PROCESS ANALYSIS AND DESIGN IN STAMPING AND SHEET HYDROFORMING

DISSERTATION

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This thesis presents initial attempts to simulate the sheet hydroforming process using Finite Element (FE) methods. Sheet hydroforming with punch (SHF-P) process offers great potential for low and medium volume production, especially for forming (a) lightweight materials such as Al- and Mg-alloys and (b) thin gage high strength steels (HSS). Sheet hydroforming has found limited applications and is thus still a relatively new forming process. Therefore, there is very little experience-based knowledge of process parameters (namely forming pressure, blank holder tonnage) and tool design in sheet hydroforming. For wide application of this technology, a design methodology to implement a robust SHF-P process needs to be developed. There is a need for a fundamental understanding of the influence of process and tool design variables on hydroformed part quality. This thesis addresses issues unique to sheet hydroforming technology, namely, (a) selection of forming (pot) pressure, (b) excessive sheet bulging and tearing at large forming pressures, and (c) methods to avoid leaking of pressurizing medium during forming. Through process simulation and collaborative efforts with an industrial sponsor, the influence of process and tool design variables on part quality in SHF-P of axisymmetric punch shapes (cylindrical and conical punch) is investigated.

In stamping and sheet hydroforming, variation in incoming sheet coil properties is a common problem for stamping plants, especially with (a) newer light weight materials for automotive applications (aluminum-, magnesium- alloys) and (b) thin gage high strength steels. Even though incoming sheet coil may meet tensile test specifications, high scrap rate is often observed in production due to inconsistent material behavior. Thus, tensile test specifications may not be adequate to characterize sheet material behavior in production stamping/hydroforming.
operations. There is a strong need for a discriminating method for testing incoming sheet material formability. The sheet bulge test emulates biaxial deformation conditions commonly seen in production operations. This test is increasingly being applied by the European automotive industry, especially for obtaining reliable sheet material flow stress data that is essential for accurate process simulation. This thesis presents a new 'inverse-analysis' methodology for calculating flow stress curves at room temperature, using the biaxial sheet bulge test. This approach overcomes limitations of previously used closed-form membrane theory equations and exhibits great potential for elevated temperature bulge test application.

To verify the developed methodologies presented in this thesis, selected case studies are presented, to (a) demonstrate the successful application of finite element (FE) simulation in tool design, process sequence design and springback reduction in stamping and sheet hydroforming and, (b) validate the developed methodology for automation/standardization of tool and process sequence design procedure and recording of existing design guidelines in transfer die stamping.
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FIELD OF STUDY

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CHAPTER 1
INTRODUCTION AND MOTIVATION

1.1. Overview of sheet hydroforming technology

Considerable hydroforming R&D is being conducted in Japan and Germany. Amino Corporation and Toyota Motor Company have used sheet and tube hydroforming technology since the 1980's [Nakamura et al, 1994, Nakagawa et al. 1988, Figure 1.1]. In the case of sheet, this forming technique replaces one of the rigid dies used in conventional stamping with an incompressible fluid medium that is used to form the sheet metal into a one-sided die cavity.

Sheet hydroforming offers several advantages as compared to conventional manufacturing via stamping and welding, including:

- A simple and versatile approach to low volume stamping of difficult-to-form materials such as magnesium alloy [Liu 2000].

- A controlled pressure distribution over part surface during forming can be used to “control” the sheet thickness and postpone local necking [Liu 2000].

- The use of only single form surface tooling, which saves time and expense in the manufacture of tooling. Absence of rigid tool contact on one surface also reduces surface friction and thus surface defects, resulting in a good surface finish.

Figure 1.2 shows example parts made by sheet hydroforming.
Figure 1.1: Schematic of the Toyota Flexible Press System. Toyota applied 'liquid pressure forming', in forming of inner and outer hood and fender geometries for the Toyota Sera as early as 1994 [Nakamura et al. 1994]

Figure 1.2: Example parts made by sheet hydroforming (a) Auto panels (b) Fuel tank, upper shell (c) Tank tray, lower shell [Maki 2005].
Various types of sheet hydroforming operations, specifically rubber forming, diaphragm forming, have been used for years in aircraft industry. Sheet hydroforming is well-suited for prototyping and low-volume production, which are predominant in the aerospace industry. The two major reasons for limited application of sheet hydroforming technology are:

- Relatively long cycle times,
- Lack of prior experience, knowledge and infrastructure.

However, in recent times, sheet hydroforming is garnering attention in the automotive industry, namely for the following applications:

- Forming relatively low formability alloys (Aluminum alloys and high-strength steels),
- Low-volume (niche applications) and prototype production.

The 2006 models of Saturn Sky and Pontiac Solstice are recent examples of successful application of sheet hydroforming by automotive stampers.

1.2. Forming of lightweight alloys at elevated temperatures

The increase in demand for maximum fuel efficiency along with higher safety and environmental standards are forcing automotive manufactures to find wider and innovative applications of strong lightweight materials, such as aluminum and magnesium alloys, as well as new manufacturing techniques to reduce vehicle weight. Also, increased competition in the marketplace is forcing automotive manufacturers to move towards new economical manufacturing methods to reduce manufacturing cost.

Magnesium (Mg) and aluminum (Al) are attractive materials for automotive use, primarily because of their lightweight and attractive strength-to-weight ratios. Both materials are much lighter than Iron (Fe), which is traditionally used in the transportation industry. Mg and Al are 78% and 65% lighter per unit volume than Fe respectively. Magnesium has the highest strength-to-weight ratio of all commercially available structural materials. Another important factor is the ease of
fabrication and joining. Magnesium is quite easy to form; often, operations that require several steps for steel can be done in only one step for Mg. However, because of its crystal structure, Mg fabrication must be done at elevated temperatures (200-300°C) [Shehata 1978].

In addition to Mg- alloys, Al- alloys offer the largest weight reduction after Mg- alloys, but their formability is also low at room temperature. Aluminum sheet alloys with yield strengths comparable to those of low carbon steels are less formable by current processes used in the automotive industry. When attempting to form Al- alloy parts on dies normally used for steels, splits often develop in the regions subjected to severe stretching or drawing. Also, in today’s practice, sections from Al- alloy tubes are only used for calibration (i.e. expanded with very limited amount of strain; 4% to 6% versus 35% to 40% in steels) due to their low formability. The reason for the lower formability of two-phase alloys, as opposed to single-phase alloys or pure metals, is that the strengthening effect of the second-phase reduces ductility. This is particularly apparent in aluminum alloys. Thus thoughts have turned to forming at elevated temperatures [Shehata 1978].

Some hard-to-form materials, such as Mg and Al alloys, may be formed by superplastic forming process (temperature >0.5 T_m, where T_m = melting temperature). Drawbacks of this process are:

- low forming rate (strain rate is in the range of 10^{-4} to 10^{-2}),
- low stiffness of the part after forming, and
- high energy to heat the part up to superplastic temperature ranges.

In order to avoid these drawbacks, most researchers are interested in forming the materials at a lower temperature range (0.2 to 0.4 T_m) [Figure 1.3]. This process is called “Warm Forming Process”. Warm forming as applied to lightweight alloy component fabrication is relatively new. Several universities/institutes around the world are in the initial stage of design and development of warm forming systems.
Elevated temperature or 'warm' hydroforming of sheet is one of the new innovative methods currently being pursued by automakers worldwide because of the advantages of combining sheet hydroforming and warm forming: Thus, *warm sheet hydroforming technology* is being continuously updated through aggressive investment in R&D by government and industries at research institutions and universities in Germany, specifically at University of Stuttgart and University of Erlangen. In U.S.A., preliminary studies are being conducted at some universities. This dissertation provides a review of warm sheet hydroforming research.

1.3. Significance of material properties in stamping/sheet hydroforming

In sheet metal forming, the variation in incoming sheet coil properties is a common problem, especially with the newer light weight materials for automotive applications (aluminum-, magnesium- alloys and high strength steels). For room temperature stamping/hydroforming applications, the biaxial sheet bulge test is a more discriminating test method in determining incoming sheet quality/formability.

Figure 1.4 shows ERC/NSM's application of the bulge test as a 'formability indicator' for testing quality of stainless steel SS409 coming from different suppliers/coils (for nominally same sheet thickness). Dome height at burst (or burst height) is used as a measure of formability. The higher
the burst height, the more formable is the material. For the same material (SS409), the burst height varied from 20 mm upto 35 mm. The error band shows variations in burst height for each supplier. This error band is a measure of consistency of material formability.

Figure 1.4: (a) Biaxial Viscous Pressure Bulge (VPB) test set up at ERC/NSM. (b) Tested samples (c) Burst height for SS409 obtained from different sources (A through H) for nominally same sheet thickness.
The biaxial nature of the bulge test allows flow stress data to be collected for larger strains as compared to the tensile test (nearly twice the range) [Figure 1.5]. Due to advantages offered by the biaxial bulge test over the uniaxial tensile test, it is increasingly being applied by the European automotive industry, especially for obtaining reliable 'true stress vs. true strain' data that is essential for accurate process simulation.

![Diagram of flow stress data](image)

Figure 1.5: Sample flow stress data obtained using biaxial bulge tests at ERC/NSM for bake-hardened (BH210) and dual phase (DP500) steel. The strain range obtained in bulge test is nearly twice that in tensile test [Palaniswamy et al. 2006b]

1.4. Rationale for this study

Preliminary bulge tests at elevated temperature indicate that some simplifying assumptions made for flow stress calculations (valid at room temperature) may not hold at elevated temperature. Thus, there is a need for a more accurate method for elevated temperature flow stress determination using the bulge test, for warm forming/hydroforming applications. To overcome limitations of the existing flow stress calculation methodology, this thesis focuses on developing a
new ‘inverse-analysis’ approach for calculating flow stress curves at both, room and elevated
temperature, using the biaxial sheet bulge test.

Due to limited application of room temperature sheet hydroforming technology, it is still a
relatively new forming process. There is limited experience-based knowledge of process
parameters and tool design for this technology. For wide application of this technology, a design
methodology to implement robust process needs to be developed. Thus, there is a need for a
fundamental understanding of the influence of process and tool design variables on part quality in
the room temperature sheet hydroforming with punch (SHF-P) process. This thesis presents a
state-of-the-art review on the sheet hydroforming technology. Forming issues unique to sheet
hydroforming technology are addressed, namely,

(a) sheet bulging in tapered part geometries,

(b) excessive sheet bulging and tearing at large forming pressures,

(c) methods to avoid leaking of pressurizing medium during forming,

(d) selection of process parameters (namely, forming pressure and blank holder tonnage)

Through process simulation and collaborative efforts with an industrial sponsor, the influence of
process and tool variables on part quality in sheet hydroforming of axisymmetric punch shapes
(cylindrical and conical punch) is investigated.

1.5. Organization of dissertation

This dissertation is divided into eight chapters. The contents of each chapter are as follows:

Chapter 1 This chapter gives a brief overview of sheet hydroforming technology and warm
forming/hydroforming process, rationale and organization of this dissertation work.

Chapter 2 This chapter outlines the objectives and planned approach.
Chapter 3  This chapter presents a state-of-the-art review on sheet hydroforming technology. The components of a sheet metal forming/hydroforming system (for room and elevated temperature) are discussed.

Chapter 4  The existing methodology for flow stress calculation is applied to an industrial sponsor's bulge test tools for determination of true stress vs. true strain curve for an Aluminum alloy (AA5754-O).

Chapter 5  Limitations of existing closed form calculation methodology used in Chapter 4 are discussed. A new 'inverse analysis' methodology is proposed and applied for flow stress calculation for steels and Al- alloys at room temperature. Extension of methodology to elevated temperature is discussed. A tool design for elevated temperature hydraulic bulge test, planned at ERC/NSM, is discussed.

Chapter 6  There is a need for fundamental understanding of the influence of tool design variables (e.g., tool corner radii, punch-die clearance) on the robustness of the SHF-P process. This chapter discusses the influence of tool parameters (namely, tool corner radii and tool clearances) on part quality in sheet hydroforming process.

Chapter 7  This chapter presents case studies in stamping and sheet hydroforming.

Chapter 8  This chapter summarizes the results of this thesis, recommendations made during case studies and outlines future work.

Appendix A  Various mathematical models available in literature to calculate the restraining forces in drawbeads are reviewed. Stoughton's model was chosen to develop a user friendly Microsoft® Excel calculator to quickly estimate drawbead forces from rectangular and semicircular drawbeads. This developed calculator was used in designing a lockbead geometry needed to lock a sheet in the elevated temperature bulge test tools (discussed in Chapter 5).
CHAPTER 2
RESEARCH OBJECTIVES AND APPROACH

2.1. Overall objective

The overall objective is to improve stamping and sheet hydroforming processes through the use of process simulation, by (a) improving existing sheet material characterization techniques, (b) understanding influence of tool variables in sheet hydroforming, and (c) developing software for standardizing tool and process sequence design procedures.

2.2. Specific objectives

− Develop a reliable inverse analysis methodology for flow stress determination in sheet materials, at room and elevated temperature, using the biaxial sheet bulge test.

− Identify and optimize tool design variables that significantly affect part quality in sheet hydroforming process; for an axisymmetric cylindrical and conical cup tool.

− Predict and control springback in the hydroforming process of large parts within required precision tolerances.

− Through process simulation, develop a procedure for identifying feasible tool design and forming sequence in the transfer die stamping.

− Develop procedures for:
  
  (a) automating tool and process sequence design,

  (b) standardizing tool design/drafting procedures and recording existing design guidelines.
2.3. Approach

Based on the outlined objectives, this study is divided into three main phases:

PHASE I: Determination of sheet material properties

This phase is conducted in co-operation with an industrial sponsor.

Task 1.1: Application of existing room temperature flow stress methodology to aluminum alloy AA 5754-O.

Task 1.2: Determination of a new flow stress calculation approach (inverse analysis) for room temperature stamping and sheet hydroforming application. Methodology verification by applying to two materials (a) Aluminum alloy AA 5754-O, (b) Stainless steel SS 304

Task 1.3: Extend the developed inverse analysis methodology in task 1.2, for calculating flow stress at elevated temperatures (using an elevated temperature bulge test). A warm bulge test tool will also be designed for future bulge testing capabilities at ERC/NSM.

PHASE II: Influence of tool design on sheet deformation in sheet hydroforming with punch

This phase is conducted in co-operation with a German press and tool manufacturer Schnupp Hydraulik that is conducting tests for verifying FEM predictions.

Task 2.1: Determine the influence of punch-die clearance on sheet hydroforming (namely, part thinning and bulging) in forming a Ø90 mm cylindrical cup.

Task 2.2: Determine the influence of punch-die clearance on sheet hydroforming (namely, part thinning and bulging) in forming a Ø90 mm conical (or tapered) cup.

PHASE III: Application of developed methodologies in stamping-sheet hydroforming
This phase is conducted in co-operation with multiple industrial sponsors.

Task 3.1: Sheet hydroforming: Predicting springback and designing tools for hydroforming of large (Ø6 m, Ø12 m) reflectors.

Task 3.2: Transfer die stamping:

(a) Tool and process sequence design of axisymmetric parts through process simulation.

(b) Automating tool and process sequence design of axisymmetric parts.

Task 3.3: Progressive die stamping: Determination and optimization of critical process and tool parameters for eliminating fracture.
CHAPTER 3

STATE-OF-THE-ART REVIEW: SHEET HYDROFORMING

Sheet metal forming using pressurized media is called sheet hydroforming (Figure 3.1). Sheet hydroforming is classified as Sheet Hydroforming with Punch (SHF-P) and Sheet Hydroforming with Die (SHF-D), depending on the male (punch) or the female (die) tool that has the shape/ impression to be formed. SHF-P is synonymous with ‘Hydromechanical Deep Drawing’ (HMD). Similarly, SHF-D is synonymous with ‘High pressure sheet hydroforming’. SHF-D is further classified into hydroforming of single blank and double blank depending on number of blanks being used in the forming process.

Figure 3.1: Classification of forming processes using liquid media [Schmoeckel et al. 1999]
3.1. The room temperature sheet hydroforming system

Successful application of SHF-P or SHF-D process requires careful consideration of all the components of the sheet hydroforming system (Figure 3.2), namely:

(a) Quality of incoming sheet,
(b) Die-workpiece interface issues [friction and lubrication],
(c) Tool design for efficient application of blank holder force (BHF) and avoiding leakage,
(d) Relationship between the internal fluid pressure (or pot pressure) and BHF,
(e) Press and tooling, and
(f) Dimensions and properties of the hydroformed part.

Figure 3.2: Components of the sheet hydroforming system [Altan et al. 1983]
3.2. Process description, advantages and disadvantages

3.2.1. Sheet hydroforming with punch (SHF-P)

In SHF-P, the female die used in conventional stamping is replaced by a pressure pot. The sheet is deep-drawn to form over the punch surface, against a counter pressure in the pressure pot generated by a pressurizing fluid (see Figure 3.3). The liquid in the pressure pot can be “actively” pressurized by an external pump, or “passively” pressurized during the forward stroke of the punch by controlling the pressure with a relief valve, allowing some fluid to escape as the punch and the sheet are pushed into the pot.

![Figure 3.3: (a) Schematic of SHF-P, (b) example parts produced by SHF-P [Aust 2001]](image)

3.2.2. Sheet hydroforming with die (SHF-D)

In SHF-D, the sheet is formed against female die by the hydraulic pressure of the fluid as shown in Figure 3.4a. During the forming process, the intermediate plate acts as a blank holder to control the material movement from the flange and also seals the fluid medium to avoid leakage.
The forming operation in SHF-D can be divided into two phases (Figure 3.4b). Phase I involves free forming, where the sheet bulges freely in the die cavity until it initiates contact with the die. Phase II involves forcing the sheet against the die cavity to obtain the desired shape. The forming parameters [blank holder force (BHF) and pot pressure] can be varied to get the desired strain path in the sheet for optimum use of formability of the material, which is part geometry dependent. Viscous pressure forming (Figure 3.5) is similar to SHF-D in which pressure acting on the sheet is generated by compressing a viscous medium rather than hydraulic fluid/water.
3.3. Advantages and disadvantages

3.3.1. Advantages

(1) Better formability

- In SHF-P, the fluid pressure acting on the sheet metal results in a high friction force at the sheet – punch interface that prevents stretching of the sheet material after it comes in contact with the punch. Therefore compared to stamping, the sheet metal in the cup wall is not stretched as much in the SHF-P process, resulting in a more uniform wall thickness and higher limiting draw ratio (LDR).

- Free bulging of the sheet in SHF-D introduces a uniform strain distribution throughout the sheet. In contrast, in conventional stamping the deforming sheet experiences high local deformation at the punch corner radius. Thereby, the formability of the material is effectively used in SHF-D. SHF-D is a viable alternative in fabrication of automotive parts from low formability AHSS grades and Al- alloys.
(2) Improved dent resistance

- Strain hardening of the material during stretching in SHF-D improves the dent resistance of the hydroformed part as compared to stamped part, since parts under the punch face are hardly stretched / strained during conventional stamping.

(3) Lower tooling cost

- Elimination of the one of the tools (punch or die) results in lower tool cost and reduced development time.

(4) Better surface quality

- Elimination of sidewall wrinkles during the sheet hydroforming, due to applied pot pressure, results in better surface quality and thus allows for more freedom in designing auto body panels.

(5) Fewer forming operations

- The ability to form complicated shapes and features results in fewer forming operations compared to conventional stamping, which reduces manufacturing costs. SHF-P processes can also be combined with regular stamping operations to reduce forming stages.

(6) Economical for small lot production

- The SHF-P process is economical for small-lot production, which is the current manufacturing trend for products of short lifetime due to increased competition [Hoffmann, et al. 2001].

(7) Higher dimensional accuracy (simple symmetric geometries)

- Better surface quality can be achieved as the outer surface of the sheet is in contact with fluid, thereby reducing the chance of tool marks [Siegert, et al. 1999b, Maki, 2003]. Also a higher dimensional accuracy can be achieved for simple symmetric parts.
3.3.2. Limitations

(1) High cycle time

(2) High ram forces needed / High press cost

- SHF-P requires presses of higher capacity compared to stamping because of the pot pressure that acts against the punch. Thus, SHF-P presses are expensive and require higher capital investment. Depending upon the part geometry, production volume and the part mix, the higher press cost might not be offset by the lower tool cost [Golle 2003].

- In SHF-D, high pot pressure is required to form the sharp corners in the part. This pot pressure depends on the sheet material, thickness and the smallest corner radius in the die geometry.

(3) Lower dimensional accuracy in complex geometries.

3.4. Sheet hydroforming – Process variations

3.4.1. Flexform fluid cell / Flexforming

This flexible-die sheet metal forming process is carried out in a fluid form cell [Figure 3.6]. Press ram with oil and a flexible rubber diaphragm replace the solid punch. The blank is kept on the rigid tool half, which defines the shape of the part. The hydroforming unit moves downward while the die cavity, blank and the trap ring are stationary. The oil and diaphragm wrap form and hold the blank over the die cavity.
The Flexform Fluid Cell process was first developed by ASEA (ABB, Sweden) Due to the flexibility of the rubber diaphragm, parts even with undercuts and sharp contours can be formed, even for sheets with different initial thickness. The ‘Hydroform’ process (developed by Cincinnati Machine Tools) and the ‘Wheelon’ process, developed by Verson (Chicago) are similar in principle to the flexforming process.

3.4.2. Combination of SHF-P process with regular stamping operations:

In this “active” SHF-P process, an external pump generates the pot pressure. The blank is initially forced to bulge upwards (pre-stretching or pre-bulging) by the pot pressure before the SHF-P process (see Figure 3.7a). Pre-stretching or pre-bulging before SHF-P process produces plastic strain and a favorable state of stress, resulting in a higher dent resistance [Hoffmann, et al. 2002]. Pre-bulging also suppresses wrinkles and improves the thickness distribution, thereby increasing
the limiting drawing ratio [Zhang et al. 2003]. Figure 3.7b shows combination of SHF-P with regular stamping, resulting in a reduction in forming stages during forming of complex parts.

Figure 3.7: Combination of SHF-P with stretching and deep drawing to produce complex parts in less forming operations [Siegert et al. 1999b]

3.4.3. Double blank hydroforming/Parallel plate hydroforming

Two flat or pre-shaped sheets that can be of different thickness, different shape welded or unwelded at the edges constitute the input blank for the parallel plate hydroforming process. The input blank is placed in the tool that has both upper and lower die containing the shape to be formed. The blank is held at the edges and the pressurizing media is introduced between the sheets using a special docking mechanism. Sheets are formed against the top and bottom die to get the desired shape by fluid pressure (Figure 3.8a).
Parallel plate sheet hydroforming is an alternate to tube hydroforming process in forming complex geometries that has different cross sections over the length of the part with large difference in expansion ratio. In THF the maximum difference in expansion ratio achievable at different cross sections in a part is limited because the input blank is circular tube with a uniform cross section over entire length. However in parallel plate hydroforming, by locally changing the blank width, cross sections with different expansion ratio can be accommodated. Figure 3.8 shows various automotive parts manufactured by parallel plate hydroforming.

![Schematic of process sequence for double blank sheet hydroforming](image)

(a) Schematic of process sequence for double blank sheet hydroforming

![Engine support cross member](image)

(b) Engine support cross member

![B-pillar](image)

(c) B-pillar

Figure 3.8: (a) Double blank sheet hydroforming [Birkert et al. 1999]. (b) and (c) show sample automotive parts produced by double blank sheet hydroforming [Habil et al 2003, Schroeder 2003]

Parallel plate hydroforming can also be used an alternative to high pressure sheet hydroforming as in both cases the sheet metal forced against the die by the liquid medium. However, in parallel
plate sheet hydroforming two parts can be produced in one production cycle thereby the productivity is increased. Parallel plate hydroforming allows top die and bottom die of different shapes. Also, it allows forming two different materials and two different thicknesses in one production cycle. Parallel plate sheet hydroforming may be economical compared to conventional stamping for production of relative small batch size [Habil et al 2003, Schwarz et al 2003, Pasino et al 2003, Schroeder 2003, Rosen et al 2001].

3.4.4. Rubber diaphragm forming

In rubber forming the die is replaced by a rubber pad supported by a pad holder. As the pad holder moves down, the rubber or elastomer comes in contact with the sheet/workpiece sitting on the punch. The deforming elastomer exerts a hydrostatic pressure on the sheet metal causing it to conform to the shape of the punch. Rubber-forming technology is particularly suited to the production of prototypes and small series (typical of aerospace industry). Among the rubber pad forming processes, the Guèrin process (Figure 3.9a) is the oldest and simplest, and is suited for small volume production [Sala 2001].

The Marform process (Figure 3.9b) was developed for using the same cheap tools as the Guèrin process, to perform deep drawing. Compared with Guèrin process, the tooling includes a steel blank holder supported by a hydraulic actuator equipped with a pressure control valve (30–70 MPa). Dies are made of cast light alloys and the rubber pad is 1.5 to 2 times thicker than the sheet metal component to be formed. Figure 3.9b shows a Marform process set up with a double-acting apparatus. The ram slides down, causing the rubber pad to exert pressure on sheet metal and blank holder. When ram force exceeds applied blank holder force, the whole assembly moves downwards causing the sheet to conform to the shape of the stationary punch [Sala 2001].
Figure 3.9: Rubber-die or rubber forming process (a) Guèrin process and (b) Marform process [Sala 2001]

3.5. Sheet hydroforming presses

3.5.1. Presses for sheet hydroforming with punch (SHF-P)

Presses used for SHF-P are hydraulic presses that provide high ram forces and have a counter pressure pot. Figure 3.10 shows the operation sequence of a 3500 ton SHF-P press designed by Müller Weingarten using the “short stroke” concept.
Figure 3.10: Schematic of the operation sequence for SHF-P press built using “short stroke” cylinder concept by Müller Weingarten [Beyer 1999].

The top die is moved up and down by a long stroke cylinder that requires a large volume of hydraulic fluid at low pressure. The ram is fixed at the desired bottom position by mechanical locks. The short stroke cylinders mounted in the top ram are activated during the SHF-P process to apply the large force required to counteract the force generated on the punch due to the pot pressure for short stroke.

Schnupp Hydraulik has introduced a similar press for SHF-P process with short stroke cylinder concept (Figure 3.11). The operation sequence is similar to the Müller Weingarten press (Figure 3.10). However, the short stroke cylinders are attached to the press bed instead of the press ram. In the final stage of the SHF-P process, the ram is fixed by mechanical locks. The short stroke
cylinders move the bed (die) against the blank holder cylinders and punch mounted on the top ram. The press seen in Figure 3.11 allows (a) independent control of blank holder force and the pot pressure, and (b) reduction in cycle time and cost by decreasing the amount of high pressure hydraulic fluid to be handled by the system. This press is equipped with multi-point cushion systems (MPC) that allow changing the blank holder force (BHF) in space over the flange area and during the SHF-P process. The MPC system is used to control local pressure on the flange so that the metal flow is optimized and leakage is reduced.

![Figure 3.11: SHF-P "short stroke" press [Schnupp et al. 2003]](image)

Schuler SMG GmbH and Co. KG has developed a sheet hydroforming process called “Active-Hydro-Mec” [Stremme et al. 2001]. The process, which allows pre-bulging of the blank prior to SHF-P, is shown in Figure 3.12a. This process allows for increased strain hardening of the sheet during forming, which increases the dent resistance of automobile body panels. A press concept used for this process is shown in Figure 3.12b. A double-action straight side press is used. An
An external hydraulic unit is used to generate the pressure required for pre-bulging the blank. Six hydraulic cylinders located in the press crown apply the required blank holder force while eight cylinders located in the press bed apply the pressing force from below. The punch is fixed during forming by six adjustable mechanical stops.

Figure 3.12: (a) Schuler “Active-Hydro-Mec” process (b) Press concept for “Active-Hydro-Mec” process (Schuler Handbook) (c) Parts formed by Schuler (Stremme et al. 2001).
3.5.2. Presses for sheet hydroforming with die (SHF-D)

Sheet Hydroforming with Die (SHF-D) presses and tools are designed and manufactured based on the technology developed and available in tube hydroforming. However, in SHF-D, higher locking force due to large area of the sheet and blank holding mechanism need to be considered. University of Dortmund (LFU), Germany, in cooperation with Siempelkamp PressenSysteme (SPS), Germany, has built a 100 MN press for SHF-D of large automotive parts (Figure 3.13).

The press is designed to have horizontal mounting for (a) inexpensive compact design, (b) easy handling of workpiece and, (c) short strokes for cylinders to reduce the cycle time. The press requires less investment for foundation in the shop floor. The press frame is cast and pre-stressed by wire winding to withstand dynamic load during forming.

The working media is supplied in a two-step sequence. The first step consists of a filling cylinder that provides high flow rates at a pressure of 315 bar (4570 psi). This step is adequate for pre-forming the part since large amounts of fluid at relatively low pressures are required. However, a second step is used to calibrate the final part geometry. This step consists of a pressure intensifier that generates 2000 bar (29 ksi) at low flow rates. The main closing cylinder works at 700 bar (10,150 psi) and has an area of 1.4 square meters. Thus, the closing cylinder can produce 100 MN (11,240 US tons) of closing force.
3.5.3. Presses for Flexform Fluid Cell Process [Source: Avure Technologies]

Quintus fluid cell presses are manufactured with rectangular trays with usable tool areas from 0.9m x 2m upto 2m X 4m [Figure 3.14]. The cycle time is normally between one and three minutes depending on press size, formed parts and selected pressure. Maximum forming pressures are 1000 bars (14.5 ksi) and 1400 bar (20 ksi). For increased productivity, the fluid cell presses can be equipped with pallet systems. Using such a system the tools and blanks are loaded onto a pallet which is lifted into and out of the tray with lifting pins, operating through the tray bottom. The system can be used in connection with tool exchange and/or part exchange.
3.6. Tool design

Tool design for sheet hydroforming is similar to regular stamping. In SHF-P, punch and blank holder are specifically designed to the part shape while pressure pot remains common for all parts. The pressure pot and punch in SHF-P are designed to withstand high pot pressure. Also, careful consideration is required for sealing at the pressure pot – sheet interface to avoid leakage of the fluid during SHF-P. Some recent advances in tool design are:

3.6.1. In-die trimming in the flexforming process

Thus, in many flexforming example parts, forming and trimming can be done in the same dies. Inset picture in Figure 3.15 shows an excavator door panel (1.5 mm thick, mild steel) formed and trimmed in one single step at 1400 bar (20 ksi) using a Kirksite punch with steel trimming inserts.
Figure 3.15: In the flexforming process, by incorporating a sharp edge and a trimming bead in
the single rigid tool half, it is possible for the rubber diaphragm to shear sheet
metal at a predetermined pressure level [Avure Technologies brochure].

3.6.2. Methods to avoid sheet bulging

SHF-P of parts with tapered sidewalls results in the bulging of the sheet in the gap between the
blank holder and the punch due to pot pressure during forming (Figure 3.16a, Detail A).

Figure 3.16: Schematic of bulging during SHF-P for conical punch (b) Upper pressure
chamber to compensate for bulging during SHF-P [Aust 2001]
This bulging could result in excessive thinning and fracture at higher pot pressures. University of Stuttgart (IFU), Germany has developed new tooling design with additional sealing at the punch-blank holder interface as shown in Figure 3.16b to create a pressure chamber in the top. During SHF-P, hydraulic fluid from the pot is circulated to the top pressure chamber so that the pressure in the top and bottom chamber is the same to avoid bulging [Siegert et al. 1999, Aust 2001].

![Diagram of bulge height control techniques](image)

**Figure 3.17:** Bulge height control techniques (a) using a modified blank holder (b) using a mechanical sliding mechanism [Khandeparkar 2007]
3.6.3. Elastic cushion to form sharp corners

The pot pressure required to completely form the part depends on the smallest corner radius in the part. Thus, parts with sharp corners require press with very high capacity resulting in increase in the investment cost. Schuler developed elastic cushions that are mounted in the pressure pot (Figure 3.18). Towards the end of the forming process, the sharp corners are formed mechanically by the cushions rather than the pot pressure resulting in reduction of the required press capacity and investment [Stremme et al. 2001].
3.6.4. Multi-point blank holder control

Figure 3.19: Schematic of tool design by LFU with multipoint cushion system (10 blank holder segments) in the die for blank holder force application [Kleiner et al. 2003]. Formed rectangular part overall dimensions = 900 mm x 460 mm.

In conventional SHF-D tooling (Figure 3.4b), the intermediate plate applies required blank holder force to seal the pressurizing media and to control the material flow during hydroforming. Hence it is difficult to precisely apply the required blank holder force on the sheet. University of Dortmund
–LFU, Germany, designed a new tooling as shown in Figure 3.19, where the sealing force is applied by the intermediate plate while the blank holder that is mounted in the flange portion of the die applies the blank holder force. Thus, material flow from the flange is precisely controlled.

In forming non-symmetric parts, thickening in the flange is not uniform, resulting in a gap that causes the leakage of pressurizing media. LFU developed modular tooling design with multipoint blank holder as shown in Figure 3.19, where the force at each cylinder can be varied independently. The multipoint blank holder is incorporated in the tooling using short stroke cylinders that provides large force in a short stroke [Kleiner et al. 2001, Kleiner et al. 2003].

3.6.5. Elastic blank holder

![Concept of segmented elastic blank holder](image)

Figure 3.20: Schematic of die assembly with segmented elastic blank holder for SHF-P [Siegert et al. 2003]

Blank holder design plays a dominant role in SHF-P because the applied blank holder force (BHF) controls the material flow during draw-in and also applies the necessary force to avoid leakage of the pressurizing medium during forming process. In forming non-symmetric parts using conventional blank holders, thickening in the flange is not uniform resulting in gap between the rigid blank holder and sheet. This causes leakage of the pressurizing medium.
At University of Stuttgart (IFU) Germany, a segmented elastic blank holder (originally designed for stamping) was used for SHF-P (Figure 3.21a). The flexible blank holder deflects elastically under applied BHF and ensures uniform contact with the sheet metal despite non-uniform sheet thickening during the process. Thus, the BHF is distributed uniformly and leakage of pressurizing medium can be avoided [Siegert et al. 2003].

Using the segmented elastic blank holder concept, IFU conducted an experimental investigation to form automotive fender geometry by SHF-P. The effect of (a) flat and (b) curved blank holder surface curvature on part quality was investigated through FEA and experiments [Figure 3.21b]. Flat binder surface allowed for easier sealing and better leakage control. For the same part depth, lesser thinning was observed in parts formed with the curved binder [Oberpriller et al. 2006].

Hydraulic cylinders of multi-point cushion plate (12 cylinders)

(a) (b)

Figure 3.21:  (a) Segmented blank holders for two configurations – curved and flat (b) Sectional view of curved and flat binder for forming the automotive fender geometry (inset) [Oberpriller et al. 2006].
3.7. Process limits, defects and process window in SHF-P

Formed part quality in SHF-P is dependent on the part geometry, incoming sheet material properties, interface conditions (friction) and the process parameters (namely, pot pressure and BHF) during forming. Common defects in SHF-P are:

(a) Excessive thinning leading to fracture,

(b) Flange wrinkles – leading to leaking of the pressurizing medium,

(c) Sidewall wrinkles,

(d) Excessive bulging – leading to fracture (Figure 3.22a),

(e) Surface marks (Figure 3.22b).

Figure 3.22: Bulge formation in SHF-P (a) Bulge at punch-blank holder interface and at inclined/tapered punch locations (b) Due to uneven contact between sheet and tool, skid marks can occur due to sheet rubbing at sharp punch corners [Oberpriller et al. 2006].

Figure 3.23 shows tearing observed in the straight section of an automotive fender geometry due to excessive bulging of the sheet in the clearance between punch and blank holder. In forming non-symmetric parts (such as the fender) by SHF-P, bulging of the sheet is observed more commonly in the straight sections of a die, where the stress state is "plane strain". This is
because sheet material shows lesser resistance to deformation in this state of stress. Sheet bulging is less common at the die corners, where state of stress is approximately axisymmetric.

Figure 3.23: For automotive fender geometry, tearing of the sheet (due to excessive bulging) is observed in the straight section [Oberpriller et al. 2006].
A process window is defined as a combination of process parameters for which the part can be formed with no defects. Figure 3.24 shows a process window defined using blank holder force (BHF) and pot pressure as process parameters. Flange wrinkling occurs when the BHF is insufficient. Side wall wrinkles can be attributed to insufficient pot pressure, part geometry (e.g., punch with tapered walls) and/or to excessive flange wrinkling. Fracture occurs after excessive thinning due to high BHF and/or insufficient pot pressure (see point A in Figure 3.24). Fracture can be avoided/postponed by increasing the pot pressure (moving from A to A” in Figure 3.24) or by decreasing the BHF (moving from A to A’ in Figure 3.24).
Leaking of the pressurizing medium and “bulging against the drawing direction” are unique possible modes of failure in SHF-P. Leaking occurs when the BHF is insufficient, resulting in flange wrinkling followed by leaking of the pressurizing medium (see point B in Figure 3.24). Leaking results in reduction of fluid pressure in the pot, which could result in fracture of the part. Leaking can be avoided by increasing the BHF (moving from B to B” in Figure 3.24) or by reducing the pressure in the pot (moving from B to B’ in Figure 3.24). Also, sealing is used to reduce the possibility of leaking during the SHF-P process. Bulging against the drawing direction occurs due to excessive fluid pressure. This is usually observed in parts with tapered walls. Bulging can be avoided by decreasing the pot pressure (moving from C to C’ in Figure 3.24). Effects of the BHF and pressure on the common defects in SHF-P were experimentally studied by [Kasuga et al. 1964, Nakagawa et al. 1997, and Kandil 2003].

3.8. Process simulation: Room temperature sheet hydroforming

Numerous investigations on deformation mechanics in SHF-P process have been conducted by [Dariani et al. 2006, Tirosh 2005, Abedrabbo et al. 2005, Wu et al. 2004]. [Lang et al. 2004], [Zhang et al. 2000], [Siegert et al. 2003] estimated the process parameters through trial and error experiments and FE simulation, which is costly and time consuming and the estimated process parameters may not be robust to variations in manufacturing process. For an automotive fender geometry (see Figure 3.21b), FE simulations were conducted at IFU (University of Stuttgart) to investigate the influence of process and tool variables such as: (a) amount of flange draw-in, (b) part orientation (or, drawing direction) (c) tool addendum design and (d) drawing clearance, on formed part quality [Oberpriller et al. 2006].

Incorrect selection of process parameters could lead to excessive stretching/fracture of the sheet or wrinkling and leakage of pressurizing medium during the hydroforming process. Even with the use of finite element (FE) simulations, prediction of the optimum process conditions is not always very easy. ERC/NSM has developed numerical optimization software which, when coupled with
FE software PAMSTAMP 2000, determines the profiles of the optimum blankholder force and pot pressure versus time. These optimized process parameters help in minimizing part thinning, eliminate wrinkling in the hydroformed part and ensure that no leakage of the pressurizing medium occurs during the process.

3.8.1. Application to sheet hydroforming with die (SHF-D)

ERC/NSM is working in co-operation with University of Dortmund (IUL), Germany in optimizing SHF-D process for a rectangular part (Figure 3.13, Figure 3.19) formed using a multipoint cushion system with a segmented blankholder. This cushion system allows different blankholder forces to be applied at ten different locations around the rectangular blank. Blankholder force can also be varied with time in each of these locations.
For a multipoint cushion system, there are four possible ways of applying the blankholder force during hydroforming process, namely a) Blankholder force constant in space and time, b) Blankholder force variable in space and constant in time, c) Blankholder force variable in time and constant in space and d) Blankholder force variable in space and time. Optimum values for all four possible modes of application of blankholder force to form the rectangular part were estimated [Vavilikolane et al. 2005]. Experiments for optimized parameters are in progress.

3.8.2. Optimization of SHF-P process

Analysis and optimization of the SHF-P process for a Ø90 mm cylindrical cup was conducted in co-operation with Schnupp Hydraulik, Germany. The pot pressure was limited to a maximum value of 400 bar due to limitation of the machine capacity. Figure 3.26 shows the optimized
blankholder force profile path for hydroforming a 90 mm diameter round cup from AKDQ steel, to a depth of 105 mm in a single forming operation. Blank holder force was observed to increase towards the end of the forming process to restrain material flow and avoid excessive flange thickening, which would result in flange wrinkles and leakage. Using this optimized blankholder force profile, round parts were successfully hydroformed at Schnupp Hydraulik. Comparison of part thinning from experiment and from FE predictions is seen in Figure 3.27.

Figure 3.26: (a) For a given pot pressure curve, the optimum blankholder force (BHF) curve is predicted using numerical optimization techniques coupled with FEM (b) Wrinkles in round cup were eliminated using optimum BHF [Braedel et al. 2005].
3.9. Industrial application of room temperature sheet hydroforming

3.9.1. Application of sheet hydroforming to high strength steels

The manufacturability of sheet hydroforming with punch process for forming a roof outer panel of an Opel Vectra (Figure 3.28b) was demonstrated in a study conducted by Siebenwurst GmbH (Germany) in co-operation with ThyssenKrupp, Germany. The part was formed in a 4000 ton Schnupp hydromechanical press from two high strength steels (a) 0.6 mm thick dual phase DP-K 30/50 steel (H300X) and (b) 0.7 mm thick BH180 (H180B) steel. Figure 3.28a shows the five process variations that were investigated. The first four are variations of the sheet hydroforming with punch (SHF-P) process, while Variant 5 is sheet hydroforming with die (SHF-D) process. Variant 2 proved to be the optimum, with comparatively low tooling costs and simpler process control [Siebenwurst et al. 2005].
Figure 3.28: (a) Overview of five process variations studied in determining the manufacturability/feasibility of sheet hydroforming process in forming two high strength steel materials (b) Roof outer panel geometry [Siebenwurst et al. 2005].

3.9.2. Flexible multi-forming system

Figure 3.29 shows a new concept production cell developed by Amino Corporation for medium volume production (10,000 to 60,000 parts/year). The main press in the production cell is a sheet hydroforming press for draw operation [upto 3000 tons, with 800 ton blank holder tonnage capacity, part size upto 2800 mm, with part depth 400 mm]. The second and third presses in the line are link-servo presses used for subsequent trimming and bending (upto 800 ton capacity each). This forming system is presently being used for production of numerous body panels, such as the 2006 Pontiac Solstice. Figure 3.29b and Figure 3.29c shows example parts [Maki 2005].
3.10. Elevated temperature sheet hydroforming

Warm sheet hydroforming is a relatively new technology that combines the advantages of elevated temperature sheet metal forming with sheet hydroforming. Depending on which tool has the shape/impression to be formed, warm sheet hydroforming can be classified as

(a) warm sheet hydroforming with punch (Warm SHF-P) or,

(b) warm sheet hydroforming with die (Warm SHF-D),
3.10.1. Warm Sheet Hydroforming with Punch (Warm SHF-P)

In warm SHF-P, the sheet and flange portion of the die and blank holder are heated to the required temperature (Figure 3.30a). The punch is cooled while the pressurizing fluid temperature is kept slightly higher than room temperature. During the process, the lower temperature of the punch and pressurizing medium cools the sheet adjacent to the punch, thereby increasing the strength of the sheet to carry the drawing load. This postpones failure caused by excessive thinning. As a result, a limiting draw ratio (LDR) of 3.0 was obtained for drawing aluminum-alloy cylindrical cup at forming temperature of 250°C [Groche et al. 2002].

[Behrens et al. 2003] conducted warm SHF-P experiments with heated Ø 50 mm cylindrical cup tooling to investigate improvement in formability for magnesium-alloys (AZ31). Conventional deep drawing at elevated temperature of 200°C showed a limiting drawing ratio (LDR) of 1.8. In warm SHF-P, an LDR of 3.0 was achieved experimentally. Besides improved formability, the formed cup showed lesser thinning in the cup wall and lower strains compared to conventionally drawn cups.
Figure 3.30: (a) Warm sheet hydroforming with punch (Warm SHF-P) using cylindrical punch [Groche et al. 2002] (b) Warm sheet hydroforming with die (Warm SHF-D) [Jäger 2005]
3.10.2. Warm Sheet Hydroforming with Die (Warm SHF-D)

Figure 3.30b shows the schematic of the warm SHF-D process. In this process, the sheet, the die and the pressurizing medium/liquid are heated to the desired temperature. The tooling consists of an upper and lower die. The upper die is heated with cartridges while lower die is heated by a band-heater. Heat transfer/loss is minimized by thermal insulation. During the process, the pressurizing medium, nitrogen gas, is supplied through the upper die (with pressure $p_i$). The pressurizing medium deforms the sheet into the lower die cavity which has the shape/impression to be formed. In this setup, the counter pressure in the lower die cavity ($p_c$) can also be controlled [Jäger 2005]. At LFT, University of Erlangen – Nuremberg, sample automotive parts from Aluminum alloy A6061, A5182 sheets and Al-Mg alloy tubes were hydroformed at temperature of 220°C (Figure 3.31) [Geiger et al 2003].
3.10.3. Challenges in warm sheet hydroforming / Issues in process simulation

ERC/NSM has investigated warm sheet forming using finite element (FE) simulations [Palaniswamy 2003] and the results were compared with experimental results available in the literature [Dröder 1998]. Magnesium alloys were used in this investigation. Warm drawing of a
cylindrical cup and a rectangular pan from magnesium alloy was simulated using commercial code DEFORM 2D&3D. In this investigation, it was observed that finite element simulations predict all the major trends shown in the experimental data, however, they over predict the punch force and the thinning distribution compared to experiment. This is despite the fact that material property inputs for the FE simulation were obtained through rigorous material testing (based on tensile test data [Dröder 1998]. The results of this preliminary FE modeling study serve to highlight both the potential and the current limitations of this flow stress obtained from tensile test data. Consequently, since sheet is biaxially formed, biaxial flow stress data is critical to accurate process modeling.

For accurate finite element (FE) simulation of the sheet hydroforming process, the mechanical and thermal changes in the workpiece need to be modeled [see Table 3.1]. Material data (flow stress, yield surface and material anisotropy) is needed over the range of forming temperatures (25 to 350°C) and deformation rates (strain rates). Data on heat transfer and friction between the workpiece and tools is also needed.

<table>
<thead>
<tr>
<th>Mechanical data (for sheet)</th>
<th>Thermal data (for sheet, forming medium and hard tools)</th>
<th>Process data</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s modulus</td>
<td>Thermal expansion (function of interface pressure and sheet temperature)</td>
<td>Friction between sheet and tools</td>
</tr>
<tr>
<td>True stress-strain data (function of temperature and strain rate)</td>
<td>Thermal conductivity</td>
<td>Temperature (sheet and tools)</td>
</tr>
<tr>
<td>Yield surface (function of temperature)</td>
<td>Heat capacity</td>
<td>Interface pressure</td>
</tr>
<tr>
<td>Anisotropy coefficients (function of temperature)</td>
<td>Heat transfer</td>
<td>Process parameters (forming or pot pressure, applied blank holder force)</td>
</tr>
<tr>
<td></td>
<td>Heat dissipation</td>
<td></td>
</tr>
</tbody>
</table>

Table 3.1: Data required as input to FEM for accurate process simulation of the warm sheet hydroforming process.
3.10.3.1. Sheet material properties: Flow stress

- Prior research on elevated temperature behavior of materials in warm stamping/hydroforming process simulation focused on determining the flow stress of sheet metal as function of temperature and strain rate. The effect of elevated temperature on (a) yield surface and (b) anisotropy coefficients, especially for Al- alloys is still ongoing. A state-of-the-art review on the effect of elevated temperature on material properties (namely, flow stress, yield surface and anisotropy coefficients) is presented by [Abedrabbo et al. 2006].

- Commercially available FE codes do not offer specialized material models developed for specific material and process. Therefore, user-sub routines need to be programmed to model material behavior as function of temperature and strain rate.

3.10.3.2. Heat transfer and interface heat transfer coefficients

- It is computationally expensive to simulate a complete thermo-mechanical system using commercial FE codes. [Geiger et al. 2003] reported that an FE thermal stress analysis can simulate the heat flow between sheet and tools, but required considerable computation time.

- The pressurizing fluid and the dies have larger heat capacity due to their relatively larger masses compared to the sheet. Also, the selected sheet material (Al- and Mg- alloys) have low heat capacity. Therefore, for a simplified analysis, a valid assumption would be to assume a step-wise constant temperature of the dies and the fluid [Geiger et al. 2003].

- To ensure accurate heat transfer calculation, correct tool, sheet and pressurizing medium interface heat transfer coefficients must be known. At present in most commercial FEM codes, the interface heat transfer coefficient is assumed to be constant. However, the heat transfer between the sheet and the tools is dependent on the interface contact pressure. Since contact pressure varies with location, it is desirable to input to FEM the heat transfer
coefficient as a function of pressure. Therefore, user-subroutines in FEM are needed to input heat transfer coefficient as function of pressure.

3.10.3.3. Interface friction coefficient

- At present in FEM, the interface friction coefficient is assumed to be constant. A detailed investigation on the influence of temperature and interface pressure on friction coefficient is needed [Spampinato et al. 2006].
4.1. Uniaxial tensile test vs. biaxial bulge test

In sheet metal forming ( stamping and sheet hydroforming), the sheet properties greatly influence metal flow and product quality. In a stamping plant, sheet properties may vary from coil to coil and affect part quality and scrap rate. Thus, quality control of incoming coil is essential for establishing robust production. Finite Element (FE) simulations are routinely used by die designers to (a) predict sheet metal flow and (b) optimize die design. To obtain reliable FE results, accurate flow stress data must be used as input. This data must be obtained at large values of strain, as seen in production stamping. Thus, a reliable and discriminating test is needed to (a) check quality of incoming sheet coil, (b) determine formability of the incoming coil and (c) determine true stress – true strain data (or, flow stress curve) for input to FE simulation. Table 4.1 shows a comparison of two commonly used tests. The biaxial bulge test is increasingly being applied by the European automotive industry for obtaining sheet material flow stress data for process simulation.
Tensile test (ASTM standard) | Hydraulic bulge test
---|---
2. Strain range / flow stress data | Flow stress data collection is limited by local necking of tensile coupon. For FEM, flow stress data is extrapolated to large strains. This is an approximation and not necessarily accurate. | Flow stress data is obtained for up to twice the strain range in tensile test. Thus, no extrapolation is needed in FEM.
3. Formability | 'n' values or strain-hardening exponent of the material (for materials that follow power law fit $\sigma = K \cdot \varepsilon^n$) is used as a measure of material formability. | Dome height at burst is a good measure of formability. This test is a quick-and-easy check on incoming sheet quality.

Table 4.1: Advantages of (biaxial) bulge test over (uniaxial) tensile test

4.2. Existing flow stress determination methodology: Membrane theory

The Viscous Pressure Bulge (VPB) test set up available at ERC, uses a viscous medium instead of hydraulic oil. In this method, a viscous pressurizing medium is trapped between the sheet and the punch. When the punch moves upwards, it compresses the pressurizing medium, which bulges the sheet [Gutscher 2004]. The geometry and process parameters of VPB test are shown in Figure 4.1.
Flow stress calculations are done using a closed form solution with membrane theory assumptions. The membrane assumption ignores bending deformation of sheet. Therefore, calculations are valid only for thin sheet materials. It is also assumed that the bulged sheet can be approximated as a section of a sphere with a defined radius. For flow stress calculations, bulging pressure \( p \), radius of curvature \( r_d \), thickness at dome apex \( t_d \) and initial sheet thickness \( t_0 \) are required. Pressure and initial sheet thickness can be easily measured. It is difficult to measure radius of curvature \( r_d \) and thickness at dome apex \( t_d \) during the test. In the VPB test, only pressure \( p \) and dome height \( h_d \) are measured during the test.

Through preliminary FE simulations of the VPB test, it was observed that both, radius of curvature \( r_d \) and sheet thickness at dome apex \( t_d \) are functions of dome height \( d_h \) and strain hardening exponent 'n'-value. They are independent of strength coefficient 'K' and material anisotropy. Therefore, an FE simulation database of (a) sheet thickness \( t_d \) at dome apex and (b) radius of curvature \( r_d \) at dome apex, for different dome heights \( d_h \) and strain hardening index 'n'-values was generated. This generated database was used along with the test measurements (instantaneous pressure and instantaneous dome height) to calculate flow stress from the closed form solution.
Figure 4.2 shows the iterative approach used to determine the power law flow stress parameters 'K' (strength coefficient) and 'n'-value (strain hardening exponent). A Microsoft Excel macro was written to automate these calculations [Gutscher 2004].

![Flow stress curve and measured data diagram]

Figure 4.2: Iterative methodology to calculate flow stress curve ['K' and 'n' values in power law fit $\sigma = K \varepsilon^n$] using a closed-form solution from membrane theory assumptions, and a database of FE simulations of the VPB test [Gutscher 2004]

4.3. Hydraulic bulge tests

This task is focused on the application of the existing flow stress methodology to a hydraulic bulge test tool for determination of flow stress curves for Aluminum alloy AA5754-O.
4.3.1. Tool set up for hydraulic bulge test

A schematic of the upper and lower plate for the hydraulic bulge test tools is shown in Figure 4.3. The sheet is clamped between the upper and lower plate, and bulged into the upper plate cavity. The difference between ERC’s and sponsors tools is the upper die corner radius (Upper plate die corner radius for sponsors tools = 0.12”, VPB tools = 0.25”). There are two lockbeads present on the upper plate, at Ø5.5” and Ø 6” (Figure 4.3). These lockbeads prevent sheet material draw in and ensure that the sheet is formed by pure stretching during the bulge test.

Before the test, the oil is filled up to the brim in the die cavity. Sheet blank is centrally positioned on the lower plate. By using load or stroke control, the press ram is lowered to clamp the sheet between the upper and lower plate. The servo-controlled pressure relief valve controls the flow of oil from the tools based on a user programmed pressure profile. The pressure intensifier can also be used to increase the oil pressure in the pressure pot.
4.3.2. Experimental matrix

Due to the absence of an instantaneous dome height measuring device, multiple bulge tests are planned as seen in the experimental matrix (Figure 4.4) to get bulge pressure vs. dome height curve. Dome height measurements were conducted outside the press using a height gauge to obtain the relationship between bulging pressure and dome height (Figure 4.5). Three readings are taken for each sample and average of the readings is used for further calculations.
Experiment: Sheet bulged to burst pressure ‘P’.

90% bursting pressure = 0.9 P

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Pressure</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Sheet bulged to burst pressure ‘P’.</td>
</tr>
<tr>
<td>2</td>
<td>90% bursting pressure = 0.9 P</td>
</tr>
<tr>
<td>3</td>
<td>0.8 P</td>
</tr>
<tr>
<td>4</td>
<td>0.6 P</td>
</tr>
<tr>
<td>5</td>
<td>0.4 P</td>
</tr>
<tr>
<td>6</td>
<td>0.3 P</td>
</tr>
</tbody>
</table>

Sheet material: AA5754-O, t = 1.01 mm
Initial blank: t = 1.01 mm, approx 7" x 7" square

Figure 4.4: Planned experimental matrix for room temperature bulge test experiments. After burst (first sample), five values of lower pressure will be tested to obtain a total of six samples with different dome heights.

Figure 4.5: (a) Dome height measurement outside the press, using a height gauge. Absolute zero is set at base of dome. (b) Measuring of height at dome apex.
4.3.3. Experimental results

For all experiments, a clamping force of ~50 tons (110 klbf) was applied to lock the sheet within the lockbeads. This is done to ensure that there is no draw in of sheet material during the test.

Figure 4.6: (a) AA5754-O sample burst at 988 psi (average over 3 samples) (b) Cut samples at different bulge pressures per experimental matrix. For all experiments, clamping tonnage of 50 tons was applied. No material draw in was observed.

Table 4.2 gives a summary of the results obtained from the planned experiments. Five data points of bulge pressure vs. dome height data are obtained after the tests. In future tests, additional data points can be obtained by stopping bulging at more values of bulge pressure.
### Table 4.2: Bulge test results summary: Measured values of pressure and dome height.

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Set pressure (psi)</th>
<th>Measured pressure in psi (bar)</th>
<th>Measured dome height in mm (inches) [using height gauge]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Not set</td>
<td>988 ±4 (~ 68 bar)</td>
<td>Burst (not measured)</td>
</tr>
<tr>
<td>2</td>
<td>900</td>
<td>863 ±2 (~ 59 bar)</td>
<td>24.57 ±0.12 (~0.97 in)</td>
</tr>
<tr>
<td>3</td>
<td>800</td>
<td>753 ±4 (~ 52 bar)</td>
<td>21.09 ±0.18 (~0.83 in)</td>
</tr>
<tr>
<td>4</td>
<td>600</td>
<td>555 ±5 (~ 38 bar)</td>
<td>16.55 ±0.23 (~0.65 in)</td>
</tr>
<tr>
<td>5</td>
<td>400</td>
<td>353 ±2 (~ 24 bar)</td>
<td>12.45 ±0.15 (~0.49 in)</td>
</tr>
<tr>
<td>6</td>
<td>300</td>
<td>257 ±1 (~ 18 bar)</td>
<td>10.27 ±0.15 (~0.4 in)</td>
</tr>
</tbody>
</table>

In this investigation, the five data points obtained from the bulge tests were fit using regression analysis (3\textsuperscript{rd} order polynomial equation, Figure 4.7) and intermediate data points were interpolated.
4.3.4. Flow stress determination: Methodology to determine true stress vs. true strain data points from bulge pressure and instantaneous dome height

For flow stress calculation (using closed form membrane theory equations), the existing VPB test Excel macro uses as input 'bulge pressure vs. dome height curve'. This curve is obtained by measuring in real time, the dome height (with a linear potentiometer) and bulge pressure (with a pressure transducer) in the VPB test.

In the existing hydraulic bulge test set up, instantaneous bulging pressure can be recorded. There is no device to measure real time values of dome height. To obtain pressure vs. dome height data, the bulge tests were stopped at intermittent bulge pressure values, and corresponding
dome heights were measured outside the press using a height gauge. Five such tests were conducted to obtain five 'pressure vs. dome height' data points.

For flow stress calculation from five data points, following two modifications were made to the existing Excel macro:

1. Regression / Interpolation to get 'bulge pressure vs. dome height curve': On entering the five data points of pressure vs. dome height, the modified Excel macro fits a 3rd order polynomial curve and interpolates intermittent data points automatically for flow stress calculation.

2. FE simulation database for sponsor's tool dimensions: ERC's existing Excel macro (for VPB test) uses an FE database which was generated by conducting FE simulations (in PAMSTAMP 2000) for the VPB tool geometry. This database was modified by conducting FE simulations in PAMSTAMP-2G for the industrial sponsor's bulge test dimensions.

4.4. Flow stress results

To validate results obtained for AA5754-O from the modified Excel macro, the calculated flow stress curve is compared with flow stress curves calculated previously by ERC using VPB test for AA5754-O (of a different batch/thickness) and tensile tests conducted at sponsor (same batch/same thickness) (Figure 4.8).
A fair match is seen in the trends for the three compared flow stress curves. Minor differences seen in the compared flow stress curves can be attributed to the following reasons:

1. Differences in material properties in the sponsor’s bulge test and VPB test: The tested samples are from different batch/sheet thickness (1.01 mm in sponsor’s bulge test; 1.3 mm in ERC’s VPB test). This could be the most significant factor for observing differences in the flow stress curves obtained from the two bulge tests.

2. Springback of the dome / Errors in the measuring dome height with height gauge: Springback of the dome after bulging is investigated in section 4.4.2.
3. Differences in measured values of bulge pressure and set values (37 psi to 47 psi difference in recorded maximum value and set maximum pressure value).

4. Influence of upper plate/die corner radius of the bulge test tools: In the VPB test, the upper die corner radius = 0.25" (6.35 mm) as compared to 0.12" (3.05 mm) in sponsor’s bulge test tool. The influence of the die corner radius is investigated in a later section.

4.4.1. Comparison of thickness at dome apex (measurement vs. FE simulation)

Sheet thickness at the dome apex was measured for tested samples using a micrometer. Figure 4.9 shows a comparison of measured thicknesses with thickness calculated by the Excel macro. Calculated values of thickness are ~4% lower than measured thickness values.

![Chart showing the comparison of thickness measured at dome apex with predictions from Excel macro. Calculated values of thickness are ~4% lower than measured thickness values.](image)
4.4.2. Recalculated flow stress curve, with springback correction

Dome heights of the bulge test samples are not measured in real time. These measurements are conducted "offline" outside the press using a height gauge. After the pressure is released, the dome is expected to show elastic deflection or "springback" inwards, as seen in the schematic (Figure 4.10). Thus, the measured values of dome height have an error in measurement due to springback. Figure 4.10 shows the effect of springback on dome height.

![Schematic shows springback error (exaggerated) in dome height measurement](image)

FE simulations were used to predict springback for the tested samples at the particular dome heights. Table 4.3 shows FE simulation input parameters. Nodes at lockbead location were constrained while simulating the sheet bulging. For springback, the outer edge of the sheet was fully constrained. This was done to take into account the influence of the sheet flange on springback of the formed part.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>AA 5754-O, t = 1.00 mm (0.039&quot;)</td>
</tr>
<tr>
<td>Power law fit ([\bar{\sigma} = K \cdot \bar{\varepsilon}^n]) parameters</td>
<td>(K = 483) MPa, (n = 0.3) (from tensile tests)</td>
</tr>
<tr>
<td>E-modulus</td>
<td>70 GPa</td>
</tr>
<tr>
<td>Symmetry</td>
<td>Quarter model</td>
</tr>
<tr>
<td>Friction coefficient ((\mu))</td>
<td>0.12</td>
</tr>
</tbody>
</table>

Table 4.3: FE simulation input parameters: Springback in biaxial sheet bulge test
Table 4.4 shows springback predicted using FE simulation for the corresponding dome heights. Springback is more prominent at lower values of dome height. In the FE simulation, springback of the dome ranged from 0.44 to 0.53 mm depending on dome height (ranging from ~ 10 mm to 25 mm). The flow stress curve was re-calculated with springback correction and compared with flow stress curve calculated without springback consideration (Figure 4.11).

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Dome height measured after experiments (mm)</th>
<th>Springback observed in FE simulation (mm)</th>
<th>Corrected dome height (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2</td>
<td>24.57 ±0.12</td>
<td>0.44</td>
<td>25.01 ±0.12</td>
</tr>
<tr>
<td>3</td>
<td>21.09 ±0.18</td>
<td>0.46</td>
<td>21.55 ±0.18</td>
</tr>
<tr>
<td>4</td>
<td>16.55 ±0.23</td>
<td>0.48</td>
<td>17.03 ±0.23</td>
</tr>
<tr>
<td>5</td>
<td>12.45 ±0.15</td>
<td>0.50</td>
<td>12.95 ±0.15</td>
</tr>
<tr>
<td>6</td>
<td>10.27 ±0.15</td>
<td>0.53</td>
<td>10.80 ±0.15</td>
</tr>
</tbody>
</table>

Table 4.4: Springback calculated for the dome heights and dome height correction

As seen in Table 4.4, at dome height of ~ 10 mm (corresponds to strain value ~ 0.06 in flow stress curve seen in Figure 4.11), even a small variation of ~ 0.5 mm in dome height (due to springback) resulted in a difference of ~10% in calculated flow stress. Thus, accurate measurement of dome height, preferably real time data collection, is essential for correct flow stress calculation.
4.5. Summary and recommendations

1. Room temperature hydraulic bulge tests were conducted for Aluminum alloy AA5754-O (thickness 1.01 mm) in an industrial sponsor's press. For flow stress determination using the existing membrane-theory methodology, instantaneous values of (a) bulge pressure and (b) dome height are needed.

- Sensors: During the bulge tests, instantaneous bulging pressure was recorded. At present there is no sensor for measuring instantaneous dome height. Therefore, the tests were conducted for five values of bulging pressure. Dome heights corresponding to these five points were measured after experiments, using a height gauge.

- Pressure calibration: During the tests, a difference was observed between measured values of pressure (at pressure relief valve) and set values of pressure (given as input using the control system). A difference of 37 psi to 47 psi was observed between maximum values of measured pressure and set pressure, for all set values in the
experiments. For future tests, it is proposed to calibrate the pressure sensors before testing.

2. Existing membrane-theory methodology (used for VPB test) for flow stress calculation was modified for the sponsor’s tools. The following two modifications were made:

- Modification no. 1 - Building an FE simulation database for sponsor’s tools: Using sponsor’s tool dimensions, finite element (FE) simulations of the bulge test were conducted using PAMSTAMP-2G. An FE simulation database was generated to determine influence of material properties (strength coefficient ‘K’ and strain hardening exponent ‘n-value’) on radius of the bulging dome and thickness at dome apex. As determined from previous ERC studies, only the ‘n-value’ of material influences the dome curvature and thickness [Gutsch 2004].

- Modification no. 2 - Curve fitting to obtain pressure vs. dome height curve: The five data points of bulge pressure vs. dome height obtained from bulge tests were fed to the modified Excel macro. The modified macro fits a third order polynomial equation to the five data points to obtain ‘bulging pressure vs. dome height curve’. The bulging pressure vs. dome height curve generated by the macro is used to calculate the flow stress curve.

3. Calculated flow stress data points for sponsor’s bulge test were compared with results obtained using the VPB test and with flow stress data obtained from tensile tests conducted by sponsor. Discrepancies in flow stress curves are attributed to the following:

- Sheet material properties used for sponsor’s bulge tests are different from ERC’s VPB test. The tested samples are of different initial sheet thickness (1.01 mm in sponsor’s bulge test; 1.3 mm in ERC’s VPB test). This could be the most significant factor for differences in the flow stress curves from the two bulge tests.
  
  o Springback of the dome / Errors in the measuring dome height with height gauge.
  
  o Errors /Differences in set and measured bulge pressures.
- Influence of springback on dome height was studied. Dome springback is higher at lower dome heights. At dome height of ~10 mm (corresponds to strain value ~ 0.06 in flow stress curve), even a small variation of ~ 0.5 mm in dome height (due to springback) resulted in a difference of ~10% in calculated flow stress. Thus, accurate measurement of dome height, preferably real time data collection, is essential for correct flow stress calculation.
CHAPTER 5

FLOW STRESS DETERMINATION USING INVERSE ANALYSIS

5.1. Problem statement / Rationale

There is increasing interest in warm forming of sheet, especially for lightweight materials such as Al- and Mg- alloys. Elevated temperature flow stress data (true stress vs. true strain curve), of the sheet material is a necessary input for accurate finite element (FE) simulation of warm forming/hydroforming processes. There is very limited data available on flow stress of sheet materials, especially at elevated temperature.

Thus, there is a need for a reliable methodology to determine elevated temperature flow stress preferably using the biaxial bulge test. Bulge test offers numerous advantages over tensile test. The most significant is the deformation range over which true stress – true strain data is obtained (nearly twice that of tensile test).

In a recent ERC/NSM experimental study [Kaya et al. 2008] elevated temperature hydraulic bulge tests with Mg AZ31-O alloy sheets were conducted using a test apparatus available at the University of Darmstadt. The following main observation was made in this investigation:

- The bulged dome in the tests appears to be of parabolic or elliptical shape. The assumption of spherical dome shape made for membrane theory flow stress calculations does not hold.

Thus, to overcome assumptions made in the existing flow stress calculation methodology, there is a need for an alternative reliable technique (such as inverse analysis) for flow stress calculation using the biaxial sheet bulge test.
This task is focused on developing a methodology for room temperature flow stress determination using "inverse analysis" (adjusting of material properties in computation, until a match with measured values is obtained). After this approach is validated at room temperature, it will be extended in a later study to elevated temperature sheet bulge test.

5.2. Objectives and approach

5.2.1. Objective

The overall objective of the study is to develop an alternative methodology, such as inverse FEM analysis, combined with optimization tools, for room temperature flow stress calculation using the biaxial sheet bulge test.

5.2.2. Approach

The following three tasks are outlined:

Task 1: Determination of an appropriate form for the flow stress equation

- The power law \( (\sigma = K\varepsilon^n) \) is the most commonly used flow stress equation and is found to be a good approximation, especially at room temperatures for steels. For the room temperature bulge test study, as a first approximation, the power law will be used.

- In a later study, flow stress equations that consider the influence of (a) temperature and (b) strain rate, will be investigated. A review of flow stress equations by [Gronostajski 2000] is available.

Task 2: Formulation of the inverse analysis problem using the dome geometry evolution

- Commercial FEM package LS-DYNA will be used for FE simulations of the bulge test.

- This package comes with an optimization tool LS-OPT that will be used for formulating the inverse analysis problem as described in Figure 5.1.
Task 3: Validation of developed methodology

For example materials (AA 5754-O and SS 304), the flow stress data obtained by applying the inverse analysis technique to room temperature bulge test, will be compared with flow stress curves obtained by other methods, namely:

- Tensile test
- Biaxial bulge test (VPB test) at ERC using closed-form membrane theory equations.

5.3. Inverse analysis methodology – Application to room temperature

Figure 5.1 shows the schematic of the proposed inverse analysis methodology. In order to determine flow stress data using a measure of geometry (of the deforming dome), some observable geometric quantities (e.g. dome height or dome curvature) must be identified, based on their sensitivity to the material properties. For room temperature bulge test, the dome height was selected as the geometric quantity. The inverse analysis problem is defined as minimization of an objective function. Thus, \textit{the objective function is defined as the least square difference between measured and simulated dome/bulge height.}
5.3.1 Finite element (FE) model of the bulge test

In the FE model, all tools are assumed rigid. Die (or upper plate) is fixed in space. All degrees of freedom for nodes of the outer edge of the blank are constrained. This is done to simulate no draw in of sheet during bulging (due to lockbeads in the tool). The blank is modeled as elastic-plastic, and meshed with Belytschko-Tsay elements with three integration nodes over the sheet thickness. A simple power law material model in LS-DYNA MAT18 (Isotropic plastic) is used in FE simulation for purpose of inverse analysis. In this study, the material anisotropy is neglected.
In order to apply a distributed pressure on the blank three different approaches in LS-DYNA were investigated and compared. For the purpose of this analysis, the “LOADMASK card was used. This card is also suitable for sheet hydroforming simulation.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>AA 5754-O</td>
</tr>
<tr>
<td>K [MPa] and n-value for power law fit</td>
<td>Varied by LS-OPT to identify optimum K and n-value to minimize objective function</td>
</tr>
<tr>
<td>Initial blank thickness / Sheet diameter</td>
<td>1.01 mm (0.039”) / Ø 104 mm (4.09”)</td>
</tr>
<tr>
<td>Coefficient of friction between sheet and die (μ)</td>
<td>0.12 (assumed)</td>
</tr>
<tr>
<td>Die corner radius</td>
<td>3.05 mm (0.12”)</td>
</tr>
<tr>
<td>Boundary conditions</td>
<td>Quarter geometry, Sheet fixed at outer edge</td>
</tr>
</tbody>
</table>

Table 5.1: FE simulation input parameters in LS-DYNA (for GM hydraulic bulge test tools)
5.3.2. Objective function

In this approach the inverse analysis problem is defined as the minimization of an objective function. The objective function represents the difference between measured and computed values of the dome height, formulated in a least square sense:

\[
E = E(K,n) = \frac{1}{N} \sum_{t=1}^{N} \left( \frac{H_{\text{measured}} - H_{\text{simulation}}}{H_{\text{measured}}} \right)^2
\]

with,

- \(H_{\text{measured}}\): measured dome height from bulge test at time = \(t\)
- \(H_{\text{simulation}}\): computed dome height from bulge test simulation at time = \(t\)
- \(t\): time increment at which dome height is measured/calculated
- \(N\): total number of increments

(Note: In the experiments, a pressure increment \(\Delta p\) may be controlling factor rather than time increment 't')

Figure 5.3 illustrates the schematic of the objective function. Five dome heights at five different bulge pressures are measured, Figure 5.3a. For example, Figure 5.3b shows measured and simulated dome heights for time \(t = 5\). The difference between the two values (for \(t = 1\) to 5) is minimized during the optimization.
Figure 5.3: Schematic illustration of the objective function. (a) Changing dome height for five time increments (t = 1 through 5). Comparison of measured and simulated dome height at final time t = 5.

5.3.3. Response surface methodology (RSM)

In this methodology, a design surface (or response surface) is fit through points in a design space to construct an approximate optimization problem (Figure 5.4).

The optimization problem is formulated as follows:

**Objective function:** Minimize the difference between measured and simulated dome height (see section 5.3.2).

**Constraint functions:** No constraints.

**Design variables:** Variable 1: Material strength coefficient 'K' in MPa,
Variable 2: Strain hardening exponent 'n-value'.
Figure 5.4: (a) 2D view of design space describing the two variables in flow stress determination problem [Design variable 1: 'K' (in MPa), design variable 2: n-value] (b) 3D view of design space, showing objective function (response) obtained for each K and n-value from simulation. The objective of this study is to find the minimum on the design surface created from response values.

Note: Objective function is minimization of difference between measured and simulated dome height.
5.3.4. Initial guess for design variables (K and n-value)

RSM requires an initial range for each variable. Therefore, an initial guess for strength coefficient 'K' and strain hardening exponent 'n-value' is made to determine starting point and initial size of the region of interest. A wide range is specified to allow for identifying correct values of K and n-value.

**Full factorial approach** (Figure 5.5): Using a full factorial design for FE simulations, for 11 values for each variable, $11 \times 11 = 121$ FE simulations (or design points) are planned. This makes the full factorial approach computationally expensive.

![Figure 5.5: Schematic of a full factorial set of FE simulations for a given input range for K and n-value. For K = 400 to 500, a set of 11 values is chosen (namely, 400, 410, 420 … 500). Similarly, for n-value = 0.1 to 0.5, a set of 11 points is chosen. The number of points is user defined. Thus, for a full factorial set of simulations, $11 \times 11 = 121$ simulations are planned by LS-OPT. This is computationally expensive.](image-url)
D-optimal approach (Figure 5.6a): To reduce unnecessary FE simulations, there are numerous fractional factorial approaches available in LS-OPT. Among these, the 'D-optimal approach' was chosen for this study. The D-optimal design criterion allows the user to select only a few FE simulations (e.g., 10 from 121 in a full factorial approach).

For each of the D-optimal simulations, LS-OPT creates one LS-DYNA keyword file with the corresponding 'K' and 'n' values and initiates each FE simulation in LS-DYNA. After completion of each D-optimal FE simulation, the optimization software LS-OPT receives one response value for the defined objective function E. After each iteration (10 FE simulations), a response surface is fit into the calculated response values by using regression (Figure 5.6b).
In this study, a polynomial response surface of second order is fit to the response values (Figure 5.6b). Based on response obtained from FE simulations in iteration no. 1, LS-OPT will select an
optimal set of K and n-values as inputs. A sub problem with a smaller margin of ‘K’ and ‘n-value’ is defined for the next iteration. FE simulations are then run as additional iterations (set of 10 FE simulations), until the objective function is satisfied.

Figure 5.7 shows results for the second iteration and the eighth (final) iteration in the minimization of objective function (difference between measured and simulated dome height).

Figure 5.7: Minimization of objective function 'E' using RSM. In each iteration (set of 10 FE simulations), the margins for 'K' and 'n-value' shrink to a smaller design space, until objective function is minimized.

Figure 5.8a and Figure 5.8b show the optimization history (or convergence) in LS-OPTview of the strength coefficient 'K' value and of the strain hardening exponent 'n'-value for the bulge test conducted at industrial sponsor. Maximum number of iterations and termination criteria are user-defined. The optimization terminates when design tolerance (step to step change of the variables) and the objective function tolerance (step to step change of objective) is smaller than 0.01.
5.3.5. Inverse analysis results – Room temperature

Bulge tests were carried out at ERC/NSM for two materials (a) Aluminum AA5754-O (t = 1.3 mm), and (b) stainless steel SS304 (t = 0.86 mm). Hydraulic bulge tests were also conducted for AA5754-O (1.01 mm) at industrial sponsor (as discussed in Chapter 4, Figure 4.3).

Measured values of 'pressure vs. dome height' collected in all these tests was used an input to this inverse analysis methodology for calculation of flow stress curves (assuming power law fit).
Figure 5.9: Flow stress results using inverse analysis methodology at room temperature: Aluminum alloy AA5754-O, sheet thickness = 1.01 mm. Biaxial hydraulic bulge tests were conducted at industrial sponsor (seen in Figure 4.3).
Figure 5.10: Flow stress results using inverse analysis methodology at room temperature: Aluminum alloy AA5754-O, sheet thickness = 1.3 mm. Biaxial bulge experiments were conducted at ERC/NSM using the VPB test tool geometry.
Figure 5.11: Flow stress results using inverse analysis methodology at room temperature: Stainless steel SS 304, sheet thickness = 0.86 mm. Biaxial bulge experiments were conducted at ERC/NSM using the VPB test tool geometry.
5.4. Previous work on elevated temperature bulge testing

5.4.1. University of Erlangen-Nuremburg, Germany [Hecht et al. 2005]

Dome curvature data was measured using non-contact sensors for elevated temperature bulge tests of Mg-alloy AZ31-O. Using radial and vertical co-ordinates measured using a CCD camera during the tests, multiple equation were fit to the obtained dome surface curvature.

Figure 5.12: (a) Elevated temperature hydraulic bulge test set up Erlangen, Germany (b) 3D and 2D view of a deformed Mg-alloy AZ31-O dome (measured by CCD camera) [Hecht et al. 2005].
A parabolic equation was fit to the surface curvature. For lower dome heights, results for a circular dome assumption were good. However for larger dome heights (dome height/diameter > 0.4), it was observed that the sphere curvature radius becomes smaller at the dome apex. Thus the assumption that curvature is same over the entire dome (i.e., spherical dome) may be incorrect. The parabolic approach provided the best fit to measured data.

5.4.2. Virginia Commonwealth University (VCU), USA

[Koc et al. 2007] used a non-contact sensor (ARAMIS system) in calculating strains at the dome apex. At elevated temperatures, the influence of strain rate in deformation needs consideration. Thus, in characterizing material properties, it is essential to collect data at constant strain rate. When strain-rate is not maintained constant, the rate dependency behavior of sheet material, may not be characterized accurately. Through flow control, this study focused on obtaining a near-constant strain rate at the dome apex during the bulging process. Flow stress was calculated using the membrane theory assumptions (spherical dome shape) discussed in section 4.2.
(a) Experimental set up (Dome diameter = Ø100 mm)

(b) Bulged samples for different temperatures (for Aluminum alloy AA 5754-O)

Figure 5.13: Elevated temperature hydraulic bulge tests at Virginia Commonwealth University, USA [Koc et al. 2007]
5.4.3. ERC/NSM (with University of Darmstadt, Germany) [Kaya et al. 2008]

As seen in Figure 5.14b, the bursting height increased from 12 mm to 38 mm when sheet temperature was increased from room to 225°C. Thus, in forming the Mg-alloy AZ31-O, significant formability improvement was achieved at elevated temperatures.

During the elevated temperature tests it was observed that the dome shape of the bulged samples deviated from the assumed spherical shape. This result is consistent with observations made by [Hecht et al. 2005]. Sheets formed to high bulge heights also exhibited an unusual thickness distribution. Measurements conducted to quantify the difference in dome shape during
bulging indicated that bulge shape does not deviate from the assumed sphere until bulge height to diameter ratio is approximately 0.2 (Figure 5.15a). This could be attributed to the influence of strain rate during the deformation. During hydraulic bulging at elevated temperatures, even though obtaining an approximate true constant strain rate is possible at the apex by controlling the flow rate, there is a strain rate gradient along the contour of the bulged sheet (Figure 5.15b).

![Image of bulging process](image)

(a) At dome height of ~ 34 mm, the dome surface showed a deviation from sphere.

(b) During elevated temperature bulge test, strain rate in the sample shows a radial gradient.

Figure 5.15: Deviation of the dome surface from spherical shape (at elevated temperature) [Kaya et al. 2008].

For flow stress calculations, the closed-form membrane theory was applied. Analytical models for thickness and radius calculations (used at room temperature under assumption of spherical
dome) were found to be acceptable up to bulge height/diameter ratio of 0.2 (bulge height < 25 mm). But at higher bulge heights (bulge height > 30 mm), comparisons between measured and calculated thickness and bulge radius values show a difference of upto 10% from experiments [Kaya et al. 2008].
5.5. Inverse analysis methodology – Application to elevated temperature

At present, the developed methodology (validated at room temperature) can be applied only for materials that follow the power law fit. In applying the developed methodology to elevated temperature, influence of strain rate must be considered. As a first approximation, by introducing a strain rate exponential term in the power law equation \( \sigma = K (\varepsilon) ^n (\dot{\varepsilon}) ^m \) the influence of strain rate can be taken into consideration.

Input data (bulge pressure vs. dome height) must be obtained by conducting multiple bulge tests at different bulging rates. The tests will be conducted at three elevated temperatures (T<sub>1</sub>, T<sub>2</sub>, T<sub>3</sub>) with two different loading paths (i.e., two pressure or flow rate variations in time, resulting in two bulge speeds, L<sub>1</sub> & L<sub>2</sub>). During the bulge tests, the bulge height (at the apex) or the bulge curvature (three points on dome surface) will be recorded over time.

5.5.1. Objective function:

This is formulated similar to the validated methodology at room temperature. The objective function is the difference between the measured values of bulge height (or curvature) and the corresponding computed one, as represented by:

\[
E = E(K, n, m) = \sum_{x=1}^{X} \left( \frac{1}{N} \sum_{t=1}^{N} \left( \frac{H_{\text{measured}} - H_{\text{simulation}}}{H_{\text{measured}}} \right)^2 \right) + \sum_{x=1}^{X} \left( \frac{1}{N} \sum_{t=1}^{N} \left( \frac{H_{\text{measured}} - H_{\text{simulation}}}{H_{\text{measured}}} \right)^2 \right)
\]

with,

- \( K, n, m \) Strength coefficient (MPa), strain hardening exponent and strain rate hardening exponent, respectively [for power law \( \sigma = K (\varepsilon) ^n (\dot{\varepsilon}) ^m \)]
- \( H_{\text{measured}} \) measured dome height from bulge test at time = t
- \( H_{\text{simulation}} \) computed dome height from bulge test simulation at time = t
- \( t \) time increment at which dome height is measured/calculated
N  total number of increments

L₁, L₂  bulge speeds 1 and 2 (corresponding to two strain rates at dome apex)

x  number of points on dome surface to characterize the curvature. In this analysis, 3 points (one at apex, two on either side) will be used (Figure 5.16a).

Initially, x = 1 (measurement only at dome apex) will be used to validate the methodology. Figure 5.16b shows the inverse analysis methodology proposed for elevated temperature bulge tests.
Figure 5.16: (a) Definition of objective function (b) Proposed inverse analysis methodology for elevated temperature bulge test, to determine flow stress curve [for materials following the power law fit $\sigma = K (\varepsilon)^n (\dot{\varepsilon})^m$] using dome geometry evolution.
5.5.2. Materials of interest

Based on sponsor interest and potential for warm forming/hydroforming applications, the following sheet materials were selected in this investigation:

- Aluminum alloy AA 5754-0
- Aluminum alloy AA 5182-0
- Aluminum alloy AA 3003-0
- Magnesium AZ31B, AZ31-O
- Stainless steel SS 304

Temperatures of interest: 125 to 300°C.

5.5.3. Tool design for elevated temperature sheet bulge test

As part of an ongoing NSF-STTR proposal, an elevated temperature hydraulic bulge test tool was designed and is presently being built. The main design considerations in designing the bulge test tools are summarized in Figure 5.17 and Table 5.2.

<table>
<thead>
<tr>
<th>Design considerations</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Die corner radius = 0.25 inches (6.35 mm)</td>
<td>Sharper the corner radius the higher the bending strains. This value was selected same as room temperature bulge test at ERC/NSM.</td>
</tr>
<tr>
<td>Die diameter = 4 inches (101.6 mm)</td>
<td>Approximately same as ERC/NSM's room temperature bulge test tools</td>
</tr>
</tbody>
</table>
| Lockbead dimensions | The objective of the lockbead is to clamp the sheet and avoid any draw-in during testing. The sharper the lockbead corner radii, 
(a) larger the bending strains during clamping (undesirable) 
(b) higher the tonnage needed to clamp and hold the sheet during bulging (undesirable) 
(c) Reduced material draw-in (desirable) |

Table 5.2: Design considerations for elevated temperature bulge test tools
5.5.3.1. Lockbead design

Preliminary simulations of the bulge test indicated that the clamping tonnage available (25 tons) was insufficient to prevent the sheet from flowing in during bulging. Thus, a lockbead was necessary to lock the sheet. In selecting & designing lockbeads/drawbeads in sheet metal forming, there are numerous analytical models used to predict,

(a) restraining force (resistance offered by drawbeads to sheet material flow) and

(b) hold down force (minimum vertical force needed to keep the sheet clamped).

Among the various analytical models for drawbead forces available in literature, Stoughton’s drawbead model matched best with experimental data, especially for Aluminum alloys (at room temperature) [Appendix A, Yadav et al. 2004].
In this study, it is desired to lock the sheet, i.e., very large restraining force - with as low a hold down force as possible. To achieve these objectives, Stoughton's mathematical model was used to select the best lockbead geometry (semicircular or rectangular), and design the following dimensions to lock the sheet,

(i) penetration depth, (ii) corner radii and (iii) tool clearances

**STEP 1:** Select lockbead dimensions (penetration depth & corner radii) based on:

- Other lockbead designs,
  - (a) Room temperature bulge test (ERC's VPB test, see Figure 1.4)
  - (b) Modified Limiting Dome Height (MLDH) tool at ERC/NSM
- Stoughton's drawbead model (Excel based calculator, see Appendix A)

**STEP 2:** Conduct isothermal FE simulation of sheet clamping within beads to satisfy following:

- Constraint 2.1: Bending strains (effective) < Fracture strain in tensile test
  *(Note: This criteria is not the best, research on better criteria on failure in bending at elevated temperature is ongoing)*
- Constraint 2.2: Tonnage needed to clamp sheet completely < 23 tons

**STEP 3:** Conduct isothermal FE simulation of sheet bulging to satisfy following:

- Constraint 3.1: Movement, or draw-in of the outer edge of flange < 0.2 mm (near zero)
- Constraint 3.2: Tonnage to hold sheet during bulging < 25 tons

Figure 5.18: Methodology for selecting lockbead dimensions for elevated temperature bulge test tools.
Using the Stoughton math model, semicircular and rectangular drawbead forces were compared. For AA5754-O, blank thickness 1 mm, sheet width 1 mm, temperature 150°C, rectangular drawbeads shows higher restraining force for nearly the same hold down force as semicircular drawbead. Therefore, rectangular lockbeads were better suited for the proposed tool design.

5.5.3.2. Tonnage requirements

A limited tonnage of 25 tons is available for clamping as well as bulging. At lower sheet temperatures, due to higher flow stress of the material larger tonnage is required to clamp the sheet within lockbeads. Therefore, tools were designed to clamp at the lowest testing temperature (125°C or 150°C). During the test, the sheet will be pressurized and bulged into the die cavity. A reaction force will be exerted by the bulging sheet on the die corner. Therefore, the available tonnage (25 tons) was designed to:

(a) hold the sheet within beads during bulging to ensure complete locking, as well as

(b) counter the reaction force exerted by the bulging sheet.
Maximum available tonnage = 25 tons

Applied holding force (approx 12 to 15 tons)

(a)

(b) Effective strain distribution in the sheet clamped within lockbeads

Figure 5.19: (a) 2D schematic of the designed axisymmetric elevated temperature bulge test tool. (b) Effective strains in the clamped sheet (within lockbeads) are lesser than uniform elongation value for AA5754-O material. Thus, design is safe.
5.5.4. Other considerations: Locating sensors for dome curvature measurement

Figure 5.20: Locating the three LVDT's for measuring the dome curvature. One LVDT will measure the dome apex, while the other two are designed for flexibility in measuring location.
5.6. Summary

For characterizing sheet material properties (flow stress curve) using the biaxial bulge test, membrane theory equations are used (with the assumption that the deforming dome has spherical shape, i.e., constant curvature over the surface). When applying the bulge test for elevated temperature flow stress determination, preliminary tests indicate that the spherical dome assumption made in the membrane theory's closed form equations may introduce errors in the calculated flow stress, at high values of strain (corresponding to large dome heights). In a recent study by [Kaya et al. 2008] these errors were estimated to be approximately 10% for large dome heights (for Mg- AZ 31-O material).

Due to the limitations of the existing flow stress calculation methodology, the main thrust of this study was to develop an improved, robust approach for flow stress calculation. Thus, 'inverse analysis' was proposed to identify the parameters of a material law (regression fit). The inverse analysis approach will also use 'pressure versus dome height' data collected from bulge test experiments. Membrane theory closed form equations are replaced by FEM engine LS-DYNA. As a first approximation, the material properties (flow stress) for the tested sheet materials were assumed to follow the commonly used power law fit \( \sigma = K \varepsilon^n \), where, ‘K’ is the strength coefficient of the material (in MPa) and ‘n’ or ‘n-value’ is the strain hardening exponent.

In this optimization-based approach,

(a) The objective function was defined as the difference between measured and FE calculated values of dome height (in a least square sense).

(b) Commercial FE code LS-DYNA and its optimization tool LS-OPT were used to identify the ‘K’ and ‘n-value’ for the tested materials.

(c) LS-OPT uses Response Surface Methodology (RSM) to construct an approximate optimization problem. The two design variables (K and n-value) are iteratively varied (using a design of experiments approach) and multiple FE simulations are run in FE code
LS-DYNA. The optimization tool LS-OPT identifies the “best” possible K and n-value to match the measured and FE simulated dome heights.

Room temperature test data (pressure vs. dome height curve) was available for the following materials, from two experimental sources:

(a) Aluminum alloy AA5754-O (t = 1.3 mm) from bulge tests conducted at ERC/NSM

(b) Stainless steel SS304 (t = 0.86 mm) from bulge tests conducted at ERC/NSM

(c) Aluminum alloy AA5754-O (t = 1.01 mm) from hydraulic bulge tests conducted at an industrial sponsor (summarized in Chapter 4)

Table 5.3 summarizes the flow stress results obtained by applying the developed inverse analysis methodology for the Al-alloy and stainless steel. The room temperature flow stress results obtained from this approach were compared with (a) tensile test flow stress and (b) bulge test flow stress obtained by using membrane theory assumptions. A fairly good match was obtained (within 5 to 10% for Al-alloy, within 5% for stainless steel).

<table>
<thead>
<tr>
<th>Material</th>
<th>'K' (MPa)</th>
<th>n-value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Aluminum AA5754-O, 1.01 mm (at industrial sponsor)</td>
<td>432</td>
<td>0.3</td>
</tr>
<tr>
<td>Aluminum AA5754-O, 1.3 mm (at ERC/NSM)</td>
<td>400</td>
<td>0.24</td>
</tr>
<tr>
<td>Stainless steel SS 304, t = 0.86 mm (at ERC/NSM)</td>
<td>1275</td>
<td>0.31</td>
</tr>
</tbody>
</table>

Table 5.3: Inverse analysis results summary: Predicted values at room temperature

The inverse analysis methodology is validated for room temperature and serves as a first step in the using this approach for elevated temperature flow stress determination using FEM (LS-DYNA) and optimization tools (LS-OPT). These simulation tools offer considerable flexibility in characterizing material behavior (numerous material laws available) and offer great potential in application to elevated temperature bulge tests. In applying the developed methodology to elevated temperature, influence of strain rate must be considered. As a first approximation, by
introducing a strain rate exponential term in the power law equation $\sigma = K (\varepsilon)^n (\dot{\varepsilon})^m$ the influence of strain rate can be taken into consideration. The dome height will be measured at three locations on the dome surface (at the dome apex, and two on either side). For preliminary studies, only the dome height at apex will be used to validate the methodology. An elevated temperature bulge test tool was designed to test Al alloys (AA5182-O and AA5754-O, $t = 1$ mm). The tool is presently being manufactured.
6.1. Problem statement

In sheet hydroforming with punch (SHF-P) process, the blank holder force (BHF) and fluid pressure in the pot (pot pressure) significantly influence the quality of the formed part.

- During forming, the pot pressure forces the sheet material against the punch. This prevents relative movement between the sheet and the punch due to interface friction. Thus, stretching is postponed and the formability in SHF-P is increased compared to conventional stamping.

- BHF controls material flow during the process and prevents leakage of fluid. BHF may vary in location (over the blank surface) during forming, thereby allowing better control of material flow at straight sides (Section 2) and corners (Section 1) where material flow and state of stress and strain are different (ex: Rectangular part, Figure 6.1).
Figure 6.1: Schematic shows top view of example non-axisymmetric (rectangular) part. Due to asymmetry, the state of stress in the deforming sheet metal is different along the perimeter.

The pot pressure acting on the sheet material is same at all locations. Along the straight edges, the sheet material is in "plane strain" condition with lesser compressive stresses along the transverse direction. Therefore, lesser pot pressure \( P_a \) is required to force the sheet against the punch wall to reduce thinning (Figure 6.2, Section 2).

Figure 6.2: Pot pressure \( P_a \) is not sufficient to lift the sheet metal off the die corner at Section 1. Thus, insufficient pot pressure causes rubbing of sheet with the die corner leading to excessive thinning.
However, the pot pressure $[P_a]$ is insufficient along the corners (Figure 6.2, Section 1), resulting in material flow similar to stamping. This is because at the corner, compressive stress in transverse/hoop direction requires more pot pressure to force the sheet against the punch wall. An increase in the pot pressure to $P_b$ ($P_b > P_a$) to force the sheet against the punch wall in corner in section 1 (Figure 6.3) would result in bulging of the sheet against the drawing direction in section 2 (Figure 6.3) resulting in failure.

Therefore, the maximum pot pressure that could be applied in forming asymmetric parts is part geometry dependent and would be the minimum pressure that avoids bulging of the sheet against the drawing direction. However, this maximum pressure might not be acceptable at the corners where maximum thinning is observed in a formed part.

Figure 6.3: Sheet metal flow in sections 1 and 2 (from Figure 1) for the pot pressure of $P_b$ (Note: $P_b > P_a$). An increased pot pressure $P_b$ ensures that sheet metal does not rub at the die corner at Section 1. However, this increased pot pressure $P_b$ causes the sheet metal to “bulge against drawing direction” at Section 2.
Figure 6.4: Process limits on pot pressure are limited by sheet bulging in straight sections (for high pot pressure) and sheet rubbing at die corner (for low pot pressure).

In summary, the excessive sheet bulging in straight sections can be avoided by reducing the pot pressure. Reducing pot pressure causes sheet metal to rub at the die corner (where state of stress is nearly axisymmetric). Therefore, pot pressure must be selected within a narrow process window to avoid bulging as well as rubbing at the die corner. Figure 6.4 illustrates the process limits on pot pressure. These process limits are part geometry dependent.
6.2. Objectives

The overall objective of this study is to develop a fundamental understanding of the influence of tool design variables (such as punch-die clearance) on sheet deformation in SHF-P process of (a) axisymmetric parts and (b) non-symmetric deep drawn parts.

The specific objectives of this study are:

- Investigate the influence of punch-die clearance on part quality (namely, part thinning and sheet bulging) in SHF-P process axisymmetric punch shapes (cylindrical and conical).

6.2.1. Tool and press set up available at Schnupp Hydraulik, Germany

Figure 6.5 shows dimensions of the Ø 90 mm cylindrical cup tools at Schnupp Hydraulik.

![Figure 6.5: Schematic of the Ø90 mm cylindrical cup tooling at Schnupp Hydraulik.]

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch diameter, $D_p$</td>
<td>Ø90 mm</td>
</tr>
<tr>
<td>Punch corner radius, $r_p$</td>
<td>5 mm</td>
</tr>
<tr>
<td>Die opening diameter, $D_d$</td>
<td>Ø95 mm</td>
</tr>
<tr>
<td>Die corner radius, $r_d$</td>
<td>8 mm</td>
</tr>
<tr>
<td>Gap, $g$ (between blankholder and punch)</td>
<td>1 mm</td>
</tr>
<tr>
<td>Initial sheet thickness, $t$</td>
<td>1 mm</td>
</tr>
<tr>
<td>Clearance, $c$</td>
<td>2.5 mm</td>
</tr>
</tbody>
</table>

Figure 6.6 shows the schematic of the double action press at the Schnupp Hydraulik facility used for Ø 90 mm cylindrical punch SHF-P experiments. Technical specifications of the press are
given in Table 6.1. In the double action press, the punch is attached to the inside ram and the
blank holder is attached to the outer ram.

Figure 6.6: Photograph of the 800 kN (80 ton) hydraulic press used for experiments at
Schnupp Hydraulik, Germany

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum values of punch force / blank holder force</td>
<td>800 kN (80 ton) / 4 x 100 kN (40 ton)</td>
</tr>
<tr>
<td>Maximum water (pot) pressure</td>
<td>400 bar (~ 5800 psi)</td>
</tr>
<tr>
<td>Maximum values of punch stroke / blank holder stroke</td>
<td>500 mm / 250 mm</td>
</tr>
<tr>
<td>Shut height / Area of press bed</td>
<td>395 mm / 580 mm x 450 mm</td>
</tr>
</tbody>
</table>

Table 6.1: Technical specifications of the press at Schnupp Hydraulik GmbH, Germany.
Blank holder force (BHF) and pot pressure control: A hydraulic cylinder actuates the inside ram (punch) independently while four hydraulic cylinders actuate the outer ram (blank holder). The four hydraulic cylinders actuating the blank holder are independently controlled thereby allowing the user to change BHF in space and punch stroke during the SHF-P process. The pressure pot is attached to the press bed. A separate hydraulic circuit controls the pressure inside the pot during the SHF-P process. The closed loop control system for the BHF and the pot pressure helps the press to trace the user specified pot pressure and BHF profile as a function of punch stroke during the SHF-P process. The pot pressure and the BHF profiles can be programmed as a step function and/or as a linear function of punch stroke.

6.2.2. Preliminary experiments conducted at Schnupp Hydraulik, Germany

Preliminary studies were conducted to hydroform a Ø 90 mm cylindrical cup of height 105 mm. The blank/sheet material used for the experiments was St14 steel (Draw Quality Steel). The process parameters [blank holder force (BHF) and pot pressure versus punch stroke curves] were estimated by Schnupp Hydraulik by trial and error experiments. The estimated BHF curve and pot pressure curve are shown in Figure 6.7a. The cylindrical cup was successfully formed with minor wrinkles on the sidewall and flange, with no leakage of the pressurizing medium during the experiment [Figure 6.7b].
6.2.3. Previous studies conducted in co-operation with Schnupp Hydraulik, Germany

Studies were conducted by [Braedel, et al. 2005] on optimization of the process parameters [BHF curve, pot pressure curve] for SHF-P process of the existing Ø 90 mm round cup available at Schnupp Hydraulik. To obtain material data at large strains, the flow stress data for St14 steel (Draw Quality Steel) sheet used in this study, was determined by the VPB test [Gutscher 2004]. The obtained true stress – true strain data points were given as input to FEM.

A “sequential optimization” routine was developed by [Braedel, et al. 2005] that coupled numerical optimization techniques with FE simulation. The developed methodology was used to estimate the optimum BHF curve for a given pot pressure curve, needed to hydroform a good part (with minimum possible thinning in formed part, no flange or side wall wrinkles, and no leakage of pressurizing medium). The optimization problem was formulated as follows:

**Objective function:** Minimize the maximum thinning in the part.
**Constraint functions:** Sidewall wrinkle, flange wrinkle and leakage of the pressurizing medium.

**Design variables:** Loading path (pot pressure vs. punch stroke and BHF vs. punch stroke).

The developed optimization methodology was used to estimate the optimum BHF curve as function of punch stroke (Figure 6.8a).

![Optimum BHF curve](image)

**Figure 6.8:** (a) The optimum BHF curve was estimated using numerical techniques coupled with FE simulation. The “ERC/NSM pot pressure curve” was estimated by [Contri, et al. 2004] through trial-and-error FE simulation (b) Part formed successfully, using ERC/NSM pot pressure curve and optimum BHF curve estimated by [Braedel, et al. 2005]
6.3. Influence of punch-die clearance on sheet deformation in SHF-P of cylindrical cup

Ø90 mm cylindrical tools available at Schnupp Hydraulik, Germany will be used (see Figure 6.5 for geometry). For this tool geometry, the optimum blank holder force and pot pressure were predicted and experimentally validated in a previous study [Braedel et al. 2005]. As seen in Figure 6.5, for the existing cylindrical punch geometry,

- Two punch-die clearances (c), 2.5 mm and 5.5 mm were studied. Thus, two die rings were used in this investigation. Die (or die-ring) of diameter Ø95 mm already exists. A second die ring of diameter = Ø101 mm was manufactured.

- Two punch corner radii (rₚ) were investigated. The existing punch has corner radius = 5 mm. A second punch with corner radius = 10 mm was machined.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch diameter (Ø Dₚ)</td>
<td>90 mm (maintained same as existing punch)</td>
</tr>
<tr>
<td>Punch corner radius (rₚ)</td>
<td>5 mm (existing punch) and,</td>
</tr>
<tr>
<td></td>
<td>10 mm (new punch)</td>
</tr>
<tr>
<td>Die corner radius (rₜ)</td>
<td>8 mm (maintained same as existing die-ring)</td>
</tr>
<tr>
<td>Punch – die clearance (c)</td>
<td>2.5 mm and</td>
</tr>
<tr>
<td></td>
<td>5.5 mm</td>
</tr>
<tr>
<td>Die diameter (Ø D₀)</td>
<td>Φ 95 mm (existing die-ring) and,</td>
</tr>
<tr>
<td></td>
<td>Φ 101 mm (new die-ring)</td>
</tr>
</tbody>
</table>

Table 6.2: Modifications to cylindrical punch tools at Schnupp Hydraulik, Germany

Using FE simulation, the influence of punch-die clearance (c = 2.5 mm and 5.5 mm) on,

a) formed part thinning and, b) deformed part geometry
will be studied for two punch geometries (punch corner radii = 5 mm and 10 mm). Experimental validation of FE predictions will be conducted at Schnupp Hydraulik.

6.3.1. Finite element (FE) simulation

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Blank material</td>
<td>St 1403 steel (Draw Quality Steel)</td>
</tr>
<tr>
<td>Initial blank geometry</td>
<td>Ø 230 mm, thickness 1.00 mm</td>
</tr>
<tr>
<td>Young’s modulus</td>
<td>210 GPa</td>
</tr>
<tr>
<td>Material flow stress (Figure 6.10)</td>
<td>K = 576 MPa, n = 0.2, for power law $\sigma = K\varepsilon^n$</td>
</tr>
<tr>
<td>Anisotropy (tensile test)</td>
<td>$r_0 = 1.65$, $r_{45} = 1.25$, $r_{90} = 1.94$</td>
</tr>
<tr>
<td>Coulomb friction coefficient</td>
<td>Punch/sheet = 0.12, Die/sheet = 0.06, Blank holder/sheet = 0.06</td>
</tr>
</tbody>
</table>

Figure 6.9: FE-Model (quarter geometry) in PAMSTAMP 2000 (AQUADRAW) for the Ø 90 mm cylindrical cup SHF-P process.

FE simulation of cylindrical cup SHF-P was conducted using FE code PAMSTAMP 2000. Figure 6.9 shows the FE model. Schnupp Hydraulik provided the technical drawings for all tools. The punch, die, blank holder and pressure pot were considered rigid while blank was modeled as anisotropic elastic plastic. Quarter FE model was used due to the axisymmetric deformation and boundary conditions, and symmetric material properties of the sheet metal.
6.3.2. Material properties, lubricant (friction) properties

Due to unavailability of the sheet material ‘St14’ used in previous studies, ‘St1403 (Draw Quality Steel)’ provided by Schnupp Hydraulik was used in this study. Flow stress for St1403 was obtained from VPB bulge tests conducted at ERC (Figure 6.10). Anisotropy for St 1403 was obtained from uniaxial tensile tests conducted at ERC.

A thin elastomeric foil was used as dry film lubricant in experiments by Schnupp. Therefore, Coulomb friction coefficient between sheet-blank holder and sheet–die was set to 0.06 in FE simulations. No lubricant was used between punch and sheet, therefore, punch - sheet interface friction coefficient of 0.12 was used in FE simulation.

\[ \sigma = 576.45 \varepsilon^{0.1995} \text{ MPa} \]

Figure 6.10: Flow stress of ‘St1403’ sheet material of thickness 1 mm estimated by Viscous Pressure Bulge (VPB) test at ERC/NSM. Uniaxial tensile tests were conducted to obtain the anisotropy coefficients (\( r_{00} \), \( r_{45} \) and \( r_{90} \)).

6.3.3. Simulation/Experimental matrix

As seen in Table 6.3, four experimental cases were planned using the Ø 90 mm cylindrical punch geometry. FE simulations were conducted for each experimental case. Two punch–die
clearances (2.5 mm and 5.5 mm) were selected to demonstrate the effect of punch-die clearance on sheet deformation. Two punch corner radii (5 mm and 10 mm) were selected for this study.

<table>
<thead>
<tr>
<th>Sim/Expt no.</th>
<th>Tool geometry</th>
<th>Process parameters (vs. punch stroke)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Punch corner radius ( (r_p) )</td>
<td>Punch-die clearance ( (c) )</td>
</tr>
<tr>
<td>1</td>
<td>5 mm</td>
<td>2.5 mm</td>
</tr>
<tr>
<td>2</td>
<td>5.5 mm</td>
<td>5.5 mm</td>
</tr>
<tr>
<td>3</td>
<td>10 mm</td>
<td>2.5 mm</td>
</tr>
<tr>
<td>4</td>
<td>10 mm</td>
<td>5.5 mm</td>
</tr>
</tbody>
</table>

Note: All other tool dimensions are same as existing tooling setup at Schnupp Hydraulik (Figure 6.5).

Table 6.3: Experimental / FE simulation matrix (for Ø 90 mm cylindrical punch)

6.3.4. Process parameters [Pot pressure, blank holder force \( \text{(BHF)} \) curves]

Pot pressure curve: In previous studies conducted with the cylindrical punch geometry, an optimum pot pressure versus punch stroke curve was estimated [Braedel et al. 2005] using numerical optimization techniques.

Preliminary FE simulations conducted with this optimum pot pressure curve [Maximum value = 400 bar, Figure 6.11] show that the deforming sheet completely lifts off the die corner as it flows into the die. The complete lifting of the sheet off the die corner was observed to be the same for different values of punch-die clearance. Thus, for the Ø 90 mm cylindrical punch geometry, for a large pot pressure of 400 bar, the sheet deformation is independent of the punch-die clearance.

To investigate the effect of punch-die clearance on sheet deformation, an arbitrary lower value of pot pressure of 180 bar was selected for this study (Figure 6.11). The same pressure curve was used for all four experimental cases.
Figure 6.11: In investigating influence of punch-die clearance in SHF-P of cylindrical cups, the previously estimated optimum pot pressure curve (Max. value 400 bar) was too high. Thus, a reduced pot pressure curve (Max. value 180 bar) was used.

Blank holder force (BHF) curve: For each experimental case, optimum BHF curve was determined using FE simulation coupled with numerical optimization methods [Braedel et al. 2005]. Figure 6.12 and Figure 6.13 show optimum BHF obtained for the four experimental cases, for pot pressure of 180 bar.
Figure 6.12: Optimum blank holder force (BHF) curves for experiments 1 and 2 for pot pressure 180 bar (see Figure 6.11), for punch corner radius = 5 mm.

Figure 6.13: Optimum blank holder force (BHF) curves for experiments 3 and 4 for pot pressure 180 bar (see Figure 6.11), for punch corner radius = 10 mm.
6.3.5. Experimental validation

Using the predicted pot pressure and BHF curves from FE simulation (Figure 6.12 and Figure 6.13), four cylindrical cups were formed at Schnupp Hydraulik. Only one cup was formed for each experimental case. For each formed cup, the thinning distribution in the part (along the rolling direction) and the flange outline (material draw-in) was compared with FE predictions.

Figure 6.14: Formed cylindrical cups for the four experimental cases.
6.3.6. Experiment 1 (punch corner radius = 5 mm, punch-die clearance = 2.5 mm)

Figure 6.15: Comparison of (a) material draw-in and (b) thinning distribution (along rolling direction of sheet) between FE simulation and experiment, for Experiment 1.
6.3.7. Experiment 2 (punch corner radius = 5 mm, punch-die clearance = 5.5 mm)

Figure 6.16: Comparison of (a) material draw-in and (b) thinning distribution (along rolling direction of sheet) between FE simulation and experiment, for Experiment no. 2.
6.3.8. Experiment 3 (punch corner radius = 10 mm, punch-die clearance = 2.5 mm)

Figure 6.17: Comparison of (a) material draw-in and (b) thinning distribution (along sheet rolling direction) between FE simulation and experiment, for Experiment 3.
6.3.9. Experiment 4 (punch corner radius = 10 mm, punch-die clearance = 5.5 mm)

![Graph showing comparison between FE simulation and experiment for Experiment 4.](image)

Figure 6.18: Comparison of (a) material draw-in and (b) thinning distribution (along rolling direction) between FE simulation and experiment, for Experiment 4.
6.3.10. Results summary: Influence of punch-die clearance in SHF-P of cylindrical cup

Flow stress of sheet material [St 1403 (Draw Quality Steel), thickness 1 mm] was determined using the VPB (Viscous Pressure Bulge) test. Anisotropy constants were obtained from the uniaxial tensile test. Existing cylindrical tools at Schnupp Hydraulik (Ø 90 mm, formed cup height = 105 mm), were used. To investigate the effect of punch-die clearance on sheet deformation, four experimental cases were proposed. Two punch-die clearances (2.5 mm, 5.5 mm) and two punch corner radii (5 mm, 10 mm) were investigated.

FE simulation (PAMSTAMP 2000) coupled with numerical optimization method was used in this study. For selected cylindrical cup, a pot pressure of 180 bar was arbitrarily selected per preliminary FE simulation results. Optimum blank holder force vs. punch stroke curve was determined using FE simulation for each of the four proposed experimental cases. According to the planned experimental matrix, four cups (one for each experimental case) were formed by Schnupp Hydraulik. Towards the end of forming process, wrinkles were observed in the side wall of the formed cups.

### Table 6.4: Influence of punch die clearance: Results summary for thinning distribution

<table>
<thead>
<tr>
<th>Case A: Ø 90 mm cylindrical punch, with punch corner radius = 5 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch-die clearance</td>
</tr>
<tr>
<td>--------------------</td>
</tr>
<tr>
<td>2.5 mm (Experiment no. 1)</td>
</tr>
<tr>
<td>5.5 mm (Experiment no. 2)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Case B: Ø 90 mm cylindrical punch, with punch corner radius = 10 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch-die clearance</td>
</tr>
<tr>
<td>--------------------</td>
</tr>
<tr>
<td>2.5 mm (Experiment no. 3)</td>
</tr>
<tr>
<td>5.5 mm (Experiment no. 4)</td>
</tr>
</tbody>
</table>

For Case A, on increasing punch-die clearance from 2.5 mm to 5.5 mm:

- The maximum thinning location shifted from the cup wall to the punch corner.
There is a marginal improvement in the maximum thinning (3%) in the formed part.

For Case B, on increasing punch-die clearance from 2.5 mm to 5.5 mm:

- There is a marginal improvement in the maximum thinning (4%) in the formed part.

<table>
<thead>
<tr>
<th>Case A: Ø 90 mm cylindrical punch, with punch corner radius = 5 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch-die clearance</td>
</tr>
<tr>
<td>2.5 mm (Experiment no. 1)</td>
</tr>
<tr>
<td>5.5 mm (Experiment no. 2)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Case B: Ø 90 mm cylindrical punch, with punch corner radius = 10 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Punch-die clearance</td>
</tr>
<tr>
<td>2.5 mm (Experiment no. 3)</td>
</tr>
<tr>
<td>5.5 mm (Experiment no. 4)</td>
</tr>
</tbody>
</table>

Table 6.5: Influence of punch die clearance: Results summary for wrinkling in part

For punch-die clearance = 5.5 mm (Experiment no. 2 and 4),

- The selected pot pressure (180 bar) was insufficient to form the cup completely against punch surface. Large pot pressure is needed towards the end of punch stroke to force the sheet completely against punch surface (Figure 6.19).

- A good part (without wrinkles) could not be formed since side wall wrinkles were observed in the formed cup during FE simulation and experiments.
Figure 6.19: Sidewall wrinkles in the part were observed for punch-die clearance 5.5 mm
(a) Formed part from Experiment no. 2 (b) Formed part from Experiment no. 4.

6.4. Additional punch-die clearance studies with increased pot pressure curve

In the punch-die clearance study with Ø 90 mm cylindrical cup geometry, the selected pot pressure curve of 180 bar was insufficient to form the cup completely against the punch. Therefore, the experiments were repeated with an increased pot pressure curve (Figure 6.20), and blank holder force (BHF) (Figure 6.21 and Figure 6.22) obtained by trial-and-error. Table 6.6 shows the modified experimental matrix.

<table>
<thead>
<tr>
<th>Expt no.</th>
<th>Tool geometry</th>
<th>Modified process parameters</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Punch corner radius ((r_p))</td>
<td>Punch-die clearance ((c))</td>
</tr>
<tr>
<td>1</td>
<td>5 mm</td>
<td>2.5 mm</td>
</tr>
<tr>
<td>2</td>
<td>5.5 mm</td>
<td>5.5 mm</td>
</tr>
<tr>
<td>3</td>
<td>10 mm</td>
<td>2.5 mm</td>
</tr>
<tr>
<td>4</td>
<td></td>
<td>5.5 mm</td>
</tr>
</tbody>
</table>

Note: All other tool dimensions are same as existing Ø90 mm tool setup at Schnupp Hydraulik, (Figure 6.5).

Table 6.6: Experimental matrix with modified process parameters
Figure 6.20: Pot pressure curve (180 bar) was insufficient to form the cup completely against the punch. Therefore, pot pressure curve was modified by Schnupp Hydraulik and experiments were repeated.

Figure 6.21: Blank holder force (BHF) curves [by trial-and-error], for experiments 1 and 2 (punch corner radius = 5 mm) using Schnupp pot pressure curve (Figure 6.20).
6.4.1. Experimental results

Figure 6.22: BHF curves obtained by trial-and-error, for experiment nos. 3 and 4 (punch corner radius = 10 mm) using Schnupp pot pressure curve (Figure 6.20).

Figure 6.23: Four cups were formed with the modified pot pressure curve (one cup for each experimental case).
Four cups (one for each experimental case) were formed by Schnupp Hydraulik per planned experimental matrix. Table 6.7 summarizes experimental results for the four experimental cases.

<table>
<thead>
<tr>
<th>Expt. no.</th>
<th>Tool geometry</th>
<th>Depth of formed cup</th>
<th>Thinning distribution comparison</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Punch corner radius ($r_p$)</td>
<td>Punch-die clearance ($c$)</td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>5 mm</td>
<td>2.5 mm</td>
<td>120 mm</td>
</tr>
<tr>
<td>2</td>
<td>5.5 mm</td>
<td>110 mm</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>10 mm</td>
<td>2.5 mm</td>
<td>120 mm</td>
</tr>
<tr>
<td>4</td>
<td>5.5 mm</td>
<td>120 mm</td>
<td></td>
</tr>
</tbody>
</table>

Table 6.7: Results summary – additional experiments conducted by Schnupp Hydraulik, with a modified pot pressure curve.

Figure 6.24: Thinning distribution comparison (along sheet rolling direction) for Experiments 3 and 4.
6.4.2. Results summary: Cylindrical punch, with increased pot pressure curve

Using the existing cylindrical punch available at Schnupp Hydraulik, four experimental cases were proposed to investigate the effect of punch-die clearance on sheet deformation. Two punch-die clearances (2.5 mm, 5.5 mm) and two punch corner radii (5 mm, 10 mm) were investigated. Initial experiments were conducted with pot pressure of 180 bar. The selected pressure for not sufficient to form the cylindrical cup completely when punch-die clearance was 5.5 mm, and side wall wrinkles were observed. Therefore, Schnupp repeated the experiments with a modified pot pressure curve (increased to eliminate side wall wrinkling). Blank holder force for each of the four experiments was determined by trial and error experiments by Schnupp Hydraulik. The following observations were made:

- Four cups (one for each experimental case) were formed successfully (with no side wall wrinkles), as per the proposed experimental matrix.
- Depths of formed cups for Experiments 1 and 2 were not same. Therefore, a comparison of thinning distribution could not be made. Thinning distribution in the formed parts for Experiments 3 and 4 were compared. An increase in punch-die clearance from 2.5 mm to 5.5 mm showed no significant improvement in part thinning distribution (seen in Figure 6.24).
6.5. Influence of punch-die clearance on sheet deformation, with conical punch

6.5.1. Problem statement

With the existing cylindrical punch, only one value of punch-die clearance can be studied at a time. With the cylindrical punch, two values of punch-die clearance (2.5 and 5.5 mm) were investigated by using two die-rings. Also, for the existing Ø 90 mm cylindrical punch, for pot pressures up to 400 bar, "bulging of the sheet against drawing direction" was not observed even for a large punch-die clearance of 5.5 mm. As seen in Figure 6.25, for a given die-ring, the punch-die clearance for a conical or tapered punch geometry changes with the punch stroke. A conical punch has the following advantages:

- For investigating the effect of punch-die clearance on sheet deformation using a cylindrical [cylindrical] punch, multiple die-rings are required to be manufactured. Conical punch geometry is a better alternative since sheet deformation over a wide range of punch-die clearances can be studied with a single die-ring.

- Sheet bulging can be investigated for a wide range of punch-die clearances.

Therefore, it is proposed to use a conical punch instead of the existing cylindrical punch.
Figure 6.25: Schematic shows three punch stroke locations using conical punch. Punch-die clearance changes with punch stroke.

6.5.2. Objectives and approach

- Determine the influence of punch-die clearance on formed part quality (namely, sheet bulging) using axisymmetric conical punch.

Conical punch geometry (Figure 6.26) was selected after preliminary FE simulations. For the selected conical punch, FE simulations will be conducted for different values of pot pressure to determine the combination of pot pressure with punch-die clearance, at which sheet bulging occurs. Pot pressure curve was varied iteratively in FE simulation and optimum blank holder force for each pot pressure curve was estimated using numerical optimization methodology developed at ERC. The optimization methodology is discussed in [Braedel et al. 2005].
As seen in Figure 6.26, for the selected conical punch,

- Die of Φ 95 mm is already available (existing cylindrical tools).
- Blank holder diameter is increased from Ø 92 mm to Ø 95 mm.
<table>
<thead>
<tr>
<th>Punch stroke (mm)</th>
<th>Punch-die clearance (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>40.25</td>
</tr>
<tr>
<td>0.4</td>
<td>37.67</td>
</tr>
<tr>
<td>2.66</td>
<td>31.25</td>
</tr>
<tr>
<td>5.4</td>
<td>26.36</td>
</tr>
<tr>
<td>7.90</td>
<td>22.87</td>
</tr>
<tr>
<td>10.41</td>
<td>21.05</td>
</tr>
<tr>
<td>12.91</td>
<td>19.22</td>
</tr>
<tr>
<td>15.42</td>
<td>17.78</td>
</tr>
<tr>
<td>17.93</td>
<td>16.94</td>
</tr>
<tr>
<td>20.43</td>
<td>15.85</td>
</tr>
<tr>
<td>22.94</td>
<td>15.1</td>
</tr>
<tr>
<td>25.45</td>
<td>14.03</td>
</tr>
<tr>
<td>27.95</td>
<td>13.05</td>
</tr>
<tr>
<td>30.46</td>
<td>12.33</td>
</tr>
<tr>
<td>32.96</td>
<td>11.22</td>
</tr>
<tr>
<td>35.47</td>
<td>10.5</td>
</tr>
<tr>
<td>37.98</td>
<td>9.45</td>
</tr>
<tr>
<td>40.48</td>
<td>8.55</td>
</tr>
<tr>
<td>42.99</td>
<td>7.7</td>
</tr>
<tr>
<td>45.5</td>
<td>6.8</td>
</tr>
<tr>
<td>55</td>
<td>2.5</td>
</tr>
<tr>
<td>105</td>
<td>2.5</td>
</tr>
</tbody>
</table>

Table 6.8: Punch-die clearance variation with punch stroke for selected conical punch

6.5.3. Finite element simulation

FE simulation of the SHF-P process was conducted using commercial FE code PAMSTAMP 2000. The punch, die, blank holder and pressure pot were considered rigid while the blank was modeled as anisotropic elastic plastic material. Only quarter model was used in the FE simulation.
due to the axisymmetric deformation and boundary conditions, and symmetric material properties of the sheet metal (Figure 6.27). Table 6.9 shows the FE simulation matrix.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Blank geometry</td>
<td>Ø 230 mm, thickness 1.00 mm</td>
</tr>
<tr>
<td>Blank material</td>
<td>St14 steel (Draw Quality Steel)</td>
</tr>
<tr>
<td>Young’s modulus</td>
<td>210 GPa</td>
</tr>
<tr>
<td>Blank properties (flow stress)</td>
<td>$K = 732$ MPa, $n = 0.25$, (for power law, $\sigma = K\varepsilon^n$)</td>
</tr>
<tr>
<td>Anisotropy (from tensile test)</td>
<td>$r_{0} = 1.912$, $r_{45} = 1.83$, $r_{90} = 2.14$</td>
</tr>
<tr>
<td>Coulomb friction coefficient</td>
<td>Punch/sheet = 0.12</td>
</tr>
<tr>
<td></td>
<td>Die/sheet = 0.06</td>
</tr>
<tr>
<td></td>
<td>Blank holder/sheet = 0.06</td>
</tr>
</tbody>
</table>

Figure 6.27: FE model (quarter geometry) and input parameters used in FE simulations in PAMSTAMP 2000 (AQUADRAW) for conical cup SHF-P process.

<table>
<thead>
<tr>
<th>Simulation no.</th>
<th>Pot pressure (Maximum value in bar)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Curve 1 (400)</td>
</tr>
<tr>
<td>2</td>
<td>Curve 2 (350)</td>
</tr>
<tr>
<td>3</td>
<td>Curve 3 (370)</td>
</tr>
<tr>
<td>4</td>
<td>Curve 4 (390)</td>
</tr>
</tbody>
</table>

Table 6.9: FE simulation matrix (for conical punch geometry)
6.5.4. Process parameters [Pot pressure and blank holder force (BHF) curve]

Pot pressure: For Simulation no. 1, the optimum pot pressure curve estimated for Ø 90 mm cylindrical punch geometry was used. For subsequent FE simulations (Simulation nos. 2, 3 and 4, Table 6.9), maximum value of the optimum pot pressure curve was iteratively reduced from 400 bar upto 350 bar [Figure 6.28] to study the effect of pot pressure on sheet bulging.

Blank holder force: For each FE simulation, the optimum blank holder force for each pot pressure curve was estimated using numerical optimization techniques.

![Figure 6.28: Pot pressure curves for conical punch FE simulations.](image)

6.5.5. FE simulation results

As seen in Figure 6.28, the pot pressure curve increases from zero bar to maximum value in the first 50 mm of punch stroke. For the selected conical punch geometry, the punch-die clearance also changes during the first 55 mm of punch stroke, from approximately 40 mm at the beginning of punch stroke to 2.5 mm (in the cylindrical portion of the punch), as seen in Table 6.8.
For each FE simulation in the simulation matrix (Table 6.9), sheet bulging was recorded for different values of pot pressure and punch strokes (or, punch-die clearances). Figure 6.29 shows a cross-sectional view of Simulation no. 1 at a punch stroke $\approx 3.5$ mm.

For FE simulation no. 1 [Pot pressure = Curve 1 (400)], sheet bulging was observed at a punch stroke of $\approx 3.5$ mm (punch-die clearance $\approx 28$ mm) when pot pressure $= 20$ bar. Planned FE simulations (as per simulation matrix Table 6.9) were completed. Results for sheet bulging observed in the planned FE simulations for conical punch, for different pot pressure curves, are summarized in Table 6.10.

Figure 6.29: Sheet bulging observed for the selected conical punch geometry, at punch stroke $\approx 3.5$ mm for Simulation no. 1. This bulge was observed for pot pressure $= 20$ bar and punch-die clearance $\approx 28$ mm.
<table>
<thead>
<tr>
<th>Punch stroke (mm)</th>
<th>Punch-die clearance (mm)</th>
<th>Pot pressure values (bar)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Curve 1 (400)</td>
</tr>
<tr>
<td>0</td>
<td>40.25</td>
<td>0</td>
</tr>
<tr>
<td>0.4</td>
<td>37.67</td>
<td>20</td>
</tr>
<tr>
<td>2.66</td>
<td>31.25</td>
<td>20</td>
</tr>
<tr>
<td>5.4</td>
<td>26.36</td>
<td>52</td>
</tr>
<tr>
<td>7.90</td>
<td>22.87</td>
<td>107</td>
</tr>
<tr>
<td>10.41</td>
<td>21.05</td>
<td>116</td>
</tr>
<tr>
<td>12.91</td>
<td>19.22</td>
<td>110.2</td>
</tr>
<tr>
<td>15.42</td>
<td>17.78</td>
<td>154</td>
</tr>
<tr>
<td>17.93</td>
<td>16.94</td>
<td>267</td>
</tr>
<tr>
<td>20.43</td>
<td>15.85</td>
<td>357</td>
</tr>
<tr>
<td>22.94</td>
<td>15.1</td>
<td>375</td>
</tr>
<tr>
<td>25.45</td>
<td>14.03</td>
<td>382</td>
</tr>
<tr>
<td>27.95</td>
<td>13.05</td>
<td>388.5</td>
</tr>
<tr>
<td>30.46</td>
<td>12.33</td>
<td>395.5</td>
</tr>
<tr>
<td>32.96</td>
<td>11.22</td>
<td>402</td>
</tr>
<tr>
<td>35.47</td>
<td>10.5</td>
<td>409</td>
</tr>
<tr>
<td>37.98</td>
<td>9.45</td>
<td>414.9</td>
</tr>
<tr>
<td>40.48</td>
<td>8.55</td>
<td>414</td>
</tr>
<tr>
<td>42.99</td>
<td>7.7</td>
<td>413</td>
</tr>
<tr>
<td>45.5</td>
<td>6.82</td>
<td>412</td>
</tr>
<tr>
<td>56</td>
<td>2.5</td>
<td>400</td>
</tr>
<tr>
<td>100.33</td>
<td>2.5</td>
<td>400</td>
</tr>
</tbody>
</table>

Table 6.10: Conical punch FE simulation results for sheet bulging for different pot pressures and punch strokes (or punch-die clearance values). Numbers in bold indicate that sheet bulging was observed in FE simulation.
Using data from Table 6.10, a chart relating observed sheet bulging to the instantaneous value of pot pressure and punch-die clearance was generated. Figure 6.30 shows a chart of punch-die clearance versus pot pressure. Data points obtained for completed FE simulations for ‘bulging’ and ‘no bulging’ are plotted on this chart.

Figure 6.30: Data points indicating bulging/no bulging for the planned FE simulation matrix. With increasing punch stroke (reducing punch-die clearance) higher values of pot pressure are needed to bulge the sheet against drawing direction.
6.5.6. Results summary: Influence of punch-die clearance in SHF-P of conical cup

Previous studies on investigating the effect of punch-die clearance in sheet hydroforming with punch (SHF-P) process were conducted with the existing cylindrical cup geometry available at Schnupp Hydraulik. For further studies, a Ø 90 mm conical punch geometry was selected since it allows a wide range of punch-die clearance values (from 2.5 mm to approximately 40 mm) to be studied with a single tool.

Finite element (FE) simulations were conducted in PAMSTAMP 2000 for the selected conical punch geometry to investigate the effect of punch-die clearance on ‘sheet bulging against drawing direction’ in SHF-P process. Sheet material St14 (Draw Quality Steel) of thickness 1 mm was used for this study. Flow stress data was estimated using the VPB (Viscous Pressure Bulge) test at ERC. Anisotropy coefficients were obtained from literature (tensile tests conducted by Technical University of Munich). FE simulations for the selected conical punch were completed for four pot pressure curves.

The following observations were made in this FE study:

- For the selected conical punch geometry, bulging against the drawing direction is observed in FE simulation during some values of punch stroke. For each bulging observation, corresponding values of pot pressure and punch stroke (or punch-die clearance) were noted. These data points were plotted on a chart of instantaneous value of pot pressure versus punch stroke (or punch-die clearance) (see Figure 6.30).

- Using this chart, a trendline relating pot pressure and punch stroke (or punch-die clearance) was plotted. Using this trendline, bulging while forming deep parts can be avoided for the selected material, by selecting appropriate values of pot pressure and punch-die clearance.

- Similar trendlines can be plotted for different sheet materials with different sheet thickness values. These trendlines could serve as guidelines to forming engineers/die designers in selecting pot pressure and punch-die clearance for SHF-P process of tapered parts.
6.6. Future work

Experimental validation of FE predictions, namely the minimum pot pressure curve versus punch-die clearance chart seen in Figure 6.30 is in progress. After experimental validation of the conical punch FE simulation predictions, the results of the sheet bulging study could be applied as design guidelines in selecting pot pressure and punch-die clearance in forming axisymmetric tapered part geometries. Validated design guidelines are relevant only to axisymmetric part geometries.

For non-symmetric part geometries (see Figure 6.31), bulging of the sheet is observed more commonly in the straight sections of a die, where the stress state is “plane strain”. This is because sheet material shows lesser resistance to deformation in this stress state.

![Diagram of stress states](image)

Figure 6.31: State of stress in example rectangular (non-symmetric) part geometry.

Therefore, a study investigating sheet bulging in the “plane strain” state of stress is needed to have design guidelines for non-symmetric part geometries. After completion of experimental validation of conical punch simulation predictions, FE simulations will be conducted for a non-symmetric part seen in Figure 6.32 (to be selected in co-operation with an industrial member). Selected part geometry will be representative of non-symmetric parts formed by SHF-P.
Figure 6.32: Example non-symmetric punch geometry. This punch geometry will be used for (a) prediction of optimum pot pressure and BHF curve, (b) an investigation on the effect of punch-die clearance on sheet deformation.
CHAPTER 7

CASE STUDIES IN STAMPING AND SHEET HYDROFORMING

7.1. Case study 1: Sheet hydroforming of precise reflectors

7.1.1. Problem statement

For space communication, an array of Ø12 m reflectors/antenna called Square Kilometer Arrays (SKA) was identified. Three methods were identified for manufacturing Ø12 m reflectors, namely,

(a) aluminum panels assembled and supported with a steel back structure,

(b) reflectors from composite material and

(c) sheet hydroforming with die (SHF-D).

Among these methods, SHF-D was considered least expensive and could produce the desired surface finish and accuracy. Preliminary FE simulations were conducted to check feasibility of hydroforming small (Ø50 inch) and large (Ø12 meter) reflectors [Antsos 2003]. This analysis indicated that springback in the part after hydroforming is large. This resulted in difficulty in maintaining the 0.2 mm Root Mean Square (RMS) tolerance on the final reflector profile (Figure 7.1). This investigation focused on identifying the best possible die or mold geometry needed to reduce springback in hydroformed Ø12 m reflectors.
Figure 7.1: (a) Large reflector (b) Schematic of the reflector geometry (2D section view)
7.1.2. Forming sequence

Smaller reflectors (Ø 30 inch) are presently manufactured as shown in Figure 7.2.

Step 1: Sagging of sheet due to gravity

Circular blank is placed on the bottom die. Due to the large size of the blank (Ø 12 m), the blank sags or relaxes under its own weight (gravitational force). This sagging is dependent on the flange angle on the bottom die geometry.

Step 2: Clamping of sheet between top and bottom die

The top die clamps down on the relaxed sheet.

Step 3: Bulging of sheet into upper die cavity

The clamped sheet is bulged by applying air pressure on the bottom of the sheet. The bulging sheet conforms to the shape of the top die/mold.

After forming, the upper die is moved up and the formed reflector is removed. Due to residual stresses in the formed reflector, it will show elastic recovery, or springback.

Figure 7.2: 2D schematic of sequence of SHF-D forming operation for large reflectors.
7.1.3. Objectives and approach

The specific objectives of this study are,

(a) Predict springback in hydroformed Ø12 m reflector,
(b) Identify influence of tool and material parameters on springback in the reflector,
(c) Estimate the die/mold geometry that compensates for springback in the reflector.

The following tasks were outlined:

Task 1: Determine material properties of AA 3003-O using biaxial sheet bulge test.

Task 2: Investigate influence of anisotropy of the sheet material on springback and thinning distribution, for a small (Ø50 inch) hydroformed reflector.

Task 3: Investigate the effect of friction conditions at the sheet-die interface, sagging of the sheet in the die due to gravity, and the die geometry on the thinning distribution and springback in the hydroformed Ø12 m reflector.

Task 4: For the Ø12 m reflector, estimate optimum die geometry that gives uniform springback distribution along the curvilinear length that can be easily compensated.

Task 5: Quantify the effect of sheet thickness on thinning and the optimum die geometry.

7.1.4. Finite element (FE) simulation

Simulations of the hydroforming process using shell elements were conducted using commercial FE code PAMSTAMP 2000. Figure 7.3 shows an isometric view of the FE model in PAMSTAMP 2000 for shell elements. Input conditions for the FE simulation are given in Table 7.1.
<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Blank material</td>
<td>AA 3003-O</td>
</tr>
<tr>
<td>Initial blank diameter</td>
<td>(a) 57.5 inches (1460 mm) for Ø50 inch reflector</td>
</tr>
<tr>
<td></td>
<td>(b) 490 inches (12450 mm) for Ø12 m reflector</td>
</tr>
<tr>
<td>Initial blank thickness</td>
<td>(a) 0.191 in (4.85 mm), (b) 0.25 in (6.35 mm)</td>
</tr>
<tr>
<td>Coulomb’s friction coefficient (μ)</td>
<td>(a) 0.05, (b) 0.1, and (c) 0.15</td>
</tr>
</tbody>
</table>

Table 7.1: Input parameters: Sheet hydroforming of large reflectors

Flow stress of Aluminum AA3003-O was obtained from Viscous Pressure Bulge (VPB) tests. In FE simulations, punch and die were modeled as rigid while blank was modeled elastic-plastic. Due to axisymmetric deformation and boundary conditions only quarter section of the blank was modeled. An interface friction coefficient (μ) = 0.1 was assumed, based on Coulomb’s friction law. Clamping simulation was conducted by moving the upper die against the sheet. After the sheet was clamped between the upper and lower die, forming simulations were conducted with the
periphery of the sheet fixed to reflect the boundary condition existing in the real process. The fluid (or air) pressure was built by using the ‘Aquadraw’ model in the FE software PAMSTAMP.

7.1.5. Effect of sheet material anisotropy on springback and thinning distribution, for a small (Ø50 inch) hydroformed reflector

FE simulations were conducted for both isotropic and anisotropic sheet for forming a Ø50 inch diameter reflector. Initial blank diameter = 57.5 inches (~1460 mm). Anisotropy coefficients for the three directions (0°, 45° and 90°) were input into FE simulation in order to evaluate the effect of anisotropy on thickness distribution and springback in the formed reflector. These anisotropy values were selected so that real forming conditions can be emulated. However, they do not reflect the actual values for Aluminum alloy 3003-O (Figure 7.4). Accurate anisotropy values for the aluminum alloy 3003-O need to be determined from tensile test. Hill’s 1948 Yield criterion was used to represent anisotropy in the sheet metal in the FE simulation.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>( R_0 ) (Rolling direction)</td>
<td>1.4</td>
</tr>
<tr>
<td>( R_{45} ) (Diagonal direction)</td>
<td>1.6</td>
</tr>
<tr>
<td>( R_{90} ) (Transverse direction)</td>
<td>1.2</td>
</tr>
</tbody>
</table>

Figure 7.4: Plastic strain ratio along rolling direction (0°), diagonal direction (45°) and transverse direction (90°) used in FE simulation

Influence of sheet material anisotropy on thinning distribution in the formed reflector:
- Thinning percentage did not change significantly along the circumference of the formed part even when anisotropic material properties were used to describe the sheet material in FE simulation (Figure 7.5).

- Maximum thinning of 14% was predicted by FE simulation in the hydroformed part for both isotropic and anisotropic description of the material properties in the FE simulation. Maximum thinning was observed in the region close to the apex of the dome but not at the apex of the dome. Thinning is constant over the curvilinear length of 150 mm from the center due to the compressive stress acting on the sheet along the radial direction in these locations towards the end of forming where the sheet metal comes in contact with the die surface.

Influence of material anisotropy on springback:

- More springback was observed in FE simulation for the anisotropic model compared to isotropic (Figure 7.6).

- For anisotropic case, springback varied along the circumference of the part. This variation in springback is due to the variation in the yield stress along the rolling and transverse to the rolling direction introduced by the anisotropic constants in the yield criteria.
Figure 7.5: Thinning distribution predicted by FE simulation for anisotropic material (Inset: Schematic of the curvilinear length of deformed Ø50 inch reflector)

Figure 7.6: Springback in the formed Ø50 inch reflector predicted by FE simulation for anisotropic sheet material ($r_0 = 1.4$, $r_{45} = 1.6$ and $r_{90}=1.2$)
7.1.6. Effect of process parameters and tool geometry on springback in a Ø12 meter reflector

Finite element (FE) simulations of the hydroforming of Ø12 m reflector were conducted using FE code PAMSTAMP 2G to study the effect of the following process parameters namely:

(a) Sagging of the blank due to its own weight in the die

(b) Interface friction conditions between the sheet and the mold

(c) Die geometry (flange angle)

on springback of the hydroformed part.

Figure 7.7: Schematic of the assumed tooling for FE simulations

<table>
<thead>
<tr>
<th>Object</th>
<th>Details</th>
</tr>
</thead>
<tbody>
<tr>
<td>Upper Die</td>
<td>Ideal parabola equation, ( z = \frac{x^2}{4F} ), Focal length ( F ) = 198 in (5040 mm), Corner radius = 1 in (25.4 mm), Flange length = 9.8 in (250 mm)</td>
</tr>
<tr>
<td>Lower Die</td>
<td>Internal diameter = 472 in (12000 mm), Corner radius = 1 in (25.4 mm), Flange length = 9.8 in (250 mm), Floor depth = 7.9 in (200 mm)</td>
</tr>
</tbody>
</table>

Table 7.2: Tool geometry used in the FE simulations
The die geometry of Ø12 m antenna was designed with the flange as shown in Figure 7.7 and Table 7.2 based on the dies currently being used for manufacturing of forming smaller reflectors. FE simulation of the hydroforming of the antenna was conducted in three stages. In stage I, the sagging of the sheet into the die cavity due to its own weight by gravity was simulated (gravity simulation). In stage II, the clamping of the sheet by the mold was considered (holding simulation). In stage III, FE simulation of the hydroforming process was conducted followed by the springback simulation. Table 7.3 shows the FE simulation matrix to study the effect of (a) interface conditions and (b) die geometry, on the springback in Ø12 m reflector. Three different interface friction conditions between the sheet and the dies and three different angles for the flange in the die geometry were considered to study the effect of friction conditions and die geometry.

<table>
<thead>
<tr>
<th>Simulation case</th>
<th>Interface Friction [µ]</th>
<th>Flange angle [°]</th>
<th>Gravity</th>
<th>Clamping</th>
<th>Forming</th>
<th>Springback</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.05</td>
<td>60°</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>2</td>
<td>0.10</td>
<td>60°</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>3</td>
<td>0.15</td>
<td>60°</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>4</td>
<td>0.05</td>
<td>45°</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>5</td>
<td>0.05</td>
<td>30°</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>6</td>
<td>0.10</td>
<td>75°</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
</tbody>
</table>

Table 7.3: FE simulation matrix to determine influence of interface friction and die geometry (flange angle) on springback and thinning in the Ø12m formed reflector

Influence of flange angle on springback:

- Springback in the formed part increases with increase in the flange angle of the die. In the Ø12 m reflector, minimum springback displacement in Z direction of 10 mm was observed for a flange angle of 30° while maximum springback displacement in Z direction of ~ 16 mm was observed for flange angle of 60°.
Springback distribution in the formed part for flange angle 30° is non-linear. It is difficult to modify the die to compensate for non-linear springback. Therefore it is preferred to choose die geometry with flange angle 45° as springback varies smoothly over the curvilinear length and can be easily compensated for in the upper die geometry.

With the 75° flange angle, radial springback is lesser than other flange angles (30°, 45° and 60°). Therefore, among the selected flange angles on the die, 75° is considered optimum to form a Ø12 m reflector with sheet thickness 0.25 in.
Figure 7.8: Comparison of Z displacement due to springback in the hydroformed part for the die geometry with different flange angle (for initial sheet thickness = 0.25 in)

Figure 7.9: Comparison of radial displacement due to springback in the hydroformed part for the die geometry with different flange angle (for initial sheet thickness = 0.25 in)
Influence of friction between sheet and tools on springback in final reflector:

- Figure 7.10 shows comparison of thinning in the reflector formed with flange angle 60º for different friction conditions. Thinning in the part does not change significantly with change in the friction conditions.

- During hydroforming, the sheet freely bulges (uniform stretching) into the upper die cavity and gradually comes in contact with the upper die from the periphery to the center. Sheet is clamped in the edges to avoid any material movement into the cavity during the hydroforming process. Thus, there is no relative motion between sheet and upper die. Therefore, the effect of friction on the thinning in the formed part was negligible.

Figure 7.10: Comparison of springback predictions in Ø12 m reflector for different interface friction conditions, for flange angle 60º (for initial sheet thickness = 0.25 in).
7.1.7. Summary and conclusions

In this study, finite element (FE) simulations of sheet hydroforming with die (SHF-D) process of Ø50 inch and Ø12 m reflectors were conducted using commercial FEM code PAMSTAMP 2G/PAMSTAMP 2000 to:

(a) Demonstrate feasibility of manufacturing the Ø12 m reflector by SHF-D,

(b) Predict springback in the Ø12 m reflector after hydroforming process,

(c) Identify the influence of process parameters/material properties (namely, anisotropy in sheet material, initial sheet thickness, interface friction conditions, gravity, and die geometry) on the springback in the formed reflector, and

(d) Estimate a die geometry for which springback in the formed reflector can be easily compensated by modifying the die.

Conclusions drawn from this study are:

- Feasibility of the SHF-D process for forming reflectors: Through the use of FE simulation, feasibility of the sheet hydroforming with die (SHF-D) process in forming large reflectors was demonstrated. There is a 0.2 mm RMS accuracy requirement on the inner surface of the formed reflector. During SHF-D process there is a thickness change due to deformation. There is also springback of the reflector due to residual stresses in the formed reflector. Thus, to get the desired accurate surface profile on the final formed reflector, the geometry of the die/mold must be modified. During SHF-D, the maximum force in Z direction (vertical) is approximately 1000 tons and thus requires careful consideration in designing the upper die geometry with high stiffness in Z direction to minimize the deflection of the upper die due to high forces. A maximum thinning of 14% was observed in the Ø12 m antenna near the apex and it is well within the fracture limit of the sheet material AA3003–O.
- **Influence of friction between sheet and tools**: Process conditions, namely sagging of the blank due to gravity, die geometry (flange angle) and initial sheet thickness influenced the thinning in the formed part. However, the thinning distribution in the part changed by only ± 2 %. The influence of interface friction conditions on the thinning in the hydroformed part was found to be negligible in the FE simulation.

- **Influence of sheet material anisotropy on springback**: Due to non-availability of anisotropy data for AA 3003-O, hypothetical data was used to quantify the influence of anisotropy of springback. Anisotropy in the sheet material resulted in non-uniform springback along the circumference of the part. Therefore, the anisotropy of the material needs to considered in springback estimation and compensation in the die geometry. Springback was influenced by a) anisotropy in sheet material, b) initial sheet thickness, c) sagging of the blank due to gravity, and d) die geometry.

- **Influence of die flange angle on springback**: Sagging of the sheet material into the die cavity resulted in more material flow into the die cavity thereby reducing the thinning in the formed part and increasing the springback after forming. Thus, die geometry (flange angle) significantly influenced the springback in the hydroformed part. Higher flange angle allowed more material flow into the die cavity in the initial sagging of the blank as well as during the clamping stage. Therefore, larger flange angle in the die geometry resulted in more springback. However, larger flange angle resulted in a more uniform springback distribution, which is desirable. Uniform springback can be more easily compensated by modifying the die geometry. Among the die geometries (flange angles) considered in this study, flange angle of 75° was found to give the most uniform springback distribution for sheet material of initial thickness 6.35 mm (0.25 inch).

- **Influence of sheet thickness on springback**: For larger flange angles, more material flows into the die cavity. Also, thinner sheets have lesser resistance to bending and flow more easily into the die cavity. Too much material flow into the die cavity leads to wrinkling and folding.
- "Optimum" die geometry for springback compensation: Lower values of die geometry flange angles (30°, 45° and 60°) resulted in non uniform springback which is difficult to compensate for. Higher flange angles resulted in excessive material flow into the die cavity in gravity and holding stage leading to wrinkling and folding. Thus, the "optimum" die geometry (flange angle) depends on the initial sheet thickness, and decreases with decreasing sheet thickness. It is estimated that in forming the Ø12 m reflector, the "optimum" die geometry (flange angle) will be in between 60° and 75° for initial sheet of thickness 4.75 mm (0.185 inches).
7.2. Case study 2: Automating tool and process sequence design in transfer die tools

A supplier of aluminum parts (alloys AA1100, AA5657) draws predominantly cylindrical (axisymmetric) parts. Depending upon part complexity, tool development (design and drafting) time is up to 4 weeks. There is a need to reduce development time by automating repetitive tasks in the tool development process. This case study describes a methodology developed for automating/standardizing the process sequence and tool design, by using tools such as Computer Aided Drafting, Microsoft® Excel and Visual Basic.

7.2.1. Existing methodology for tool and process sequence design

Figure 7.11 shows a cross sectional view of a typical axisymmetric part. The outer diameter, the shell height, base thickness, corner radius and the “nomar” height vary according to job specification. In the forming of any new axisymmetric part, the typical stages in the process sequence are: (a) blank and cupping, (b) draw (upto 7 stages), (c) clipping (trimming), and (d) "nomar" (hemming) stages.
7.2.1.1. Existing process sequence design

Based on customer specifications, a “raw part print” is generated. A solid model of the raw part is generated in SolidWorks™ (commercial CAD software). The volume of the generated solid model is noted. Approximately 8% to 10% of volume of the part is lost in clipping (trimming) stage. Using volume constancy (with allowance for volume lost in clipping), the initial blank diameter is calculated (using an existing Excel sheet). The first stage is the cupping stage. Reduction ratio is defined as:

\[
\text{Percent Reduction Ratio} = \left(1 - \frac{\text{Outer Diameter(after operation)}}{\text{Outer Diameter(before operation)}}\right) \times 100
\]
The number of draw stages is based on experience. The outer diameter of the part for each stage is estimated based on the number of draw stages. Reduction ratios for each draw stage are calculated based on experience. Table 7.4 shows reduction ratios used for draw stage design.

<table>
<thead>
<tr>
<th>Stage</th>
<th>Operation</th>
<th>Reduction ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Maximum</td>
</tr>
<tr>
<td>1</td>
<td>Cupping</td>
<td>45</td>
</tr>
<tr>
<td>2</td>
<td>First draw</td>
<td>20</td>
</tr>
<tr>
<td>3</td>
<td>Second draw</td>
<td>18</td>
</tr>
<tr>
<td>3</td>
<td>Final draw</td>
<td>15</td>
</tr>
</tbody>
</table>

Table 7.4: Experience-based rules for reduction ratios in drawing stages

The parts for each stage are modeled individually using SolidWorks™. Part height for each stage is determined by maintaining volume constancy. Corner radii for each part are equal to the punch corner radii. This is determined by using the following experience-based formula:

Punch corner radius = ½ (Part diameter of preceding station – Part diameter of current station)

7.2.1.2. Tool design

Once process sequence is developed, the press is selected for forming this part. Selection criteria for the press are, (a) shut height of the press, (b) initial blank diameter, (c) part volume and (d) press availability. Tools for each stage are developed to conform to the dimensions of the parts in each stage. The last tool stage is designed first. Tool design is done backwards (opposite to process sequence) to allow for changes to be made in the shell line (if needed). After the tools are designed, the tool designer/draftsman generates a master sheet which summarizes, (a) tool dimensions, (b) part dimensions, (c) reduction ratios for each stage (d) press to be used, and (e) blank material. This sheet is used as a summarized information data sheet by the tool maker. Once solid models are completed, technical drawings for each tool, in each stage are generated using SolidWorks™.
7.2.2. Objectives and approach

The overall objective of this study is to expand/improve the existing tool and process sequence methodology developed at industrial sponsor, to reduce the development time (tool and process sequence design) for producing an axisymmetric round shell. The specific objectives are:

- Automate tool and process sequence (shell line) design using existing design guidelines.
- Document the existing design rules.
- Replace experience-based approach with FE-based tool and process sequence design.

![Diagram showing the flow of data and tools](image)

**INPUT DATA**: Required part (shell) dimensions

**Microsoft Visual Basic**
- The developed VB program determines:
  - Number of forming stages,
  - Shell height (per volume constancy),
  - Percentage reductions,
  - Corner radii on each shell,
  - Press
  - Tool dimensions for each stage.

**SolidWorks™ (CAD)**
- **1: Shell line development**
- **2: Tool design** (based on shell development)

**OUTPUT**: Solid models for:
- Shell line (process sequence)
- Tools for each stage

Figure 7.12: Methodology for automating the process sequence and tool design.

Figure 7.13 shows the steps followed in tool and shell line design. The input data (namely, final part dimensions) are fed to the developed Microsoft ® Visual Basic program (Figure 7.14). Using existing design guidelines, the VB program first calculates the process sequence. Based on the obtained process sequence, tool dimensions for each stage are calculated. Parametric solid models of the (a) process sequence and (b) tools for each stage have been modeled beforehand.
in SolidWorks™ and saved as “Drawing Templates”. After calculating the required process sequence and tool dimensions based on user input, the VB program communicates with SolidWorks™ to,

(a) access the drawing template files in SolidWorks™, and

(b) modify the dimensions in the templates, to create a new process sequence and tools.

Figure 7.13: Steps followed in tool and process sequence design
7.2.3. Automating the process sequence design

Figure 7.14 shows an input form to collect desired dimensions of the “Final part”. The form is designed to accept as input, (a) outer diameter of the final part, (b) part height, (c) base thickness, (d) corner radius, (e) wall thickness, (f) transition angle, and (g) 'nomar' height. This form can be easily modified in Visual Basic to add/remove input parameters.

The first step is calculating the initial blank diameter to be used. Calculations are based on the principle of constancy of volume.

\[(\text{Draw Volume}) \; \text{Volume of the Blank} = \text{Volume of the final part} + \text{Volume of clip head}.\]

The volumes of the final part and the clip head are calculated based on the following equation,

\[\text{Volume} = \text{Surface area} \times \text{Sheet Thickness}\]
The surface area for the final part is calculated by using simple geometrical formulae as shown in Figure 7.15. Surface area of the final part is split into two for simplifying calculations. (Area 1 and Area 2 as in Figure 7.15)
Let the inner surface area of hollow cylindrical portion of the final part be, Area 1

Let the inner surface area of the base of the final part be, Area 2

\[ \text{Area 1} = \pi BH \]

\[ \text{Area 2} = (A^2 \times 0.7854) + [2\pi R^2 + (\pi^2 RA)/2] \] (as seen in Figure 7.15)

Volume of the final part = (Area 1 x wall thickness) + (Area 2 x base thickness)

The values of wall thickness and base thickness are used from the user input values in the form in Figure 7.14. The calculation for the surface area of the clip head is similar to Area 1(Figure 7.15). Table 7.5 shows the design rules for assuming clip head (height).

<table>
<thead>
<tr>
<th>Final part outer diameter</th>
<th>Clip head</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lesser than ¾&quot;</td>
<td>1/8&quot; (or 0.125 inch)</td>
</tr>
<tr>
<td>Greater than ¾&quot;</td>
<td>3/16&quot; (or 0.185 inch)</td>
</tr>
</tbody>
</table>

Table 7.5: Design rules for clip head
Thickness for the clip head is assumed to be 80% of the base thickness.

\[ \text{Volume of the Clip Head} = (\text{Surface area of clip head}) \times (\text{Clip head thickness}) \]

Thickness of the sheet is assumed to be equal to the base thickness of the part (user input). Initial blank diameter is calculated as:

\[ \text{Volume of Blank} = \frac{\pi}{4} \times (\text{Blank Diameter})^2 \times (\text{sheet thickness}) \]

\[ \text{Blank diameter} = \sqrt{\frac{(\text{Volume of the blank} \times 4/\pi)}{\text{sheet thickness}}} \]

After initial blank diameter is calculated, number of draw stages need to be determined. Part outer diameter in the final draw stage is known (equal to diameter of final part). Based on the number of draw stages, the shell diameters for each stage can be calculated. Design guidelines for allowable percent reduction ratios for each draw station are seen in Table 7.4. For example, the reduction ratio in Cup Draw is limited within 30 to 45%. Similarly, First Draw is within 18 to 20%.

An initial assumption is made that the shell can be formed in five (5) drawing stages. Default values assumed for the reduction ratios are 35% in the Cup Draw stage, 18% in the First Draw Stage, 16% in the Second Draw Stage, 15% in the Third Draw Stage. Based on assumed reduction ratios, the following calculations are made:

(a) Shell diameters at each draw stage, and

(b) Reduction ratio for the final draw stage

The following can be changed iteratively (a) Number of draw stages, and (b) Reduction ratios in previous draw stages. The developed methodology allows user to iteratively vary both, the reduction ratios as well as the number of forming stages, until the calculated final draw reduction ratio is acceptable. Thus, draw stages can be designed with considerable flexibility.

The deliverables at the end of process sequence design are:

(a) Draw diameter (or part diameter) for each draw stage
(b) Draw height (or part height) for each draw stage, and

(c) Corner radius for each draw stage

At any point during the design process, the user is allowed the option of overriding/modifying the calculated values for any/all designed draw stages.

Figure 7.16: Deliverable of developed methodology: Process sequence for an axisymmetric part (with 4 drawing stages).
7.2.4. Automating the tool design

Once the process sequence is developed, tools for each stage are designed. Methodology for tool design is the same as process sequence design. Template files for each tool, in each stage, for each press, need to be created beforehand. Based on design calculations made by the developed VB program, the dimensions in template files (created in SolidWorks™) are modified to make new tools. Note that the tool design depends on the press. Quantitative criteria for press selection are (a) the height of the final part, (b) initial blank diameter (c) the strip width. Other qualitative criteria also decide press selection, for example, press availability.
7.2.5. Summary

In this study a software application was developed to design (a) process sequence for forming simple round (axisymmetric) shells and, (b) transfer die tools to form the round parts.
Volume constancy was used for calculating the process sequence for axisymmetric part. By using the final part dimensions as an input, dimensions of the part at all intermediate stages, starting with initial blank diameter were calculated. This process was automated with the use of Microsoft® Visual Basic + CAD package SolidWorks™.

The developed process sequence was then used in designing tools for each forming stage. As a first approximation, the percentage reduction ratio between stages is obtained from experience-based rules. At a later time, it is proposed to replace the experience-based rules with finite element method based calculations.

A step-by-step graphical user interface was developed as part of this software tool, to enable users to give as input the dimensions of the final shell to be formed.

7.2.6. Broader impact of this study

The main thrust of this research was on improvement of profitability in a simple stamping operation by reducing shell line and tool design/development time.

The developed application presents a case study in efficient shop-floor data management. This software is presently in use for simple axisymmetric parts. The developed methodology has helped tool engineers to reduce their process sequence and tool design development time from 4 weeks to less than a week. In the past year, the sponsor has also expanded the capabilities of this software internally to include simple non-symmetric parts as well.

In order to achieve profitability and remain competitive, stamping companies in industrialized countries need to apply advanced computational tools such as FE simulation, besides practicing efficient shop-floor management practices. The sponsor of the study is presently investigating use of finite element (FE) simulation to replace the experience-based knowledge in process sequence and tool design.
7.3. Case study 3: Tool design in transfer die forming

7.3.1. Problem statement

In forming a stainless steel case (Figure 7.18a), two possible process sequences were conceptualized (Figure 7.18). Based on available presses to form this part using a transfer die forming operation, a process sequence to form the axisymmetric "donut-shaped" part was selected.

![Figure 7.18: (a) Stainless steel case for a flow measuring device. Sketches of conceptualized process sequences to form possible final part geometries, namely (b) donut-shaped part (c) half donut-shaped part. The donut-shaped part was selected for process/tool design using commercial finite element (FE) code.](image)

It is proposed to form the donut-shaped part in two stages. Starting with an initial blank with a center pre-hole, the inner curvature of the part will be formed in the first stage. In the second stage, the outer curvature of the part will be formed. FE simulations of the proposed two-stage process will be conducted to determine tool design and forming tonnages needed to successfully form the donut-shaped part.
7.3.2. Objectives and approach

Through the use of commercial FE code PAMSTAMP-2G (v.2007):

- Design the tools needed to form a defect-free part (no wrinkles or tear/some springback).
- Estimate the required forming tonnages (punch and blank holder).

Material properties for steel (SS 304 and SS304L) used in the analysis are seen in Figure 7.19. An initial blank thickness of 0.11 inch (~ 2.8 mm) was assumed.

Figure 7.19: Material properties (flow stress data) for SS304 and SS304L [Source: Kalpakjian, S., "Manufacturing Processes for Engineering Materials", Addison Wesley 1991]

During preliminary FE simulations of the process, two ways to form the donut-shaped part were identified. They are:

- Approach 1: Forming inner curvature (1st stage), followed by outer curvature (2nd stage).
- Approach 2: Forming outer curvature first (1st stage), followed by inner curvature (2nd stage).

This approach was investigated after reviewing results from Approach 1.
7.3.3. FE simulation results – Approach 1

Preliminary axisymmetric finite element (FE) simulations were conducted in DEFORM-2D to design the tools needed. 3D FE simulations were then conducted with PAMSTAMP-2G to analyze thinning, wrinkling and springback of the formed part for both forming stages. Figure 7.20 shows two-dimensional sectional view of the 3D FE model for the two stages.

Figure 7.20: Finite element (FE) model. (a) Two dimensional view of the 1st stage forming process, inner curvature forming. (b) Two dimensional view of the 2nd stage forming process – outer curvature forming.
<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sheet/blank material</td>
<td>SS 304, [see Figure 7.19]</td>
</tr>
<tr>
<td>Initial blank dimensions</td>
<td>Thickness = 0.11 inches (~ 2.8 mm), Blank diameter = Ø 38.5 in (<del>978 mm), Pre-punched center hole = Ø 5.5 in (</del> 140 mm)</td>
</tr>
<tr>
<td>Coefficient of friction (between blank and tools)</td>
<td>0.08 (Coulomb's law)</td>
</tr>
<tr>
<td>* Blank holder tonnage</td>
<td>10 tons (1st stage), 40 tons (2nd stage)</td>
</tr>
<tr>
<td>* Knock out pin tonnage</td>
<td>10 tons (2nd stage)</td>
</tr>
</tbody>
</table>

* Note: Determined iteratively from FE simulations.

Table 7.6: FE simulation input parameters (Approach 1) for forming donut shaped case

(a) Forming tonnages: Predicted punch force in stage 1 is ~200-250 tons. Maximum applied blankholder force is ~10 tons. Predicted punch force for stage 2 is ~200-300 tons. Maximum applied blankholder force is ~40 tons. Predicted value of tonnages confirms that either of the two presses at Polar Ware could be used to form the part. The two hydraulic presses are:

(a) A 550 ton punch force, BHF 350 ton, bed size 57 x 51 inch front to back, and

(b) Inverted press, punch force 825 ton, BHF 330 ton, bed size 78 x 65 inch front to back.

(b) Wrinkling: For the predicted blankholder tonnages, no wrinkling was observed.

(c) Thinning: Maximum thinning of ~30% was predicted on inner curvature lip. Thinning did not improve with changes in (a) blankholder force (b) friction conditions at tool–sheet interface.
Figure 7.21: (a) Quarter geometry view showing thinning distribution in formed part, maximum value ~30% (b) Thinning distribution along curvilinear length of the part.

(d) Springback: Formed part showed a maximum deflection of 0.1 inches (~2.5 mm).

Figure 7.22: Quarter geometry view showing “springback” in the formed part. Maximum springback predicted is ~2.5 mm (0.1 inches) (a) Back view (b) Front view

7.3.4. FE simulation results – Approach 2

Based on results observed in Approach 1 and discussions with sponsor, the following modifications were made:

(a) Blank material changed from SS304 \( \rightarrow \) SS304L (annealed material)
(b) Blank thickness increased 0.11 inch → 0.135 inch.

(c) Initial blank has no center pre-hole

(d) Friction coefficients reduced from 0.08 → 0.04 (for a dry film lube) and

(e) Process sequence changed (forming of outer curvature first).

FE simulations were re-run with the modifications. In the 1st stage, outer curvature is formed, to get a “tub-shaped” part from a blank without a pre-hole. Formed blank (with strain deformation history) is carried over to 2nd stage, where inner curvature is formed. Figure 7.23 shows 2D sectional view of the 3D FE model for both stages. Table 7.6 shows simulation input parameters.

![Finite element (FE) model](image.png)

Figure 7.23: Finite element (FE) model. (a) Two dimensional view of the 1st stage forming process, outer curvature forming. (b) Two dimensional view of the 2nd stage forming process – inner curvature forming.
Table 7.7: FE simulation input parameters (Approach 2) for forming donut shaped case

- Forming tonnages: Predicted punch force in both forming stages is ~ 250 tons. Maximum applied blankholder force is ~ 200 tons in 1st stage and ~ 40 tons in 2nd stage.
- Wrinkling: For the predicted blank holder tonnages, no wrinkling was observed.
- Thinning: Simulation was stopped when a maximum thinning of ~ 30% (at punch corner) was observed (~ 90% of required punch stroke). This is because the draw ratio for forming the inner curvature is high (~ 2.8). Thus, sheet material does not draw-in and excessive thinning is observed.
Figure 7.24: (a) Quarter geometry view of the thinning distribution in the part formed at 90% of punch stroke, (b) Thinning distribution along the curvilinear length.
7.3.5. Summary

Two forming approaches were investigated using FE code PAMSTAMP-2G. Material properties were obtained from literature. Deliverables of this study are: (a) forming sequence, (b) forming tonnages and (c) tool design. The following approach was identified as a feasible forming sequence.

Step 1: 1st stage forming - Starting with an annealed blank (SS304L) with no pre-hole, form the outer curvature to form a "tub-shaped" part (as described in Approach 2).

Step 2: 2nd stage forming - Form the inner curvature up to 80-90% of stroke. Part thinning is expected to be ~ 30% (as predicted in Approach 2).

Step 3: Trimming - Trim the donut-shaped part into two halves. Outside flange is maintained. The flange is expected to provide rigidity needed to prevent springback after trimming.

Step 4: Anneal - Anneal the two halves of the donut-shaped part.

Step 5: Re-strike operation - Place the two halves after annealing in the 2nd stage tools to re-strike the part to required dimensions. A center hole may be cut before re-strike.

Step 6: Trim the flanges on the inside and outside, to obtain the final part shape.

7.3.6. Recommendations: Other forming approaches

The approaches described above require an intermediate anneal operation. Other forming approaches that avoid intermediate annealing are discussed below:

(1) Two step forming of inner curvature: The 2nd stage (inner curvature forming) could be conducted in two steps. In this approach, two spherical punches will be used. First, the smaller punch (Ø 5.5 inch) will be used to form the inner curvature of the part, followed by the
larger punch (Ø 11 inch). The smaller punch is expected to stretch the material more, thus allowing a deeper inner curvature to be formed before excessive thinning is observed.

(2) Incremental forming: The inner curvature forming process can be split into multiple steps [instead of two steps as described in (1)]. In this approach, multiple spherical punches will be used. The largest spherical punch will be used first, followed progressively by the smaller spherical punches. The objective is to incrementally bend the inner curvature over the inner surface contour of the die, stretching sheet material evenly, allowing a deeper inner curvature to be formed.

(3) Spinning: This approach is similar to incremental forming. The blank will be spun on a central axis and a single point punch will be used to form the part incrementally. The punch will be moved radially inward, incrementally pushing the sheet surface against the die.
7.4. Case study 4: Eliminating fracture in progressive die forming

7.4.1. Problem statement

In progressive die stamping of a non-symmetric part [Figure 7.25a], failure in the side wall is observed in production. Existing process sequence consists of four stages. Figure 7.25b shows the strip layout of the part at all four stages of the existing process sequence.

![Figure 7.25: (a) Steam trap part manufactured by progressive die stamping process (b) Existing 4-stage progressive die forming sequence for the steam trap part](image)

7.4.2. Finite element (FE) simulation

FE simulations of the process sequence for the part were conducted using commercial dynamic explicit FE code PAMSTAMP™. Annealed stainless steel sheet (ASTM A240 T305) of thickness 0.034 inches (0.87 mm) and diameter 5.72 inches (145.3 mm) was used as an initial sheet to make the part. The flow stress [Figure 7.26] of this material was obtained from uniaxial tensile test.

Input conditions for the FE simulations are summarized in Table 7.8. In the FE simulations, punch, die and lifter were modeled as rigid while sheet was modeled using shell elements as...
elastic-plastic in all forming stages. Normal anisotropy ($R$) of the material was $\approx 1$, hence anisotropy was not accounted in the FE simulation.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>ASTM A240 T305</td>
</tr>
<tr>
<td>Initial blank diameter</td>
<td>5.72 in (145.3 mm)</td>
</tr>
<tr>
<td>Initial blank thickness</td>
<td>0.034 in (0.87 mm)</td>
</tr>
<tr>
<td>Friction coefficient ($\mu$)</td>
<td>To be determined by matching thinning distribution in each stage</td>
</tr>
</tbody>
</table>

Table 7.8: Input parameters for the process simulation

Due to part symmetry about one axis only half model (180°) of the sheet and tools was modeled and symmetry boundary conditions were applied accordingly. Due to non-availability of interface conditions between the sheet and the tool, Coulomb friction coefficient ($\mu$) were obtained by matching FE results with production data for thinning and draw-in. FE simulation results, which include the deformed geometry, strain distribution and stress distribution from each stage, were carried on to the next stage to accurately model the multistage forming process.
Figure 7.26: Flow stress of stainless steel (ASTM A240 T305) material

For $\varepsilon_p < 0.4$, $\sigma = 180 (\varepsilon_p + 0.012)^{0.415}$ KSI
For $\varepsilon_p > 0.4$, $\sigma = 124$ KSI
For $\varepsilon_p < 0.4$, $\sigma = 1274 (\varepsilon_p + 0.012)^{0.415}$ MPa
For $\varepsilon_p > 0.4$, $\sigma = 880$ MPa
Figure 7.27: Finite element (FE) model in PAMSTAMP 2000 (half geometry), for the four forming stages.
7.4.3. Thinning distribution in the final formed part

Since the final stage (stage IV) is a restrike operation, there was no significant change in thinning compared to stage III in both experiment and simulation. During restrike operation only thinning near the die corner radius (Curvilinear length 50 – 100 mm, Figure 7.28) was changed in both simulation and experiment. Figure 7.28 shows the comparison of thinning distribution from the final formed part (production data versus FE simulation).

![Figure 7.28: Comparison of stage IV thinning distribution from FE and production part](image)

7.4.4. Comparison of flange outline (material draw-in) in the final formed part

Direct comparison of the draw-in could not be made because in the FE simulation the flange portion of the sheet was lifted from the die while in production part the flange part of the sheet remained in contact with the sheet as shown in Figure 7.29. Figure 7.30 shows the flange draw-in comparison between FE simulations and production part, obtained in plane by outlining the formed blank circumference. Due to difference in height of the flange geometry, projected flange
draw-in outlined from production part was lesser than the projected flange draw-in obtained from simulation as seen in Figure 7.30.

Figure 7.29: Discrepancy in flange height between FE simulation and production part at end of fourth stage restrike operation.
Figure 7.30: Comparison of the flange draw-in from the FE simulation and production part

7.4.5. Summary

Finite element (FE) simulations of multistage progressive die forming of a steam trap part were conducted using commercial code PAMSTAMP. The sponsor provided tool CAD data and sample part geometry at each forming stage. The part was formed using stainless steel sheet (AISI A240 T305). Flow stress of sheet material, was obtained from uniaxial tensile test. Anisotropic behavior of sheet metal was not considered since normal anisotropy of stainless steel was ≈ 1.0. Major conclusions drawn from this study are,

- In the first stage simulation, initially uniform friction coefficient was considered and friction coefficient was increased as high as 0.2 however, thinning predictions were less compared to production data. Therefore, different friction coefficient at sheet-punch interface, sheet-die
interface were used to match the thinning from FE simulation with production data. For the punch-sheet friction of 0.08 and die-sheet friction of 0.12, thinning distribution and draw-in from FE simulation matched well with production data. However, more thickening was observed in the FE simulation in the flange region compared to the production data because towards end of forming in FE simulation, the applied blank holder was insufficient and the blank holder lifted allowing material to flow in easily resulting in more thickening.

- In the second stage simulation, for the punch-sheet friction of 0.11 and die-sheet friction of 0.1 in the FE simulation, thinning distribution from FE simulation matched well with production data. FE simulations indicated more thickening in the part compared to production part in the flange area. This deviation in thickening of the sheet is due to a) Difference in the sheet thickness between the production data and FE simulation carried from stage I and b) Lifting of blank holder observed in FE simulation of stage II due to insufficient force towards the end of forming when the thickened sheet bends and unbends in the die corner radius.

- Direct comparison of draw-in in stage II between experiment and simulation could not be made because in the FE, the sheet flange was lifted from the die at the end of forming while in the production data the flange remains in contact with the die at the end of forming.

- In the third stage simulation, thinning distribution from FE simulation was less compared to production data. Also, the thinning distribution was insensitive to the friction coefficient because no blank holder force was applied during forming. The possible deviation could be due to fact that even though cylinder pressure is set to zero, cylinders might apply some small amount of blank holder force during forming, due to compressibility of the pressurizing medium inside the cylinder that would have resulted in higher thinning in the production part compared to FE simulation.

- In fourth stage simulation, there was no significant change in thinning distribution compared to stage III, in both simulation and experiment as the only change in die geometry is the
decrease in die radius from 0.25 inches (6.35 mm) to 0.03 inches (0.762 mm). The thinning
distribution predicted in stage IV by simulation was less compared to production part due to
carry over of deviations from stage III. Friction conditions do not influence thinning distribution
as no blank holder force is used in simulation.
8.1. Summary

The main thrust of this research was on the following aspects in sheet metal forming,

(a) Material characterization: Improvement of flow stress calculation methodology,

(b) Sheet hydroforming process: Develop a fundamental understanding of the influence of punch-die clearance on sheet deformation and formed part quality. Develop FE-based methodologies for identifying tool designs and influence of material and process variables on formed part quality.

(c) Improving multi-stage (transfer/progressive die) stamping processes.

8.1.1. Sheet hydroforming

- For wider application of sheet hydroforming technology, a design methodology to implement a robust SHF-P process needs to be developed. This thesis provides a state-of-the-art review on sheet hydroforming. There is a need for a fundamental understanding of the influence of process and tool design variables on hydroformed part quality. This thesis addresses issues unique to sheet hydroforming technology, namely, (a) selection of forming or pot pressure, (b) excessive sheet bulging and tearing at large forming pressures, and (c) methods to avoid leaking of pressurizing medium during forming.

- An FEA-based methodology is presented for selecting punch-die clearances in designing tools for forming axisymmetric parts with sheet hydroforming with punch (SHF-P) process.
Developed methodology was validated with experiments conducted by Schnupp Hydraulik, Germany. Two axisymmetric part geometries were identified (a) cylindrical and (b) conical.

An FEA-based methodology was developed for prediction of springback in sheet hydroforming with die (SHF-D) process of large parabolic reflectors. Using the developed methodology, a parametric analysis was conducted to identify the best possible tool design and to demonstrate the influence of the tool, process and material variables on the final hydroformed part quality. Feasibility of the SHF-D process was demonstrated.

8.1.2. Material characterization: Improvement of flow stress calculation methodology

- There is an increasing interest in warm forming of sheet, especially for lightweight materials such as Al- and Mg- alloys. Elevated temperature flow stress data (true stress vs. true strain curve), of the sheet material is a necessary input for accurate finite element (FE) simulation of warm forming/hydroforming. There is limited data on flow stress of sheet materials, especially at elevated temperature. Thus, there is a need for a reliable methodology to determine elevated temperature flow stress preferably using the biaxial bulge test.

- In the existing flow stress calculation methodology using the biaxial sheet bulge test, membrane theory assumption equations are used (deforming dome has spherical shape, i.e., constant curvature over the surface). When applying the bulge test for elevated temperature flow stress determination, preliminary tests indicate that the spherical dome assumption made in the membrane theory's closed-form equations may introduce errors in the calculated flow stress at high values of strain (corresponding to large dome heights). The closed-form equations also offer limited flexibility in defining material laws. For example at room temperature, the commonly used power law is good for most steels, but does not show a good "fit" with some Al- alloys. Also at elevated temperature, the material softens and does not follow the power law hardening model \( \sigma = K \varepsilon^n \).
To address these limitations of the existing flow stress calculation methodology, an improved, robust 'inverse analysis' approach for flow stress calculation was developed for the biaxial bulge test. As a first approximation in demonstrating the developed methodology, a power law model was chosen to characterize material behavior. Material parameters (strength coefficient 'K' and strain hardening exponent 'n-value') were identified using the optimization tool LS-OPT, which uses a Response Surface Methodology (RSM) to build an approximate optimization problem with multiple variables. LS-OPT with FE code LS-DYNA, calculates the "best possible" K and n-value for a material. These simulation tools offer considerable flexibility in characterizing material behavior (numerous material laws available) and offer great potential in application to elevated temperature bulge tests.

The validity of the developed methodology was demonstrated by calculating flow stress at room temperature for two materials, (a) Aluminum alloy AA5754-O (1.3 mm and 1.0 mm) and (b) Stainless steel SS304 (0.86 mm).

A methodology for elevated temperature flow stress determination is proposed. Tools for elevated temperature bulge test were designed and are presently being manufactured.

§8.1.3. Improving multi-stage (transfer/progressive die) stamping processes

In order to achieve profitability and remain competitive, stamping companies in industrialized countries need to apply advanced computational tools such as FE simulation, besides practicing efficient shop-floor management practices. Some of the needs of the stamping industry deemed relevant to this research are (a) reducing the die/tool development (design + drafting) time by automating design process (b) identifying and optimizing critical tool design and process parameters for eliminating part failures seen in production. The desired solutions and computational techniques were developed using production examples (transfer and progressive die) through active cooperation with multiple stamping industry partners.
An FEA-based methodology was developed for designing (a) process sequence and (b) tool design for multi-stage stamping tools. This methodology was applied in determining critical tool variables and process parameters for two case studies, one each in transfer die and progressive die stamping.

Using volume constancy, a methodology was developed to automate (a) selection of forming sequence (b) tool design for selected forming sequence (c) presses, mechanical fingers and other equipment. For ease of operation, the developed methodology was programmed into a user-friendly software application (with Microsoft® Visual Basic combined and CAD package SolidWORKSTM) and is presently being applied in designing and developing tools for axisymmetric cups with hemmed lips. For an existing stamping industry member, all past designs were recorded into a database that can be accessed at will for (a) referring to existing design rules and (b) recording new tool design guidelines. Thus, besides standardizing tool design, part design and forming sequence design procedures, the developed methodology also presents a case study in efficient shop-floor data management. The developed methodology has helped the tool engineers to reduce their shell line and tool design development time from approximately 4 weeks to less than a week.

8.2. Future work

Room temperature sheet hydroforming: Experiments for sheet hydroforming with conical punch are in progress. After experimental validation of the conical punch FE simulation predictions, the results of the sheet bulging study could be applied as design guidelines in selecting pot pressure and punch-die clearance in forming axisymmetric tapered part geometries. However, these validated design guidelines are relevant only to axisymmetric part geometries. For non-symmetric part geometries (see Figure 6.31), bulging of the sheet is observed more commonly in the straight sections of a die, where the stress state is "plane strain". This is because sheet material shows lesser resistance to deformation in this stress
state. Therefore, a study investigating sheet bulging in the “plane strain” state of stress is needed to have design guidelines for non-symmetric part geometries.

- **Material characterization:** An 'inverse analysis' methodology, using FE code LS-DYNA and optimization tool LS-OPT, for determining flow stress data at elevated temperature is proposed. Tools for elevated temperature bulge test are presently being manufactured. A test matrix will be planned to bulge two materials (a) AA5754-O and AA5182-O at different temperatures (between 125°C and 300°C) for various strain rates. During the tests, the bulge/dome height will be measured at three locations on the dome surface (at the dome apex, and two on either side, to quantify the influence of changing dome curvature on flow stress curve. In preliminary studies, only the dome height at apex will be used to validate the methodology at elevated temperature. Initially, inverse analysis will be conducted using power law equation $\sigma = K (\varepsilon)^n (\dot{\varepsilon})^m$, where influence of strain rate is be taken into consideration by the strain hardening exponent ($m$). At a later time, other flow stress equations will be considered.

- **Improving multi-stage (transfer/progressive die) stamping processes:** The software tool developed in this study uses experience-based rules in designing tools and process sequence for forming multi-stage parts .and tools for each stage. Using the experience-based rules as a first approximation, an FE-based approach will be developed to obtain process sequence and tool design.
REFERENCES


<table>
<thead>
<tr>
<th>Reference</th>
<th>Description</th>
</tr>
</thead>
</table>


[Macrae et al. 2003] Macrae, C., Heinmann, W., 2003, “Machines used for producing hydroformed components the past, the present, the future”, Hydroforming of Tubes, Extrusions and Sheet Metals, Edited by


<table>
<thead>
<tr>
<th>Reference</th>
<th>Title</th>
</tr>
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</table>


APPENDIX A:

DRAWBEAD FORCE CALCULATOR
Stoughton’s mathematical model for drawbead forces [Stoughton, 1988]

Principle:

- Deformed geometry of the sheet metal estimated based on the relative position of the bead with respect to groove geometry, thus able to account for effect of clearance and partial bead penetration on the drawbead force.

- Assuming the energy required to compress inner fiber in bending and unbending is equal to energy required to compress the outer fiber and assuming all forces in bending, and unbending can be calculated by the same formula, force required to pull the sheet at the $i^{th}$ bend for a material with current thickness $t$ and current average strain $\varepsilon_{ai}$, can be defined as [Tufekci et al 1993],

$$F_i = \frac{wKn}{(1+n)} \left(1 + \frac{2R_{\text{eff}}}{t_i}\right) \left(1 + \frac{2\bar{R}}{1+2\bar{R}}\right) $$

$$ \times \left[ \frac{1}{2+n} \left( \frac{a_i}{\sqrt{1+2\bar{R}}} - b_i \varepsilon_{ai} \right) \left( (\varepsilon_{ai} - \varepsilon_{mi})^{2+n} - \varepsilon^{2+n}_{ai} \right) \right] $$

$$ + \frac{b_i}{3+n} \left( (\varepsilon_{ai} - \varepsilon_{mi})^{3+n} - \varepsilon_{ai}^{3+n} \right) $$

$$ - \left[ a_i \frac{1+\bar{R}}{\sqrt{1+2\bar{R}}} + \frac{1}{2} b_i \varepsilon_{mi} \right] \varepsilon_{mi} (\varepsilon_{ai})^{1+n} $$

...Equation 1

where: $i = 1, 2 \ldots 10$

$n$=strain hardening index, $K$ = Material strength coefficient, $\bar{R}$ = Normal anisotropy,

$R_{\text{eff}}$ = Effective bead radius,

$$R_{\text{eff}} = \frac{R}{\sin(\phi)}$$

...Equation 2

where $R$ = Radius of the bead/groove

$\phi$ = Contact angle
\[ \phi = \tan^{-1}\left( q \frac{\sqrt{(1-p)^2 + p(2-p)(1-q^2)} - (1-p)}{1-q^2} \right) \]  

...Equation 3

\[ p = \frac{d}{2R+t} \]

\[ q = \frac{(2R+t)}{(2R+g)} \]

where \( d \) is the bead depth, and \( g \) is the bead gap and \( t \) is the sheet thickness.

\[ a_i = 1 - \left( \frac{t_i^2}{48R_{\text{eff}}} \right), \quad b_i = 1 + \left( \frac{t_i^2}{4R_{\text{eff}}} \right) \]  

...Equation 4

\[ \varepsilon_{m_i} \] is the effective strain on the outer fiber (surface strain) and is given by,

\[ \varepsilon_{m_i} = \left( 1 + \frac{1}{\sqrt{1 + 2R}} \right) \ln \left( \frac{1 + \frac{t_i}{R_{\text{eff}}}}{1 + \frac{t_i}{2R_{\text{eff}}}} \right) \]  

...Equation 5

\( t_i \) = instantaneous sheet thickness,

\[ t_i = t_{i-1} e^{-\gamma \varepsilon_{m_i}} \]  

...Equation 6

and the average strain is given by:

\[ \varepsilon_{a_i} = \varepsilon_{a_i-1} + \gamma \varepsilon_{m_i-1} \]  

...Equation 7

A simple assumption can be made that work hardening results in average strain through the thickness equal to half of the strain on the outside fiber, \( \gamma = 0.5 \) [Tufekci, 1993].

- Friction in bending and sliding was accounted using coil friction formula. However, friction in straight sides is not accounted. Tufekci 1993 added the frictional force due to the applied blank holder force in the straight sides to the total drawbead restraining force that was not accounted by Stoughton.
The drawbead restraining force for a semicircular bead that involves three bending and three unbending [Figure A.1] is given by

\[ \text{DBRF} = (\mu F_N + F_i) e^{4\mu j} + \left( F_2 + \frac{\mu F_e}{2} + F_3 \right) e^{3\mu j} + \left( F_4 + \frac{\mu F_e}{2} + F_5 \right) e^{2\mu j} + F_6 + \mu F_N \] ...

Equation 8

where \( F_i, i = 1...6 \) is the force due to bending and unbending [Figure A.1].

\( F_N \) is Normal force on the bead.

\( F_e \) is the normal force due to elastic component during bending at the entry and exit of the bead [Figure A.1].

\[ F_e = \frac{2Ew\delta t^3}{(2R+g)^3}, \delta = \min \left( d, 2(2R+t) \frac{R\sigma_y}{tE} \right) \] ...

Equation 9

\( E = \) Young’s modulus, \( w = \) Width of the sheet, \( t = \) sheet thickness, \( R = \) Groove radius, \( g = \) clearance between the bead and the groove, \( d = \) bead depth, \( \sigma_y = \) Yield stress.

Figure A.1: Schematic of locations of bending and unbending as the sheet metal passes over the semicircular bead [Tufekci et al 1993]
The drawbead restraining force for a rectangular bead that involves four bending and unbending [Figure A.2] is given by,

\[
DBRF = \left( \mu F_N + F_1 \right) e^{4ij} + \left( F_2 + \frac{\mu F_e}{2} + F_3 \right) e^{3ij} + (F_4 + F_5) e^{2ij} + \\
\left( F_6 + \frac{\mu F_e}{2} + F_7 \right) e^{ij} + F_8 + \mu F_N
\]

...Equation 10

where \( F_i, i = 1 \ldots 8 \) is the force due to bending and unbending [Figure A.2].

![Figure A.2: Schematic of locations of bending and unbending as the sheet metal passes over the rectangular bead [Tufekci et al 1993]](image)

The binder hold down force that is necessary to prevent lifting of the binder while the sheet metal flows through the drawbead is summation of all the vertical component of the bending and unbending force at the bead radius.

The binder hold down force for a semicircular bead is given by
\[ BHF = \frac{1}{2(1+\mu^2)} \left\{ \left( e^{i\phi} \left( 2\mu \cos \phi + (1-\mu^2)\sin \phi \right) - 2\mu \right) F_1 \right. \]

\[ + \left( e^{2i\phi} \left( \mu \cos \phi + \sin \phi \right) - \mu \cos \phi + \sin \phi \right) \left( F_1 e^{i\phi} + \mu F_e + F_2 + F_3 \right) \]

\[ - \left( e^{i\phi} \left( 2\mu \cos \phi - (1-\mu^2)\sin \phi \right) - 2\mu \right) \left( F_1 e^{i\phi} + F_2 + F_3 + \mu F_e \right) e^{2i\phi} + F_4 + \mu F_e + F_5 \} \]

...Equation 11

where \( F_i, i=1\ldots6 \) is the force due to bending and unbending given by equation 2.17.

\( F_e \) is the normal force due to elastic component during bending at the entry and exit of the bead given by equation 9.

- The binder hold down force for a rectangular bead is given by

\[ BHF = Y_1 + Y_2 + Y_3 + Y_4 \]  
...Equation 12

\[ Y_1 = \frac{1}{2(1+\mu^2)} \left( e^{i\phi} \left( 2\mu \cos \phi + (1-\mu^2)\sin \phi \right) - 2\mu \right) F \]

\[ Y_2 = \frac{1}{2} e^{i\phi} \sin \phi \left( F_1 e^{i\phi} + F_2 + \frac{\mu F_e}{2} + F_3 \right) \]

\[ Y_3 = \frac{1}{2(1+\mu^2)} \left( F_1 e^{i\phi} + F_2 + \frac{\mu F_e}{2} + F_3 \right) e^{i\phi} + F_4 + F_5 \left( e^{i\phi} \left( 2\mu \cos \phi + (1-\mu^2)\sin \phi \right) - 2\mu \right) \]

\[ Y_4 = \frac{1}{2} \left( \left( F_1 e^{i\phi} + F_2 + \frac{\mu F_e}{2} + F_3 \right) e^{i\phi} + F_4 + F_5 \right) \left( e^{i\phi} \left( 2\mu \cos \phi - (1-\mu^2)\sin \phi \right) - 2\mu \right) \]

where \( F_i, i =1\ldots8 \) is the force due to bending and unbending. \( F_e \) is the normal force due to elastic component during bending at the entry and exit of the bead given by equation 9.
EXCEL Calculator for Stoughton’s model

Analytical equations developed by Stoughton and later modified by Tufekci were programmed in EXCEL to obtain restraining force and binder hold down force for a given semicircular and rectangular bead geometries. Figure A.3 & Figure A.4 shows the user interface in the EXCEL calculator for the semicircular drawbead and rectangular bead respectively. There are two sections for input and one section for output. Input section consists of a) Material data section, where the user can enter the material properties of the sheet material and b) Drawbead geometry section where the user can enter dimensions to describe the drawbead geometry. Also enclosed is the figure that schematically explains the dimensions required to describe the respective drawbead geometry in the calculator. The outputs from the program are the drawbead restraining force and the binder hold down force.
Figure A.3: EXCEL interface for semicircular drawbead force calculations
Figure A.4: EXCEL interface for rectangular drawbead force calculations

Table A.1: Sheet material input data in the EXCEL calculator
Table A.1 shows the required sheet material properties to estimate the drawbead force using the calculator. The following data are required.

a. Geometry of the sheet material (thickness, width of sheet), and
b. Sheet material properties (Young’s Modulus of the material, Yield Stress of the material, K-value:- Strength of material, n-value:- strain hardening exponent, r-value: normal anisotropy coefficient, m-value:- strain rate exponent, e0-value:- pre-strain value). The flow stress in the calculation is expressed using the equation \( \sigma = k(\epsilon_0 + \epsilon)^n \)

Table A.2: Drawbead geometry (semicircular) input data in the EXCEL calculator

Table A.2 shows the dimensions required to describe the semicircular bead geometry in the calculator. The following data are required

a. Geometry of the drawbead (bead depth/penetration, bead corner radius, groove corner radius, bead thickness, groove thickness, draw velocity),
b. Friction conditions (Coefficient of friction of the sheet with the dies)

Table A.3: Drawbead geometry (rectangular) input data in the EXCEL calculator

<table>
<thead>
<tr>
<th>Tooling Conditions</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Bead Depth (d) (mm)</td>
<td>0.71</td>
</tr>
<tr>
<td>Bead Corner Radius (Rb) (mm)</td>
<td>4</td>
</tr>
<tr>
<td>Groove corner radius (Rg) (mm)</td>
<td>4</td>
</tr>
<tr>
<td>Bead thickness (Lb) (mm)</td>
<td>16</td>
</tr>
<tr>
<td>Groove thickness (Lg) (mm)</td>
<td>20</td>
</tr>
<tr>
<td>Draw Velocity (mm/sec)</td>
<td>1</td>
</tr>
<tr>
<td>Friction (μ)</td>
<td>0.1</td>
</tr>
</tbody>
</table>

Table A.3 shows the dimensions required to describe the rectangular bead geometry in the calculator.

a) Geometry of the drawbead (bead depth/penetration, bead corner radius, groove corner radius, bead thickness, groove thickness, draw velocity),

b) Friction conditions (Coefficient of friction of the sheet with the dies)

EXCEL calculator to estimate the drawbead geometry [Bead depth (d)] for a required restraining force.

Analytical equations developed by Stoughton and later modified by Tufekci were programmed in EXCEL to obtain restraining force and binder hold down force for a given semicircular and rectangular bead geometries. The developed EXCEL calculator was modified to estimate the drawbead geometry for both the semicircular (Figure A.6) and the rectangular geometry (Figure A.7) with the sheet material properties, the estimated blank holder force $BHF_{est}$ and the cushion capacity $BHF_{cushion}$ as input.

Figure A.6 & Figure A.7 shows the user interface in the EXCEL calculator to estimate the drawbead geometry (Bead depth (d)) for the semicircular drawbead and rectangular drawbead, respectively. There are two sections for input and one section for output. Input section consists of a) Material data section, where the user can enter the material properties of the sheet material and b) Drawbead geometry section where the user can enter dimensions to describe the drawbead geometry except the bead depth (d). Also enclosed is the figure that schematically explains the dimension required to describe the respective drawbead geometry in the calculator.

The output from the program are the bead depth (d), drawbead restraining force, binder hold down force and comparison of the restraining force required and the restraining force provided by combination of cushion and the drawbead.
Figure A.5: Flowchart for the methodology to estimate the drawbead geometry
Figure A.6: EXCEL interface for semicircular drawbead calculations
Figure A.7: EXCEL interface for rectangular drawbead calculations