for the expansion test has to be determined. From the discussion in section I, 10mm hole is extremely small and is nowhere similar to the actual industrial application. Apparently a larger hole size is necessary. Larger hole size has the benefits of cracking the material at high stroke, which will reduce the experimental error. Also, larger hole size can reduce the eccentricity issue during blanking. From EWI’s preliminary simulations 3in/76.2mm diameter hole was selected as the hole size for expansion test.
Based on this hole size, different punch shapes (spherical, conical with 45°, 75° and 90°) were simulated in hole expansion simulation. For comparison purpose, a 100mm hole size was also
simulated. The simulation parameters are the listed in Table 9.3. Here in these simulations, fracture criterion selected is 6% thinning. The results are shown in Figure 9.14 and Figure 9.15.

It can be concluded from the results that:

1) Conical punch provides slightly better HER than spherical punch.

2) The modification of punch shape and the change of hole diameter do not have significant influence on HER.

3) With 100mm hole, the fracture of the material can only occur with a larger the punch stroke.

Although 100mm hole can trigger the fracture at even higher punch stroke, it is not suggested to use due to the ease of DIC camera to be set in the tooling.

So we can conclude that 6in diameter punch with 60° conical shape should be used as the hole expansion tooling punch.

9.5 Summary

1. The “standard” 10mm hole expansion test is not reliable enough for estimating the stretchability of the material. Based on our study, 3in hole is a suitable selection that will provide robust and repeatable HER of the material.

2. Finite element simulation can accurately emulate the material’s deformation in hole expansion test, and calculate the thickness distribution and strain values at the hole edge with accuracy.

3. Critical thinning is a practical method to predict fracture in hole expansion.

4. Fracture strain is another method that can be used to predict fracture in hole expansion.

Especially in the case when the pre-strain in blanking has to be considered.
5. Strain superposition is an effective approach to estimate the total strain after hole expansion of the blanked hole. It can reduce the simulation time significantly.

6. 6in punch with 60° conical shape is the “optimized” punch shape for hole expansion test. 3in diameter is the suitable hole size for the tooling in EWI. Spherical punch could also be used for comparison purpose.
CHAPTER 10 Summary and Future Work

10.1 Summary

The formability of the material and fracture related phenomenon during metal forming was extensively investigated in this research. With the help of the integration of fracture models into Finite Element code, multiple metal forming processes were simulated to evaluate the existing operation or develop novel manufacture procedure. The fracture issues in both hot forging and sheet metal forming (blanking and hole flanging) were carefully studied and fracture formation was predicted using FE software with acceptable accuracy.

The conclusions we can make for specific areas are:

1) In valve forging: the fracture of the “blade” area is due to the deformation level at the outer edge of the valve head, when new valve material with high flow stress is used. An expansion of the extrusion die diameter could reduce the fracture of the valve head. New designed extrusion punch shapes are also proposed to reduce cracking.

2) In open die forging: the center cracking of the Al bar is due to the extremely high temperature generated during forging the material. Once the temperature goes above the melting point of the Al alloy, the strength of the material is close to Zero. Fracture is initiated due to the Mannesmann effect. Start of forging with a lower initial Al billet temperature could help to avoid the fracture of the Al billet during open die forging.

3) In bi-metallic forging: a complete novel forging process was conceptualized. The tasks include billet design, forging feasibility, heating method, die design and the actual forging experiment. Multiple issues were found in forging experiments. Specifically, the cracking of the inner wall in 0.5” billet is due to the low temperature at the inside of the
wall. The cracking of the outer wall in 0.25” billet is due to the extensive stretching and formation of tensile stresses in the steel.

4) In blanking of AHSS: humped punch and conical flat punch can both improve the edge quality in blanking of AHSS in the sense of improvement of hole expansion ratio, compared with the conventional flat punch.

5) In hole flanging of AHSS: critical thinning and critical strain are the two major fracture criteria that could be used to predict the fracture of the material and then calculate the HER. 3 in hole is a suitable selection that will provide robust and repeatable HER of the material. 6 in punch with 60° conical shape is the “optimized” punch shape for hole expansion test.

10.2 Future Work

1. In valve forging, the proposed punch shapes needs to be manufactured and tried out to test the effectiveness of the design.

2. In open die forging, the lower initial forging temperature needs to be tried out and prevent the cracking of the Al bar.

3. In bi-metallic forging, more forging test with different temperature has to be run to prevent the cracking of the part. Forging of the gear with teeth will also need to be investigated.

4. In blanking, the punch shape has to be optimized and validated with a 3in punch hole.

5. In hole flanging, the actual hole flanging test has to be conducted with the new tooling to test the stretchability of the blanked material.

10.3 List of ERC/CPF reports

The following ERC/CPF reports are referred in this study:
1) Report No. ERC/NSM-10-R-01, Investigation Of Crack Formation In Precision Forging Of Engine Valves From Nickel Alloys
2) Report No. ERC/NSM-09-R-11, Investigation Of Crack Formation In Precision Forging Of Engine Valves From Nickel Alloys
3) Report No. ERC/NSM-09-R-11-II, Estimation Of Die Wear In Forging Of Engine Valves-Progress Report II
4) Report No. ERC/NSM-09-R-15, Investigation of crack formation in precision forging of engine valves
6) Report No. ERC/NSM-13-R-01, Prediction Of Fracture In Hot Forging
7) Progress Report No. 2 Project – 60036768, Prediction And Elimination Of Fracture In Hot Forging
8) Report No. Cpf-5.2/09/01 Development Of A Methodology To Predict Edge Cracking Of Advanced High Strength Steel (Ahss) Dp 590 In Hole Flanging - Fem Simulations And Use Of Fracture Criteria
10) Report No. Cpf-5.2/14/01, Research In Blanking, Piercing And Hole Flanging (Progress Report In Hole - Flanging)
References


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