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Numerical Prediction of Limiting Draw Ratio in Aluminum Cylindrical Cup Drawing

by

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B.Sc (Mechanical Engineering)

A thesis submitted to
the Faculty of Graduate Studies and Research
in partial fulfilment of the requirements
for the degree of
Master of Engineering
in Mechanical Engineering

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May, 1997**

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**Numerical Prediction of Limiting Draw Ratio in Aluminum
Cylindrical Cup Drawing**

submitted by Eric T. Harpell
in partial fulfilment of the requirements for
the degree of Masters of Engineering
in Mechanical Engineering

A handwritten signature in black ink, appearing to read 'M. J. Worswick', written over a horizontal line.

Thesis Supervisor M. J. Worswick

A handwritten signature in black ink, appearing to read 'Robert Bell', written over a horizontal line.

Chair, Department of Mechanical and Aerospace Engineering

Carleton University
May, 1997

*I would like to dedicate this thesis to
my family for all of their support,
my parents Tim and Barb Harpell, and
my brothers David, Mike, and Greg.*

Abstract

The results from a study of the numerical prediction of limiting draw ratio (LDR) are presented. A series of ten different tooling geometries were considered for a cylindrical cup drawing process, with the differences being variations in the die and punch profile radii.

A validation study in which numerical predictions were compared with experiments was performed on six different tooling geometries. These studies demonstrate that the numerical model accurately captured the forming process. The punch forces, blank draw-in, and strain history are found to be in reasonable agreement with the experiments. Some concern exists that the friction coefficient used was somewhat low as indicated by predicted major strains in the wall being consistently lower than measured.

The choice of anisotropic yield criterion was shown to have a strong influence on predicted strains. The numerical model somewhat overpredicts LDR for 6-12 mm profile radii due to the use of a low coefficient of friction. If the data were available to support an increase in friction coefficient, then the model would lie in better accord with the experiments. For the sharp, 3 mm die radius, the model largely over-predicts LDR (unconservative) which suggests a change in failure mechanism, to a bending failure, since the bend radius to thickness ratio approaches the bendability limit for AA5754-O.

Acknowledgments

I would like to thank Prof. M. J. Worswick for his guidance and support throughout this research. His generosity and willingness to share experiences and insights has made my stay at Carleton a very gratifying experience. I thank him for opening my eyes to an interesting field of study and bringing me into an amiable working environment.

To the staff in the Department of Mechanical and Aerospace Engineering I extend my thanks for making my stay at Carleton memorable. I would like to extend a special thanks to the office staff who helped me pull things together throughout my stay and to Bruce Johnson and Ian Jefferson for their assistance and support.

To Mark Finn at ALCAN, I would like to express my gratitude. His assistance in exploring the use of the numerous aspects of LS-DYNA3D in sheet metal forming was invaluable. His patience and willingness to share knowledge was of great assistance to me while I was working my way up the learning curve. I would also like to acknowledge the assistance provided by Dr. Mukesh Jain. My research could not have been accomplished without the material and experimental data he provided.

I would also like to express my sincere thanks to Dr. Pierre Martin for his collaboration in this research. Exposure to his experience and knowledge in experimental forming made my introduction to this field a valuable experience.

I would also like to thank MTL/CANMET and KRDC of ALCAN International Ltd. for providing financial support for this research.

To Kevin Williams and Chantal Lamontagne I wish to express my appreciation for the strong foundation in metal forming research they provided for my research. I would also like to thank Keith Pilkey and John Fowler for their development of the grid analysis system which saved me considerable effort and time making strain measurements.

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List of Symbols

A_T	Total contact area
a, c, h, p	Anisotropic coefficients used in Barlat's 1989 yield criterion
A_i	Area of contact of the i th element
D_i	Punch major diameter
$d_{0,1,2}$	Diameter of circle grid
e	Engineering strain
E	Modulus of Elasticity
$f(x), g(x)$	Functions of the variable x
f	Frictional force
F, G, H, L, M, N	Anisotropic parameters used in Hill's 1948 yield criterion
K	Bulk Modulus
k	Strength constant
M	Barlat yield exponent
$\underline{m}$	strain rate sensitivity exponent
m	Hill 90 yield exponent
n	Strain hardening exponent
P	Contact interface pressure
r	Lankford coefficient
$\bar{r}$	Average anisotropic parameter
R, S, T	Principal anisotropic axes shear yield stresses
V_i	Volume of the i th element
X, Y, Z	Tensile yield stresses along principal anisotropic axes
ϵ	True (logarithmic) strain
ϵ_p	Plastic strain
$\bar{\epsilon}^p$	Effective plastic strain
$\dot{\epsilon}$	Strain rate

ε_0	Yield strain
f_n	Contact force
η	Friction factor
μ	Coefficient of friction
μ_b	Coefficient of friction in the boundary layer regime
μ_s	Coefficient of friction based on the shear strength of the lubricant
ρ	Density
ξ	Interface stiffness
τ_f	Friction stress
τ	Shear stress
τ_Y	Yield stress in pure shear
γ	Strain ratio
σ	Stress
σ_b	Yield stress in equibiaxial tension
$\bar{\sigma}$	Effective stress
σ_Y	Yield stress
ϕ	Material angle measured in the plane of the sheet from the rolling direction
Φ	Yield function
ν	Poisson's ratio

Subscripts

0, 45, 90	Material directions, rolling, compound, and transverse
1	Major principal direction
2	Minor principal direction
x, y, z	Directions corresponding to the coordinate reference system

List of Acronyms

BHF	Blank Holder Force
CCD	Charge Coupled Device
CD	Compound Direction (45°)
CPU	Central Processing Unit
FE	Finite Element
FEM	Finite Element Method
FLC	Forming Limit Curve
FLD	Forming Limit Diagram
FLR	Forming Limit Ratio
KRDC	Kingston Research and Development Centre
LDH	Limiting Dome Height
LDR	Limiting Draw Ratio
LHS	Left Hand Side
LVDT	Linearly Variable Differential Transformer
MTL	Materials Technology Laboratory
PC	Personal Computer
PPF	Peak Punch Force
RD	Rolling Direction (0°)
RHS	Right Hand Side
SGI	Silicon Graphic Industries
TD	Transverse Direction (90°)
UTS	Ultimate Tensile Stress

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Chapter 1

1. Introduction

Studies into the use of aluminum alloy sheet in automotive applications were originally prompted by the energy crisis of the mid 70s. The use of aluminum alloy sheet did not gain wide spread acceptance for a number of reasons: (i) automotive manufacturers were able to attain substantial improvement in fuel efficiency [1] through improvements in engine and drive train efficiency, reduction in vehicle weight through down-sizing of the structure, and finding methods to decrease drag forces by improving rolling resistance and aerodynamics of vehicles; and (ii) government regulations and the state of the economy in the 1980s were such that these improvements negated the need to use expensive aluminum alloys to further improve vehicle efficiency.

With the growing awareness of environmental issues prevalent in the 90s, automotive manufactures have been forced to look at additional methods to improve the efficiency of their vehicles [2]. Consumer awareness and desire to preserve the environment, in addition to anticipated higher standards being set for vehicle emission and efficiency have lead to increased interest in the use of aluminum for automotive

applications. Towards the end of the 80s, several car companies [3], including Rover, Pininfarina, Volvo, Porsche, Mercedes, Audi, and Ferrari, were increasing use of aluminum in their vehicles. Today, Ford, General Motors, and Chrysler all make use of aluminum alloy sheet in selected outer panel assemblies such as hoods and deck lids. In addition to improved fuel economy, lighter bodies improve braking performance and vehicle acceleration for an engine of given size making it more attractive to the consumer.

The Honda NSX is one of the first cars to make extensive use of aluminum in its construction [4]. A conventional car will typically consist of about 7.2 percent aluminum by weight whereas the NSX contains 31.3 percent. This utilization allowed Honda to effectively reduce the weight of the car by 200 kg over the weight using steel components. Thus, the use of aluminum provides a significant weight savings, leading to increased efficiency. In addition the aluminum also provides comparatively good corrosion resistance and conforms to recycling goals of the automotive industry.

Historically, the majority of stamped parts have been fabricated using steel sheets, resulting in a comprehensive formability database for steel. Comparatively little research [1] has been done with aluminum alloys which limits the experience and knowledge which can be applied to these forming processes. This lack of fundamental understanding is coupled with the lower formability most aluminum alloys must overcome when trying to replace steel components. This emphasizes the need for accurate methods of predicting the forming behavior of complex parts, a task to which the finite element method (FEM) is well suited.

Investigation of a typical sheet metal forming process must consider the effect of a large number of process variables on the production of components. Factors which must be considered include, but are not limited to, the frictional and mechanical characteristics of the process, material considerations, and process parameters such as blank holder force and punch velocity.

Ultimately, the goal of this research is to improve our ability to accurately predict material behavior in sheet metal forming operations. To that end, a study of

cylindrical cup drawing has been undertaken, in an effort to use knowledge gained in modeling this simple formability test to improve the methods used when modeling more complex industrial stampings. A numerical model of the cylindrical cup deep drawing process has been developed and used to predict the limiting draw ratio (Section 1.4) for a series of tooling combinations consisting of various die and punch profile radii.

Model verification consisted of comparing various predicted process parameters to those measured in corresponding experimental investigations. These included comparisons of strain distributions of fully formed and interrupted cups, as well as comparing both punch forces and blank draw-in. In addition, the effect of process variables such as blank holder force and friction coefficient was examined. The effect of yield surface representation on the numerical predictions was also investigated.

The balance of this chapter presents a review of previous research pertinent to the current work. The topics covered include sheet formability and factors influencing formability, material constitutive behavior, and the use of FEM to simulate sheet forming processes. This chapter closes with a brief description of the current work.

1.1 Sheet Formability

1.1.1 Formability Tests

The formability of sheet materials has, for years, been evaluated in press shops through simple formability tests. A great number of tests have been proposed to provide simulative representation of sheet metal forming processes. Lamontagne [5] performed an extensive review of a number of these simulative tests, including, the Olsen and Erichsen cup tests, which assess the stretching performance of the material, the Swift cup test for deep drawing, the Fukui cup test which combines both stretching and drawing, and the limiting dome height (LDH) tests as described by both Hecker and Ghosh. The Swift cup test directly relates to the drawing of cylindrical cups and will therefore be described in greater detail.

The Swift cup test [6], as shown in Figure 1.1, utilizes a cylindrical punch with a spherical cap to draw a circular blank into a cylindrical die cavity. The material is

clamped between two tooling surfaces, and the test is performed with sufficient clamping force to prevent the formation of wrinkles in the sheet. Swift used this test to evaluate the drawing ratio, the ratio of the blank to punch diameter, in terms of both punch-load displacement data and the thickness strains of the material. Chung and Swift [7] also investigated cup drawing performed with a flat bottomed punch, as shown in Figure 1.2, in order to reduce the complex stretching around the hemispherical surface of the punch. With this tooling geometry, the material in the bottom surface of the cup was assumed to be locked, allowing simplification of the analytical solution for the cup drawing problem.

Woo [8] developed an analytical method of predicting cup drawing behavior incorporating the effect of plastic anisotropy, characterized by the strain ratio (γ). This is the ratio of thickness to width strain in a tensile test specimen, also known as the Lankford coefficient, r , described in Section 1.4.3. This work coincided with similar studies performed by Budiansky and Wang [9], and Chiang and Kobayashi [10].

1.1.2 The Forming Limit Diagram

A forming limit diagram is a useful tool to characterize the limiting strain conditions in a forming process. In order to define this diagram it is easiest to first describe how they are typically generated. In a forming process various loading paths can be used to determine the principal limit strains, ϵ_1^* and ϵ_2^* , at which localized necking is first observed. The most commonly used technique for measuring these forming limit strains involves the use of a circle grid system. A series of circles, of original diameter, d_0 , are imprinted on the specimen prior to deformation. After forming, the circles are deformed into ellipses with major and minor diameters, d_1 and d_2 , respectively. These may be expressed as either true (logarithmic) strains,

$$\epsilon_1^* = \ln \left(\frac{d_1}{d_0} \right) \quad \epsilon_2^* = \ln \left(\frac{d_2}{d_0} \right) \quad (1.1)$$

or engineering strains,

$$e_1^* = \ln \left(\frac{d_1 - d_0}{d_0} \right) \quad e_2^* = \ln \left(\frac{d_2 - d_0}{d_0} \right) \quad (1.2)$$

where ϵ and e are the principal true and engineering strains, respectively. By plotting the strains near the failed regions of the specimens, it is possible to construct the forming limit diagram (FLD) which is sometimes called a Keeler-Goodwin diagram [11]. The loci of forming limit strains define a curve known as the forming limit curve (FLC) when they are plotted in strain space, as initially suggested by Gensamer [12]. This FLC is often used in press shops, to determine the proximity of a part to material failure for a particular process.

The forming limit diagram is segmented into three main regions of interest as indicated in Figure 1.3. These are the biaxial tension side, on which both the principal strains are positive (tensile), the drawing side, in which the major strain is positive and the minor is negative (compressive), and the line of plane strain, on which only one strain is non-zero. Keeler [13] used the strains measured in the biaxial tension region adjacent to the failure locations to define his FLC. In contrast, Goodwin [14] measured the strains at the actual site of failure, also expanding the work to include the tension and compression or drawing side of the diagram. This led to the development of an uncertainty band, known as the Keeler-Goodwin band, in which failure of the material may or may not have occurred dependent upon the criterion used to define failure. This uncertainty band is depicted in Figure 1.4 [15].

1.1.3 Grid Strain Analysis

The circle grid strain analysis method, proposed by Keeler [16] in 1968, has become one of the most widely used methods of experimentally determining strains. A regular circular grid pattern is deposited upon the specimen prior to the sheet forming process. The circular grids are assumed to deform into ellipses with the principle strains

determined at the grid locations from the major and minor axes of the deformed ellipses. Thus the measured strains are a representation of the deformed surface strains within the component. In recent years, the use of square grids to replace the circle grids has been promoted [17] due to the increase in the rate at which strain data can be acquired through computer systems.

In the square grid measurement method, the strains are computed by calculating the nodal displacements of the corner grid points. The grids are deposited on the material typically through either a chemical etching or silk screening process. A series of undeformed grids are measured as a calibration set, and compared with the measured shape of deformed grids. The use of digital imaging techniques and image analysis software provides a means of rapidly determining the strain distributions for a component.

Grid strain analysis is limited by the assumptions made in calculating the strains. Typically it is assumed that there is no strain gradient across the deformed grid. This makes it relatively difficult to identify sharp strain gradients without going to a fine grid spacing. This fine spacing in turn requires more measurements be made to capture the distribution. A further drawback of this technique is that the measured values are strain path independent; Thus, the same strains will be measured for two grids having the same final shape regardless of the strain path followed. Therefore this analysis does not measure a state variable, but rather indicates the instantaneous surface strains at the time of measurement.

1.2 Tribological Considerations

Tribology is defined [18] as the science and technology of interacting surfaces in relative motion and of related subjects and practices. The sheet metal forming process clearly fits well into this broad area of science which takes into account factors such as the surface chemistry and topography, material characteristics, and lubrication mechanisms.

It has been well established [1,19] that lubrication has a tremendous impact on a sheet metal forming process. The type of lubrication regime which occurs is of great

importance during the forming process and the factors which affect this will be described. The material interactions with the lubricant will affect properties such as the surface quality of the finished component as well as determining whether or not the part can be formed.

1.2.1 Lubrication Characteristics

In order to understand the tribology of sheet forming a review of some general concepts is required. Lubrication, where liquid lubricants are concerned, has been classified [19] into three general lubrication mechanisms. These are:

1. **Boundary Lubrication** - The surfaces are separated by an adsorbed molecular layer of lubricant molecules.
2. **Elastohydrodynamic** - The local pressures are so high and the lubricant layer so thin that elastic deformation of the surfaces can not be neglected.
3. **Hydrodynamic** - The surfaces are separated by a fluid film, which is usually thick in comparison to the asperity heights of the bearing surfaces.

This gives rise to four general lubrication regimes as illustrated in Figure 1.5. They are summarized as follows:

- Thick Film (Hydrodynamic)
- Thin Film (Elastohydrodynamic)
- Mixed (Elastohydrodynamic & Boundary)
- Boundary

Lubrication Characteristics

In steels, the iron oxide is soft and exhibits a layered nature, allowing it to deform and act as a lubricant. Thus the iron oxide which naturally forms as part of the surface layer will actually improve the forming characteristics of the steel sheet. In

galvanized sheet, the oxide (ZnO) is much harder and its behavior is closer to that of aluminum oxide.

The differences in frictional behavior between steel and aluminum are primarily due to differences in surface topography and surface chemistry [1] of the two materials. In forming processes, the contact pressure of the asperity interactions tends to break up oxide layers exposing highly adhesive substrate materials thus allowing metal to metal contact. Consequently, the frictional behavior will be greatly affected by the adhesion process unless a suitable lubricant intercedes with the breaking of this oxide layer.

Aluminum oxide can be either amorphous (glassy) or crystalline in nature. The state of the oxide will depend [1] on the alloy composition, temperature, and exposure time as shown in Figure 1.6. In the later phases of the oxide formation, magnesium, a common alloying element, may migrate out of the alloy. Thus Mg oxide will form on top of the amorphous layer which has an additional impact on the frictional behavior.

The effect of surface topography is to entrain lubricant in the asperity valleys which act as lubricant reservoirs during the forming process. To enhance this effect, surface textures have been developed using various techniques utilizing lasers, electron beams, and electro-discharge machining to enhance lubricant entrainment. Rolling parameters such as roll diameter, speed, lubrication, and the final cold roll reduction also affect the imprinting of the roll texture on the sheet.

It has been noted [1] that tool roughness is an important factor if soft tools are used. Under these conditions the tooling should be polished with a fine grit size to improve the frictional character of the process.

It is commonly accepted that the interlocking of asperities occurs in frictional processes. During the course of the asperity interaction, wear particles will be generated. The effect of these particles on the forming process can be minimized if the surface topography exhibits large valleys between the asperity peaks to act as lubricant reservoirs and prevent the lodging of wear particles between the surfaces.

In work related to the mixed lubrication regime, the regime under which most metal forming operations fall, five characteristic parameters [20] have been proposed for the description of the frictional behavior in forming processes. They are:

1. *Load-carrying surface fraction*: The percentage of the projected surface area which will act to carry the load,
2. *Wave ratio in the direction of movement*: This relates the average wavelength of the workpiece and tooling asperities in the direction of movement,
3. *Relative cavity volume*: The amount of intermediate material which could be accommodated if the surface was in contact with an ideally flat tool,
4. *Mean non-lubricated frictional length*: Valleys exist between the asperity peaks and serve as reservoirs of lubricant during the process. As a peak crosses one of these regions its boundary film layer is replenished. Therefore, this length is the mean distance any point on the surface must move in lubricated motion before there is any chance of the intermediate layer being replenished, and
5. *Specific wetting diameter*: The lubricant reservoirs, or valleys will have physical characteristics described by the perimeter or circumference around the region bounded by adjacent valley peaks and the surface area of the valley projected onto an ideal flat tooling surface. The ratio of the circumferential length of the lubrication cup and its surface area was used to define the specific wetting diameter.

1.2.2 Friction

Several common methods are used to characterize general frictional behavior [20,21]. The most common methods will be discussed here. The friction stress (τ_f), is defined as the frictional shear force (f) divided by the total area of contact (A_T) as shown,

$$\tau_f = \frac{f}{A_T} \quad (1.3)$$

The most common characterization is the classical Coulomb coefficient of friction, μ . This coefficient relates the friction stress to the interface pressure in the following form:

$$\tau_f = \mu P \quad (1.4)$$

where τ_f is the friction stress, and P is the interface pressure. The Coulomb friction coefficient is the most widely used method of relating the normal force of pressure to the observed tangential force of friction.

A second form of frictional characterization relates the friction stress to the shear strength of the workpiece material. This characterization makes use of the friction factor (η) which is dependent on the shear strength of the material. For a thin plastic film lubricant, η is the ratio of the shear strength of the lubricant to that of the workpiece. The relation has the following form:

$$\tau_f = \eta \tau_{\text{work piece}} \quad (1.5)$$

Several factors can result in these simplified characterizations providing an inaccurate representation of the actual forces in a process. The use of these "constants" should only be undertaken when a thorough understanding of the basic principles inherent to the particular process exists. Their use is valid only in regions where their values are stable. These requirements imply that in a complex drawing process there may exist regions in which the friction coefficient will remain essentially constant. However, the frictional characteristics of a cup wall region may be significantly different than those found in a punch profile radius region, for example. Therefore, care must be taken to account for the variations in the friction coefficient which are likely to occur.

These characterizations do not take into consideration the effect that different lubrication regimes or tooling geometry will have on the process. Therefore, any

friction coefficient obtained experimentally is only valid under the conditions under which it was obtained. Different friction regimes may exist at various locations on the tooling which can result in vastly different frictional behavior.

Improved characterizations of frictional behavior have been proposed [21] which account for the fact that in many frictional processes contact occurs only over a certain percentage of the surface areas. These methods also allow the transition between lubrication regimes to be captured but they are difficult to implement numerically and would result in additional computational expense, through the introduction of another state variable.

1.3 Material Constitutive Behavior

1.3.1 Stress, Strain, and Strain Rate Measures

Under constant temperature conditions, the relationship between stress and strain for most metals can be described by an equation of the following form:

$$\sigma = f(\epsilon) g(\dot{\epsilon}) \quad (1.6)$$

where ϵ is the strain and $\dot{\epsilon}$ is the strain rate. Hollomon [22] proposed an equation to model the material strain hardening response which had the following form:

$$f(\epsilon) = k \epsilon^n \quad (1.7)$$

where k is the strength coefficient and n is the strain hardening exponent defined by:

$$n = \left. \frac{\partial \ln \sigma}{\partial \ln \epsilon} \right|_{T, \dot{\epsilon}} \quad (1.8)$$

Another commonly adopted method of representing this response was proposed by

Swift [23] in 1945 with the form:

$$f(\epsilon_1) = k (\epsilon_0 + \epsilon_1)^n \quad (1.9)$$

where ϵ_0 is the strain due to prior cold working.

A power-law is a commonly employed method used to represent the strain-rate dependence of stress, $g(\dot{\epsilon})$:

$$g(\dot{\epsilon}) \propto \dot{\epsilon}^m \quad (1.10)$$

where m is the strain rate sensitivity exponent. The material response indicated by these equations is for that of uniaxial tension which represents a much simpler strain state than typically occurs in a forming operation. In a general sheet metal forming process the material is deformed under a multi-axial stress state causing the strain to develop along a complex strain path. The uniaxial test data is readily obtained from simple tensile tests and can be related to the more complex states through the use of an effective stress-strain relation [24]. In order for the effective stress to be defined, we must first define the multi-axial yield behavior of the material, often referred to as a yield surface. This surface represents a locus of all possible combinations of stress states which will give rise to yielding or plastic flow in a material of a given mechanical hardness [25].

1.3.2 von Mises Yield Criterion

The Huber-Mises criterion, commonly called the von Mises yield criterion, is one of the most widely accepted yield functions for isotropic materials. This criterion, also called the distortion energy criterion, assumes that yielding will occur when a critical level of distortion energy is exceeded. The von Mises criterion was originally proposed in 1913 by von Mises, as described in [26] and, in terms of a general stress

state, is written as:

$$(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2 + (\tau_{xy})^2 + (\tau_{zx})^2 + (\tau_{yz})^2 = 2(\sigma_Y)^2 \quad (1.11)$$

or in terms of principal stresses:

$$(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 = 2(\sigma_Y)^2 \quad (1.12)$$

which, with the assumption of plane stress ($\sigma_3 = 0$) reduces to:

$$\sigma_1^2 + (\sigma_1 - \sigma_2)^2 + \sigma_2^2 = 2(\sigma_Y)^2 \quad (1.13)$$

Here, σ_x , σ_y , and σ_z are the normal components of stress in an arbitrary set of orthogonal directions, with σ_{xy} , σ_{zx} , and σ_{yz} representing the corresponding shear stresses, σ_Y is the material yield stress in uniaxial tension, and σ_1 , σ_2 , and σ_3 are the principal stresses. The effective stress is defined as:

$$\bar{\sigma} \equiv \sqrt{\frac{2}{9} [\sigma_1^2 + (\sigma_1 - \sigma_2)^2 + \sigma_2^2]} \quad (1.14)$$

Utilizing the Lévy-Mises [24] relation, the effective plastic strain increment for plane stress can be written as:

$$d\bar{\epsilon}^p \equiv \sqrt{\frac{2}{9} [(d\epsilon_1 - d\epsilon_3)^2 + (d\epsilon_1 - d\epsilon_2)^2 + (d\epsilon_2 - d\epsilon_3)^2]} \quad (1.15)$$

It is then possible to define the effective plastic strain rate as:

$$d\bar{\epsilon}^p \equiv \sqrt{\frac{2}{9}[(d\epsilon_1 - d\epsilon_3)^2 + (d\epsilon_1 - d\epsilon_2)^2 + (d\epsilon_2 - d\epsilon_3)^2]} \quad (1.16)$$

1.3.3 Anisotropy

Traditional yield functions, such as the von Mises and Tresca [26] criteria, are based on the assumption that the material behaves in an isotropic manner. Under forming conditions for sheet materials, this assumption is very seldom valid. Typical sheet materials develop rolling induced texture and therefore exhibit some degree of anisotropy. The metallurgical factors contributing to the development of anisotropy has been described in detail by Williams [27] and Lamontagne [28]. The anisotropy can be manifest in the form of planar isotropy, in which the sheet behaves in an isotropic manner in the plane of the sheet but has different mechanical properties in the sheet thickness direction, or in a fully anisotropic manner in which the mechanical properties of the sheet also vary with direction in the plane of the sheet.

Anisotropy is typically characterized by Lankford's coefficient [29], r , which represents the ratio of width strain increment to thickness strain increment in a tensile test. The normal or through-thickness anisotropy parameter, r , is a direct measure of the ability of the material to be deep drawn. A high r value indicates a resistance to both wrinkling and thinning of the sheet [30], as the material has a greater ability to deform in the plane of the sheet rather than by changing thickness. The Lankford coefficient is defined as:

$$r = \frac{d\epsilon_2}{d\epsilon_3} \quad (1.17)$$

where ϵ_2 and ϵ_3 are the strains in the width and thickness directions, respectively, as shown in Figure 1.7. Typically r is measured from tensile specimens machined parallel, perpendicular, and at forty five degrees to the rolling direction (RD) with these directions being represented by a subscript, r_0 , r_{90} , and r_{45} , respectively. Lankford [29]

suggested the material anisotropy be represented by an average strain ratio, $\bar{r}$, defined as:

$$\bar{r} = \frac{r_0 + 2r_{45} + r_{90}}{4} \quad (1.18)$$

The use of this average anisotropy parameter greatly simplifies the solution process, however variations in the material behavior in the plane of the sheet are not included. The most common method to represent the variation of r in the plane of the sheet utilizes a Δr term given as

$$\Delta r = \frac{r_0 - 2r_{45} + r_{90}}{2} \quad (1.19)$$

Variations in strain ratio with material direction often leads to the formation of peaks around the lip of the formed cup, commonly called “ears”. The tendency to ear is intensified for values of Δr significantly greater than zero.

1.3.3.1 Hill's Quadratic Yield Function

The most commonly applied yield criterion accounting for material anisotropy is the classical Hill [31] criterion proposed in 1948. This criterion, sometimes termed Hill's 48, is a generalization of the von Mises isotropic criterion to capture the anisotropic behavior. Hill's assumptions included that:

1. The material possesses three principal axes of anisotropy which are orthogonal,
2. No Bauschinger effect is exhibited by the material,
3. Internal stresses resulting from differential orientation of the grains associated with anisotropy are ignored,
4. The hydrostatic stresses do not influence yielding,

5. The yield criterion must reduce to von Mises in the case of isotropic materials (i.e. the criterion is quadratic in the stress components),
6. The anisotropic parameters increase in strict proportions as the material yields.

Hill [31] then proposed the following yield criterion:

$$2f \equiv F(\sigma_y - \sigma_z)^2 + G(\sigma_z - \sigma_x)^2 + H(\sigma_x - \sigma_y)^2 + 2L(\sigma_{yz})^2 + 2M(\sigma_{zx})^2 + 2N(\sigma_{xy})^2 = 1 \quad (1.20)$$

where F, G, H, L, M, and N are constants characteristic of the current state of anisotropy. Hill chose the principle axes of anisotropy to coincide with the selected reference axes, with the x, y, and z directions representing the rolling, transverse, and sheet normal directions, respectively. If X, Y, and Z then represent the tensile yield strengths in the principle anisotropic directions, the material constants can be defined using

$$\begin{aligned} \frac{1}{X^2} &= G + H & 2F &= \frac{1}{Y^2} + \frac{1}{Z^2} - \frac{1}{X^2} \\ \frac{1}{Y^2} &= H + F & 2G &= \frac{1}{Z^2} + \frac{1}{X^2} - \frac{1}{Y^2} \\ \frac{1}{Z^2} &= F + G & 2H &= \frac{1}{X^2} + \frac{1}{Y^2} - \frac{1}{Z^2} \end{aligned} \quad (1.21)$$

If R, S, and T are the yield stresses in shear with respect to the principle axes of anisotropy, then L, M, and N are defined by:

$$2L = \frac{1}{R^2} \quad 2M = \frac{1}{S^2} \quad 2N = \frac{1}{T^2} \quad (1.22)$$

If the reference co-ordinate system does not coincide with the principal axes of anisotropy an appropriate transformation of the stress components is required. Under

conditions of planar isotropy, where the anisotropy is rotationally symmetric about the z axis or sheet thickness direction, then the anisotropy parameters reduce to:

$$F = G \quad \text{and} \quad N = G + 2H \quad (1.23)$$

and for isotropic conditions (complete spherical symmetry):

$$F = G = H \quad \text{and} \quad L = M = N = 3F \quad (1.24)$$

effectively reducing the yield function to the von Mises criterion.

In sheet metal forming analyses, a common assumption is that the material is deformed under a state of plane stress. Using this assumption, Equation 1.20 reduces to:

$$2f \equiv (G + H) \sigma_x^2 - 2H \sigma_x \sigma_y + (H + F) \sigma_y^2 + 2N \sigma_{xy}^2 = 1 \quad (1.25)$$

Further, if planar isotropy is assumed and the through thickness shears are assumed to be negligible, this equation [31] can be written in terms of principal stresses and Lankford coefficient as:

$$\bar{\sigma} = \sqrt{\sigma_1^2 + \sigma_2^2 - \frac{2r}{1+r} \sigma_1 \sigma_2} \quad (1.26)$$

where $\bar{\sigma}$ is the equivalent stress. This equation, valid only if the principal stresses coincide with the principle axes of anisotropy, can be used in conjunction with the Levy-Mises [24] relation to define the effective plastic strain increment as follows:

$$d\bar{\epsilon}^p = \frac{1+r}{\sqrt{1+2r}} \left[d\epsilon_1^2 + d\epsilon_2^2 + \frac{2r}{1+r} d\epsilon_1 d\epsilon_2 \right]^{\frac{1}{2}} \quad (1.27)$$

Figure 1.8 is a plot of the yield locii generated with the Hill 48 criterion with different r values and clearly indicates that for a low r material, the Hills 48 criterion predicts yielding to occur at a much lower biaxial stress level than with the von Mises criterion. This low equi-biaxial yield stress has been termed the “anomalous” behavior of low r materials. In an attempt to correct this behavior, Hill proposed new yield criteria in 1979 [32] and in 1990 [36] utilizing a non-quadratic yield exponent, as described in the next section.

1.3.3.2 Hill's 90 Non-Quadratic Yield Function

The Hill 90 criterion was proposed with the intention of providing an improved model to be used to predict planar anisotropic material behavior. For a material whose reference system coincides with the principal axes of anisotropy this criterion has the form

$$\begin{aligned} & \left| \sigma_x + \sigma_y \right|^m + \frac{\sigma_b^m}{\tau_s^m} \left| (\sigma_x - \sigma_y)^2 + 4\sigma_{xy}^2 \right|^{\frac{m}{2}} \\ & + \left| \sigma_x^2 + \sigma_y^2 + 2\sigma_{xy}^2 \right|^{\left(\frac{m}{2}\right)-1} \left\{ -2a_{H90}(\sigma_x^2 - \sigma_y^2) + b_{H90}(\sigma_x - \sigma_y)^2 \right\} = |2\sigma_b|^m \end{aligned} \quad (1.28)$$

where the Hill 90 parameters a_{H90} and b_{H90} are defined by

$$a_{H90} = \frac{1}{4} \left[\left(\frac{2\sigma_b}{\sigma_{90}} \right)^m - \left(\frac{\sigma_b}{\sigma_0} \right)^m \right] \quad \text{and} \quad b_{H90} = \frac{1}{2} \left[\left(\frac{2\sigma_b}{\sigma_0} \right)^m + \left(\frac{\sigma_b}{\sigma_{90}} \right)^m \right] - \left(\frac{2\sigma_b}{\sigma_{45}} \right)^m \quad (1.29)$$

where σ_b is the uniform biaxial yield stress, σ_0 and σ_{90} are the yield stresses along the anisotropic axes, and m is the non-quadratic yield exponent. The yield exponent for this formulation will vary for different materials and is loosely coupled to the r value. In general, a material with a $r > 1$ will have $m > 2$ whereas for $r < 1$, $1 < m < 2$ will hold.

1.3.3.3 Barlat's Non-Quadratic Yield Function

An alternative non-quadratic yield function is the Barlat criterion, proposed by Barlat and Lian [34] in 1989 in an attempt to formulate a material model which would accurately predict the anisotropic behavior of materials with a r value less than 2. Under conditions of plane stress it has the non-quadratic form,

$$\Phi = 2\bar{\sigma}^M = a|K_1 + K_2|^M + a|K_1 - K_2|^M + c|2K_2|^M \quad (1.30)$$

where the parameters, K_1 and K_2 are defined as:

$$K_1 = \frac{\sigma_{xx} + h\sigma_{yy}}{2} \quad \text{and} \quad K_2 = \sqrt{\left(\frac{\sigma_{xx} - h\sigma_{yy}}{2}\right)^2 + p^2\sigma_{xy}^2} \quad (1.31)$$

The material parameters, a , c , h and p can be determined using the Lankford coefficients, r_0 , r_{45} , and r_{90} as described in [28,34]. Based upon crystalline plasticity considerations for face centered cubic (FCC) materials, Barlat determined that a value of $M = 8$ is appropriate, while for body centered cubic (BCC) materials a value of $M = 6$ is appropriate based on crystalline plasticity calculations.

Figure 1.9 plots the yield locii generated utilizing the von Mises, Hill 48 and 90, and Barlat 89 yield criterion in the biaxial tensile region under plane stress conditions. The locii were plotted in Figure 4.9 (d) with a Lankford coefficient of 0.7. The exception is for the von Mises locii, which by definition has a r value of 1. The remaining plots indicate the effect of r value on the yield locii for the Hills 48 and 90, and Barlat 89 criterion, Figures 1.9 (a), (b), and (c), respectively. Increasing the r value results in a tendency for the yield surface to expand, increasing the level of stress the material can carry before yielding occurs. The greatest variations are seen in the plot of the Hills 48 criterion.

1.4 Limiting Draw Ratio

The cup drawing process has been used for years as a measure of sheet formability. One of the most commonly used measures is the limiting draw ratio (LDR), the ratio of the largest blank diameter which can be successfully drawn to the punch diameter, as shown in Figure 1.10. A material having a large LDR is said to display good drawing formability or drawability. Material behavior in an LDR test is a complex function of a number of process variables including lubrication, blank thickness, binder force, punch velocity, and tooling geometry.

The International Deep Drawing Research Group (IDDRG) has invested considerable effort in the study of general deep drawing processes. It has only been in the last ten to fifteen years where their efforts have shifted to consider the use of aluminum in these processes. An analytical study was performed by Chu [35] which outlines a procedure that can be used to predict the material deep drawing behavior, incorporating anisotropy and considering shells of revolution. While this, and similar studies [7,8,9,10], provide valuable insight into the mechanics of the process examined, they do not provide an effective method to predict the material performance for the complicated range of shapes required in automotive stampings.

The circular cup drawing process used in limiting draw ratio (LDR) experiments considers a circular blank placed in the tooling geometry, as depicted schematically in Figure 1.10. The binder ring presses downwards, restraining the sheet material through friction as it is drawn into the die cavity by the motion of the punch. Thus, the clamping action of the binder prevents the material from wrinkling as the cup is formed. The binder pressure can be used to control whether the cup fails through fracture at the punch profile radius or through excessive wrinkling. Excessive binder pressure will cause fracture whereas too little pressure will cause wrinkling [36], as depicted in Figure 1.11.

1.4.1 Mechanics of Lubrication in Cup Drawing

There are several primary areas to be lubricated in a cylindrical cup deep drawing process. These areas of interest were outlined by Wilson [19] and are indicated in Figure 1.12 as follows

1. Blank/Die and Blank/Binder Interfaces
2. Blank/Die Corner Interface
3. Blank/Punch Interface
4. Blank/Punch Head Interface

In these regions, the thickness of the lubricant layer controls which lubrication regime will occur in the forming process and is therefore of great importance. The binder region is generally considered to involve mild surface interaction. The binder is closed on the blank, initially causing lubricant to be squeezed out of this region, although a residual film will remain. As the drawing process starts, the residual film will be wiped inwards and will thin due to the materials tendency to thicken upon drawing. Under these conditions, the lubricant tends to thin out the most near the inner radius however a relatively thick film can be maintained throughout the process. In the case of solid lubricants, the conditions in the binder region are relatively mild and the lubricant thickness is generally not significantly effected.

Wilson [19] described the die radius and wall as a simple thick film lubrication region whereas the blank/punch head interface was found to be far more complex in terms of its tribology. Initially the lubricant is squeezed from the punch head region, as occurred in the binder region. As the blank begins to deform, the lubricant, which becomes entrapped by the asperities, will be transported around the punch nose profile radius. A cycle is established in which the lubricant affects the deformation while the deformation is dependent on the lubrication. Perhaps due to the complexity, no analytical solutions have been found which describe the frictional behavior in this region.

1.4.2 Ironing

The deep drawing process causes the material to plastically deform with positive first and negative second principal strains. The second principal strain acts in the hoop direction whereas the first principal strain acts radially. This strain state causes the material to thicken as it is drawn into the die cavity. As a result, it is possible for the material thickness to exceed the punch-die clearance when it is drawn into the die cavity leading to a process known as ironing in which the material is squeezed into the die cavity as illustrated in Figure 1.13. The ironing process in cup drawing can also be initiated at wrinkling sites on the cup. In the case of an automotive panel forming process, ironing results in a defective component, the defect being in the form of an unsuitable surface finish in the painted part.

Figure 1.13 depicts the ironing process occurring at the interface between the cup wall and the die profile radius. Under these conditions the mechanical forming process is significantly altered and the lubrication regime breaks down due to the inability of the entrained lubricant to pass through the clearance. This results in vastly different frictional characteristics [19] than occur in the majority of the forming process.

1.4.3 Blank Holder Force

The applied blank holder force (BHF) has a tremendous impact on the sheet metal forming process. Blank holder forces act to (i) suppress wrinkling of the sheet and (ii) to apply surface tractions, through friction, which stretch the material thereby controlling flow into the die cavity. Increases in the BHF will result in increased thinning in addition to decreasing the degree of wrinkling in the sheet. A BHF level exists above which the formation of wrinkles in the component can be kept below an acceptable level. The BHF must, however, remain low enough to limit the thinning of the material, thus avoiding fracture. The purpose of the binder is therefore to reduce or prevent the formation of these wrinkles. The increase in the tensile force may also

occur if ironing is allowed by either the material exceeding the tooling clearance or at sites initiated when excessive wrinkles have to be removed from the component.

1.4.4 Blank Thickness

Closely coupled with the BHF is the material thickness. An increase in the material thickness has the beneficial advantage of increasing its resistance to wrinkling. However, an increase in material thickness also results in increased material weight and cost of the component. Increased thickness has also been shown to have the effect of increasing the formability of the material, as determined through limiting dome height tests [37] and to influence the development of strain distributions within the components [38].

1.5 FEM in the Simulation of Sheet Forming

A wide variety of analytical techniques have been applied to the prediction of metal forming processes. These techniques include slab analysis, the slip-line field method, and the upper- and lower-bound techniques. While these methods allow the prediction of general deformation, they are typically limited to relatively simple geometries and often require significant restrictions in their assumptions. Since the introduction of the finite element method (FEM) in the 1960s, methods have been developed to allow the accurate prediction of strains and material failure behavior in complex forming operations.

The development of FEM techniques applicable to the prediction of strains and fracture in sheet forming operations was reviewed by Williams [27] and Lamontagne [28]. These reviews focused upon what was required of the code to provide acceptable predictions, the relevant friction and contact boundary conditions, the constitutive models, and the factors affecting choice of time integration scheme (implicit versus explicit). A review of the trends in FEM simulation of metal forming is also given in those theses and reflects the state of development in FEM techniques up until the early 1990s.

The development of FEM tools for modeling sheet metal forming operations has continued to advance at a rapid rate, primarily due to the high level of interest, and hence funding, provided by the automotive industry. Owen *et al* [39] recently reviewed the state of computational strategies for 3-D metal forming simulations and highlighted the heated academic debate over the use of implicit versus explicit codes. Implicit codes are severely limited in their application towards large scale industrial forming problems due to the need to form and solve large systems of highly non-linear equations. However, they are generally regarded as more accurate due to the fact that modifications to the loading, and material characteristics do not have to be made to result in acceptable computational costs. The recent Numisheet '96 conference highlights the efforts being made in both camps; the first invited paper [40] shows the advances being made for implicit solvers, whereas the second [41] demonstrates the efficiency and robustness of the explicit implementation. The debate over this issue is likely to continue well into the next century as computing resources increase and improvements are made to the implicit solvers making their use in industrial forming problems a realistic possibility.

1.5.1 FEM Applied to the Cup Drawing Process

In the last ten years the use of the FEM in predicting sheet forming has increased considerably. This section reviews some work done with FEM simulations specifically concentrating on the cylindrical cup drawing process. The effect of material parameters, constitutive formulation, tooling geometry, and friction is presented.

Logan *et al* [30] examined the effect of anisotropy on the cup drawing process, where it was determined that the choice of yield criterion had strong impact on the predictions. Investigations into the numerical and experimental prediction of deep drawing were performed recently by Saran *et al* [42]. This involved the use of Al sheet as well as a number of other engineering materials utilizing a Hills 48 formulation. The results of this study demonstrated the large influence n , k , and μ have on the predicted

punch forces while the Lankford coefficient, r , was found to have only small influence on the predictions.

The choice of constitutive formulation was shown by Hosford [30] to be one of the most significant factors in the assumptions made in modeling sheet metal forming. He further indicated that the use of the Hills 48 criterion overestimated the effect of r on the predictions of LDR. This criterion, while suitable for steels, is questionable in its applicability towards low r materials such as aluminum. It has only been recently [44,45,46] where advanced constitutive formulations have been applied in efforts to capture the forming behavior of low r materials such as aluminum. Experimental validation of these new formulations have only just begun to be published. Therefore, there exists a requirement to investigate the ability of non-quadratic yield criteria to predict the material behavior of low r materials under sheet forming conditions.

Hayashida *et al* [43] performed a study to evaluate the six component anisotropic yield criterion developed by Barlat *et al* [44]. In this work Hayashida *et al* discussed the effect of yield surface shape on formability. A cup drawing investigation was also performed which demonstrated that this criterion provided a reasonable description of the plastic behaviour of an aluminum alloy. Additional investigations involving cup drawing and the use of this criterion have been performed by Chung and Shah [45], and Shah and Dick [46]. The results of both studies indicated that the new criteria provided better agreement with experiment than either the Hills 48 or von Mises criteria. The author of this thesis has also presented studies [47,48] demonstrating that the three-parameter Barlat 89 criterion provides better predictions than those obtained using either the Hills 48 or the von Mises criteria.

The effect of tool clearance [49] in cylindrical cup drawing was investigated for a low carbon steel using Hill's 1948 yield criterion. The results of this study demonstrated that an optimal tooling clearance exists for a cup drawing operation, above which excessive wrinkling occurs and below which excessive ironing occurs. Darendeliev and Altan [50] considered the effect of friction on FEM simulations of the cup drawing process. They concluded that small variations in the friction coefficient do

not effect the deformation appreciably as long as the material is not close to its stability limit. When this limit is approached small variations in the friction coefficient were shown to alter the point at which failure occurred as the result of excessive thinning.

1.6 Current Work

The goal of the current research is to develop an accurate numerical model which can be used to predict the cylindrical cup drawing process and to assess the predictions of non-quadratic yield criteria for aluminum automotive sheet. The numerical model is to be used to predict the limiting draw ratio for a series of ten tooling geometries.

The development of the model and its predictions were undertaken in parallel with experimental studies conducted at ALCAN Kingston Research and Development Center (KRDC) and MTL/CANMET as part of a collaborative research arrangement. Thus, the contribution of Carleton to the research was in FEM simulations of the cup drawing process and making experimental strain measurements for use in the validation studies. The experiments consisted of parametric variations of the punch and die profile radii at CANMET and KRDC, respectively. The results of these experimental studies, in the form of punch force, draw-in, and strain distributions were used to assess the numerical predictions of LDR. In addition to these process conditions, the effect of yield function on the predictions was also investigated.

Six tooling combinations were manufactured and corresponding experiments were performed to facilitate the numerical validation study. The experimental study is described in Chapter 2 while the numerical models are described in Chapter 3. The results of the numerical study as well as the model validation details are given in Chapter 4. Additional parametric studies to examine the influence of constitutive model, blank holder force, and friction coefficient have also been included. Discussion of the results, conclusions, and future work to be considered can be found in Chapter 5.

Chapter 2

2. Experimental Programme

Cylindrical cup drawing tests were performed on a commercial aluminum alloy sheet material as a means of determining the limiting draw ratio (LDR). This research project considered ten different tooling geometries as shown in Table 2.1. This table further indicates that of the ten geometries considered, experiments were performed on six. These tests were done in two series, the first at the ALCAN Kingston Research and Development Center (KRDC), consisting of four tooling geometries, and the second at the Materials Forming Technology Laboratory of CANMET, consisting of three tooling geometries. One tooling geometry was duplicated by each facility to provide a controlled comparison of the two sites.

The tooling geometries were selected to provide insight into the effect of moving from a primarily drawing process, in the case of large tooling radii, to a process in which the bendability of the sheet material becomes more significant. Tests were conducted on circular blanks of increasing diameters to determine the LDR for a given tooling geometry. Tests were also performed on several tooling geometries at their critical blank diameters using gridded specimens. These tests provided a means of experimentally determining the

strain distributions. Interrupted tests were performed at various punch depths to obtain strain history data for the process.

2.1 Material Selection

A commercial aluminum alloy, AA5754-O, was considered. This aluminum alloy is a solution strengthened, work hardening material which has become a leading candidate for use in structural automotive applications. The uniaxial properties and chemical composition of this material are given in Tables 2.2 and 2.3, respectively. The aluminum sheet was supplied by ALCAN KRDC. The blanks were all cut from the same coil (coil # 11341PAA1) which provided for uniformity of the specimen material properties.

2.2 Material Characterization

The mechanical properties of the 5754-O aluminum alloy were determined using tensile tests performed at the mechanical testing laboratory at ALCAN KRDC. Three tensile specimens were cut for each of the 0° or rolling direction (RD), 45° and 90° from the RD as shown in Figure 2.1. The uniaxial tensile tests were performed in an Instron Series 9 Automated Testing System H8910 with an Instron 4200 Controller and conformed to ASTM E517 [51] and E646 [52]. The average tensile test data is shown in Table 2.4 for the 0°, 45°, and 90° sheet directions. Table 2.5 summarizes the material properties of an equivalent planar isotropic material. The nominal gauge thickness was 1.45 mm whereas the average sheet thickness was 1.49 mm $\pm$ 0.01 mm based on measurements at several locations.

The true stress-strain curves for the 5754 alloy, as determined at KRDC using a hydraulic bulge test on a 1.6 mm nominal sheet thickness, is shown in Figure 2.2. This alloy, as indicated in Table 2.3, has a high magnesium content resulting in the formation of magnesium rich atmospheres within the alloy. During deformation these mobile solute atmospheres effectively pin dislocation movement resulting in the serrated flow observed in the stress-strain response [53,54,55].

Forming limit diagram (FLD) data was provided by ALCAN KRDC for the 1.45 mm gauge AA5754-O sheet material [56]. The experimental procedures by which this data was obtained have been clearly outlined in reference [57]. The FLD data has been plotted in Figure 2.3. The diagram inset in the figure shows how the forming limit ratio (FLR), a measure of the proximity to forming limit, is defined.

2.3 Limiting Draw Ratio Test

The experiments were designed to investigate the effect of tooling geometry on the cup drawing process. Figure 2.4 shows a schematic of the tooling geometries considered. In the case of the experimental work done at ALCAN KRDC, the focus was to determine the effect of die profile radius on the limiting draw ratio. In contrast, the experiments done at CANMET concentrated on the effect of the punch profile radius on LDR.

2.3.1 Specimen Preparation

AA5754-O sheet material of 1.45 mm nominal gauge thickness were machined into circular blanks of various diameters, as summarized in Table 2.6. This range of blank sizes was selected based on previous research performed by ALCAN [58]. A typical undeformed blank is shown in Figure 2.5. Cups produced with gridded blanks were used to determine experimental strain distributions along the cup profile.

With the exception of a series of blanks used to study the effect of the solid film pre-lubricant A070, all blanks were washed with clean water and ethanol prior to testing. The silk screened blanks used in the strain measurement studies were cleaned prior to application of the square 2.79 mm grid. In order to ensure a uniform lubrication of the sheet, a thin Teflon film was used during the drawing process.

2.3.2 ALCAN KRDC Experimental Setup and Procedures

Experimental data for this study from KRDC consisted of process data including punch displacement and force data, as well as flange draw-in as a function of punch

displacement for fully drawn cups. The limiting blank size, and hence the LDR values were determined for the four tooling geometries considered.

2.3.2.1 Experimental Setup

The formability press facility at KRDC was used to conduct the cylindrical cup drawing tests with various die sets. Four die profile radii were considered, these being 12, 9, 6, and 3 mm, indicated as the R2 dimension in Figure 2.4. A single 150 mm (5.9") diameter punch with a 12 mm profile radius (Figure 2.4, dimension R1) was used.

The cylindrical cup drawing tests were conducted on a servo-hydraulic test system consisting of two concentric actuators, as shown schematically in Figure 2.6. Figure 2.7 is a photograph of the experimental setup at KRDC. The first actuator is a 500 kN inner punch actuator with a stroke of 254 mm with the second being a 445 kN outer clamp actuator with 25.4 mm stroke. The lower die is mounted on the outer clamp and utilizes two LVDTs for displacement and one differential pressure transducer for load measurements. The upper die is mounted on a movable cross head, as shown in the figure, allowing the specimen to be observed as it is drawn upwards by the punch actuator.

The test system has a fully digital closed loop control capability through the use of two interconnected Instron 8500 servohydraulic controllers. This system allows tests to be conducted with the punch and binder under either position or load control. In the LDR experiments, the binder was under force control, while the punch was under position control with a fixed test velocity. The data logging is carried out by a PC based data acquisition and analysis software interfaced with the test system.

Additional flange draw-in information was obtained through the use of an LVDT located in the plane of the flange between the die and the binder. This LVDT, with a displacement range of 0 mm to 101.6 mm, was mounted to the outer surface of the binder and interfaced with the data acquisition system used for the test.

2.3.2.2 Experimental Procedures

The actual cup drawing is performed after the die and punch have been cleaned of any material debris left by previous tests. A 0.127 mm (0.005") Teflon sheet is placed on

the binder, followed by the specimen. The specimen is centered with the aid of a clear alignment template and a second layer of Teflon is applied to the top surface. The die crosshead is then lowered into position within the 25.4 mm stroke of the outer binder actuator. Force is then applied to the binder actuator up to the desired level, 60 kN in the case of this study, at which point the motion of the punch can commence. The punch motion occurred at a constant value of approximately 76 mm/min. (1.27 mm/s) for all specimens. Process information such as flange draw-in, punch location and velocity, and binder force were all recorded by the data acquisition system.

A rapid method of estimating LDR, developed by ALCAN KRDC, was used to narrow down the possible maximum blank diameters. In this procedure two undersized blanks are fully drawn at sufficient binder force to prevent wrinkling, and the maximum punch loads are recorded. A further test is conducted with an oversized blank for which fracture occurs. The peak punch force obtained for the fractured blank is then used to define the fracture force for the specimen. A line joining the peak punch forces for the undersized specimens is then extended to intersect this horizontal fracture line as shown in Figure 2.8. Where the lines intersect is the estimated upper blank diameter. Tests are then performed at the selected blank size closest to the value of the intersection. Tests are also performed at one size above and one below this value using diameter increments of 6.4 mm (1/4"). The LDR is then calculated based on the largest blank which does not fail.

2.3.3 CANMET Experimental Setup and Procedures

Experimental data for this study from CANMET consisted of process data including punch displacement and force data for fully drawn as well as interrupted specimens. The interrupted specimens have been used for the purpose of verifying the numerical model used to predict LDR values. The limiting blank size, and hence the LDR values were determined for the three tooling geometries considered.

2.3.3.1 Experimental Setup

The Forming Test facility at CANMET was used to conduct the cylindrical cup drawing tests with various punch sets for verification of the numerical models. Three punch profile radii were considered, these being 12, 9, and 6 mm, indicated as the R1 dimension of Figure 2.4. A single 6 mm profile radius die was used during the experiments (Figure 2.4 dimension R2). The press facility is shown schematically in Figure 2.9 and is pictured in Figure 2.10. The tooling consists of three punch head inserts of 150 mm outside diameter with the given profile radii. The die plate had a cavity diameter of 153.84 mm resulting in a clearance of 1.92 mm between the die and punch, identical to that used at ALCAN KRDC.

The press at CANMET, as shown in Figures 2.9 and 2.10, has clamping and pressing actuators with capacities of 55 and 80 metric tons, respectively. The punch cylinder has an operating stroke of 150 mm and can operate in the speed range of 0.1 to 100 mm/s. This type of forming operation is typically tested in the range of 1 to 2 mm/s. The experimental data is recorded by a Macintosh IIfx computer system and includes punch velocity, punch force, and binder force. The press is controlled by an open loop system in which the punch velocity and binder force are pre-set for a given forming operation. These parameters are then recorded through the use of an LVDT for the displacement and velocity of the punch and by a pressure transducer for the binder force.

2.3.3.2 Experimental Procedures

The cup drawing performed at CANMET was done after the die and punch were cleaned with alcohol and emery paper of any debris left by previous tests. A 0.127 mm (0.005") Teflon sheet was placed on the die, followed by the specimen. The specimen was carefully centered relative to the die with the aid of a scale and a second layer of Teflon is applied to the top surface. The binder is then lowered into position and a force is applied. The actual force used was varied in the range of 53 kN to 66 kN. Once the binder is in place, the motion of the punch begins at a value of between 1 and 6 mm/s for all of the specimens. Process information such as punch location and velocity, and binder force

were all recorded by the data acquisition system. Table 2.7 summarizes the specimens and actual test conditions.

In addition to determining the LDR blank size, several interrupted runs were performed providing information on strain history throughout the drawing process and used to validate the numerical models. One further aspect which was considered was the effect of leaving the A070 pre-lube on the blanks, in addition to the Teflon lubricant, on the LDR values.

2.4 Experimental Strain Measurements

A video-based image acquisition system, developed at Carleton University, was used to determine the strain distribution in the formed cups. This system makes use of a square grid measurement technique [17]. The grid image is acquired by a contact video probe which incorporates a Panasonic micro CCD camera. The image is captured by an 8-bit gray scale Imaging Technologies PC Vision Plus frame grabber and is stored as a digitized 480 x 640 pixel image. The corner locations of the deformed grids are then manually selected and compared with an undeformed calibrated set of points. The strains are then calculated as recommended by Sowerby [17].

The image of a particular deformed grid typically accounted for 80 and 60 percent of the vertical and horizontal image size, respectively. Based on a series of measurements taken of a series of test grids, the accuracy of the strain measurements was determined to be on the order of 1-2 percent strain. These tests accounted for systematic variations caused by the manual selections of grid corner locations.

Chapter 3

3. Numerical Models

Numerical simulation of the cylindrical cup drawing process was undertaken using the LS-936 version of the explicit finite element code LS-DYNA3D [59]. Analyses were run on SGI R4600 and R5000 Indis, an R8000 Onyx workstation, and a Cray J90 super computer.

LS-DYNA3D employs a central difference method of explicit time integration which is well suited for vectorization and parallelization. Unlike implicit codes, in which a large system of global equations must be solved simultaneously, the explicit formulation provides a significant advantage in terms of storage requirements. LS-DYNA3D has a well developed contact formulation [60,61] providing an effective method of dealing with the intermittent contact which occurs during sheet forming operations.

A disadvantage of an explicit formulation is the small time step, as governed by the Courant criterion, required for stability. The time step size for an explicit solution is typically between 100 to 1000 times [60,62] smaller than that of a corresponding implicit

solution. This timestep is approximated by the time required for a stress wave to traverse the shortest characteristic length of the smallest element and is related to the highest natural frequency of the system.

Typically metal forming operations occur over a period of several seconds. Such durations are quite long for explicit dynamic simulations and result in large CPU costs due to the small time step. In order to combat the excessive CPU costs associated with real time simulations, forming simulations are typically run with artificially high tooling velocities. In general, sheet forming operations are quasi-static, operating at velocities in the range of 0.1 to 10 mm/s. The punch velocities in the current simulations were artificially raised to a peak value of 40 m/s, several orders of magnitude greater than the 2.0 mm/s used in the experiments. This value, while artificially high, did fall below the practical upper limit [63] of five percent of the wave speed of the sheet material required to avoid inertial effects. This guideline should be observed for all but the simplest geometric conditions. Caution must still be used to prevent unrealistic inertial effects from distorting the solution, and hence to ensure a realistic answer is obtained.

3.1 Finite Element Mesh

The numerical models were generated using I-DEAS Master Series 2.1, a commercial engineering software package [64], based on drawings provided by ALCAN KRDC. Figure 3.1 is an exploded view of the finite element mesh describing the 6 mm die and punch profile radii tooling with a 317.5 mm (12.5") blank. The tooling surfaces of the die, binder, and punch were modeled using rigid, four-node quadrilateral shell elements. As is shown in the figure, one quarter of the geometry was modeled, taking advantage of symmetry to reduce the computational expense.

The number of elements and nodes used for each of the blanks are tabulated in Table 3.1. The table shows that the number of deformable elements ranges from 3379 to 4819 between the smallest and largest blanks, respectively. Figure 3.2 is a series of mesh plots representing the range of blanks used in the LDR simulations. This figure clearly shows the similarities of the various blank meshes. The finite element mesh for the blank

comprises three regions. The outer, uniform circular section, begins at a radius of 60 mm and extends to the blank periphery. The elements in this region have been biased to carefully control their aspect ratio. The aspect ratio used was the result of preliminary studies [47,48] which determined the value to ensure that the elements are square when they transit the die profile region. The inner two regions are identical in all ten meshes, with an inner 15 x 15 element square region biased away from the center, and a thin, free meshed region to facilitate a smooth transition from the square inner to the circular outer region. Appropriate symmetry boundary conditions were applied along the sides of the quarter blank in the x-z and y-z planes.

The number of rigid tooling elements and nodes in the various tooling components considered, as well as the number of elements around the tooling radii, are summarized in Table 3.2. The tooling components were interchangeable and consist of 3, 6, 9, and 12 mm punch profile radii models, a binder ring model, and 6, 9, and 12 mm die profile radii models. The punch, binder, and die meshes can be seen in Figures 3.3, 3.4, and 3.5, respectively. The tooling and blank meshes were then combined to produce a total of sixty one geometrically unique finite element models. The number of rigid tooling elements ranged from 3864 to 4194 for the various geometries considered.

3.2 Element Formulation

A comparative study of the various shell element formulations available in LS-DYNA3D was undertaken by Galbraith and Halquist [65]. From this study the Belytschko-Lin-Tsay [61,66] element formulation emerged as providing the best balance between computational efficiency and the accuracy provided by a fully integrated shell formulation. Therefore the aluminum 5754-O blanks were modeled using four-node quadrilateral, Belytschko-Lin-Tsay, shell elements with a thickness update option.

The bilinear, four node, Belytschko-Lin-Tsay element uses a co-rotational coordinate system which accounts for the elements efficiency. A local element spatial coordinate system is embedded on the reference surface providing a greatly simplified co-rotational system. This approach satisfies the frame invariance of the constitutive

relation [67] by automatically accounting for the rigid body rotations in the element formulation.

3.2.1 Thickness Update Option

A thickness update option, developed by Hughes and Carnoy [68], was used to incorporate sheet thinning effects. Through-thickness variations are captured in shell elements by using a series of 5 through-thickness integration points. Trapezoidal integration is used to integrate the stress over the element thickness after the constitutive model has been evaluated at each integration point. In this procedure, the mean through-thickness strain component is then applied to the nodal fibre lengths to determine the current shell thickness. The need to accurately capture thickness changes and their effect on the contact forces around the sharp radii and in the flange region were outlined by Mattiasson *et al.* [69]. Thickness variations in the cylindrical cup drawing process are particularly important as they can result in intermittent contact of the flange in the binder region.

3.2.2 Hourglass Control

Modes of deformation which do not cause stress at the central integration point result in a phenomenon known as hourglassing. This numerical effect is a by-product of using a single point in-plane integration scheme and is shown graphically in Figure 3.6. The first three modes of deformation correspond to rigid body movement of the element and do not adversely affect the solution. The fourth, kinematic mode, can cause the solution to oscillate and in some cases results in the formation of singularities, impeding or preventing the solution.

The default LS-DYNA3D hourglass control technique due to Flanagan and Belytschko [67] was used to prevent these kinematic modes. This technique introduces an artificial damping term into the system which acts orthogonally to the normal deformation modes and, as such, has little or no influence on the rigid body modes of deformation.

Hourglassing can also be controlled by a selective choice of mesh definition. If the mesh can be altered so as to prevent the excitation of the hourglass deformation modes, hourglassing problems can be reduced. In this study hourglassing did not present itself as a significant problem and the default hourglassing control strategies were sufficient to provide a stable solution.

3.2.3 Displacement and Force Boundary Conditions

Figure 3.7 shows the cylindrical cup drawing process schematically, showing the initial tooling locations and the direction of movement. The cup drawing process consisted of applying the clamping force to the binder, followed by moving the punch through its range of motion, while fixing the die as shown in the figure. The motion of the rigid tooling was controlled through the application of appropriate force and displacement boundary conditions.

The die was held fixed by nodal constraints in the x, y, and z translational and x, y, and z rotational degrees of freedom. Both the punch and binder were constrained so as to allow only movement in the z direction, which corresponds to the direction of the punch axis. The binder motion was controlled by an applied force of one quarter the experimental value in the negative z direction, and was additionally subjected to a rigid body stopper constraint as described in Section 3.4.3. The punch motion was specified using a sinusoidal velocity profile for the duration of its motion in order to limit inertial effects. This velocity profile, as recommended in prior research [27,28,60], is plotted for a typical LDR loading case in Figure 3.8. The effect of the sinusoidal velocity curve is to smoothly accelerate and then decelerate the punch to minimize the inertial effects over the course of the punch stroke. A full series of runs has been performed [70] on an individual finite element model using various draw times to a depth of 145 mm, allowing the effect of the accelerated punch speed on the predictions to be determined. Based on these simulations, a peak punch velocity of 40 m/s was established as an upper limit to prevent inertial effects from affecting the solutions.

Figure 3.8 is a plot of a typical load curve used to describe the motion of the punch, to a depth of 150 mm, and the applied binder force of 60 kN. The motion of the punch was delayed by 0.1 ms to allow the binder force to be engaged and stabilize. The various velocity profiles used in this study are summarized in Table 3.3.

3.2.4 Mesh Convergence

A mesh convergence study was undertaken to determine the effects of various aspects of the finite element mesh on the numerical predictions, prior to beginning production runs for the LDR project. In addition to the mesh convergence study, the effect of the accelerated punch speed on the predictions was studied. Comparison with experiment [47,48] has demonstrated a reasonable agreement between the predicted and measured punch forces in the cup drawing process and in predicted strains within the cup wall. Based on the results from the convergence study the current mesh and punch velocity profile were determined to be acceptable.

3.3 Constitutive Model

As described in Section 2.2, the material properties of the Aluminum 5754-O sheet were determined by a combination of bulge testing, for the stress-strain data, and tensile testing, for the anisotropy parameters. These properties were then incorporated into the material model used in the simulations and have been summarized in Table 3.4. A power law hardening uniaxial stress-strain equation,

$$\bar{\sigma} = k(\epsilon_o + \epsilon_p)^n \quad (3.1)$$

was adopted in which $\bar{\sigma}$ is the flow stress, ϵ_o is the strain to cause first yield, and ϵ_p is the plastic strain. Hardening parameters, k and n , were obtained from regressions [71] on data for 1 mm thick 5754-O sheet. The regressions were performed on the natural logarithm of equation (1),

$$\ln \bar{\sigma} = \ln k + n \ln (\epsilon_o + \epsilon_p) \quad (3.2)$$

The regression results are given in Table 3.5 and are plotted in Figure 3.9 against a polynomial fit to the stress strain data.

The majority of the LDR simulations have been performed using the Barlat [34] constitutive model as implemented in LS-DYNA3D as material model Type 36. Selected simulations also considered alternative quadratic yield formulations, namely von Mises and Hill-1948 [31] to assess the impact of yield criteria on predicted strains.

For plane stress conditions, the Barlat [37] yield criterion can be written as described in Section 1.3.3.2 as

$$(1.32) \quad 2\bar{\sigma}^M = a|K_1 + K_2|^M + a|K_1 - K_2|^M + c|2K_2|^M$$

where

$$(1.33) \quad K_1 = \frac{\sigma_x + h\sigma_y}{2} \quad \text{and} \quad K_2 = \left[\left(\frac{\sigma_x + h\sigma_y}{2} \right)^2 + p^2 \tau_{xy}^2 \right]^{\frac{1}{2}}$$

Barlat demonstrated from crystallographic plasticity considerations that $M=8$ is an appropriate choice for FCC materials. The anisotropic material parameters a , c , h , and p are determined from the Lankford coefficients R_0 , R_{45} , and R_{90} as described in Section 1.3.3.2. These coefficients are defined as the ratio of thickness to width strains in the tensile specimens taken at orientations of 0° , 45° , and 90° relative to the rolling direction of the sheet.

3.4 Contact Modeling

Penalty function-based contact interfaces, known as slidelines, were used to enforce the intermittent contact and sliding boundary condition between the sheet metal and the tooling elements. Thickness-based contact was used in which separate reference surfaces, corresponding to the top and bottom of the sheet, are used to enforce contact with the tooling. This contact option is more expensive computationally, compared to a

membrane contact formulation, but is necessary for drawing operations to simulate local variations in clamping pressure due to sheet thickening and ironing. The LS-DYNA3D automatic contact generation option was utilized to generate this contact interface.

Three contact interfaces were defined between the punch, die, and binder and sheet. From these interface definitions, the resultant force history on the binder and punch was extracted for each of the simulations.

3.4.1 Contact Formulation

The efficient contact algorithms available for explicit finite element formulations are one of the most significant advantages possessed by these codes for sheet metal forming simulations. The intermittent contact between the tooling and sheet metal surfaces is modeled through the use of a penalty stiffness method. Contact is enforced by assuming that no penetration of one surface is permitted by the nodes of its adjacent surface. In the event of penetration, numerical springs (penalties) are introduced into the system to move a penetrating node back to the contact surface. These artificial springs are placed between the nodes and the surface with a stiffness proportional to the bulk modulus of the penetrated surface. The interface stiffness, k_i of element i , is given by

$$k_i = \frac{\xi_i K_i A_i^2}{V_i} \quad (3.3)$$

where ξ_i is the user defined interface stiffness scaling factor, K is the bulk modulus of the penetrated surface, and A and V are the face area and volume of the penetrated element, respectively. The depth of penetration, interface scale factor, and the interface stiffness all combine to determine the magnitude of the force exerted to restore the node to a position above the contact surface.

The spring force is applied as long as penetration is detected and is removed as soon as penetration ceases. In this manner no external force is added to the system and energy is conserved. The penalty stiffness method is well behaved in terms of not exciting hourglassing modes, when compared to other contact treatments. If hourglassing is initiated as a response to the contact treatment, the stiffness can be scaled

down to effectively soften the interface. This interface scaling was used by Williams [27] as an effective method of preventing and controlling the hourglassing response of a limiting dome height (LDH) model.

3.4.2 Coulomb Friction Model

Coulomb friction was also modeled for the interfaces using a single constant coefficient for both the static and dynamic coefficients of friction. This friction coefficient represented the frictional behaviour obtained when a Teflon sheet was inserted between the blank and the tooling surfaces for lubrication. The standard Coulomb friction formulation was imposed between the contact interfaces of the sheet and the tooling surfaces. This formulation has the following form

$$f = \mu |f_n| \quad (3.4)$$

where f is the frictional force, μ is the Coulomb coefficient of friction, f_n is the normal surface contact force determined by the contact algorithm. If the interface frictional shear force exceeds the shear strength of the material, the frictional force is set to the limiting tangential shear stress.

Lamontagne [28] conducted extensive testing into appropriate friction coefficients to be used in sheet forming simulations. Based on these results and suggestions from ALCAN KRDC, a friction coefficient of 0.05 was utilized in the preliminary convergence studies [47,48]. Following these studies it was decided to increase the friction coefficient to a value of 0.06. With the exception of parametric variations in friction, discussed in Section 4.4, a coefficient of 0.06 was held as a standard value throughout the simulations. While this constant coefficient does not take into account the complex tribological processes which occur during the sheet forming operation, it was felt that the solid Teflon lubricant would be adequately represented by a model of this form.

3.4.3 The Use of Rigid Body Stoppers

So called rigid body stoppers were imposed between the binder and die in order to limit any dynamic motion of the binder. This “stopper” limits the velocity of the binder

to 0.01 m/s during the ramped loading of the binder to the clamping force and then imposes a maximum velocity of 0.001 m/s between the binder and the die for the duration of the drawing process. In general, oscillation or “chatter” of the binder can result in the simulation localizing prematurely as the material is instantaneously pinned between the surfaces due to the “bounce” of the binder. In previous studies done at Carleton by Williams [27] and Lamontagne [28], a large point mass was added to the binder to reduce chatter. The use of the rigid body stopper has been found to have the same effect on the simulation and does not require the fine tuning required to determine an appropriate mass. The stopper method was also found to exert a constant force on the binder throughout the simulation as shown in Figure 3.10.

Chapter 4

4. Experimental and Numerical Results

This chapter presents the results of the numerical simulations of the cup drawing process and comparison with experiment. The numerical predictions and validation data are presented for representative set of tooling geometries. In addition, the predicted and experimental LDR values are presented for the ten tooling geometries considered. The results of a parametric study on the effect of blankholder force and friction coefficient are presented as well as an assessment of the effect of constitutive formulation on the numerical predictions.

4.1 CANMET 6 mm Die and Punch Tooling

The results for the 6 mm die and punch profile radii tooling are presented in detail because they provide a good representation of typical cup drawing simulation. A larger number of gridded specimens were tested using this tooling thus providing the greatest amount of experimental data for model validation. Strain history data was obtained

through the grid measurement technique on a series of interrupted cup tests. Figure 4.1 is a photograph of two typical formed cups produced in the LDR study at CANMET.

4.1.1 Predicted Strain Distributions

Fringe plots of predicted major and minor strain for the fully formed cup, from a 12 inch blank, are plotted in Figures 4.2 and 4.3, respectively. These figures indicate a relatively low strain level in the bottom surface of the cup. An increase in major strain accompanies the transition through plane strain which occurs around the punch profile radii. Both major and minor strain then show a relatively uniform increase in magnitude along the cup wall until the cup lip is reached. In the cup lip region, the distribution of major strain takes on an irregular form. This distribution is attributed to wrinkling of the material, as observed both numerically and experimentally, which occurs when the outer edge of the blank is released from the binder ring.

These figures also show the ability of the Barlat yield function to capture in plane anisotropic effects on the strain distributions. Subtle differences in the height at which strains occur in the cups can be observed by starting at a fringe boundary in the rolling direction (RHS of the quarter cup) and following this boundary around the cup to the transverse (LHS) direction. This material effect can also be seen by examining the fringe plots showing the strain predictions at an interrupted punch depth of 90 mm (Figures 4.4 and 4.5). Note that wrinkling has not occurred at this punch depth since the blank is still clamped by the binder.

A steep strain gradient is present in the major principal strains around both the die and punch profile radii. This gradient forms around the punch profile region in the early stages of the drawing operation and remains stationary around the punch profile radius throughout the process. This behaviour is indicated in plots of major principal strain for the 50 and 30 mm interrupted states in Figures 4.6 and 4.7, respectively. The steep gradient around the die profile radius, appears to migrate up the cup wall during the process as the cup is drawn into the die cavity, as indicated in these figures.

Figures 4.8, 4.9, and 4.10 are a series of fringe plots of predicted thickness for the 50 and 90 mm interrupted, and the fully drawn cup, respectively. The steep gradients around the profile radii are clearly seen at a punch depth of 50 mm. The ability of the Barlat constitutive model to capture in plane anisotropic effects is again noticeable in these plots. The original 1.48 mm thick material is predicted to thin over the bottom surface and up the cup wall. While the material thins in the wall region, the drawing in the flange region causes a considerable increase in the material thickness. As indicated in Figure 4.10 for the fully drawn cup, the material in the last three or four elements actually exceeds the tooling clearance of 1.92 mm. The current contact treatment and Coulomb friction model does not account for the complex behaviour which occurs at this late stage in the process. In the experiments, ironing of the cups was observed in the final stages of the drawing process, which would result in a much larger frictional force. The current penalty function-based contact treatment would not account for the onset of ironing.

Figure 4.11 is a fringe plot of the predicted forming limit ratio (FLR), a measure of proximity to forming limit, for a fully drawn 12 inch blank. As indicated by the plot, a peak FLR was found to occur around the punch profile radius and also in the mid-wall region of the cup. A comparison of this plot with the FLR predictions for the 50 mm interrupted simulations (Figure 4.12) provides some interesting insights. The peak value of FLR is very close in the interrupted state to that of the full cup. This corresponds to the experimental observation that, once a specimen was formed past a depth of 45 to 50 mm, the test could be continued without further fear of cup fracture. This behaviour indicates that a critical stage in the forming process occurs at an intermediate depth corresponding to development of the peak punch force. This can be seen in the punch force history presented in Section 4.2.2.

4.1.2 Principal Strain Locii

Figure 4.13 shows a typical strain distribution for a drawn cup. The figure plots locii of major versus minor strain along a line running from the center of the fully drawn cup around the die profile radius and up the wall in the rolling direction of the material.

The figure shows that stretching occurs at the bottom surface of the cup as seen in the lower right hand corner of the figure marked position A. The plane strain bending of the material around the punch radius is depicted by the sharp bend in the lower right corner of the figure indicated by position B. The distribution continues up the cup wall (deep drawn state) through the middle of the figure, position C. The cup lip is indicated in the top left corner of the plot, position D.

4.1.2.1 Effect of Constitutive Formulation

Use of the Barlat yield criterion was found to provide a significant advantage over both the von Mises and Hills 1948 criteria for low r materials. Figure 4.14 is a plot of predicted major and minor strains in the cup for the 6 mm die and punch profile radii tooling combination. Three constitutive models were compared and indicate improved predictions with the use of the Barlat constitutive model. The ability of the model to capture the behaviour towards the top of the cup wall is also evident.

Figure 4.15 plots the major and minor strains as a function of the distance along the cup outer surface measured from the bottom center. This plot clearly indicates that the stretch predicted in the bottom surface of the cup (0 to 69 mm) is substantially higher for the Hills representation than for either the von Mises or Barlat criteria. This can be explained by referring to Figure 4.16, which plots the predicted yield locii for a plane stress condition for the 5754-O aluminum alloy. The Hills surface is shifted towards the origin on the biaxial stretching side, relative to both the Barlat and von Mises, indicating that the predicted yield will occur in this model well before either of the other criterion. Thus, because the material yields earlier, more strain will occur in the stretched region for this representation. This effect is confirmed by examining the effect of constitutive model on the element thickness, shown in Figure 4.17. In this figure, the Hills and von Mises criteria show considerably less thinning over the punch bottom than the Hill 48 model.

4.1.2.2 Experimental Validation

Figure 4.18 shows a series of distributions of the type depicted in Figure 4.13. The predicted strain distributions are indicated by lines while the measured experimental strains are plotted as a series of individual points. The top left and right figures and the bottom left figure represent comparisons in the rolling, transverse, and compound direction (CD) (forty five degrees from the rolling direction), respectively. Examination of the measured strains in the rolling direction, as shown in the figure, indicates that the measured values in both the rolling direction and 180 degrees from the rolling direction were closely matched. These experimental distributions suggest that the press facility at CANMET was in good alignment during the process. Included on each of the graphs is the forming limit curve for the 1.45 mm sheet material.

The predicted distributions along the bottom surface of the cup and around the punch profile radius compare well with the measured strains. The simulations appear to slightly under predict the major strains in the die wall region when compared with the experimental values. This error is possibly attributed to a friction coefficient that was lower than occurred during the experiments. This error could result in the strains in the direction of punch movement being too low.

An alternative comparison of the principal strains serves to provide further insight into this feature of the model. Figure 4.19 plots the measured and predicted principal strains against the distance measured from the bottom center of the cup along the outer surface in the rolling direction. The distance from 0 to 69 mm corresponds to the bottom surface of the cup. The punch profile radius of the cup occurs from 69 to 78.5 mm, at which point the cup wall begins and continues for the remaining distance shown. This figure shows that the model does indeed under predict the major principal strain in the cup wall. It does however, capture the material behaviour quite well in its predictions of the minor strains in the wall. If the strains in the bottom surface are examined, one notices that the predicted strains are noticeably higher by about 4-5 percent strain. This indicates that the friction model is allowing too much movement to occur in this region. The material appears to be locked in place after the punch profile radii forms in the actual

experiments. Thus, in the experiments, the nature of the friction around the profile radius of the punch seems to reduce the stretching which occurs across the bottom of the cup.

The fourth plot in the bottom right of Figure 4.18 superimposes the simulation and experimental data from all three directions onto a single figure. This plot indicates that only a very small degree of anisotropy was detected either experimentally or in the numerical predictions.

4.1.2.3 Interrupted Tests

Interrupted experiments were conducted using a 305 mm blank with the 6 mm radius die and punch tooling at CANMET, with a binder force of 52 kN applied during the drawing operation. Figure 4.20 summarizes the results from this series of interrupted tests. Measured and predicted strains for three interrupted states at 30, 50, and 90 mm punch stroke, as well as those for a fully drawn cup are shown. The numerical model shows a good correlation with the measured values. Again the predictions in the mid-wall appear to fall below the experimentally determined values by 4 to 5 percent strain and over predict the principal strains in the cup bottom. The results show good qualitative prediction of strain distribution development with increasing punch depth. A small error in major strain persists throughout the strain history, again attributed to a low value of friction, μ , in the model. Note that the author of this thesis resisted using μ as a calibration coefficient to “tune” the predictions. Additional interrupted tests have been included for other tooling geometries in Section 4.1.2.5.

4.1.2.4 Effect of Blank Size

Figure 4.21 shows the effect of various blank sizes on the predicted strain distributions. Included with this diagram is the FLD curve provided by ALCAN for the 1.45 mm material. The results for blanks of increasing diameter are superimposed, including 279, 305, 318, and 324 mm models. As the blank size increases, the strains in the wall region can be seen to move closer, and remain parallel to, the drawing side of the FLD curve. However, in the punch profile region, the distributions shift further towards

the stretch side of the diagram with a similar increase in the major principal strain. This trend indicates that the cup drawing process may be limited by the ability of the material to stretch in the region of the punch profile radius.

4.1.2.5 Strains from Other CANMET Tooling

For completeness, strain measurements were taken from selected specimens tested using the 12 and 9 mm punch radius and are discussed here. Figure 4.22 is a plot of the strain distributions for interrupted tests on the 6 mm die and 12 mm punch profile radii tooling with a 318 mm blank tested at CANMET. The two interrupted and the one full cup again show reasonable correlation between predicted and measured results. The predicted major strains in the wall appear slightly low, which is consistent with the predictions for the other tooling configurations.

The final experimental case to be examined includes a failed specimen. During one of the tests at CANMET with the 6 mm die and 9 mm punch tooling, a low blankholder force of 52 kN was applied. A 318 mm blank was formed until it was almost complete, at which point excessive wrinkling developed due to the low binder force causing material failure. This failure occurred when the punch force increased to overcome the additional resistance caused by the wrinkles. Subsequent tests at a low punch velocity and with the full 64 kN blankholder force, allowed the formation of a completed cup for this blank diameter. Figure 4.23 shows the strain distributions for both the full cup and the failed specimen. The measured strains in the wall region are almost identical between both specimens. The proximity of the predicted strains around the punch profile region to the FLD curve indicates that this specimen is very close to the limiting blank size. It is interesting that, with the exception of the localized failure near the punch profile region, both strain distributions are remarkably similar.

Figure 4.24 shows the strain distribution for the interrupted tests done on this tooling geometry with a blank size of 311 mm, 6 mm smaller than considered in the previous figure. With the exception of the low major strain predicted in the cup wall, this

data shows a good correlation between measured and predicted strains for the 6 mm die and 9 mm punch profile radii tooling configuration.

4.1.3 Thickness Distributions

The effect of blank diameter on the predicted thickness distributions in the formed cups can be seen in Figure 4.25. This figure plots predicted element thickness against distance measured from the bottom center of the cup along the outer surface in the rolling direction. The effect of the punch profile radius can be seen acting at a distance of about 75 mm (the punch radius), with distances greater than 75 mm corresponding to locations in the wall of the cup. The simulations clearly show that the material in the base of the cup thins due to the uniform stretching to which it is subjected. Around the punch radii the sheet material is even thinner than in the bottom of the cup due to excessive stretching. Large thickness increases occur beyond the punch radius within the cup wall as drawing of material leads to thickening.

4.1.4 FLR Distributions

The effect of blank size on the predicted forming limit ratio (FLR) development in the cups is plotted in Figure 4.26. The two upper plots and the lower left plot show the predicted FLR in the rolling, transverse, and compound directions, respectively. The FLR is plotted against distance measured from the punch center along the outer surface. Two peaks are clearly evident in the distributions. The first, and largest peak, occurs near the punch profile radius, while the second, smaller and flatter peak, occurs in the cup wall. The effect of blank size on the FLR is quite clear. As the blank size increases, the entire FLR distribution shifts upwards. This trend is consistent with the increase in major strains with blank size in Figure 4.21 throughout the cup and the corresponding increase in major and minor strains at the punch radius. The fourth plot in the bottom right indicates that there is very little difference in the predicted FLR distribution for the various material directions.

4.1.5 Cup Height

Figure 4.27 is a plot of variation in cup height with angle from the rolling direction. These four plots are for different initial blank sizes and indicate the predicted cup heights for the 279, 305, 318, and 324 mm blanks. The ability of the Barlat model to capture anisotropic behaviour is evident in the variations of cup height with material direction. These figures further demonstrate the ability of the finite element model to capture earing effects.

4.2 KRDC 9 mm Die and 12 mm Punch Tooling

Results for the 9 mm die and 12 mm punch profile radii tooling are presented to show a comparison between the strain distributions, punch forces, and blank draw-in as measured at ALCAN KRDC and simulated numerically.

4.2.1 Strain Distributions

Locii of major and minor principal strains in the rolling, transverse, and compound directions are compared for this tooling in Figure 4.28. These distributions show a similar level of correlation between the numerical predictions and the experimental data to those presented in Section 4.1.2. One noticeable difference can be seen if the rolling and 180 degree directions are compared. The lower right plot is a close up view of the strain data in the draw region. The RD or 0° side of the cup shows a similar distribution to those seen previously, with the exception that it appears slightly flatter than usual. When this shape is compared to the data for the 180 degree direction, a distinct difference can be seen. The 180 data hooks sharply back onto itself, close to what occurs in the interrupted cup tests for an incomplete state. This effect, which was also noted in other ALCAN specimens, appears to indicate the presence of an alignment problem during the tests. Despite this uncertainty in the experimental results, the model appears to give a reasonably good representation of the measured strain distributions.

4.2.2 Punch Forces

In order to determine if the numerical model is providing a realistic representation of the physical process, tooling forces can be compared. Figure 4.29 is a plot of the punch force as a function of punch displacement. Comparison of the numerical predictions with the experimental punch force for this geometry, indicates an excellent correlation over most of the stroke. Small differences occur late in the punch stroke, presumably due to the errors in the predicted wrinkling behaviour. Note that punch force data was not available from the CANMET press.

4.2.3 Blank Draw-In

The experimental set up at ALCAN KRDC, as described in Section 2.3.2, included the use of a LVDT probe for measuring blank draw-in. The probe was aligned in the rolling direction and would stay in contact with the edge of the blank through a spring loading mechanism. The data for the 305 mm blank used with this tooling is compared with the corresponding predicted draw-in data in Figure 4.30. This figure clearly shows that the predictions closely follow the experimental data until an 80 mm punch displacement is reached. At this point, the LVDT probe has reached its displacement limit and remains in this extended position for the remainder of the punch stroke.

4.3 Predicted LDR by Peak Punch Force and FLR

In order to parallel the experimental method of quickly determining the limiting draw ratio based on the peak punch forces, as described in Section 2.3.2, the peak forces were compiled for each of the simulations. Figure 4.31 is a plot of the predicted punch force on the quarter geometry versus the simulated blank size for the 3 mm die and 12 mm punch radii models. Six peak punch force values were obtained from simulations with the 279, 318, 299, 305, 311, and 330 mm blank models. The predicted results were examined for signs of localized deformation indicating failure of the specimen. Failures

were detected for the three largest blank models and an average of the peak punch forces for these models was used to define the horizontal failure line. A linear regression was done on the three punch forces from the “safe” cup simulations and the intercept was determined as indicated in the figure. This intercept of 307 mm, represents the upper blank size limit as predicted by the punch force method.

Figure 4.32 summarizes the punch force results for the nine remaining tooling geometries considered in this research. Examination of this figure indicates that the LDR blank sizes predicted using the peak punch force approach do not appear to be influenced by the punch profile radii. A trend does exist if the variation in the die radii is considered. Scanning from top to bottom for any of the three die profile results clearly indicates that an increase in predicted blank size results from a corresponding increase in die profile radii. The LDR blank sizes are tabulated in Table 4.1 for all ten geometries with the die profile trend clearly evident.

In several cases, the predicted blank diameters, as given by the punch force method, were found to produce FLR values in excess of one, indicating a point above the FLC, and therefore a prediction of material failure. Table 4.2 summarizes the predicted FLR values and where the peak punch force method was found to be unsuitable, an alternative is suggested based on the FLR evaluations. These new blank size predictions are summarized in Table 4.3 and compared with the values obtained experimentally at the two experimental facilities, CANMET and KRDC. In general, the predicted LDR values were higher than those determined experimentally. The typical error was between 13 and 6 mm on the blank diameter for the CANMET and 9 and 12 mm KRDC tooling geometries. The predictions for the smaller radii tooling at KRDC were found to be in error by 83 mm on the 3 mm die profile and 25 mm on the 6 mm tooling. Some uncertainty exists in the measured LDR size for the 6 mm tooling as the value determined at CANMET was 19 mm greater than the diameter obtained at KRDC for this configuration. The overall error is attributed to low predicted major strains, thought to be caused by a low friction coefficient.

4.3.1 Effect of Tooling Geometry on Predicted Strains

The punch profile radius was not found to have a large effect on the predicted LDR blank size for the various tooling geometries. This trend is further demonstrated when the locii of principal strains are plotted for a constant die profile radius. Figure 4.33 is a plot showing that the predicted strain distributions for the LDR blank sizes for each punch radius did not show significant differences. This particular plot is for a constant die profile radius of 12 mm with strains from the indicated LDR blank sizes corresponding to the 12, 9, and 6 mm punch tooling.

The variation of the die profile radius was found to have a greater effect. The predicted strains for a constant punch profile radius of 12 mm are shown in Figure 4.34. The predicted LDR blank sizes corresponding to 12, 9, 6, and 3 mm die profile radii are indicated in the legend. A greater variation in the strain distributions is evident in this figure than in the previous one. Intuitively the 9 mm distribution should lie between the 6 and 12 mm die results. However, the strain distribution for the 324 mm blank with this tooling is lower than expected around the punch radius. This apparent anomaly can be accounted for by examining the FLR values, as indicated in Table 4.2. A predicted FLR of 0.911, indicating a strain state well below the FLC, is observed for the 324 mm blank diameter. In contrast, the 1.03 value for the 330 mm blank is well above unity, indicating probable material failure. The strain predictions around the punch radius for this blank were indeed found to exceed those of the smaller 6 mm tooling. Therefore, an actual LDR blank size between these two diameters is suggested, and would most likely result in a better representation for the effect of changing die radius.

The effect of the larger die radius is to reduce the amount of stretching which occurs over the punch profile radius of the cup. As the die radius increases, both principal strains in the punch radius region are reduced. Increasing the die radius also has the effect of reducing the peak punch force. This results in a reduction in the strain developed in the wall of the cup. Interestingly, for the sharpest (3 mm) die radius, the fracture location transitions from the punch radius to the die radius. In this case, the maximum FLR was found to occur near the die profile radius as indicated in Figure 4.35,

a FLR fringe plot for the 318 mm blank, with 3 mm die and 12 mm punch profile radius tooling, at an interrupted depth of 40 mm.. This change in predicted failure location results in lower strains near the punch profile radius. Additionally, the peak FLR can be seen on the inner surface of the cup as opposed to the usual location on the outer surface.

The importance of tracking strain evolution with punch stroke is evident if we examine the fringe plots of predicted FLR for the inner and outer surfaces of the completely formed cup in Figure 4.36. Comparison of the peak FLR between Figures 4.36 and 4.35 indicates that the material appears to have “healed” itself in the full cup. The FLR of 1.05 in the interrupted state indicates material failure while the peak value of 0.994 in the full cup indicates a safe process. This emphasizes the need to be aware of the forming history and not just examine the strains predicted for the final state in a process.

4.4 Effect of Friction and Blankholder Force

In order to determine the potential error associated with the friction coefficient, simulations were performed in which the friction coefficient was adjusted from the baseline value of 0.06 to a value of 0.08. The effect of changing this parameter on the punch force prediction method is shown in Figure 4.37. As indicated in the figure, the effect of increasing the coefficient of friction reduces the limiting blank size from a value of 323 to a value of 324 mm. This shift of 1 mm does not appear to be large, however, this shift is on the same order of magnitude as that caused by variations in the tooling geometries. Figure 4.38 shows a comparison of the principal strain distribution in the rolling direction for the two friction coefficients used. The strains in the punch region are greater in the simulation with the higher μ value of 0.08. This particular data is for the 318 mm blank diameter simulations with the 6 mm die and punch profile radii tooling.

The effect of blankholder force was also examined and found to be of much smaller consequence in the absence of wrinkling. The change in the predictions for a shift of 20 kN was determined to be only 0.05 of an inch for the 6 mm die and punch profile radii tooling as indicated in Figure 4.37. In a second comparison for the 12 mm punch

and 6 mm die tooling, the same 20 kN change in blankholder force produced a shift of only 0.04 inches. This data has been plotted in Figure 4.39.

4.5 FLD Validation

The entire set of measured strain data has been plotted in Figure 4.40. One issue that quite often arises in applying FLD data to deep drawing applications is the disparity in strain magnitudes. Typical strains from hemi-spherical dome experiments on draw specimens tend to be low and a common approach is to extrapolate the draw side of the FLD curve. In order to assess this extrapolation, the experimental FLD curve with its linear extension (dashed line) has also been included on this figure. The linear extension appears to be a fair representation for the experimentally determined “safe strains” on the deep drawn side of the FLD. There are four points which have been included that are above the FLD curve. These correspond to strains which were measured from fractured specimens.

Chapter 5

5. Discussion and Conclusions

5.1 Discussion

The experimental validation of the model supports the use of an explicit finite element code as an effective method of predicting the material behaviour in a sheet metal forming process. There are numerous factors which can effect the simulation of a sheet metal forming operation. These include the choice of an appropriate constitutive model, the selection of an appropriate FE mesh, and numerous process variables such as frictional behavior, clamping force, and contact conditions. Based on the results presented in the previous chapter, the current model has been shown to provide a reasonable representation of the LDR experiments.

Several distinct experimental parameters, such as the punch force, blank draw-in, and strain history, were found to have a close correlation with the numerical predictions. This level of accuracy allows the model to be used with a reasonable degree of confidence in predicting the LDR values for the remaining tooling geometries. Two

methods were used to determine the LDR blank size: the peak punch force and the forming limit ratio (FLR) methods; with the latter found to provide a better correlation with the experiments.

The peak punch force method of predicting LDR blank size, as shown in Table 4.3, did not capture the variations in limiting blank size due to punch profile radii. When the FLR values were considered, a distinction between the 6 mm tooling and the larger radii, 9 and 12 mm punches, was noted. The predicted LDR blank size for a given die radius and the 9 and 12 mm punch radii, were nearly identical for both methods considered. This insensitivity to punch profile radii suggests that the friction model used may not have been sufficient to capture the complex behaviour occurring as the Teflon lubricant was stretched around the punch profile radii. The simulations indicated that the strains in the punch radius region developed early in the process and did not appear to change by a substantial amount for the remainder of the process. This agrees with the results of the interrupted experiments; however, prior to localization in larger blanks, it is conceivable that the solid Teflon lubricant may alter its frictional characteristics. If this change occurs, the simple Coulomb frictional formulation will no longer accurately represent the process. While this transition may not have a significant effect on the overall strain distribution, it may be enough to cause failure at the punch radius.

The simulations did predict the trend towards smaller blank size for decreasing die profile radius between 12 and 6 mm. The predicted LDR blank size for the sharpest die profile radius of 3 mm was too large. In this case, the peak forming limit ratio was found to occur on the inner cup surface, as it was drawn around the die profile radius. This differs from the other tooling cases where the location of highest strain occurred on the external cup surface near the punch profile radius.

The experimental LDR values determined by KRDC were lower than those predicted, with the greatest difference between the predicted and experimental values occurring for the 3 mm die profile radius case. The predicted maximum blank size for the 3 mm tooling is 2.25 inches greater in diameter than was determined experimentally. Several factors may have lead to this tooling being beyond the predictive capability of the

current blank model. The radius/thickness (r/t) ratio for the 3 mm die is 2.07, which is quite small and approaches the bendability limit for aluminum alloy sheet. The 3 mm die tooling was also added late in the study after the convergence study had been completed using the 6 mm die and punch radii tooling as a baseline. Therefore, the mesh used for the 3 mm radii simulations has not been optimized for this tooling and may not be fine enough to provide consistent results. Furthermore, the shell formulation used in these simulations may be insufficient to capture the intense bending which occurs over a 3 mm profile radius.

A further concern over the experiments is evident in a comparison of the “control” geometry results from both test facilities. The “control” geometry was established so that both facilities would have the same tooling configuration for at least one of the tests. This common geometry consisted of a 6 mm die and 12 mm punch radius combination. A difference of 19 mm on the reported LDR blank size has been reported by the two labs, with the KRDC value being the lower of the two. It is possible that this discrepancy is the result of a geometric difference in the tooling. The KRDC die tooling has a strictly right angled cylindrical die cavity. This means that the stresses caused as the cup begins to experience ironing are not relieved substantially and effectively increase as more material is drawn into the die cavity. The CANMET tooling, on the other hand, has been manufactured with a slight taper in the die wall commencing below the die radius. This taper may serve to relieve some of the ironing stress on the cup wall allowing a larger blank to be drawn.

Numerically, the choice of an appropriate value for the friction coefficient is of more interest in this research than the friction formulation itself. Based on the simulation experience of ALCAN KRDC, a coefficient of 0.06 was used to represent the behaviour of the Teflon layer. Unfortunately, experimental validation of this value was not possible, as the draw bead simulator at CANMET, which has been used in previous research to determine friction coefficients, could not accommodate the solid Teflon lubricant layer.

The current model also lacks the ability to predict the onset of ironing which was found to occur experimentally. The current contact formulation does not increase the

contact force beyond that necessary to ensure that no penetration occurs. As shown in Figure 4.9, the material thickness actually exceeds the tooling clearance of 1.92 mm in the last two to four rows of elements near the cup lip. The behaviour in this region may be more accurately captured through the use of a modified contact formulation. By accurately capturing the ironing behavior, the major strain in the wall may be increased providing a better prediction. The presence of large ironing stresses would also result in a significant normal compressive stress on the sheet, violating the plane stress assumption. It may be useful to consider a modified element formulation which accounts for ironing stresses in the yield surface formulation.

An alternative to modification of the contact algorithm would be to represent the last few rows of elements with several layers of eight node bricks rather than the four node shell elements currently employed. Although this would greatly increase computational expense, a contact surface could be defined along the top and bottom surfaces of these elements. This representation would allow the ironing behaviour in the final stages of the process to be captured while still maintaining the efficiency of the shell elements for the bulk of the sheet representation.

Previous research at Carleton by Lamontagne *et al* [28] compared predictive strain distributions for the LDH test using the von Mises, Hill 1948, and Barlat yield criterion. In that work, it was concluded that the Barlat criterion provided predictions that were in closer accord with experiments than either the von Mises or the Hill 1948 criterion for AA 5754 under stretch and plane strain conditions. However, their results were inconclusive for the LDH draw specimens. The present work has served to confirm that the Barlat model does provide superior strain predictions under deep drawing conditions.

5.2 Conclusions

The following conclusions can be drawn from this research:

1. Predictions of punch forces, strain distributions, and process history were achieved through the use of an explicit finite element code. The predicted peak punch forces were found to be within 5 kN of measured values. The predicted strain distributions were within 5 percent of the measured values and the blank draw-in was found to be in excellent agreement (within 2 mm throughout the process).
2. The predicted LDR values for the nine tooling geometries with profile radii greater than 3 mm were found to be in agreement with the experiment to within 13 mm (1/2 inch) on the diameters. The prediction for the 3 mm die radius was 83 mm larger than the experimental value. This error has been attributed to the use of a coarse mesh and sharp bending causing changes in the failure mode of the material.
3. The LDR predictions were found to be sensitive to die radii effects but were less sensitive to the effect of changes in punch radii.
4. The complex frictional behaviour and ironing which occurs at the end of the process is not captured by the current model but does not appear to have a large impact on the strain distributions.
5. The coefficient of friction was found to have a significant effect on LDR blank size predictions. An increase in the coefficient of friction from 0.06 to 0.08 was shown to decrease the LDR blank size by an amount comparable to the changes occurring as a result of altering the die profile radius.
6. In the LDR predictions, the blank holder force was found to have only a very weak influence on the predictions, provided the force was adequate to prevent wrinkling.
7. The Barlat yield criterion provides superior predictions to those obtained using either the Hills 1948 or the von Mises criteria.

5.3 Recommendations

Future work should include investigation of friction models and alternative methods of capturing the ironing response of the sheet material. The use of other lubricants should also be considered as a means of characterizing the sensitivity to friction. Further experiments to more accurately determine the friction coefficient for the Teflon lubricant should be considered.

A review of the experimental procedures of the two laboratories should be made to determine the cause of the inconsistency of the experimental results. Alternatively, an additional common geometry should be decided upon and further experimental comparisons made.

A convergence study should be performed for the 3 mm profile radii tooling. This extreme forming condition may require extensive modification of the current model to achieve an accurate representation. The use of brick elements may have to be considered to properly capture the bending in this region. Incorporation of damage-based constitutive models should be considered in order to predict the influence of severe bends on forming.

References & Endnotes

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Table 2.1: Summary of tooling punch and die profile geometries considered during numerical simulations (S), and experiments at ALCAN KRDC (E_{KRDC}) and CANMET (E_{CANMET}).

Die Radii (mm)	Punch Radii (mm)		
	6	9	12
3			S, E_{KRDC}
6	S, E_{CANMET}	S, E_{CANMET}	S, E_{KRDC} , E_{CANMET}
9	S	S	S, E_{KRDC}
12	S	S	S, E_{KRDC}

Table 2.2: Uniaxial material properties of the commercial 5754-O alloy [68].

Property	AA5754-O
Thickness (mm)	1.48
Yield Strength (Mpa)	90-110
UTS (MPa)	215-230
Lankford coefficient, $\bar{r}$	0.7-0.9
Uniform Elongation (%)	18-24
Total Elongation (%)	21-27
Strain Hardening Exponent, n	0.28-0.31
Minimum Radius / Thickness	0-0.3

Table 2.3: Chemical composition of the commercial 5754-O alloy [68].

Alloy	Chemical Composition (Weight %)						
	Mg	Si	Cu	Fe	Mn	Cr	Ti
AA5754	2.6-3.6	≤ 0.4	≤ 0.1	≤ 0.4	≤ 0.5	≤ 0.3	≤ 0.1

Table 2.4: Uniaxial tensile test data for 5754-O aluminum alloy [68].

Specimen	Yield Stress (0.2%) (MPa)	Ultimate Tensile Stress (MPa)	n Value	R Value (10%)	Uniform Elongation (%)	Elongation (%)
5754 (0°)	110.4	231.6	0.2937	0.7434	19.25	22.40
5754 (45°)	99.32	220.8	0.3198	0.7235	22.93	26.32
5754 (90°)	103.0	222.2	0.3164	0.7958	23.41	25.96

Table 2.5: Average uniaxial material properties of the 5754-O aluminum alloy [68].

Property	AA5754-O
Thickness (mm)	1.49
Yield Strength (0.2 %) (Mpa)	104
UTS (MPa)	225
Anisotropy Parameter, $\bar{r}^*$ (10%)	0.747
Δr^{**} (10%)	0.046
Uniform Elongation (%)	19-24
Total Elongation (%)	22-27
Strain Hardening Exponent, n	0.31
Young's Modulus (GPa)	70.0

$$* \bar{r} \equiv \frac{R_0 + 2R_{45} + R_{90}}{4}$$

$$** \Delta r \equiv \frac{R_0 - 2R_{45} + R_{90}}{2}$$

Table 2.6: Summary of circular blank sizes used in LDR determination.

Blank Diameter (in)	Blank Diameter (mm)
11	279
11 1/4	286
11 3/4	299
12	305
12 1/4	311
12 3/8	314
12 1/2	318
12 3/4	324
13	330

Table 2.7: Summary of LDR experiments at CANMET with 6 mm die and punch profile radii.

Punch Radius (mm)	Blank Diameter (mm in)		Punch Depth (mm)	Gridded Specimen	Lubricant	Punch Speed (mm/s)	Binder Force (kN)
6	279	11.00	full cup	No	Teflon	1-2	64
6	292	11.50	full cup	No	Teflon	1-2	64
6	305	12.00	full cup	No	Teflon	1-2	64
6	305	12.00	full cup	Yes	Teflon	4.5	64
6	305	12.00	90 mm	Yes	Teflon	5.0	52
6	305	12.00	50 mm	Yes	Teflon	4.5	52
6	305	12.00	30 mm	Yes	Teflon	4.5	52
6	311	12.25	full cup	No	Teflon	1-2	64
6	311	12.25	full cup	No	Teflon	1-2	64
6	318	12.50	fail 45	No	Teflon	1-2	64
6	318	12.50	fail 45	No	Teflon	1-2	64
6	330	13.00	fail 45	No	Teflon	1-2	64
6	279	11.00	full cup	No	ALO70 + Teflon	1-2	64
6	292	11.50	full cup	No	ALO70 + Teflon	1-2	64
6	305	12.00	full cup	No	ALO70 + Teflon	1-2	64
6	305	12.00	full cup	No	ALO70 + Teflon	1-2	64
6	311	12.25	full cup	No	ALO70 + Teflon	1-2	64
6	311	12.25	full cup	No	ALO70 + Teflon	1-2	64
6	318	12.50	fail	No	ALO70 + Teflon	1-2	64
6	318	12.50	fail	No	ALO70 + Teflon	1-2	64
6	330	13.00	fail	No	ALO70 + Teflon	1-2	64

Table 2.7 (Cont.): Summary of LDR experiments at CANMET with 6 mm die with 9 and 12 mm punch profile radii.

Punch Radius (mm)	Blank Diameter (mm in)		Punch Depth (mm)	Gridded Specimen	Lubricant	Punch Speed (mm/s)	Binder Force (kN)
9	305	12.25	full cup	No	Teflon	1-2	53
9	305	12.25	45 mm	Yes	Teflon	1-2	53
9	305	12.25	full cup	Yes	Teflon	1-2	53
9	316	12.44	full cup	Yes	Teflon	< 1.0	64
9	316	12.44	fail 115	Yes	Teflon	1.5	53
9	316	12.44	fail 41	Yes	Teflon	5-6	53
9	318	12.50	fail 45	No	Teflon	1-2	64
9	318	12.50	fail 45	No	Teflon	1-2	53
9	316	12.25	full cup	No	ALO70 + Teflon	1-2	64
9	305	12.25	full cup	No	ALO70 + Teflon	1-2	64
9	318	12.50	full cup	No	ALO70 + Teflon	1-2	64
9	318	12.50	full cup	No	ALO70 + Teflon	1-2	64
9	324	12.75	fail	No	ALO70 + Teflon	1-2	64
9	324	12.75	fail	No	ALO70 + Teflon	1-2	64
12	316	12.44	45 mm	Yes	Teflon	2.0	64
12	316	12.44	90 mm	Yes	Teflon	0.7	64
12	316	12.44	full cup	Yes	Teflon	1.2	64
12	318	12.50	full cup	No	Teflon	1-2	64
12	324	12.75	fail 45	No	Teflon	1-2	64
12	279	11.00	full cup	No	ALO70 + Teflon	1-2	64
12	292	11.50	full cup	No	ALO70 + Teflon	1-2	64
12	305	12.00	full cup	No	ALO70 + Teflon	1-2	64
12	318	12.50	full cup	No	ALO70 + Teflon	1-2	64
12	318	12.50	full cup	No	ALO70 + Teflon	1-2	64
12	318	12.50	full cup	No	ALO70 + Teflon	1-2	64
12	324	12.75	full cup	No	ALO70 + Teflon	1-2	64
12	324	12.75	full cup	No	ALO70 + Teflon	1-2	64
12	324	12.75	fail	No	ALO70 + Teflon	1-2	64
12	324	12.75	fail	No	ALO70 + Teflon	1-2	64
12	330	13.00	fail	No	ALO70 + Teflon	1-2	64

Table 3.1: Summary FEM representations of the quarter blanks used in LDR simulations.

Blank Diameter (in)	Blank Diameter (mm)	Elements	Nodes
11	279	3379	3476
11 1/4	318	3499	3598
11 3/4	299	3739	3842
12	305	3859	3964
12 1/4	311	3979	4086
12 1/2	318	4099	4208
12 3/4	324	4219	4330
13	330	4339	4452
13 1/4	337	4459	4574
14	356	4819	4940

Table 3.2: Summary of LDR tooling finite element meshes.

Component - Profile Radii (mm)	Elements	Nodes	Elements Around Radii
Punch - 6	1524	1596	10
Punch - 9	1644	1720	15
Punch - 12	1704	1782	19
Binder	840	899	4
Die - 3	1470	1550	5
Die - 6	1500	1581	10
Die - 9	1590	1674	15
Die - 12	1650	1736	19

Table 3.3: Summary load curves for velocity profiles.

Draw Depth (mm)	Time (ms)	Peak Velocity (m/s)
130	5.1	40.04
140	5.6	39.98
145	5.7	39.96
150	6.0	39.94
155	6.1	39.91
160	6.4	39.89
175	7.0	39.27

Table 3.4: Summary of material values used in LS-DYNA3D simulations.

Property	AA5754-O
Thickness, t (mm)	1.487
Density, ρ (g/mm ³)	2.643×10^{-3}
Modulus of Elasticity, E (Gpa)	70.23
Poisson's Ratio, ν	0.33
Hardening Parameter, k (Mpa)	416.52
Hardening Exponent, n	0.25599
Barlat Exponent, m	8.0
R ₀	0.7434
R ₄₅	0.7235
R ₉₀	0.7958

Table 3.5: Summary of regression fit parameters to the stress-strain curve.

k (MPa)	ln k	Standard Deviation (ln k)	n	n ln($\epsilon_o + \epsilon_p$)	Standard Deviation n ln($\epsilon_o + \epsilon_p$)
416.5	6.032	0.024	0.256	0.2560	0.0084

Table 4.1: Predicted LDR blank size in mm (inch) as a function of die and punch profile radii as determined using the peak simulation punch forces.

Die Radius (mm)	Punch Radius (mm)	6	9	12
3				307 (12.10)
6		323 (12.72)	323 (12.71)	323 (12.71)
9		326 (12.83)	328 (12.93)	328 (12.93)
12		329 (12.95)	330 (12.98)	329 (12.97)

Table 4.2: Comparison of predicted limiting blank size and corresponding simulation FLR values.

Die (mm)	Punch (mm)	Predicted Blank Size (mm)	Closest Simulation Blank Size (mm)	FLR	Comments on FLR
3	12	307	305	1.97 ¹	0.970 for 286 ¹ mm
6	12	323	324	1.019	0.900 for 318 mm
9	12	328	330	1.031	0.911 for 324 mm
12	12	329	330	0.998	
6	9	323	324	0.977	
9	9	328	330	1.070	0.944 for 324 mm
12	9	330	330	1.030	0.911 for 324 mm
6	6	323	324	1.060	0.995 for 318 mm
9	6	326	324	0.982	1.07 for 330 mm
12	6	329	330	1.049	0.979 for 324 mm

¹ Note: The maximum values of FLR for this tooling occurred on the inner cup surface near the die profile radius.

Table 4.3: Predicted limiting blank size and corresponding experimental values as determined at ALCAN KRDC and CANMET.

Die Radius (mm)	Type of Result	Punch Radius in mm (inch)		
		6	9	12
3	ALCAN Sim. PPF Sim. FLR			203 (8.00) 307 (12.10) 286 (11.25)
6	ALCAN CANMET Sim. PPF Sim. FLR	305 (12.00) 323 (12.72) 318 (12.50)	311 (12.25) 323 (12.71) 324 (12.75)	299 (11.75) 318 (12.50) 323 (12.71) 330 (13.00)
9	ALCAN Sim. PPF Sim. FLR	326 (12.83) 324 (12.75)	328 (12.93) 324 (12.75)	311 (12.25) 328 (12.93) 324 (12.75)
12	ALCAN Sim. PPF Sim. FLR	329 (12.95) 324 (12.75)	330 (12.98) 330 (13.00)	324 (12.75) 330 (12.98) 330 (13.00)

* PPF - Peak Punch Force Method
FLR - Forming Limit Ratio Method

Figure 1.1 Schematic of the Swift cup test for a hemispherical bottomed cup.

Figure 1.2 Schematic of the Swift cup test for a flat bottomed cup.

Figure 1.3 Representation of practical modes of deformation in strain space.

Figure 1.4 Failure strains predicted at the fracture point (Goodwin) compared to strains predicted adjacent to the fracture (Keeler) [15].

Figure 1.5 Graphical representation of the four lubrication regimes for a liquid film lubricant.

Figure 1.6 Evolution of the oxide layer of an aluminum sheet material with time.

Figure 1.7 Definition of the co-ordinate system for a tensile specimen used to determine the Lankford coefficient, r .

Figure 1.8 The “anomalous” behavior of the Hill 48 yield criterion when used with low r materials.

Figure 1.9 The effect of constitutive formulation on the shape of the yield surfaces with variations in r value shown for the (a) Hills 48, (b) Hills 90, and (c) Barlat 89 criterion. The three yield surfaces are plotted in (d) with r equal to 0.7. The von Mises criterion has been added for comparison purposes.

$$\text{LDR} = \frac{\text{Blank Diameter}}{\text{Punch Diameter}} = \frac{\text{Blank Radius}}{\text{Punch Radius}}$$

Figure 1.10 Definition of the limiting draw ratio (LDR) and a schematic of a typical cup drawing process.

Flange wrinkling

Wall wrinkling

Figure 1.11 Typical location of wrinkles in a deep drawn cup.

Figure 1.12 Four principle regions which effect the frictional behavior in a cup drawing operation. 1) Blank/Die and Blank Binder Interface, 2) Blank/Die Corner Interface, 3) Blank Punch Interface, and 4) Blank/Punch Head Interface.

Figure 1.13 Typical ironing location in a cup drawing process.

Figure 2.1 Orientation of tensile test specimen relative to the sheet rolling direction.

Figure 2.2 True stress-strain curve fit from bulge data for the 1.6 mm aluminum 5754-O alloy sheet material.

Figure 2.3 Experimentally determined forming limit diagram (FLD) for the 5754-O alloy material. The dashed lines represent linear extensions of the FLD. Inset is a diagram indicating the method used to determine the forming limit ratio (FLR)

Figure 2.4 Tooling geometry used in experimental determination of the limiting draw ratio.

Figure 2.5 Photograph of a typical gridded blank used in the LDR study.

Figure 2.6 Schematic of the cup drawing press at ALCAN KRDC.

Figure 2.7 Photographs of the cup drawing press at ALCAN KRDC.

Figure 2.8 Graphical representation of the ALCAN technique for predicting LDR values based on punch forces. Points A and B represent values from undersized blanks while point C represents the oversized blanks value.

Figure 2.9 Schematic of the forming test press facility at CANMET.

Figure 2.10 Photographs of the forming test press facility at CANMET.

Figure 3.1 An exploded view of the 6 mm die and punch profile radii tooling with a 317.5 mm (12") blank as a typical example of a tooling mesh configuration used in the LDR simulations.

3.2 a) 279.4 mm (11") blank FE mesh.

Figure 3.2 Detailed view of four typical finite element meshes used in the LDR simulations for various blank sizes. The location of the punch diameter is indicated by the solid line.

3.2 b) 304.8 mm (12 ") blank FE mesh.

3.2 c) 330.2 mm (13 ") blank FE mesh.

3.2 d) 355.6 mm (14") blank FE mesh.

3.3 a) 6 mm punch FE Mesh.

Figure 3.3 Mesh plot of the three punch finite element meshes used in the LDR simulations.

3.3 b) 9 mm punch FE Mesh.

3.3 c) 12 mm punch FE Mesh.

Figure 3.4 Mesh plot of the binder finite element mesh used in the LDR simulations.

3.5 a) 3 mm die FE Mesh.

Figure 3.5 Mesh plot of the three die finite element meshes used in the LDR simulations.

3.5 b) 6 mm die FE Mesh.

3.5 c) 9 mm die FE Mesh.

3.5 d) 12 mm die FE Mesh.

Figure 3.6 Quadrilateral shell element regular displacement modes and hourglass mode. i) translation, ii) elongation, iii) shear, and iv) hour-glass.

Figure 3.7 Schematic of the cup drawing process indicating force and velocity boundary conditions.

Figure 3.8 Typical load curve controlling the binder force and the punch velocity used in the LDR studies. The 15 kN binder force is applied to the quarter geometry as indicated representing a total binder force of 60 kN for the process. The velocity curve moves the punch through a stroke of 150 mm.

Figure 3.9 Stress versus plastic strain curve with a regression fit for power law hardening. The 1.6 mm thick bulge test data is contrasted to the material data obtained using tensile test data on 1.0 mm material.

Figure 3.10 Comparison of rigid body stopper versus point mass method to reduce the inertial effects on the binder forces generated during cup drawing simulations. This data was obtained during the initial sensitivity studies for the 6 mm die and punch radii tooling with a 305 mm blank using a 40 kN total binder force represented by a 10 kN quarter geometry force.

Figure 4.1 Photograph of typical formed cups using a gridded and blank specimen.

Figure 4.2 Fringe plot of predicted major principal strain for the full depth cup formed with a 305 mm blank size and the 6 mm die and punch profile radii tooling.

Figure 4.3 Fringe plot of predicted minor principal strain for the full depth cup formed with a 305 mm blank size and the 6 mm die and punch profile radii tooling.

Figure 4.4 Fringe plot of predicted major principal strain for the 90 mm interrupted cup formed with a 305 mm blank size and the 6 mm die and punch profile radii tooling.

Figure 4.5 Fringe plot of predicted minor principal strain for the 90 mm interrupted cup formed with a 305 mm blank size and the 6 mm die and punch profile radii tooling.

Figure 4.6 Fringe plot of predicted major principal strain for the 50 mm interrupted cup formed with a 305 mm blank size and the 6 mm die and punch profile radii tooling.

Figure 4.7 Fringe plot of predicted major principal strain for the 30 mm interrupted cup formed with a 305 mm blank size and the 6 mm die and punch profile radii tooling.

Figure 4.8 Fringe plot of predicted material thickness for 50 mm interrupted cup using a 305 mm blank with the 6 mm die and punch profile radii tooling.

Figure 4.9 Fringe plot of predicted material thickness for 90 mm interrupted cup using a 305 mm blank with the 6 mm die and punch profile radii tooling.

Figure 4.10 Fringe plot of predicted material thickness for full cup using a 305 mm blank with the 6 mm die and punch profile radii tooling.

Figure 4.11 Fringe plot of predicted forming limit ratio (FLR) for the 50 mm interrupted cup using a 305 mm blank with the 6 mm die and punch profile radii tooling.

Figure 4.12 Fringe plot of predicted forming limit ratio (FLR) for full cup using a 305 mm blank with the 6 mm die and punch profile radii tooling.

Figure 4.13 Typical strain pair distribution for an LDR simulation. Indicated are A) the cup bottom surface, B) the punch profile radius, C) the cup wall, and D) the cup lip region.

Figure 4.14 Comparison of the predicted strain distributions for various constitutive models on the 6 mm die and punch radius tooling utilizing a 305 mm blank and 60 kN BHF.

Figure 4.15 Effect of constitutive formulation of the predicted major and minor strains in the rolling direction as a function of distance along the bottom surface of the cup measured from the punch central axis.

Figure 4.16 Effect of constitutive formulation of the predicted yield surface under plane stress conditions for a low r material using von Mises, Hills 48 and 90, and the Barlat 89 yield criteria.

Figure 4.17 Effect of constitutive formulation of the predicted element thickness in the rolling direction as a function of distance along the bottom surface of the cup measured from the punch central axis.

Figure 4.18 Comparison of measured and predicted strain distributions with respect to material direction. Fully drawn 305 mm blank with 6 mm die and punch profile radii tooling.

6 mm Die and Punch Radii Tooling

Figure 4.19 Measured and predicted strain distributions as a function of distance from the bottom center of the cup along the outer cup surface for a 305 mm blank with 6 mm die and punch profile radii tooling.

6 mm Die and Punch, CANMET, 52 kN BHF

Figure 4.20 Comparison of measured and predicted strain distributions for interrupted tests. A 305 mm blank size with 6 mm die and punch profile radii tooling and a 52 kN blank holder force.

Figure 4.21 Effect of blank size on predicted proximity to forming limit curve.

Figure 4.22 Comparison of experimental and predicted strain distributions for interrupted tests. A 318 mm blank size with 6 mm die and 12 mm punch profile radii tooling was used with a 64 kN blank holder force.

Figure 4.23 Effect of wrinkling on the strain distributions in the three material directions. One fully drawn cup and a second cup in which wrinkles caused failure are compared with predictions for the 318 mm blank with 6 mm die and 9 mm punch profile radii tooling.

6 mm Die and 9 mm Punch, CANMET, 52 kN BHF

Figure 4.24 Comparison of experimental and predicted strain distributions for interrupted tests. A 311 blank size with 6 mm die and 9 mm punch profile radii tooling was used with a 52 kN blank holder force.

Figure 4.25 Effect of blank size on predicted material thickness.

Figure 4.26 Effect of blank size on predicted forming limit ratio (FLR) and variations due to material direction.

Figure 4.27 Cup height variations with respect to sheet rolling direction.

Figure 4.28 Comparison of experimental and predicted strain distributions with respect to material direction. A fully drawn 305 mm blank with 9 mm die and 12 mm punch profile radii tooling produced at ALCAN KRDC.

Figure 4.29 Comparison of measured and predicted punch forces for a 305 mm blank tested with the 9 mm die and 12 mm punch profile radii produced at KRDC.

Figure 4.30 Comparison of experimental and predicted blank draw-in for a 305 mm blank tested with the 9 mm die and 12 mm punch profile radii produced at KRDC.

Figure 4.31 Prediction of limiting blank size through the peak punch force method for the 12 mm punch and 3 mm die tooling.

Figure 4.32 Summary of the predicted limiting blank size through the peak punch force method for the indicated nine tooling configurations.

Figure 4.33 Effect of changes in punch profile radius on the strain distributions for the predicted LDR blank sizes.

Figure 4.34 Effect of changes in die profile radius on the strain distributions for the predicted LDR blank sizes.

Inner surface FLR

Outer surface FLR

Figure 4.35 Fringe plots of FLR on the inner and outer surfaces of a 286 mm blank at an interrupted punch depth of 40 mm for the 3 mm die and 12 mm punch profile radius tooling.

Inner Surface FLR

Outer Surface FLR

Figure 4.36 Fringe plots of FLR on the inner and outer surfaces of a 286 mm blank at full depth for the 3 mm die and 12 mm punch profile radius tooling.

Figure 4.37 Effect of changing friction coefficient and blank holder force on the LDR prediction for the 6 mm die and punch profile radii tooling.

Figure 4.38 Predicted strain distributions for various coefficients of friction.

Figure 4.39 Effect of changing blank holder force on the LDR prediction for the 6 mm die and 12 mm punch profile radii tooling.

Figure 4.40 Summary of all experimental strain measurements.

IMAGE EVALUATION TEST TARGET (QA-3)

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