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Numerical Prediction of Springback in U-Channel Forming of Aluminum Tailor Welded Blanks

By

Xiao Zhou

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

**Department of Mechanical and Aerospace Engineering
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acceptance of the thesis

**Numerical Prediction of Springback in U-Channel
Forming of Aluminum Tailor Welded Blanks**

submitted by Xiao Zhou

in partial fulfilment of the requirements
for the degree of Master of Engineering
in Mechanical Engineering

Thesis Supervisor M. J. Worswick

Chair, Department of Mechanical and Aerospace Engineering

Carleton University
October, 1999

*I would like to dedicate this thesis to
my family for all of their support and encouragement,
my wife Xia, my daughter Xinxin,
my parents, and my sister.*

Abstract

The results from an investigation of the numerical prediction of springback behaviour of AA5754 and AA5182 sheet after forming are presented. A series of tooling geometries and blank sizes were considered for the U-channel forming process for both monolithic (uniform thickness) and tailor welded blanks (TWBs) with dissimilar thickness. The effects of a number of process and material parameters on springback were simulated by the sequential use of an explicit FEM code (LS-DYNA) to model the forming stage, and an implicit code (LS-NIKE3D) to simulate springback after forming.

Experiments designed to assess the accuracy of the numerical predictions were conducted on a series of tooling geometries for a variety of monolithic blanks and TWBs of AA5754 and AA5182. The FEM predictions obtained using the explicit and implicit FEM models demonstrated reasonably good agreement with experiments. The comparisons show that the numerical model using the Barlat constitutive formulation accurately simulated the forming phase, with the punch force time history agreeing well with the experiments. The comparisons also demonstrate that the numerical model was capable of reasonably accurate prediction of the springback stage after unloading for both monolithic and TWBs, with the predicted general trends of springback versus the process and material parameters considered being consistent with the experimental results.

TWBs exhibited a different springback trend from that demonstrated by monolithic blanks due to the change of the interface friction behaviour and the redistribution of the binder force (BHF). The delicate combination of BHF and frictional coefficient (COF), as well as the COF itself, has a strong influence on springback. Some concern exists that the predicted magnitudes of both the flange and the sidewall springback angles tend to be lower than the measured.

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List of Symbols

a, b, c, f, g,	anisotropic parameters used in Barlat's non-quadratic yield criterion
h, p, K_1 , K_2	
d ϵ	incremental strain
e	engineering strain
E	Young's Modulus
E'	plane-strain modulus
f	frictional force
f_n	normal surface contact force
F, G, H,	anisotropic parameters used in Hill's quadratic yield criterion
L, M, N	
I	the second moment of area
k	strength constant
m	strain rate sensitivity exponent
m, M	exponents used in Hill's and Barlat's non-quadratic yield criteria
M_e , M_p	elastic and plastic moments
n	strain hardening exponent
P	contact interface pressure
r	normal anisotropic ratio, Lankford's coefficient
$\bar{r}$	average anisotropic parameter
r_p , r_d	punch, die profile radii
R, R'	radii of curvature of bending fibre
R_1 , R_2	punch corner radii in "spanking" operation
R, S, T	principal anisotropic axis shear yield stresses
t, t_1 , t_2	sheet thickness
w	sheet width
X, Y, Z	principal anisotropic axis tensile yield stresses

Δr	planar anisotropic parameter
$\epsilon, \dot{\epsilon}$	(true) strain, strain rate
ϵ_0	strain due to prior coldworking
ϵ_p	plastic strain
$\bar{\epsilon}_p$	effective plastic strain
θ, θ'	springback angles
θ_1, θ_2	flange springback angle, sidewall springback angle
μ	Coulomb friction coefficient
ρ	density
σ	normal stress
$\bar{\sigma}$	effective stress
σ_{ij}	stress tensor
σ_Y	yield stress
τ	shear stress
τ_f	friction stress
ν	Poisson's ratio
Φ	Barlat yield function

Subscripts

0, 45, 90	material directions, rolling, compound and transverse
1, 2, 3	directions corresponding to the principal stress axes
x, y, z	directions corresponding to the coordinate reference system
xx, yy, zz	the first subscripts indicating the directions of the corresponding outward normal vectors of the planes on which the stress is applied, the second
xy, yz, zx	indicating the directions of the corresponding coordinate reference system

List of Acronyms

BCC	Body Centered Cubic
BHF	Blank Holder Force (Binder Force)
CD	Compound Direction (45°)
COF	Coefficient Of Friction
CPU	Central Processing Unit
FCC	Face Centered Cubic
FE	Finite Element
FEM	Finite Element Method
FLC	Forming Limit Curve
FLD	Forming Limit Diagram
GTAW	Gas Tungsten Arc Welding
HSLA	High-Strength Low Alloy (steel)
KRDC	Kingston Research and Development Centre (Kingston, Ontario, Canada)
LDH	Limiting Dome Height
LDR	Limiting Drawing Ratio
LVDT	Linearly Variable Differential Transformer
MMO	Materials and Manufacturing Ontario
MTL	Materials Technology Laboratory
PC	Personal Computer
RD	Rolling Direction (0°)
SGI	Silicon Graphics Iris
TD	Transverse Direction (90°)
TWB	Tailor Welded Blank
UTS	Ultimate Tensile Strength
VDC	Viscous Damping Coefficient

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Chapter 1

1. Introduction

The world-wide awareness of the effects of global warming and CO₂ emissions since the middle 1970's has been deemed as the original driving force behind the demand for better fuel economy of vehicles. Natural resource saving, lower energy consumption, and measures against environmental pollution have become ever-growing public concerns. Automotive manufacturers have been forced to discover and adopt new methods to improve the fuel consumption and efficiency of their vehicles [1].

There have been a variety of methods developed for doing this since then. Given the present level of economy, the automotive industry has chosen to reduce the weight of their vehicles as the most direct route to conforming with existing safety, emissions and economic regulations [2]. Replacing conventional body materials with lighter weight material has become more and more prevalent. In this regard, aluminum, with its high strength/weight ratio, emerges as an excellent replacement candidate for sheet steel in the automotive industry. In addition, aluminum also offers good corrosion resistance and meets the recycling requirements of the automotive industry.

One of the first automobiles to make extensive use of aluminum in the body structure is the Honda NSX [3], in which 31.3% of the car weight was aluminum, compared to 7.2% for conventional cars. This usage reduced the vehicle weight by 200 kg compared with a steel design. Thus, utilization of aluminum provides a considerable weight saving and increases efficiency.

Rapid progress has been made in the utilization of aluminum sheet since 1980's [4]. Companies such as Rover, Pininfarina, Volvo, Porsche, Mercedes, Audi, and Ferrari have used aluminum alloys in their vehicles. Today the Big Three (Ford, General Motors, and Daimler-Chrysler) all make use of aluminum alloy sheet in selected outer panel assemblies, hoods and deck lids.

However, the transition to aluminum in place of steel is impeded by several features of aluminum. The relatively high cost of aluminum is discouraging and there are formability differences between steel and aluminum. Because the experience base is much less extensive than that with steel, lead times and costs can be increased by inadequate die design.

While one of the two important trends in automotive body development is the substitution of aluminum sheet alloys for steel, the other is the use of steel tailor-welded blanks (TWBs). Recently there has been a strong motivation to combine these two technologies in order to produce aluminum TWBs for applications in the automotive industry [5].

In order to achieve greater accuracy for automotive body assembly, to increase body rigidity and improve dimensional tolerance, to reduce scrap and component mass, and thus to decrease sheet metal forming and assembly costs in automotive manufacturing, a new method of sheet forming has been developed recently, in which several blanks of different strength and/or thickness are integrated using welding techniques. A TWB is composed of two or more flat sheets which are welded in a planar configuration and subsequently stamped into a three-dimensional component. Although this technology was almost nonexistent a decade ago, steel TWBs are in rapidly-expanding production and it was estimated in 1993

that the demand for TWBs would increase to more than ten million units per annum by 1997 [6]. Last year's production of TWBs (steel) is estimated at 50 million units. Although there are still some obstacles to producing aluminum TWBs, there are recent reports which suggested that laser welding [7] and gas tungsten arc welding (GTAW) [8] have potential for successfully producing TWBs for aluminum body sheet applications.

The present trend in the automotive industry to aim for shorter lead times for the development of new models has encouraged the rapid development of numerical techniques for the simulation of sheet metal forming for the production of car bodies. Efficient simulation techniques will reduce lead times for tool design and try-out, saving both time and resources [9]. In the past decade, there has been a tremendous advance in the use of finite element method (FEM) simulations for sheet metal forming processes. The exponential increase in the raw CPU speed of computing hardware and the maturing of FEM software available for the analysis have led to the rapid and accelerating development of effective general-purpose software packages for this type of analysis. The interest in this area has expanded during the past few years because of the interdisciplinary involvement of mechanics, materials, numerical methods, computer science, mathematics, manufacturing, and others, through cooperation between industry, academia, and government laboratories [10].

The objective of the current research is to investigate the numerical prediction of laboratory-scale sheet metal forming processes utilizing aluminum TWBs, and ultimately to improve our ability to accurately predict material behaviour in sheet metal forming operations. For that goal, an investigation of the U-channel drawing process has been undertaken, in an attempt to model this simple forming process to improve methods used when modelling more complex stampings in automotive industry. In general, channel cross-sections are known to display a large amount of springback upon unloading due to the bending-dominated nature of the channel-forming process. Aluminum also typically exhibits large springback due to the higher ratio of yield stress to elastic modulus, compared to steel.

Therefore, the combination of aluminum and a channel geometry provides a useful example for the current investigation of the springback behaviour of TWBs. Several numerical models of the U-channel drawing process using monolithic (single parent metal sheet) blanks and TWBs have been developed to predict the springback behaviour for a series of tooling geometries, blank widths, and weld line positions. Here, springback refers to the change in the shape of the sheet after the complete removal of the part from the tooling. Prediction of springback is crucial to sheet metal forming industries for designing tools which produce accurate final component shapes.

The following subsections of this chapter present a review of previous research work related to the current investigation. The main topics covered include sheet formability and factors influencing formability, material constitutive laws, mechanics of springback in sheet forming, application of TWBs in sheet forming operations, and FEM simulation of sheet forming processes.

1.1 Overview of Formability of Sheet Metal

The formability of a sheet metal, which is often defined as the suitability of sheet metal for specific forming operations, has been one of the key topics in the study of sheet metal forming processes. A complex forming operation can be resolved into three major deformation components: drawing, stretching and bending. Very often the deformation in a complex stamping results from a specific combination of the three components acting collectively.

A great number of simulative tests have been developed to evaluate various deformation modes. The most commonly accepted are the Olsen and Erichsen cup tests [11], which determine the stretchability of materials, the Swift cup drawing test, which assesses the drawability of materials, the Fukui cup test [12] which combines both drawing and stretching, the Swift cup cylindrical drawing test [13], the limiting dome height (LDH) test

[14] and the square cup deep drawing test [15]. Various press heights or penetration depths when fracture occurs or local strains at necking are taken as the formability indices.

It must be noted that formability is not easily quantified as it depends on several interacting factors, such as flow strength of material, ductility, die material and geometry, lubrication, etc. The relative contribution of each component varies from part to part and from location to location in the same part. There is no single formability test that can describe or quantify formability for all types of stamping applications. All of the above formability tests represent and assess one or more aspects of a specific type of deformation.

1.1.1 Failure and Net Shape of the Formed Part

In sheet forming processes in the automotive industry, a high quality forming product not only means that the part is formed successfully without failure of the material, which is mainly determined by the load-carrying ability of the material under a specific deformation mode, but also requires that the desired net shape of the final product is achieved, a direct consequence of good control of the springback. Although springback has been treated as one of the key factors influencing the quality of stamped sheet metal, work still needs to be done in the area of the performance and behaviour of deformed sheet parts after forming. In addition, the sidewall curl, referred to as the curvature of the sidewall of a part after forming, has become more problematic with aluminum forming and is very detrimental in the assembly of U-shaped and rail-type parts [16].

The core of all simulative tests described above is to evaluate the failure behaviour of materials. However, in many stampings, the metal is drawn in from the flange, then stretched and simultaneously bent over small radii. Unlike drawing or stretching, bending, the most significant factor contributing to the magnitude of springback, causes little metal thinning because the outer surface stretches and thins while the inner surface compresses and thickens. Accordingly, no necking occurs and limit strains increase appreciably, compared

to situations where localized necking causes limit strains diminish. Another important characteristic of bending deformation is its dependence on the thickness of sheet material. With heavier gages, the through-thickness strain gradient resulting from bending becomes important. In forming operations involving bending, the sheet thickness to bending radius (t/R) ratio is indicative of the magnitude of the strain gradients both in the plane and through the thickness of the sheet metal [17]. Thus it is evident that satisfactory control of the net shape of the deformed part is equally important as the evaluation of formability of materials in a forming process.

1.1.2 Forming Limit Diagrams and Strain Measurement

Although the strain levels during a bending/drawing process, such as in the forming of a U-channel, are well below from the limit strains, it is worthwhile to present the concept of the forming limit diagram (FLD). This technique has been widely applied, with the use of the grid strain measurements, as a diagnostic tool to measure, analyze and judge the strain occurring in a deformed sheet metal component.

The FLD of a material can be constructed by plotting the principal limit strains, at which localized necking occurs, near the failure zones of the specimens [18]. The forming limit curve (FLC) is defined by the loci of forming limit strains when plotted in strain space. This FLC is often used in press shops to determine and judge how close the part is to failure or necking for a particular process [19, 20]. A typical FLD is shown in Figure 1.1. Usually three main regions, the biaxial tension or stretching zone, the drawing zone and the plane strain zone, are characterized in a FLD, depending on the sign or combination of the major and minor strains.

Used closely in conjunction with the FLD is the grid strain measurement technique to experimentally determine strains [21, 22]. The most commonly adopted grid patterns are circle or square grids. The rapid development and application of digital imaging techniques

and image analysis software provides an efficient way to determine strain distributions for a formed component.

1.2 Process Variables Influencing Sheet Metal Forming

Process variables, interacting with material properties (to be discussed in Section 1.3), influence the formability of a sheet metal in a very complex fashion. According to Siekirk [23], more than thirty process variables can be identified that affect the viability of a forming process. They are listed here under seven major headings:

- Blank Geometry (e.g. blank dimension),
- Lubrication (e.g. lubrication type),
- Press Variables (e.g. punch guidance, punch speed, binder force, rigidity),
- Work Material Variables (e.g. nominal thickness and thickness profile, mechanical properties),
- Die Variables (e.g. alignment, drawbeads, punch radii, profile radii),
- Interactive Variables (e.g. work material temperature),
- Miscellaneous Variables.

Some of the variables, such as press rigidity, die surface finish, and wear, are often neglected in sensitivity analysis due to their general insignificance compared to other parameters or to the extreme difficulty in characterizing them. Of particular importance in the simulation of sheet metal forming are the lubrication and the binder force; they both interact to effectively control the drawing of the deformed material through the tooling, hence controlling the material behaviour during forming. Springback is a direct result of the residual stress within the deformed part. Therefore lubrication and the binder force have tremendous impact on the final shape of deformed parts. In addition, the effect of blank size

and thickness on formability and final shape can not be underestimated. A brief discussion of their effects is presented as follows.

1.2.1 Lubrication and Friction

Friction between the blank and the tool surface is considered to be one of the most important conditions in sheet metal forming, having a large influence on the material flow. The localization and eventual failure of a sheet in a forming operation is often controlled by the amount of friction in the process and the lubrication conditions. By modifying the frictional behaviour of the tooling/sheet interface, the strain distribution in a pressing can be varied. Generally, with the friction reduced, a more uniform strain distribution results, allowing the material to stretch or draw over the punch. A higher friction coefficient results in an increased punch force [24].

Of the general methods [25] to describe frictional behaviour, the most common characterization is the Coulomb formulation:

$$\tau_f = \mu P \quad (1.1)$$

where τ_f is the friction stress, P the contact pressure and μ the Coulomb friction coefficient. In finite element analysis, the friction contact problem is commonly described by this Coulomb formulation.

However, several factors in this simplified characterization can cause an inaccurate representation of the actual friction in a forming process. For example, when a mixed lubrication regime is an important component of the overall lubrication system, which is often the case in at least some of the tool/workpiece interfaces, it is almost certain that the effective coefficient of friction will vary quite widely in different regions of the workpiece and that μ may not be a simple function of local conditions. Therefore the use of a constant Coulomb friction coefficient is appropriate only in regions in which the friction coefficient remains essentially stable. In practice, the frictional characteristics in a complex drawing and

bending process may vary significantly from one region to another, such as the wall region compared to the punch or die profile radii regions. Normally, the effects of friction and lubrication in sheet forming are mainly derived from observations made in simple laboratory tests [26]. Therefore any friction coefficient determined experimentally can, in the strict sense, only be applied to the conditions in which it was obtained.

In an attempt to better capture the frictional behaviour during forming processes, improved friction models have been developed [25]. These approaches can account for the fact that in many processes, frictional contacting occurs only over a certain fraction of the surface areas, and can also capture the transition between lubrication mechanisms. However it is still difficult to implement these methods in numerical simulations due to the additional complexity and computational costs involved.

1.2.2 Binder Force

It is well established that the binder force, also known as blank holder force (BHF) plays a pivotal role in sheet metal forming processes. The binder force serves to suppress the formation of wrinkles in the deformed part and thus to apply surface traction forces, through friction, which stretch the material and control the flow into the die throat. In a drawing-bending operation, a BHF that is too high will result in the early fracture or failure of the part, or at least in a highly nonuniform, undesirable strain distribution. On the other hand, a BHF that is too low can cause various surface instabilities (e.g. wrinkling), excessive amounts of draw-in, or insufficient straining of the material in the formed part such that the sheet does not conform to the tool profiles. Thus the proper variation and control of the BHF during the forming stroke can improve the drawability and quality of the formed part. However, selecting the correct BHF remains one of the most difficult procedures for production engineers when optimizing a forming operation.

While the commonly used BHF mode is the constant BHF, variable BHF mode is

also used in forming processes [27]. In the constant mode, the clamp force usually remains unchanged until the end of the forming, while in the variable mode this clamp force is reduced when the punch reaches a pre-determined stroke value, and will be maintained up to the final punch displacement. Often a smooth transition is introduced to avoid the abrupt force value changes. The variable BHF trajectory has a strong effect on the stamping performance, and is usually used for forming of parts with complex geometry.

In particular, there is a dependency of springback on the binder restraining force. Lee *et al.* [28] performed a simulation of the effect of BHF on the springback, with the geometry of the test tooling being shown in Figure 1.2, while all other process parameters were held fixed during the analysis. Figure 1.3, from Lee *et al.* [28], shows that the springback increases as the binder restraint force decreases for the analysis. This effect is discussed in more detail in Section 1.4 of this thesis.

1.2.3 Blank Size and Thickness

The inappropriate selection of blank size can result in adverse effects on sheet forming, such as the formation of large “ears” at relatively undeformed boundary regions of a blank. Earring can prevent draw-in and cause fracture to occur at a very shallow punch depth. To prevent earring, the blank shape is often altered, for example, by shearing the blank corners at an angle of 45 degrees prior to forming [29]. For a given punch geometry, there exists an optimal combination of blank size and blank holder force. This optimal size greatly depends on the blank shape itself and affects the punch load.

The effect of the blank thickness on the formability of sheet metals is also important. In general, an increase in thickness has the effect of improving formability. Usually, an increase in blank thickness will also result in a better resistance to wrinkling of blanks during forming [30].

In forming operations involving bending, blank thickness has a significant effect on

sheet bendability, which is rated by the so-called minimum bend ratio. As the name implies, a series of bending tests are used to characterize the ability of a material to be bent to a required geometry without failure. The smallest radius of tool, R , not causing visible cracking is divided by the sheet thickness, t , to determine the minimum bend ratio. As will be described in Section 1.4, springback will increase with an increased R/t ratio [17, 31, 32]. As well, the R/t ratio is indicative of the magnitude of the strain gradients in the plane and through the thickness of the sheet metal.

1.3 Material Properties and Constitutive Laws

1.3.1 Stress, Strain and Strain Rate Measures

There are a large number of methods to describe the relationship between the stress, strain and strain rate for a material undergoing plastic deformation. In some cases, metals deformed under a uniaxial stress state typically abide by a stress-strain rate relation described by an equation of the following form:

$$\sigma = f(\epsilon)g(\dot{\epsilon}) \quad (1.2)$$

where ϵ is the strain and $\dot{\epsilon}$ the strain rate. The strain dependence, $f(\epsilon)$, can sometimes be depicted by one of many simple expressions in relation to the material strain hardening. One of the most frequently used is the equation proposed by Hollomon [33]

$$f(\epsilon) = k\epsilon^n \quad (1.3)$$

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

$$n = \left. \frac{\partial \ln \sigma}{\partial \ln \epsilon} \right|_{\epsilon, T} \quad (1.4)$$

Another commonly used expression for representing the material strain hardening response was proposed by Swift [34]

$$f(\epsilon_1) = k(\epsilon_0 + \epsilon_1)^n \quad (1.5)$$

where ϵ_0 is the strain due to prior cold-working, or the initial yield strain.

A power-law dependence also often adopted to describe the strain rate dependence of stress:

$$g(\dot{\epsilon}) \propto \dot{\epsilon}^m \quad (1.6)$$

where m is the so-called strain rate sensitivity exponent and is defined as

$$m = \frac{d \ln \sigma}{d \ln \dot{\epsilon}} \quad (1.7)$$

These simple parameters n and m , representing strain hardening and strain rate sensitivity, respectively, may be derived from behaviour under uniaxial testing and have been commonly accepted as reasonable descriptions of material characteristics. It is generally accepted that the n value has the greatest influence on strain localization. A high n value produces a more uniform strain distribution and, consequently, a lower peak strain, thereby increasing the formability. It was also shown that the n value has a great influence on the press force and should be determined carefully. The so-called strain sensitivity index m is usually positive; it is known that a positive m value will improve formability by delaying the development of localized necking. The n and m effects strongly enhance each other [26], with the interaction increasing in importance for large n and m values. At room temperature,

however, the m values of commonly used sheets are very close to zero, and seem not to play as significant a role as n value in determining formability of sheet metals.

1.3.2 von Mises Yield Criterion

The equations in Section 1.3.1 indicate the material response under a uniaxial stress state only, while most metal forming operations occur under a far more complex stress state which causes the strain to develop along a complex path. However, the material behaviour of complicated forming operations can be related to the uniaxial condition, by using an effective stress-strain relation [35]. In order to define the effective stress, the multi-axial yield behaviour must be first defined. This yield criterion is a three-dimensional surface, in stress space, of all possible combinations of stress states that will lead to yielding or plastic flow in a material [36].

There are a great number of criteria to describe the constitutive behaviour of materials. One of the most widely accepted yield criteria is the von Mises yield criterion which applies for isotropic materials. Also known as the maximum distortion energy criterion, the von Mises yield criterion assumes that yielding will occur in a material when a critical value of distortion energy is reached [37].

The von Mises yield criterion, in terms of general stress state, is given by:

$$(\sigma_{xx} - \sigma_{yy})^2 + (\sigma_{yy} - \sigma_{zz})^2 + (\sigma_{zz} - \sigma_{xx})^2 + 6(\tau_{xy}^2 + \tau_{yz}^2 + \tau_{zx}^2) = 2\sigma_Y^2 \quad (1.8)$$

In principal stress space, this becomes:

$$(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 = 2\sigma_Y^2 \quad (1.9)$$

and, furthermore, in the plane stress state (assuming $\sigma_3=0$), it reduces to:

$$\sigma_1^2 + (\sigma_1 - \sigma_2)^2 + \sigma_2^2 = 2\sigma_Y^2 \quad (1.10)$$

where σ_{xx} , σ_{yy} , and σ_{zz} are the three normal components of stresses along the coordinate directions in an arbitrary orthogonal coordinate set, and τ_{xy} , τ_{yz} and τ_{zx} are the corresponding

shear stresses. σ_1 , σ_2 , and σ_3 are the principal stresses and σ_Y is the current yield strength of the material in uniaxial tension. The effective stress is written as:

$$\bar{\sigma} = \sqrt{\frac{1}{2}[\sigma_1^2 + (\sigma_1 - \sigma_2)^2 + \sigma_2^2]} \quad . \quad (1.11)$$

1.3.3 Anisotropy

Almost all sheet materials produced by rolling operations demonstrate some extent of anisotropy due to preferential crystallographic texture developed during processing. The metallurgical aspect attributed to the development of anisotropy has been discussed by Lamontagne [38].

Anisotropy is commonly characterized by Lankford's coefficient [39], r , which is defined as the ratio of width strain increment to thickness strain increment during a uniaxial tensile test,

$$r = \frac{d\epsilon_2}{d\epsilon_3} \quad (1.12)$$

where ϵ_2 and ϵ_3 are the strains along the width and thickness directions respectively. Generally a large r value indicates a higher strength in the through-thickness direction, with a larger ability to deform in the plane of the sheet than through changes in thickness. The normal or through-thickness anisotropy parameter r directly reflects the drawability of a material, as high values indicate a resistance to both thinning and wrinkling, but have a detrimental effect on stretchability [39].

However, the anisotropy level in a sheet material can differ considerably along the different orientations of the sheet with respect to the rolling direction (RD). This variation

of r value with the testing direction is termed planar anisotropy and is defined as [39]:

$$\Delta r = \frac{(r_0 - 2r_{45} + r_{90})}{2} \quad (1.13)$$

where r_0 , r_{45} and r_{90} are measured along the RD, at 45 degrees to the RD, and transverse to the RD, respectively.

The variation of r values in the plane of the sheet complicates analysis of sheet metal forming. The utilization of an average normal (through-thickness) anisotropy parameter, greatly simplify the analysis process [39],

$$\bar{r} = \frac{r_0 + 2r_{45} + r_{90}}{4} \quad (1.14)$$

In springback predictions, the role played by the material parameters r and n has been investigated by Leu [40]. Incorporating the anisotropy value r and the straining hardening exponent n , Leu developed a method to estimate springback, bendability (the minimum bending radius), and the maximum bending moment in pure bending. It is concluded that the degree of springback is almost proportional to the r value, the springback decreases sharply with respect to smaller n value. The minimum bending radius decreases with the r value, and decreases sharply with the n value. The maximum bending moment increases with the r value, but decreases with the n value. The results of this investigation are presented in Figure 1.4.

1.3.4 Hill's Quadratic Yield Criterion (1948)

In spite of the wide use of the classic von Mises yield criterion, the isotropic assumption limits its scope of application as the majority of materials exhibit some

anisotropic behaviour. In 1948, Hill [41] proposed a quadratic anisotropic yield criterion towards anisotropic materials, as a generalization of the von Mises yield criterion.

Hill expressed this yield criterion assuming orthotropic anisotropy in which the material possesses three mutually perpendicular principal axes of anisotropy, corresponding to the directions parallel to the rolling direction (RD), transverse to the RD, and normal to the plane of a sheet metal, respectively:

$$2f(\sigma_{ij})^2 = F(\sigma_{yy} - \sigma_{zz})^2 + G(\sigma_{zz} - \sigma_{xx})^2 + H(\sigma_{xx} - \sigma_{yy})^2 + 2L\tau_{yz}^2 + 2M\tau_{zx}^2 + 2N\tau_{xy}^2 \quad (1.15)$$

where f is the flow stress, which is a function of the stress tensor, σ_{ij} . The constants F , G , H , L , M and N in Equation (1.15) are characteristic of the anisotropy state and are given in [41].

For plane stress conditions and assuming normal anisotropy ($\Delta r=0$), Hill's criterion can be reduced to the following expression in terms of the principal stress components:

$$\bar{\sigma} = \sqrt{\sigma_1^2 + \sigma_2^2 - \frac{2r}{1+r}\sigma_1\sigma_2} \quad (1.16)$$

where $\bar{\sigma}$ is the equivalent stress.

A number of experimental and analytical investigations of Hill's quadratic yield criterion have been carried out. It has been concluded that this criterion is, as far as yielding behaviour is concerned, reasonably satisfactory for materials with r values larger than 1, such as steel. For materials exhibiting low r values, such as aluminum, Hill's 1948 criterion is unable to capture the appropriate material behaviour. It has been shown that Hill's 1948 criterion predicts yielding to occur at quite a low biaxial stress level compared to the von Mises criterion [26]. This "anomalous" behaviour prompted Hill to modify his original criterion into a more general formulation, suited for a wider range of r values.

1.3.5 Hill's Non-Quadratic Yield Criteria (1979 and 1990)

In 1979, Hill [42] proposed a more flexible anisotropic yield criterion in order to account for the anomalous behaviour of low r materials.

$$f|\sigma_2 - \sigma_3|^m - g|\sigma_3 - \sigma_1|^m - h|\sigma_1 - \sigma_2|^m + a|2\sigma_1 - \sigma_2 - \sigma_3|^m + b|2\sigma_2 - \sigma_3 - \sigma_1|^m + c|2\sigma_3 - \sigma_1 - \sigma_2|^m = \bar{\sigma}^m \quad (1.17)$$

where constants a, b, c, f, g, h and exponent m are material parameters.

For sheet metals demonstrating planar isotropy and undergoing plane stress deformation, Equation (1.17) reduces to:

$$\bar{\sigma} = \left[\frac{1}{2(1+r)} [(1+2r)|\sigma_1 - \sigma_2|^m + |\sigma_1 + \sigma_2|^m] \right]^{1/m} \quad (1.18)$$

The exponent m has the effect on the shape of the yield locus. For $m=2$ and an arbitrary r value, Equation (1.17) will reduce to the Hill 1948 quadratic anisotropic yield function, as given by Equation (1.16). For $m=2$ and $r=1$, the equation reduces to the von Mises isotropic yield theory, as shown in Equation (1.11). Experimental results have suggested that the value of m falls within a range of 1.6 to 2.0 for aluminum alloys [43].

In 1990, Hill proposed an improved model with the intention to predict planar anisotropic behaviour of materials [44]. In a reference system which coincides with the principal axes of anisotropy of a material, this yield criterion has the form:

$$\begin{aligned} & \left| \sigma_{xx} + \sigma_{yy} \right|^m + \frac{\sigma_b^m}{\tau_s^m} \left| (\sigma_{xx} - \sigma_{yy})^2 + 4\tau_{xy}^2 \right|^{\frac{m}{2}} \\ & + \left| \sigma_{xx}^2 + \sigma_{yy}^2 + 2\tau_{xy}^2 \right|^{\left(\frac{m}{2}\right)-1} \left[-2a_{H90}(\sigma_{xx}^2 - \sigma_{yy}^2) + b_{H90}(\sigma_{xx} - \sigma_{yy})^2 \right] = |2\sigma_b|^m \end{aligned} \quad (1.19)$$

where the Hill 1990 parameters a_{H90} and b_{H90} are given by

$$a_{H90} = \frac{1}{4} \left[\left(\frac{2\sigma_b}{\sigma_{90}} \right)^m - \left(\frac{\sigma_b}{\sigma_0} \right)^m \right] \quad b_{H90} = \frac{1}{2} \left[\left(\frac{2\sigma_b}{\sigma_0} \right)^m + \left(\frac{\sigma_b}{\sigma_{90}} \right)^m \right] - \left(\frac{2\sigma_b}{\sigma_{45}} \right)^m \quad (1.20)$$

where σ_b is the uniform biaxial yield stress, τ_s the pure shearing yield stress, σ_0 , σ_{45} and σ_{90} the yield strengths along the anisotropic axes, and m the non-quadratic yield exponent. The yield exponent in this model will vary for different materials and is roughly incorporated with the r value. In general, a material with $r > 1$ will have an $m > 2$, whereas for a material with $r < 1$, the m value will fall between 1 and 2.

1.3.6 Barlat's Non-Quadratic Yield Criterion

An alternative constitutive material formulation, developed in 1989 by Barlat and Lian [45] and known as the Barlat-89 criterion, is gaining more and more attention from industry recently. In an attempt to accurately predict the anisotropic behaviour of materials with a r value that is less than 2, this yield model is based on a polycrystalline plasticity model, and incorporates shear stresses so that the principal stress does not need to be aligned with the principal axes of anisotropy of the material.

For a state of plane stress, the Barlat criterion has the form:

$$\Phi = 2\bar{\sigma}^M = a|K_1 + K_2|^M + a|K_1 - K_2|^M + c|2K_2|^M \quad (1.21)$$

in which the parameters K_1 and K_2 are

$$K_1 = \frac{\sigma_{xx} + h\sigma_{yy}}{2} \quad K_2 = \sqrt{\left(\frac{\sigma_{xx} - h\sigma_{yy}}{2} \right)^2 + (p\tau_{xy})^2} \quad (1.22)$$

The material constants a , c , h and p can be determined from the Lankford's coefficients r_0 , r_{45} and r_{90} as defined in Section 1.3.3. The yield exponent M controls the

shape of the yield locus. Barlat has demonstrated from crystalline plasticity considerations that a value of $M=8$ is appropriate for face centred cubic (FCC) materials, while $M=6$ is recommended for body centred cubic (BCC) materials. Under the condition of planar isotropy, the constants a , c , h and p are given by:

$$a = \frac{2}{1+r} \quad c = \frac{2r}{1+r} \quad h = p = 1 \quad (1.23)$$

which reduce Equation (1.21) to an in-plane isotropic, non-quadratic form. By setting $M=2$, Equation (1.21) is reduced to the Hill-1948 criterion (Equation (1.16)). By also setting $r=1$, a von Mises criterion (Equation (1.11)) results.

In order to account for three-dimensional stress states and to better approximate polycrystalline-based yield surfaces, Barlat *et al.* [46] later proposed a more general, six-component anisotropic yield formulation:

$$2\bar{\sigma}^M = |S_1 - S_2|^M + |S_2 - S_3|^M + |S_3 - S_1|^M \quad (1.24)$$

where S_1 , S_2 and S_3 are the principal values of a symmetric matrix, S , whose stress components are defined as follows:

$$\begin{aligned} S_{xx} &= [c(\sigma_{xx} - \sigma_{yy}) - b(\sigma_{zz} - \sigma_{xx})]/3 \\ S_{yy} &= [a(\sigma_{yy} - \sigma_{zz}) - c(\sigma_{xx} - \sigma_{yy})]/3 \\ S_{zz} &= [b(\sigma_{zz} - \sigma_{xx}) - a(\sigma_{yy} - \sigma_{zz})]/3 \\ S_{yz} &= f\sigma_{yz} \quad S_{zx} = g\sigma_{zx} \quad S_{xy} = h\sigma_{xy} \end{aligned} \quad (1.25)$$

The material constants a , b , c , f , g and h represent anisotropic properties. When they all equal unity, the material becomes isotropic and the yield surface will reduce to the von Mises yield criterion with $M=2$. The exponent M governs the shape of the yield locus.

Various experimental and analytical investigations and comparisons have considered Hill's and Barlat's non-quadratic yield criteria. Hill's 1948 criterion remains the most utilized constitutive equation in industry, but non-quadratic yield criteria such as Hill's 1990 criterion and Barlat's 1989 criterion are now receiving wider acceptance. They better capture the plastic behaviour of sheet metals, especially materials with low r values, such as aluminum.

Harpell [47] compared yield loci predicted using the von Mises, Hill's 1948 and 1990, and Barlat's 1989 yield criteria in the biaxial stress region under the condition of plane stress. A Lankford's coefficient value of 0.7 was used except for the von Mises locus which by definition has a r value of 1. The investigations addressed the predicted sensitivity of the biaxial yield stress to r value and yield criteria. The greatest contraction of the yield locus (anomalous) is observed in the results using the Hill's 1948 criterion.

1.4 Bending, Drawing and Springback

Accurate prediction of springback is essential for controlling a sheet forming process and for designing tools used in sheet metal forming, especially for automotive stamping operations. Although the majority of forming processes are under three-dimensional deformation, sheet metals are most susceptible to fail under the plane strain condition. Therefore it is useful to discuss plane strain bending types of forming operations, which have been widely used in sheet metal forming industries to produce structural stamping parts such as braces, brackets, supports, hinges, frames, channels and other non-symmetrical sheet metal parts. In such operations, plane strain bending or straight line bending with a single curvature is a common deformation mode when the ratio of sheet width w to thickness t (w/t) is greater than about 8. The understanding and development of the bending mechanics are aimed at obtaining three kinds of information important for industrial applications:

- springback prediction for die design, process/press control and component

shape control;

- failure prediction and bendability (or the minimum bending ratio R/t) assessment for a given bend radius, R , sheet thickness, t , and material properties; and
- estimation of the bending force for press selection and for strength analysis and design of dies.

The fundamentals of bending deformation have been studied for many years. Four assumptions are used for plastic bending of non-work-hardening material:

- plane cross sections remain plane during bending;
- the transverse stress (normal to the sheet) and its distribution through the sheet thickness is ignored;
- the neutral (strain free) layer coincides with the mid-plane; and
- the removal of the bending moment after bending is equivalent to the elastic response obtained by superposition of a moment with equal magnitude but opposite sign.

Forming of high-strength aluminum alloys in the aerospace industry and forming of HSLA (high strength and low alloy) sheet steels in the automobile industry has encountered severe elastic distortion problems: springback and side-wall curl. A number of studies have dealt with springback in press forming where the deformation mode is bending under tension.

1.4.1 Bending Mechanics and Springback

A number of mathematical models have been proposed for describing sheet

bending/drawing problems under a plane strain condition. Basic two-dimensional springback theory is well known: the total force and bending moment at a blank cross section, calculated from the given constitutive equation of the material and the radius of the bending, are used to determine the residual stress and the resultant post-springback curvature.

The pioneering work using such an approach, with the additional assumptions of a plane stress condition and the blank being elastic-perfectly plastic, was reported by Gardiner [48]. Woo and Marshall [49] considered a more general constitutive equation which accounted for work hardening effect, and they derived a springback equation to account for tension applied during stretch-bending. The theory entailed analysis of two separate cases, depending on the magnitude of the tension, which may or may not suppress compressive yielding at the inner-most fibre of the blank. Using an alternative form of the constitutive equation, Queener and DeAngelis [50] derived a springback equation for pure bending (plane strain condition).

Various other workers have conducted similar studies under various contexts, such as that of Sheppard and Roberts [51] for the roller-levelling process, and Misaka and Masui [52] and Patula [53] for the tension-levelling process. Most of these studies have not deviated from the basic approaches adopted by the previous workers. When more complicated constitutive relationships are used, the resultant formulation then requires a numerical solution. In addition, for the case of stretch-bending, there is the extra complexity of the unloading sequence, namely, whether the tension is released prior to the bending moment, simultaneously, or in the reverse order (the precise sequence is normally governed by the forming process). This aspect has been discussed by Hessami and Yuen [54].

While the theory of springback is quite straightforward, its application to practical sheet forming processes has been limited because of the uncertainty of the forming parameters involved. This limitation has led to various empirical equations derived from experimental data, each suited only for the forming process under study.

Several recently developed models include Wang, Kinzel and Altan's work [55] in

which they took a number of factors into account, such as material characteristics, true strain hardening, planar and normal anisotropy, nonlinear bending strain distribution across the sheet thickness, the shift of the neutral axis, and tool geometry and dimensions.

A nonlinear strain distribution across the sheet thickness was introduced as shown in Figure 1.5. Using Swift's equation (1.5) and Hill's non-quadratic yield criterion, the stress distribution across the sheet thickness, the plastic moment M_p in the plastic zone of the sheet, and the elastic moment M_e at the elastic core were found.

Elastic recovery after unloading causes the redistribution of stresses and the springback phenomenon in which the radius of curvature, R , of any fibre in bending increases to R' after the bending moment is removed. If it is assumed that the unloading moment has the same magnitude but opposite sign to the applied bending moment, the change in curvature due to springback is

$$\frac{1}{R} - \frac{1}{R'} = \frac{M}{E'I} = \frac{12(1-\nu^2)}{wt^3E}(M_e + M_p) \quad (1.26)$$

or in terms of the springback angle:

$$\Delta\theta_s = \theta - \theta' = \Delta S \left(\frac{1}{R} - \frac{1}{R'} \right) = \frac{M}{E'I} \Delta S \quad (1.27)$$

where E' is the plane-strain modulus, and I the second moment of area about the centreline of the sheet. It is seen that springback is a function of material properties, sheet thickness, bend radius, stress/strain states and bend arc length. The general observations are:

- Springback increases with the yield stress, work hardening and anisotropy, since the higher these values are, the greater the resistance to plastic yielding;
- Springback decreases with increasing elastic modulus E because the resistance to elastic bending also increases;

- Bending of a thin sheet with a large bend radius (R), or a large relative bending radius (R/t), increases springback;
- Wide sheet bending under the plane strain condition has larger springback than narrow sheet bending under a uniaxial stress condition for isotropic materials;
- The total springback angle is the sum of springback angles at individual sections along the bending arc. The longer the bending arm, the greater the total springback. The bending arm increases in some bending operations, such as free bending, while it decreases in other types of bending operations, such as V-die bending. Hence, the total springback is different for these two types of bending.

Pourboghrat and Chandorkar [56] presented a model based on a mechanics approach, which can accurately predict the total strain (bending plus stretching) using a FE membrane solution. In this method, springback is calculated using the moment-curvature relationship. The accuracy of this method, as regards strain prediction, was thoroughly verified with measured data, but springback predictions were verified only qualitatively due to available data.

The most complex form of springback in sheet forming occurs when the sheet undergoes both bending and unbending deformations. This phenomenon takes place when the sheet first makes contact with the tool surface (bending) and then leaves the tool surface and partially or completely loses its curvature (unbending). Pourboghrat and Chu [57], using Hollomon's equation (1.3) and Hill's 1948 yield criterion for anisotropic material, developed a model which considered both bending, unbending and stretching problems for calculating bending and unbending strain corrections to be added to membrane strains. Their model has been implemented into a finite element code which was used to predict springback and side-wall curl for a two-dimensional drawing-bending operation. Their predictions were compared with experimental work and ABAQUS FE results. It was indicated that predicted

strains due to unbending and sidewall curl were very accurate, and springback of the sheet was slightly underestimated due to the use of a kinematic hardening rule.

1.4.2 Various Considerations for Compensating Springback

Springback affects all aspects of sheet metal forming, from the stamping of large outer body automobile panels to the bending of simple brackets. Allowances have to be made in die design so that the final product meets the designer's objective for both appearance and ease of assembly. Often, allowances for springback are based upon the prior experience of the die designer. It would be more efficient to provide to the part designer a method of evaluating the potential springback so that one could determine, prior to die fabrication, whether it is feasible to make the part with a particular shape and from a given material. Such a tool for the designer could be either an empirical representation of experimental results or an analytical treatment of the springback. Recent progress with respect to this aspect of sheet forming are reviewed in the following.

A "double-bend" technique has been experimentally developed by Liu [58]. This technique is shown schematically in Figure 1.6. The sequence of events may be envisaged as follows: (1) bend at A , which has a springback of θ degrees; (2) a flattening out of this bend with a residual springback of θ' ; and, (3) a rebending at B (springback of θ). The overall springback between the major arms is $\theta - \theta'$ degrees. This technique has also been applied to the forming of U-channels. Figure 1.7 shows the springback as the function of the die gap.

Davies, Chu, and Liu [59] further developed a "spanking" operation which was a variation of "double-bend". The channel is first formed with a punch of corner radius R_1 , and then struck with a punch of corner radius R_2 , with the punch width and die gap kept constant for both steps, as shown in Figure 1.8. The second punch will cause a rebending of the corner created by the first punch. Various results of springback changes with several values of R_1 and R_2 are presented in Figure 1.9. There have been some FEM analyses using

the “double-bend” technique to understand the relationship between springback and the forming variables.

Numerically, Karafillis and Boyce [60] developed a numerical algorithm for tooling design which compensates for part shape error due to springback by modifying the tooling shape. In this algorithm the internal force distribution on the part, obtained from FEM analysis, was used to perform an inverse simulation of springback of the fully loaded part. In other words, the fully loaded part, whose shape matched the desired part shape, was sprung forward, thereby reversing the springback phenomenon. The sprung-forward shape was obtained and repeated until the desired part was obtained.

Wu [61] studied several options of generating updated meshes for an iterative FEM die surface design algorithm to compensate for springback in sheet metal forming. He recommends deforming the die mesh or the meshes describing the contact areas into the shape of the part after spring-forward deformation. This technique is easy to use and cost effective, and the mesh conforms well to the geometric features of the design.

1.5 Application of Tailor Welded Blanks (TWBs) in Sheet Forming

The increasing application of TWBs in the automotive industry is attributed to several factors. During the past few years there has been concerted effort to reduce vehicle weight and thereby increase fuel economy. In parallel with this development is the drive toward better corrosion resistance and more intricate contours of the vehicle [62]. In pursuit of a solution to weight reduction, the concept of tailor-welded blanks (TWBs) has become an area of particular interest. The material within a single panel can have different properties, such as high strength or high corrosion resistance, or may be of different thicknesses.

1.5.1 Aluminum TWBs and the Welding Process

Although TWBs are quite a new application, steel TWBs are used presently in production already, and they offer numerous advantages which include reduction in cost, scrap, and weight, part consolidation, and improvements in structural integrity [63]. The attempt of the current study is to apply TWB technology to aluminum alloy sheet and investigate the behaviour of TWBs during forming and springback so as to expand the experience base of aluminum formability as well as to contribute to practical application of this technology.

Currently the greatest obstacle to producing aluminum TWBs is the limited development in welding technology for aluminum alloys. Although steel TWBs can be joined by various welding processes and successful welding of steel is well documented, aluminum alloys have proven more difficult to weld successfully. The most commonly reported difficulties included excessive porosity and hot cracking in the fusion zone, poor coupling during laser welding due to the high reflectivity of the metal, degradation of strength and loss of alloying elements in the fusion zone, amongst other problems [64]. All of these difficulties make welding of aluminum alloys very challenging from both welding process and formability viewpoints. Recent reports suggest that laser welding [65] and gas tungsten arc welding (GTAW) [8] are both potentially successful methods for producing aluminum TWBs in automotive body sheet applications.

1.5.2 Formability of TWBs

The problems associated with the production of TWBs panels are manifested mainly at the stamping stage, in the form of weld or parent material failure. Introducing a weld into a blank alters the flow characteristics of the surrounding material and the weld itself has its own forming characteristics. Work has begun with small scale formability tests of laser welded samples to establish the forming behaviour of the weld. Forming limit diagrams

(FLD) have been constructed for several welded and unwelded material combinations to establish the limit strain conditions during a forming operation. Scriven, Brandon and Williams [66] investigated the influence of weld orientation on the formability of similar/dissimilar thickness laser welded joints, using the Erichsen 145/60 cupping tests with several mild steels in the unwelded and laser welded conditions. They concluded that the weld had a controlling influence on the location of the fracture point; the sheet rolling direction was much more influential in determining the allowable strain levels than the weld orientation; and the formability of different thickness samples was predominantly affected by the thickness of the thinner material, as opposed to the thickness ratio.

Azuma *et al.* [67] studied the behaviour of steel TWBs in three standard forming operations: stretch forming, stretch flangability, and deep drawability. In stretch forming, the weld bead ductility is the limiting factor when the weld is oriented parallel to the major stretch axis, while the formability of the weaker parent metal is the limiting factor when the weld is normal to the stretch axis. Stretch flangability dropped by 25% to 30% due to the presence of the weld, while the deep drawability was unaffected. Radlmayr and Szinyur [68] evaluated the formability of steel TWBs using the Nakazima test and uniaxial tensile test. The results of the test were very similar to the results of Azuma *et al.* They also found that increasing the welding speed increased the formability. Wang *et al.* [69] determined the stress-strain behaviour of the actual weld bead for various combinations of thickness and strength for laser and mash-seam welds. They investigated the properties of the weld bead by compensating for the loads carried by the base material regions of the tensile test. It was concluded that the laser welds were significantly stronger than the base materials, while the mash-seam weld bead had similar properties to those of the higher strength materials.

Although the study of the formability of aluminum TWBs has not been widely reported, some experimental research in this area has been reported recently. Hibben, Steimmel and Fischer [70] performed a study on AA5182 in which different welding processes were evaluated. The forming behaviour of the TWBs with equal or different sheet thickness was examined with a segmented experimental drawing tool. It was found that

fracture always occurred in the radius of the punch corners at the bottom, not in the weld seam, and that the sheet thickness combination did not influence this result. It was shown that the aluminum TWBs in the combination of thicknesses from 0.8 to 1.5mm could be made and formed without difficulty.

Stasik and Wagoner [71] conducted a more comprehensive experimental study on the formability of the TWBs of aluminum alloys AA5754-O and AA6111-T4. Tensile tests were performed for specimens in both longitudinal and transverse directions, as shown in Figure 1.10. The formability of longitudinal weld orientation was evaluated using the Ohio State University Formability Test (OSUFT) too. They concluded that satisfactory forming performance for aluminum TWBs can be realized in spite of limited inherent weld ductility, and the formability of aluminum TWBs relative to the respective unwelded blanks is similar to that of steel TWBs. AA5754-O and 6111-T4 TWBs exhibited adequate ductility under longitudinal loading relative to the weld line, especially given sufficient specimen widths. AA5754-O performed better than AA6111-T4 under transverse loading, because of the softening of the heat-affected zone in AA6111-T4. AA6111-T4 TWBs could be heat-treated after welding to recover weld hardness and improve transverse formability.

The available database of the formability of aluminum TWBs is expanding, however, both numerical and experimental studies of the springback behaviour of aluminum TWBs are at quite an early stage. The purpose of the current investigation is to numerically model and predict TWBs springback behaviour and experimentally validate the predictions.

1.6 FEM Simulation of Sheet Forming

A variety of analytical approaches have been developed and applied to the analysis and prediction of metal forming processes. These traditional approaches include the slab method, slip line field method and limit analysis technique. While these methods have been useful in the prediction of general deformation behaviour, they are usually confined to

comparatively simple geometries and forming conditions, as well as requiring significant simplifications in their assumptions. The introduction of the finite element method (FEM) in the 1960's and its rapid development in the following years have allowed the accurate prediction of material behaviour in a wide variety of sheet metal forming processes.

1.6.1 Expected Role of FEM and Explicit-Implicit Comparison

FEM simulation of sheet forming operations, prior to actual manufacture of the tools, provides information concerning the flow of the material, the danger of cracking or wrinkling, the amount of springback and the quality of the deformed part. This information permits modifications to be made at the design phase in order to optimize the product and process. This allows a drastic reduction in the time and cost of initial adaptation of the tools and for the determination of the process parameters, with a simultaneous improvement in the product quality and process reliability. In their investigations of finite element procedures for sheet metal forming simulations, Honecker and Mattiasson [9] determined that successful numerical simulation of a sheet metal forming operation must provide information regarding the strain distribution in the material, the occurrence of wrinkles, the draw-in at the edges, as well as springback. To optimize performance, the analyst must also be able to control and vary a number of design parameters, such as binder force and lubrication (friction), the use of drawbeads and their location, material parameters, the initial blank shape, and the speed of the punch [9]. They concluded that an effective simulation program must have the ability to meet these requirements and, in addition, be robust and reliable, producing results with acceptable accuracy, in a reasonable amount of time and at a reasonable cost.

In their work at Carleton University, Williams [72], Lamontagne [38] and Harpell [47] reviewed the development of FEM techniques applied to the prediction of strains and fracture behaviour of materials in forming operations. These reviews dealt with material constitutive models, element formulations, the accurate description of friction and boundary conditions, and the appropriate selection of time integration scheme. Also, a review of the

trends and major issues in FEM simulation of metal forming is presented in their theses.

The development of FEM simulation tools and computational strategies for modelling three-dimensional sheet metal forming processes continues to advance at a rather rapid rate, while heated academic debate over the use of various FEM programs, especially of explicit versus implicit FEM codes continues.

Dynamic explicit formulations have already proven to be amongst the most versatile programs for sheet metal forming process modelling. It was also determined that explicit programs avoid convergence problems and model intermittent contact more effectively than implicit codes. In terms of flexibility and time efficiency, the explicit formulation is significantly better, especially for larger, more complex problems, such as in the modelling of thin sheet metal forming [73, 74].

In general, implicit codes are thought not to be well suited for the application towards large scale industrial forming problem due to the requirement to form and solve large systems of highly non-linear equations. In the Numisheet '96 Conference which highlighted the efforts made in the field of FEM solvers, it was shown that the debate over this issue continues and advances had been made for implicit solvers, whereas the efficiency and robustness of explicit formulations had been demonstrated as well. With increased computing resources and improved FEM codes, it is possible and realistic to make more use of implicit solvers in industrial forming problems.

1.6.2 Coupled Use of Explicit and Implicit Solvers for Springback Calculations

The application of FEM techniques to springback prediction in sheet metal forming is the focus of the current investigation. One of the significant features of the simulation of springback phenomenon employed in this research is the use of both explicit and implicit codes. While explicit codes handle the contact portion of the forming analysis very well and are computationally efficient, springback calculations are very CPU intensive using explicit codes. Conversely, implicit codes are not very well suited to solving the interaction of a

large number of nodes with rigid tooling, but they do solve the static equilibrium equations required to predict springback very efficiently. Therefore an effective solution for this problem can be obtained by combining an explicit approach for the forming phase with an implicit solution for the unloading phase. The stress state at the end of the forming phase, predicted by the dynamic explicit formulation, is supplied to the implicit numerical code which performs a simple elastic step. Such a procedure allows an accurate springback prediction and, at the same time, a significant CPU time saving [75].

1.6.3 FEM Applied to the Springback Analysis

Springback is one of the key factors influencing the quality of stamped sheet metal parts in sheet metal manufacturing. There have been diverse efforts to evaluate and/or decrease springback for a long time. Analytical methods have been developed mainly for analyzing parts with two-dimensional or simple three-dimensional geometries. Since it is almost impossible to apply analytical approaches to complex three-dimensional geometries, numerical methods including FEM have been developed and applied to sheet metal parts of general shape as a result of rapid progress of computing environments. However, springback has proven more difficult to simulate accurately than the overall strain determination. Accurate springback simulation requires both faithful handling of sheet bending and accurate determination of instantaneous stresses. This section reviews some work done with FEM simulations specifically concentrating on the bending-drawing process and springback analysis.

One of the most frequently adopted forming processes in springback analysis is the benchmark problem of Numisheet 93---two-dimensional bending/drawing problem, which is schematically demonstrated in Figure 1.11. The final shape upon unloading is sought. The springback in this problem is expected to be large and the elastic-plastic behaviour of the material and the bending of the sheet must be modelled accurately. A great number of workers attempted to accurately predict the springback behaviour in this problem.

Taylor, Cao, Karafillis, and Boyce [76] examined the effect on springback of using different materials (mild steel, high tensile steel, and aluminum) with a BHF of 19.6kN. A dramatic effect on springback is seen in the ratio of the yield stress to the elastic modulus. Much larger amounts of springback are observed in the aluminum and high tensile steel due to the larger elastic contribution to the deformation than in the case of the mild steel. It is noted as well that the greater the magnitude of the yield stress to the elastic modulus ratio, the greater the effect of the BHF on the springback. Taylor *et al.* also examined the effect of BHF (19.6 kN vs. 2.45 kN) for the three materials. The use of continuum versus shell elements was investigated using implicit procedures with mild steel and a BHF of 19.6 kN. Excellent agreement was obtained between the two simulations where the plane strain and shell elements were used.

Using a 1 mm thick 2008-T4 aluminum alloy with different punch/die radii and clearances, Pourboghraat and Chu [58] investigated both the springback and the sidewall curl of the Numisheet 93 benchmark problem. More pronounced curl was observed with sharper die radius, while greater wall springback was found with increased die gap (clearance). It was also concluded that when a 50 kN BHF is applied in this problem, the tension developed in the sheet is sufficiently large to cause fully plastic deformation along the sidewall of the channel. This causes the sheet to be stretched far beyond the elastic limit and thus eliminates side-wall curl and springback of the hat section upon unloading.

Applying the explicit loading/implicit unloading approach, Forcellese *et al.* [77] performed a fully three-dimensional analysis of the V-die bending process in which the sheet was coined applying increasing levels of bending force. The effects of the coining load and punch nose radius on springback and the geometrical distortion of the bend were investigated. The elastic springback effect was measured by means of the ratio K , defined as the ratio of bend angle after unloading to that before unloading; thus the higher the springback ratio, the lower the elastic springback. The results can be summarized as follows: using a punch with a larger nose radius, the springback ratio increases with the coining load and tends towards unity asymptotically; using a punch with a smaller nose radius, a negative

springback, with an increase in the bend angle after unloading, is observed. The smaller punch nose radius produces a less marked elastic springback, and allows the definition of a narrow range of the coining load, at relatively low load levels, where the elastic springback is negligible.

A more complex problem is the deep drawing of an S-rail and its wrinkling and springback analysis, as shown in Figure 1.12. Li, Yang and Zhong [78] analyzed the forming and springback of mild steel, using BHF levels of 10 kN and 200 kN. It was found that the shapes of sections JD and IE before and after springback with the BHF of 200 kN changed very little. Duarte *et al.* [79] also concluded that for the lower BHF, the aluminum and the high strength steel presented a higher springback. This result is consistent with the smaller Young's modulus of the aluminum and the higher yield strength of the steel. The shape distortion (the current z coordinates along the straight profile lines JD and IE) decreases with an increase of the BHF especially for aluminum, while the mild steel presents a smaller level of distortion for all BHFs used.

It is important to understand that results on springback simulated by FEM vary according to the manner in which the given problem is modelled (see the U-channel drawing/bending benchmark problem discussed above). In other words, although the geometry and the material properties of the tools and the sheet blank are fixed, the variations in the method of FE modelling by users may result in a great influence on springback results [80]. Using statistical methods, Lee and Yang [81] quantitatively assessed numerical factors influencing springback. They concluded that element size in the blank and the number of corner elements were the most significant factors influencing predicted springback. It was shown that the punch velocity, contact damping parameter and penalty parameter were also important.

1.7 Current Work

Numerical and experimental studies on the springback behaviour of aluminum TWBs have not been reported. The goal of the current research is to develop an accurate numerical model that can be used to predict the bending-drawing process and the subsequent springback of aluminum blanks which are either of uniform thickness as well as tailor-welded (TWBs). The numerical models are to be used to simulate the U-channel bending-drawing process and predict the springback behaviour after forming for a series of tooling geometries, blank widths, dissimilar thicknesses and welding line positions.

The development of the models and their simulations were undertaken in conjunction with experimental work conducted by the author of this thesis at the University of Waterloo. The experiments were composed of parametric variations of the punch widths (die gap), blank widths, blank thicknesses, and thickness ratios (for TWBs). The results of the experiments, in the form of measured punch force, draw-in, springback angles and sidewall curls were used to assess the numerical predictions of the springback behaviour of aluminum alloy TWBs.

The experimental study is described in Chapter 2. FEM models of the experiments were developed and analyzed using a series of commercial FEM codes, as described in Chapter 3. The experimental results are presented in Chapter 4, while the numerical results and the predicted springback behaviour of aluminum alloy TWBs are presented in Chapter 5. Also included in Chapter 5 are the parametric studies which investigate the effects of several process variables and material parameters on the springback behaviour of aluminum alloy sheet. Discussion of the results, conclusions, and recommendations for future work are presented in Chapter 6.

Chapter 2

2. Experimental Program

U-channel bending and drawing tests of both monolithic (parent metal only) blanks and TWBs were performed on two commercial aluminum alloys as a means of experimentally determining the springback behaviour of sheet metals. This research project considered three different tooling geometries and four different blank widths as shown in Table 2.1. The tests were performed at the Waterloo 250 kN Formability Press in two series: the first considered the monolithic blank U-channel forming, consisting of three tooling geometries and four blank widths; the second considered TWB U-channel forming, using two tooling geometries and one blank width.

The U-channel tooling was designed as part of this research and was intended to provide insight into the effect of punch-sidewall clearance, process condition and blank geometry on the degree of springback. The tooling incorporates punch inserts allowing variation of the clearance between the punch and die sidewall. The width of the blank was varied for the experiments on monolithic blanks. Tests were also performed on TWBs with several combinations of dissimilar thickness and different weld line placements under a

range of blank holder forces (BHF) relative to the tooling geometry. These tests allowed the experimental determination of the effect of binder or blank holder force, friction coefficients (COF), different blank width and thickness, and tooling clearance on springback.

2.1 Material Selection

Two commercial aluminum alloys, AA5754-O and AA5182-O, were considered for the experimental investigation as they have become the leading light weight replacement candidates for steel in automotive structural applications (inner structure). The 5182 aluminum alloy has a higher yield stress and a higher UTS, thus a larger springback is expected. The uniaxial properties and chemical compositions for both materials are given in Tables 2.2 and 2.3, respectively. Both the monolithic blanks and the tailor welded blanks (TWBs) were supplied by the ALCAN Kingston Research and Development Laboratory (KRDC), Kingston, Ontario. The monolithic blanks with the same thickness were all cut from the same sheet, which provided for uniformity of the specimen material properties. Weld lines of the TWBs were parallel to the orientation of the rolling direction (RD) of the sheet metal.

The welded blanks were joined using two different welding processes. The 2.0:1.0, 2.0:1.6 and 1.6:1.0 AA5754 blanks used a non-vacuum electron beam fusion process, whereas the 1.5:0.8 AA5182 blank was welded using a dual beam Nd:YAG laser process. Further descriptions of these processes are given in [64, 82].

2.2 Material Characterization

The mechanical properties of both the AA5754-O and the AA5182-O sheets were obtained using tensile tests conducted at the mechanical testing laboratory at ALCAN KRDC. For each material, three tensile specimens were cut for each of the 0°, 45° and 90°

directions, measured with respect to the rolling direction (RD), as shown in Figure 2.1. The uniaxial tensile tests utilized an Instron Series 9 Automated Testing Standards, with an Instron 4200 Controller and conformed to ASTM E517 [83] and E646 [84]. The average tensile test data is given in Table 2.4. Table 2.5 summarizes the material properties of their equivalent planar anisotropic materials. 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 both alloys, as determined at KRDC using a hydraulic bulge test on a 1.6 mm nominal sheet thickness, are shown in Figure 2.2. AA5182 bears similar mechanical properties to those of AA5754, and with a higher yield stress and UTS, which are of interest for the purpose of examining the springback behaviour of different aluminum alloys.

2.3 U-Channel Forming Tests of Monolithic Blanks

The experiments were designed to investigate the effect of binder force, lubrication condition, tooling geometry and clearance, and blank geometry on the U-channel forming process and subsequent springback. This section describes the experiments performed on monolithic blanks without welds. The experiments on TWBs are described in the following section. Figure 2.3 shows a schematic of the tooling geometries considered. In these experiments, the focus was on the effect of a variety of process parameters as well as blank size on the degree of springback, whereas the experiments on the TWB U-channel forming address the effect of dissimilar thicknesses and different weld line positions on springback.

2.3.1 Specimen Preparation

AA5754-O (used in the majority of the experiments) and AA5182-O sheets of

various thicknesses (1.0, 1.6 and 2.0 mm) were sheared into 400 mm long rectangular blanks with different widths, as summarized in Table 2.6. This length was designed to allow a draw depth of 65 mm while leaving a long enough flange for measurement of springback.

All blanks were washed with clean warm water and wiped with a dry, clean cloth, except for a series of pre-lubricated 1.0 mm 5754 blanks which were used to study the effect of the wax coating. In order to ensure uniform lubrication of the sheet metal, Teflon films were used for the majority of the tests. In several cases, mineral oil was also used to examine the effect of different lubrication/friction situations.

2.3.2 Experimental Setup and Procedures

Experimental data for this investigation comprised process time history data including punch displacement and punch force data for deformed channels. The springback angles of both the flange and the sidewall were determined from the profiles of deformed channels for the various process parameters, tooling geometry and blank sizes considered.

2.3.2.1 Experimental Setup

The 250 kN formability press facility at the University of Waterloo was used to carry out the U-channel forming tests with various punch sets and blank sizes. Three punch widths were considered, these being 100 mm (3.937"), 102 mm (4.016") and 104 mm (4.094"), while the die opening width was kept unchanged at 107 mm (4.213") for all test runs. The punch profile and die profile radii were both 10 mm.

The U-channel forming tests were conducted on a servo-hydraulic test system which incorporates two concentric actuators, as shown in Figure 2.4 (a), which is a photograph of the experimental setup at Waterloo. The punch actuator is a 250 kN MTS single-ended actuator with a capacity of 253.5 kN in compression or 161.9 kN in tension, and a stroke of

508 mm. The binder actuator is a 500 kN MTS single-ended actuator with a capacity of 496 kN in compression or 291 kN in tension and a stroke of 254 mm. The punch actuator operates in compression and is coupled to the upper plate of the die set on which the punch tooling is mounted. The binder tooling is mounted on the middle plate of the die set and is driven by the clamping actuator which is beneath the die and operates in tension. The die tooling is mounted on the fixed bottom plate of the die set.

The test system has a closed loop control capacity through the use of two interconnected MTS 407 servo-hydraulic controllers, which allow tests to be performed with the punch and the binder under position or load control. In the U-channel forming tests, the motion of the punch was under displacement control with a *ramp* function (fixed test velocity), while the binder was under force control. The punch velocity was 1 mm/s (0.0394 in/s) and the punch displacement is monitored with a LVDT mounted within the punch actuator. The binder was operated under load control with a constant applied clamping load. The data acquisition of the forming process history was carried out using a Nicholet Pro30 digital oscilloscope which was interfaced with the test system.

2.3.2.2 Experimental Procedures

Before the actual U-channel forming test is conducted, the punch and die are cleaned to ensure that there is no material debris left by previous tests. During the tests, two types of Teflon films were employed, with thicknesses of 0.004" (0.102 mm) and 0.005" (0.127 mm), respectively. To avoid tearing of the Teflon films during forming, two Teflon films were placed at the two sides of the die separately, followed by the specimen. The specimen was positioned with the help of an alignment gauge so that it is properly centred and aligned with the die edge. Next, another layer of Teflon films, which are again composed of two or three separate films, were applied to the top surface of the specimen. At this point, the punch was lowered so that it is just about to contact the specimen. The binder was then brought

down into position above the specimen. Once the binder and the die close, the binder force on the specimen is increased to the desired level. The motion of the punch into the die opening was triggered. The punch movement was set at a constant velocity (*ramp* mode in the controller) of approximately 65 mm/min (1.08 mm/s) for all monolithic specimens. The test was terminated when the punch reaches the specified stroke set by the controller. Time history information from the forming process such as the punch displacement and the punch force were stored and saved as binary waveform files by the Nicholet Pro30 digital oscilloscope.

In order to minimize the effect of the friction force within the bushings when the binder is lowered, and thus to ensure a more consistent application of the binder force on the specimen during testing, it is necessary to regularly inject grease via the grease nipples into the bushings.

2.4 U-Channel Forming Tests on TWBs

Experiments were also conducted on TWBs to investigate the effect of dissimilar thickness and different weld line positions on their springback behaviour.

2.4.1 Specimen Preparation

AA5754-O (used in the majority of the tests) and AA5182-O sheets of various thicknesses (nominal gauge thicknesses 1.6 mm welded with 1.0 mm, 2.0 mm with 1.0 mm, 2.0 mm with 1.6 mm and 1.5 mm with 0.8 mm) were sheared into 400 mm long, 100 mm wide rectangular blanks. The lengths of these blanks was determined based on the binder/die size of the press facility and the channel depth after forming, while the blank width of 100 mm was selected based upon the fact that a wider blank demonstrated a larger degree of springback in the monolithic blank tests.

Most of the experiments considered a weld line initially located on the tooling symmetry plane. A few experiments considered initial offsets of the weld line relative to the centre of the tooling. All blanks were cleaned before pressing. In order to ensure uniform lubrication of the sheet metal, Teflon films were again used for the TWB experiments.

2.4.2 Experimental Setup and Procedures

Experimental data for this investigation included process time history data such as punch displacement and punch force data for deformed channels. The springback angles of both the flange and of the sidewall from the thick and thin sides of TWBs were determined from the profiles of deformed channels.

The punch widths considered were 100 mm (3.937"), 102 mm (4.016"), while the 104 mm (4.094") punch was not used since the smallest thickness for the thick side of the TWB samples was 1.6 mm, which exceeded the minimum available tooling clearance.

The same procedures as for the monolithic blanks were followed for the TWBs during the specimen preparation. Due to the fact that there is a thickness difference at the thick and thin sides of TWBs, it is necessary to use shims (spacers) between the thin side of the TWB and the die surface to ensure the binder force can be properly applied on the thin side of the TWB. The thickness of the shim was equal to the thickness difference at the thick and thin sides of the TWB; for example, a 1.6/1.0 mm TWB will need a shim of 0.6 mm at the thin side. No Teflon film was used between the shim and the die surfaces. However, Teflon films were used in interfaces such as between the thick side of the TWB and the die, thin side of the TWB and the shim, the punch and the TWB. The flat surface (aligned) side of the TWB was placed facing the punch nose to ensure that the TWB wrapped around the punch nose tightly.

Given the blank size and thickness of the TWBs tested, a higher binder force was used, compared to that for the 1 mm monolithic blanks. The punch moved at a constant

velocity of approximately 70 mm/min (1.17 mm/s) for all TWB specimens. Table 2.7 is a summary of specimens tested and experimental conditions for both the monolithic blank and TWB tests.

2.5 Measurement of Springback Angles

Measured springback angles were obtained for both the monolithic blanks and TWBs. For all blanks, the deformed profiles of two edges were extracted by tracing each profile on a piece of grid paper and directly measuring the flange springback angle with respect to the horizontal reference line and the sidewall springback angle with respect to the vertical reference line. Figure 2.5 is a schematic showing definitions of springback angles θ_1 (flange angle) and θ_2 (sidewall angle).

To obtain a better precision in the measured springback angles, the average springback angles from the both edges of both the flange and the sidewall were reported for both monolithic and TWBs.

Chapter 3

3. Numerical Models

Numerical simulations of the U-channel forming process were performed using the LS-940 version of LS-DYNA [85], an explicit dynamic finite element code, and the LS-972 version of LS-NIKE3D [86], an implicit, quasi-static, finite element code. Analyses were run on Silicon Graphics Iris (SGI) R8000/R10000 workstations.

LS-DYNA and other explicit dynamic programs are particularly suitable for sheet metal forming applications [87]. It uses a central difference method of explicit time integration which requires comparatively little storage. LS-DYNA is capable of accurate and efficient simulation of contact surfaces and slide lines and is well suited for vectorization and parallelization.

One of the disadvantages of the explicit formulation is its small stable time step size, governed by the Courant criterion, with the critical largest allowable time step being approximated by the time a stress wave takes to cross the smallest dimension of the smallest element in the mesh [88]. Typically, the stable size of the time step for an explicit solution is 10 to 1000 times smaller than that corresponding to an implicit solution, which implies

that explicit codes are best suited for problems of short duration.

Very often sheet metal forming operations occur in a time period of several seconds, which is fairly long for explicit codes and can cause very high computational costs because of the small time step. Generally, sheet metal forming operations utilize low, quasi-static punch speed varying between 0.1 to 10 mm/s. In order to resolve the problem of excessive CPU costs associated with real time simulations, the tooling speed is artificially increased for the forming simulations. In the current simulations, the peak values of punch velocities have been artificially raised to roughly 10 m/s. It must be noted that care needs to be taken while applying this "load factoring" approach as undesired and unrealistic inertial effects, such as oscillation of the binder, can result from the increased punch speeds.

LS-DYNA can, however, become prohibitively expensive to conduct long duration static analyses. When the blank is removed from the tooling in the model to allow springback, there is no way to increase the speed of the analysis and there is no tool with which to force the blank to move more quickly. The oscillation of the deformed sheet around the new equilibrium position during the unloading phase makes the prediction of the elastic springback a very time consuming step in the FEM analysis of sheet stamping processes. It is not unusual to spend twice as much time solving springback with an explicit code as is spent in solving the forming process.

With its implicit formulation, the less-known LS-NIKE3D supplies complimentary abilities to its sister code LS-DYNA. Static equilibrium problems such as sheet metal springback after forming are more naturally solved using LS-NIKE3D. Indeed, each code has the capability to generate an input deck for the other, allowing them to be used together to simulate problems with more than one time scale.

For implicit solutions, the user may select how many time steps will be used, and how frequently the stiffness matrix may be reformulated. For small deformation problems, or problems where an approximate solution is acceptable, a single step solution can be obtained with a single stiffness and Jacobian matrix formulation. For large deformation problems, a

number of stiffness matrix re-formulations and Jacobian calculations will be required in order to obtain a converged solution. For typical sheet forming problems, the springback deformation is sufficiently small that adequate accuracy can be achieved using the single time step approach. In this study, the single step method was employed in LS-NIKE3D for springback calculation throughout all the models.

3.1 Characterization of Springback and Sidewall Curl

In the current investigation, the flange and sidewall springback angles were used to indicate the amount of springback. The flange springback angle, θ_1 , is defined as the angle between the horizontal line and the deformed flange; likewise, the wall springback angle, θ_2 , is defined as the angle between the vertical line and the deformed wall. For the deformed model, the "ideal shape" in terms of springback angle, for both the flange and the wall should be 0° . For example, a -10° angle means a upward springback of the flange, whereas a 3° angle indicates an downward springback of the flange. Similarly, a -8° indicates an inward springback of the wall, while 2° would represent an outward springback of the wall.

In order to determine springback angles, the nodal coordinates of the U-channel profile in the XY symmetry plane after unloading were extracted so that the springback angle could be obtained by linear regression. Angle θ_1 is determined by the coordinates of all nodes on the flange, while θ_2 is based on the coordinates of the nodes which can be best fitted linearly, depending on the degree of the sidewall curl, as shown in Figure 3.1, the schematic of the springback definitions.

The sidewall curl occurs in both XY and XZ planes for a 3D problem. In the current investigation, the sidewall curl in XY plane is defined by the curvature of a line through points A, B, and C in the XY symmetry plane, as defined in Figure 3.1. The sidewall curl in the XZ plane is displayed by the profile of a series of X and Z coordinates of nodal points at a height of 35 mm from the bottom of the U-channel, as defined in Figure 3.2. This

consideration is based on the profile radii of the punch and die and the depth of the U-channel so that the location of the most noticeable amount of the sidewall curl in XZ plane can be reached. Unlike the XY curl, the XZ curl is characterized in the current investigation by the X displacement of the node on the outmost edge to the node at XY symmetry line of the defined XZ profile.

3.2 Finite Element Modelling

The numerical models of the experiments were built using I-DEAS Master Series 5, a commercial engineering software package [89]. A universal file with meshing data for each FE model was generated and exported by I-DEAS, after which the output universal file was translated into an input file for LS-DYNA.

The die, the binder, and the punch tooling surfaces in all simulations were generated using rigid, four node quadrilateral shell surface elements, therefore only a surface representation of the tooling was required for the models. By modelling the tooling as rigid in the finite element model, a fine mesh may be used to accurately resolve the details of tooling geometry, such as small radii of curvature, without significantly increasing the cost of the analysis. Due to the geometrical symmetry, only one-quarter of each geometry was modelled for the monolithic, and one-half of the geometry was required for the TWB models. For TWB models, weld lines were modelled using rigid link elements (see subsection 3.3.2).

Figure 3.3 is an exploded view of a typical finite element mesh used in one of the monolithic models, which represents the 100 mm wide punch and 107 mm wide die with 10 mm profile radii and a 25 x 400 mm monolithic blank. Figures 3.4 to 3.6 are exploded views of other monolithic models with the same tooling geometry and different blank widths ranging from 50 mm to 100 mm. Figure 3.7 is an exploded view of the finite element mesh of one of the TWB models representing the 100 mm wide punch and 107 mm wide die with 10 mm profile radii and a 100 x 400 mm TWB and the weld line initially being positioned

at the tooling symmetry.

Models of the deformable blanks were generated utilizing Belytschko-Lin-Tsay four node quadrilateral shell elements for all blank geometries, and the number of elements and nodes used in monolithic blank models are listed in Table 3.1. The table shows that the number of deformable elements in monolithic blank models ranges from 1600 to 6400, depending upon the width of the blank.

For monolithic blank models, certain symmetry boundary conditions were imposed along the sides of the quarter blank both in the XY and YZ planes, while symmetry boundary conditions were only applied along the sides of the half blank in the XY plane for the TWB models. The element size was roughly 1.33 mm by 1.4 mm. The aspect ratio value of quadrilateral shell elements has been determined [90] to ensure the elements are nearly square when they transit the die radii region.

There are three different sets of punch inserts and four different blank widths in the monolithic models. The number of rigid tooling elements and nodes is listed in Table 3.2. The total number of elements and nodes for various combinations of tooling geometries and blank widths are also included in this table. The total number of elements ranged from 2770 to 11080 for different blank widths considered.

For the TWB models, there are three thickness combinations: 1.6 mm sheet welded with 1.0 mm sheet, 2.0 mm with 1.0 mm, and 2.0 mm with 1.6 mm. As mentioned previously, the half blank was modelled applying a symmetry boundary condition in the XY plane. Models of the deformable TWBs were constructed in the same fashion as used to create uniform thickness models, except that there is a weld line along the Z direction, which was created using rigid link elements (described in subsection 3.3.2). This is an approximation of the actual weld lines where it is assumed that no deformation will occur. Figure 3.8 is a series of mesh plots reflecting different weld line positions and the use of shims in the TWB models, and Figure 3.9 is a close up view of a typical weld line that joins the two sides of dissimilar thickness, with the letter “R” in the figure indicating rigid link

elements. Relevant information on the number of elements and nodes in the TWB models is presented in Table 3.3.

3.3 Element Formulation

Selecting an appropriate element formulation is critical to an accurate numerical simulation. Due to its efficiency and accuracy in the application to thin sheet metal problems, the shell element is employed for deformable blanks in this investigation. It is advantageous over brick or hexahedral continuum elements which restrict the time step to a very small value based upon the through-thickness element dimension, thus increasing the computational expense. In addition, the use of through-thickness integration points (typically three to seven) within a shell formulation allows close approximation of bending effects, which significantly influences the springback behaviour of sheet metals.

Among various shell element formulations available in LS-DYNA, the Belytschko-Lin-Tsay (BLT) formulation provides the best compromise between computational efficiency and accuracy, according to Galbraith and Hallquist [88]. The four node, bilinear BLT formulation uses a co-rotational local coordinate system to simplify the element formulation. A local element spatial coordinate system is embedded on the reference surface of the element, defined by the mid-surface containing the four corner nodes. This approach automatically accounts for rigid body rotations [88].

U-channel forming, though conceptually a simple process, becomes more complicated when springback is considered. Predicting the shape of the sidewall after springback is quite difficult and requires a fine mesh of shell elements with a number of integration points through the shell thickness. In this investigation, seven through-thickness integration points were employed in the simulation of the forming phase of the blanks.

After running the forming stage of the models in LS-DYNA, an output deck from LS-DYNA is generated for use as an input deck for springback analysis in LS-NIKE3D. An

elastic material model was used for the springback step since yielding was not expected. Although the deformation during springback should be predominantly elastic, there may still be large deformations, and thus the problem is fundamentally nonlinear. Within LS-NIKE3D, there are thirteen user selectable nonlinear numerical time integration methods available. They all are iterative in nature. The BFGS (a quasi-Newton method) was selected because of its good combination of robustness and convergence rate.

3.3.1 Displacement and Force Boundary Conditions

Figure 3.10 (a) is a schematic of the monolithic blank model and Figure 3.10 (b) depicts the TWB model used in this investigation, showing the initial blank and tooling positions and the direction of tool movement. The U-channel forming operation is typically composed of closing the binder (downward), exerting the binder clamping force, moving the punch through a prescribed displacement time history (also known as a load curve), while maintaining the die in its initial fixed location. After this forming phase, the binder force is raised, the punch is retracted and the deformed blank is allowed to springback naturally through removal (deletion) of the tooling elements. The sequences described here are generally the same as the experimental procedure. The only boundary condition difference between the monolithic blank model and the TWB model is the application of the YZ plane symmetry condition in the monolithic blank model because only one-quarter of the whole geometry was modelled, while one-half of the TWB geometry was simulated. Thus the following description of the displacement and force boundary conditions holds true for both the monolithic and TWB models.

Displacement boundary conditions control the movement of the rigid tooling. The die position was maintained fixed by nodal constraints in X, Y and Z translational and X, Y and Z rotational degrees of freedom. The binder and the punch had the same constraints so that they were allowed to translate only along the Y direction, which corresponds to the

punch axis direction. A force equal to one-quarter of the total clamping force is applied to the binder for the quarter-symmetry monolithic blank model. A force of one-half of the total clamping load is applied for the half-symmetry TWB model. The binder motion was also subjected to a rigid body stopper constraint as described in Section 3.5.2. The punch movement was prescribed by a sinusoidal velocity profile (load curve) for a period of time determined so as to minimize inertial effects. A typical load curve for U-channel drawing in this investigation is plotted in Figure 3.11. The sinusoidal velocity serves to gradually accelerate the punch to a peak value and then decelerate the punch velocity to zero over the punch travel stroke. A parametric series of runs have been performed by Harpell and Worswick [90] to examine the effect of the accelerated punch speed on numerical simulations of the deep drawing process. Based on their results, a peak value of punch speed of 12 m/s was selected as an upper bound to prevent inertial effects from affecting the numerical solutions.

Figure 3.12 shows a typical load curve used to control the binder force for this investigation. The desired binder clamping force is rapidly achieved and remains constant throughout the forming stage. Figures 3.11 and 3.12 also show a time delay of 0.1 ms prior to motion of the punch, which allows the binder to fully close and the binder force to be engaged and stabilized. The load curves used in this investigation are summarized in Table 3.4.

3.3.2 Spotweld Constraints

In the TWB models, the weld line was defined in I-DEAS using rigid link elements which connected the nodes on the thinner and thicker side of the sheets, as shown in Figure 3.9. Note that the nodes lie on the mid-plane of each sheet and are therefore separated by a distance equal to one-half of the difference in sheet thicknesses. The rigid link elements were then translated as spotweld constraints in the LS-DYNA input file. A massless rigid

beam is assumed between the nodal pairs, thus nodal rotations and displacements are coupled.

This constraint is defined for both the LS-DYNA forming calculation and the subsequent LS-NIKE3D springback model.

3.3.3 Use of Shims in the TWB Models

Due to the difference in sheet thickness in the welded blanks, the action of the binder on the TWB can be uneven, with the majority of the binder force (BHF) acting on the thicker side of the TWB. This causes a very distorted draw-in, which occurs mostly at the thinner side of the TWB, while the flange on the thicker side of the TWB remains nearly stationary. In practice, two approaches are used to overcome this problem. One method is to adjust the gap between the thinner sheet and the die or that between the thinner sheet and the binder so that the BHF can be evenly distributed on the TWB. Usually this method is expensive, requiring modification to the tooling for each gauge combination. Another approach is to use a spacer, or a shim, which is a thin sheet placed between the thinner sheet of the TWB and the die. The thickness of the shim is the thickness difference between the thicker and the thinner sheet of the TWB so that the BHF will act evenly on the surface of the TWB.

Aluminum sheet was used as the shim stock in this investigation. In the models, the entire shim was free of constraints except for the symmetry condition applied along the XY symmetry plane. To maintain the appropriate contact between the deformed blank and the shim, the contact friction between the deformed sheet and the shim was defined as $\mu=0.21$ throughout the forming process (unlubricated contact) and that between the shim and the die was $\mu=0.06$ (Teflon). A summary of different finite element models of the shims, and of various models with different positions of the weld lines (at the tooling symmetry axis, around the punch radius, at the wall area, and around the die radius, respectively) is given in Table 3.3.

3.3.4 Mesh Convergence

A preliminary study of mesh convergence was performed before the actual runs for the springback research, to determine the effects of various aspects of the finite element discretization on the numerical predictions. Harpell and Worswick [90] have shown that the comparison of their work in cup drawing research with experiments demonstrated a reasonable agreement between the predicted and measured punch forces and predicted strains within the cup wall. Based upon the results of the convergence study and their work [90], the current mesh was determined to be acceptable. In addition, the effect of different accelerated punch velocities on the predictions was studied as well, and the punch load curves and the binder load curves used were judged acceptable.

3.3.5 Springback Interface

To facilitate springback prediction after forming, a material subset is defined in LS-DYNA for the subsequent implicit springback calculation in LS-NIKE3D. Upon termination of the forming analysis, LS-DYNA writes an input deck to LS-NIKE3D, which includes the node and element connectivity, the stress tensor, and the effective plastic strain, etc. for each integration point in the deformable material after the forming analysis. The stresses, strains, and nodal displacements from the LS-DYNA run are then used to initialize the model of the deformed blank for the springback calculations in LS-NIKE3D. An additional group of nodal points (at least two) are constrained to eliminate rigid body degrees of freedom.

To reduce the effect of large rotational moments occurring during springback, the Y symmetry axis of the deformed blank is determined as the location for pinning the nodes to eliminate rigid body modes. In this study, the nodal points along the symmetry axis of the deformed blank were fully constrained, allowing neither the translation in any direction nor

the rotation around any coordinate axis of these nodes.

For the monolithic blank model, the Y-symmetry axis of the deformed blank remains fixed after forming. For the TWB model, the deformed blank will be drawn in unevenly, and the weld line will deviate off the tooling axis and towards the thicker sheet. The degree of weld line movement is dependent on several factors, such as the binder force, the friction coefficient, and the sheet thickness discrepancy (thickness ratio) at each side of the weld line. A manual modification was required each time for properly locating the springback constraints of the nodes in the LS-NIKE3D input deck.

3.4 Constitutive Modelling

The material properties of the AA5754 and AA5182 were determined by combining uniaxial tensile test results for the anisotropy data (r value) and bulge test data for stress-strain relations (see Section 1.3.1). These properties were incorporated in the material data files used in the numerical simulations. Table 3.5 summarizes the material data used in the models. Note that material rate dependence was neglected in the current investigation.

The numerical simulations in current investigation have been carried out using the Barlat [45] constitutive law, which is implemented in LS-DYNA as material model Type 36. For plane stress conditions, the Barlat yield criterion can be reduced to the form of equation (1.21) as described in Section 1.3.6. The planar anisotropic model of Barlat non-quadratic constitutive law allows the effect of in-plane variation with direction in the sheet plane to be examined. The anisotropic material parameters a , c , h and p are determined from the Lankford's coefficients r_0 , r_{45} and r_{90} , as defined in Section 1.3.3, while the exponent M can be established as $M=8$ for FCC metals, based upon crystallographic plasticity considerations [45].

3.5 Contact Modelling

Intermittent contact and sliding boundary conditions between the sheet metal and tooling elements are enforced in the numerical simulations through the use of contact algorithms which limit penetration between the sheet metal and tooling surface. Penalty function-based contact interfaces, termed *slidelines*, are employed in LS-DYNA to define the elements corresponding to the two sides of the interfaces; that is, tooling and deformable sheet.

The LS-DYNA '*surface to surface contact*' option was employed to generate the contact interfaces. These were defined between the punch, die, and binder and the sheet, respectively. In the TWB models, the spacer (shim) used underneath the thinner side of the welded blank necessitated definition of contact interfaces between the sheet and the spacer, and between the spacer and the die. LS-DYNA calculates total contact forces acting across each interface which can be output to obtain, for example, punch force versus time history. The implementation of the thickness-based contact formulation limits the contact force from increasing to a level higher than needed for preventing penetration. Figure 3.13 is a schematic of the contact interfaces for TWB models with different weld line positions. Further description of the contact interfaces in LS-DYNA is given in [85].

The appropriate use of a viscous damping coefficient (VDC) in this study alleviated and even prevented the emergence of so-called hourglassing problems, therefore a stable solution was provided [85].

3.5.1 Friction Coefficients

In LS-DYNA, frictional effects across contact interfaces are usually implemented via a linear Coulomb formulation, which has the following form

$$f = \mu |f_n| \quad (3.1)$$

where f is the frictional force, μ the Coulomb coefficient of friction, f_n the normal surface contact force determined by the contact algorithm.

The Coulomb friction model in LS-DYNA uses a single constant coefficient for both the static and the dynamic friction across the contact interface. In this study, the friction coefficient is intended to represent the frictional behaviour when either the Teflon film or the Cromac lubricant oil was used between the blank and tooling surfaces. It is difficult, however, to take into account all aspects of the complex tribological processes occurring during the sheet forming operation [38, 47].

It was decided early in the investigation to make use of realistic frictional coefficients (COFs). The experimental work on the aluminum alloy 6111 (1 mm material) by the Metal Forming Technology Laboratory (MTL) of CANMET provided useful data [91] for specifying appropriate friction coefficients, as presented in Table 3.6. It is shown that this material has a strong surface texture, thus the range of the COFs with ALCAN pre-lube AL070 differs in the longitudinal and transverse direction. Since the melting point of this lubricant is low, temperature has a strong effect on the COF. The COFs tested at 31°C are quite different from those at 21°C. It is concluded from the tests that the lowest COF values for a very good lubricants are probably from 0.041 to 0.077. Therefore it was decided to use a COF value of 0.04-0.06 for a lubricant such as Cromac or AL070, a value of 0.07-0.09 for Lamson oil, and a COF of 0.06-0.07 is for Teflon film lubricant. Based on these results, with the exception of the parametric study of the variations of friction, a value of 0.06 was held as a "standard" friction coefficient in both the monolithic and TWB models. However, for the interface between the thinner side of the TWB and the shim, where no lubricant is used in the actual pressing, a μ value of 0.21 was adopted.

3.5.2 Rigid Body Stoppers

So-called "rigid body stoppers" were used to limit dynamic oscillation or "bounce" of the binder. Generally, oscillation of the binder can cause the premature localization as the

material is instantaneously pinned between the surfaces due to "bounce" of the binder. Throughout the simulations, rigid body stoppers were applied between the binder and the die to reduce the dynamic motion of the binder. The "stopper" limits the binder velocity to 0.01 mm/ms during the ramped loading of the binder to the binder force, and then enforces a maximum velocity of 0.001 mm/ms between the binder and the die over the forming process.

Chapter 4

4. Experimental Results

This chapter presents the results obtained from the U-channel experiments. Trends in the measured springback angles versus various forming parameters are presented for both monolithic blanks and TWBs.

4.1 Measured Springback for Monolithic Blanks

As described in Section 2.3, a series of tests at various binder forces using punches of different widths for monolithic blanks of different widths were conducted for AA5754 and 5182 blanks. The profiles of the deformed parts were extracted and the springback angles of the flanges and sidewalls were measured with respect to the horizontal and vertical axes, respectively. These measured springback angles were then plotted against the corresponding forming parameters to observe their effect on springback.

In order to examine the measurement error range (error band) and the validity of springback angles measured, several preliminary experiments were repeated under identical

forming conditions. The channel profiles were measured at both the left and right sides, and from both the front and back edges of the formed channel, after which the average value of the four springback angles was calculated for each experiment. Figures 4.1 and 4.2 are plots of the springback angles for 1 mm x 75 mm x 400 mm monolithic blanks of AA5754 under BHF of 7 kN and 12 kN, respectively. There is a consistent variation in springback angles on each side of the part. The right side flange angle was 4° to 5° larger than the left side, indicating a certain amount of press misalignment. In spite of this misalignment, the average springback angles tended to fall into a range of roughly $\pm 3^\circ$. Therefore it was decided to adopt the average springback angle as the measure of the final profile of the formed U-channels.

4.1.1 Effect of BHF

The measured springback angles for 1 mm x 400 mm blanks of various widths and under a series of BHFs are shown in Figure 4.3. The trend exhibited shows that with the increase of the binder force from a very small value of 2 kN up to 16 kN, both the flange and the sidewall exhibit a decrease in the springback angle. This is logical because a higher BHF has the effect of weakening the bending-induced compression component of the internal stress, which, when released, is the greatest contributing factor causing springback of deformed parts. The flange springback angle θ_1 decreases from 23° to 18°, while the sidewall springback angle θ_2 decreases from 17° to 11.6°, demonstrating similar sensitivity to variation of the BHF. Figure 4.3 serves to show the effect of BHF as well as specific width on springback angles. It must also be noted that there is a lack of smoothness in the correlation between θ_1 and θ_2 and the BHF; this is probably due to slight variation of test conditions. It was found in this series that for the narrowest 25 mm “strip”, a higher BHF than 12 kN will lead to a draw-in that is severely non-uniform in the two sides of the channel.

4.1.2 Effect of Lubrication Condition on Springback

Although Teflon films were used as the lubricant for the majority of the experiments, a limited number of experiments were performed with different lubricants. Figure 4.4 presents the springback results, using Teflon film, Cromac oil, and without additional lubrication for pre-waxed AA5754 blanks. The wax coating as pre-lubricant was insufficient for this forming operation since the U-channel was highly distorted as it was drawn into the die.

Another interesting phenomenon is that the Cromac oil provided a lower degree of springback after forming, compared to the Teflon. This reduced springback is attributed to a higher friction coefficient for the Cromac. Its use generated a more symmetrical U-channel. In addition, it was thought that thinning or tearing of the Teflon film during forming was one possible factor that causes the asymmetrical U-channel shape.

4.1.3 Effect of Blank Width on Springback

To further investigate the effect of different blank widths, Figure 4.5 plots springback angles for 1.0 mm thick AA5754 blanks with four widths, 25 mm, 50 mm, 75 mm, and 100 mm when tested under three BHF's, 4 kN, 8 kN and 12 kN, respectively. A systematic dependency exists between springback and the blank width, when other forming parameters remain unchanged. As shown in the graph, both θ_1 and θ_2 will increase with an increase in blank size. The curves all appear to asymptote above a blank width of 75 mm~100 mm, indicating that the 100 mm blank is adequate to simulate larger structural channel sections.

4.1.4 Effect of Blank Thickness on Springback

To examine the effect of blank thickness on springback, 1.0 mm, 1.6 mm and 2.0 mm

thick AA5754 blanks of dimension 75 mm x 400 mm were tested using Teflon as a lubricant. Figure 4.6 presents the results for a series of BHF's. The results demonstrate that springback will decrease with increased blank thickness for all BHF's used. For example, under a 4 kN BHF, the flange angle θ_1 decreases from 22° to 14° when the blank thickness increases from 1.0 mm to 2.0 mm. The wall angle, θ_2 , varies from 17° to 10° for the same change in thickness.

Assuming that the tooling geometry and other parameters remain fixed, a thicker blank means a larger bending rigidity and a smaller minimum bend ratio R/t (bendability). Under such a circumstance, the influence of BHF is overshadowed by the smaller R/t , as seen in the figure. The current experimental results are consistent with the published trend that the springback will decrease with a decreased R/t ratio [17, 31, 32].

4.1.5 Effect of Punch/Die Clearance (Die Gap) on Springback

The die cavity width remains fixed at 107 mm in the current experiments. Three punches were available with widths of 100 mm, 102 mm and 104 mm. Substituting punches has the effect of changing the punch/die clearance from 3.5 mm to 2.5 mm to 1.5 mm, respectively. In the tests, 1.6 mm x 75 mm x 400 mm AA5754 blanks were tested using 100 mm and 102 mm punches, and 1.0 mm x 25 mm x 400 mm AA5754 blanks were used for the 100 mm and 104 mm punches.

Figure 4.7 shows the results for the 1.6 mm x 75 mm x 400 mm blanks for several BHF's, while Figure 4.8 shows the results for the 1.0 mm x 25 mm x 400 mm blanks. It can be seen from both figures that springback angles θ_1 and θ_2 decrease somewhat for the wider punch width (or a narrower die gap). In Figure 4.7, when die gap changes from 2.5mm to 3.5 mm, θ_1 has an increase of roughly 0.5°, while the wall springback angle θ_2 increases by 1° to 2°.

Similarly, it can be seen from Figure 4.8 that the θ_1 and θ_2 also increase when the die

gap changes from 1.5 mm to 3.5 mm. Overall, it should be recognized that the effect of die clearance on springback is relatively mild and, in fact, within the scatter discussed in Section 4.1.

4.1.6 Measured Springback of AA5182 Blanks

Due to the limited availability of AA5182 sheet, only a few tests were conducted for AA5182 monolithic blanks. Figure 4.9 presents the results for 1.0 mm x 75 mm x 400 mm monolithic AA5182 blanks, using a 100 mm punch width with Teflon lubrication. The figure shows that the springback angles θ_1 and θ_2 are significantly larger than those of AA5754. It can be seen that springback angle θ_1 for AA5182 is 5° to 6° higher than for AA5754 under the same forming conditions,. In addition, angle θ_2 for AA5182 is 2° to 5° larger than θ_2 for AA5754. The higher level of springback exhibited by AA5182 can be explained by its higher ratio of yield stress to elastic modulus compared to AA5754 (see Figure 2.2 for stress/strain curve).

4.2 Springback in TWBs

After the experiments on monolithic blanks, a series of tests were performed on tailor welded blanks (TWBs) of dissimilar gauge thickness. Various arrangement of punch widths and binder forces were considered as well as various blank thickness ratios for AA5754. A multi-alloy blank consisting of 1 mm AA5182 welded to 2 mm AA5754 was also tested. The extraction of the profiles of deformed parts and the measurement of the springback angles of flanges and sidewalls were performed following the same procedures as for monolithic blanks. The measured springback angles were then plotted as functions of the various forming parameters considered.

Note that due to limitation in the welded blank sizes available for the current

experiments, all specimens were cut in a manner such that the weld lines would initially be positioned at the tooling line of symmetry. Thus the effect of different weld line positions on the springback of TWBs was not examined experimentally in the current work.

4.2.1 Effect of Dissimilar Thickness (Thickness ratio) on Springback

Three configurations of TWBs were tested: 2.0 mm AA5754 welded to 1.6 mm; 1.6 mm welded to 1.0 mm; and, 2.0 mm AA5754 welded to 1.0 mm AA5182. Unfortunately, 2.0 mm to 1.0 mm AA5754 welded blanks were not available for this work.

Figures 4.10 and 4.11 present the effect of dissimilar thickness (or thickness ratio) on springback in TWBs, under 20 kN and 40 kN BHF, respectively. It is observed that the TWBs demonstrate a different trend of springback from that exhibited by monolithic blanks. First of all, the larger springback tends to take place at the thick side of the TWB. The springback angles of the thick side are usually 2° to 4° larger than those of the thin side. This phenomenon is surprising since the springback angles in monolithic blanks decreased with increased thickness (see Figure 4.6). It is likely that differences in forming the TWBs, such as the use of shims underneath the thin side of TWBs, could account for this phenomenon. Secondly, the springback angles do not illustrate a clear relation with the thickness ratio. For example, the largest springback of both the flange and the wall for both 20 kN and 40 kN BHFs occurred in the 1.6:1.0 TWB of AA5754, whose thickness ratio is intermediate to the two other blanks considered. Results for the 2.0:1.0 blank are likely confounded by the use of higher strength AA5182 in the thinner sheet which would tend to reduce the effect of the thickness difference.

4.2.2 Effect of BHF on Springback

Figure 4.12 shows the effect of BHF on the springback of TWBs. As seen from the

figure, both the flange (θ_1) and sidewall (θ_2) demonstrate a decrease in springback when the BHF is increased from 10 kN to 50 kN for AA5754 TWBS of 1.6 mm welded to 1.0 mm. This result is consistent with that obtained for monolithic blanks (see Figure 4.3). Beyond a 20 kN BHF, however, the variation in springback angle is small, while the difference in angles θ_1 and θ_2 between the thin and thick sides remains consistent and largely independent of BHF.

Chapter 5

5. Numerical Results

This chapter presents the results from the numerical simulations described in Chapter 3. The FEM results for the springback behaviour of aluminum alloy TWBs with a set of dissimilar thicknesses and weld line positions under the U-bending/drawing condition, are outlined in this chapter. The results of a parametric study of the effects of the BHF, friction coefficient, and tooling geometry on the springback and sidewall curl of TWB and monolithic blanks are presented as well. Finally, the predictions are compared with the experimental measurements.

5.1 Parametric Springback Results for Monolithic Blanks

Prior to undertaking detailed investigation of forming and springback of TWBs, a parametric study was performed to numerically investigate the effect of a variety of material properties and process variables on sheet metal forming operations. Of particular interest is their influence on springback in U-channel forming of monolithic blanks. The parameters

studied include tooling geometry, blank geometry, binder force, friction coefficient, and FEM element mesh.

Figure 5.1 shows the predicted effect of blank size on springback for a series of punches. It can be seen that the flange springback angle θ_1 will positively increase by $2^\circ\sim4^\circ$ with increased blank width, while the variation of the sidewall springback angle θ_2 is limited, with a change of roughly 1° when blank width varies. Figure 5.2 shows the predicted effect of tooling clearance varied by using different punch widths, and indicates that both the flange springback angle θ_1 and the sidewall springback angle θ_2 decrease with increased die gaps; θ_1 decreases by 3° when the clearance changes from 1.5 mm to 3.5 mm, while θ_2 decreases only by 1° . Overall, the effect of die gap on the springback of monolithick blanks is quite mild.

Figure 5.3 shows the effect of blank thickness on the final shape and on the springback of monolithic blanks for a BHF of 10 kN. The figure shows clearly that springback will decrease for thicker blanks. This behaviour is logical because a thicker blank needs more bending moment in order to overcome the bending rigidity to achieve a degree of springback equal to that of a thinner monolithic blank. It also shows that when the blank thickness increases from 1 mm to 2 mm, springback angles drop considerably, however, the channel bottom has more upward springback when the blank is thicker.

Figure 5.4 demonstrates the effect of BHF on final U-channel profiles and springback. The close up view of the channel profiles clearly demonstrates that the springback will reduce when the BHF gradually increases. The increase in BHF from 2 kN to 16 kN causes a decrease in predicted springback angles by about 5° for both θ_1 and θ_2 . This prediction is consistent with published results, as seen in Figure 1.3. Figure 5.5 shows the effect of friction (μ -value) on final U-channel profiles and springback. When the COF increases from 0.01 to 0.10, the springback angles decrease by $5^\circ\sim7^\circ$. This result is expected since the binder force exerts a restraint on drawing of the sheet through friction. When the COF increases further until a value of 0.21, which corresponds roughly to a dry lubrication,

the springback angles do not show any significant further increase. At this level of restraint on draw-in, the bending stresses are reduced substantially.

5.1.1 Mesh Convergence

To examine mesh convergence, a refinement of the finite element mesh was performed in the early stage of this research. The re-meshing focussed on the regions near tool radii with the effect of halving the element size locally. In this convergence study, the number of nodes and elements of deformable blank (quarter) mesh are initially 606 and 500, respectively, while there are 1407 nodes and 1200 elements in the remeshed blank model. Due to the relatively simple geometry of this model, no apparent discrepancy is observed in the springback angles, as shown in Figure 5.6. However, in all TWB models, the finer mesh was used in an attempt to ensure mesh convergence.

5.2 Predicted Final TWB Profiles

In order to better describe the nature of the FEM results for the TWBs, predictions are described in detail here for one tooling and blank size; that is the 100 mm width punch and 107 mm width die using 100 mm x 400 mm blanks with a 1.6:1.0 thickness ratio. This section concentrates on the results of predicted final shapes of U-channel profiles after springback and the forming parameters affecting these final shapes. The degree of springback and sidewall curls occurring in these TWBs will be specifically discussed in Section 5.3.

5.2.1 Predicted Final Shape of TWBs

A plot of the predicted final shape of a U-channel, from a 100 mm x 400 mm TWB

of AA5754 with the weld line initially positioned along the YZ-symmetry plane (the punch centreline), is shown in Figure 5.7. The translation of the weld line across the punch is due to different degrees of draw-in due to the different thickness of the blank on either side. The predicted profile of the XY-plane symmetry line of the TWB is presented in Figure 5.8; also shown in this figure is the predicted profile of the sidewall along the Z-direction at a height of 35 mm from the U-channel bottom. The profile in the XY-plane shows the significant predicted springback and sidewall curl, whereas the profile in the XZ-plane indicates the lateral sidewall curl.

Several general observations are made in terms of the springback behaviour of the TWB after unloading. It is clearly shown that the flange (beyond the die radius) of the U-channel remains very flat at both sides of the sheet metal, with a downward springback, while the wall (between the die radius and the punch radius) illustrates both a outward springback in the XY-plane and some degree of sidewall curl in both the XY- and XZ-planes. Although the deformation and the elastic recovery of the U-channel bottom itself is small, upon the removal of tools the residual strains within the deformed part force the part to rotate about the pin points (at the U-channel bottom), which gives rise to quite large displacements. The springback and the XY-plane sidewall curl is a direct consequence of these displacements, however, the XZ-plane sidewall curl in the current investigation appears not to be generated during the elastic recovery of the part, but occurs during the forming process.

5.2.2 Effect of Dissimilar Thickness

Figure 5.9 presents the predicted final profiles of the U-channel in both the XY- and XZ-planes, for three sets of dissimilar thicknesses: 1.6 mm welded to 1 mm, 2 mm to 1.6 mm, and 2 mm to 1 mm. Unlike the results for the monolithic blanks, that springback decreases with the blank thickness, this figure shows that for all three thickness combinations, larger springback takes place at the thick sides of TWBs, as described in the

general observations above. It also indicates that the thick side of the 2.0/1.6mm TWB has the largest springback, while the thin sides do not demonstrate significant difference in terms of springback angle. When the dissimilarity of TWBs, which is characterized by thickness ratio (defined as the ratio of the thick sheet thickness to the thin one, t_1/t_2) becomes larger, the channel bottom does not wrap around the punch profiles very well, as seen in the channel profile of the 2.0/1.0mm TWB in figure 5.9 (a).

The sidewall curl in the XZ-plane remains limited for all three models. Unlike springback and XY-plane curls, the XZ sidewall curl is generated during forming, instead after unloading. Presumably, then, the XZ-plane sidewall curl is limited by the punch-die clearance.

As seen in Figure 5.3, the springback decreases with blank thickness for monolithic blanks. This paradoxical results for the TWBs is partially explained by the choice of the location of the pinning points at the tooling symmetry position for the TWB. Springback calculations thus introduce an apparent tilt in the U-channel as seen in Figure 5.3.

Generally, the final shape or the degree of springback of a formed part is determined by the residual elastic strain within the formed part, which is caused by the stresses and plastic flow at the last step of forming. Figure 5.10 shows the plot of the effective stress distribution along the XY symmetry lines of the channel profiles for the three models. Because the overall stress level at the flange is so low as to be nearly zero, only the stress distribution within interesting areas (from die radii area to channel bottom) is plotted, so that a better comparison can be made for the three models. Several trends are demonstrated in the figure for the three thickness combinations. Larger overall effective stress within the 2.0/1.6 mm TWB lead to a larger degree of springback, as seen in the unloaded channel shape. The U-channel forming of TWBs demonstrates a difference on either side of the weld line, due to the variation of the effective stress for different gauge thicknesses. The thick side of the TWB has generally larger stress values than does the thin side for all three cases, which cause more springback at the thick side. As well, there exist two peaks, which

correspond to the punch and die radii areas, at both the thick and the thin sides, and the overall average stress level is about 200 Mpa. The differences in the stress levels, especially in the peak values at the tooling radii areas, lead to the difference in final shape and springback of the three TWBs. However, the symmetry of the stress distribution at the two sides improves with a decreasing thickness ratio; thus, the 2.0/1.6 mm TWB appears more symmetrically shaped after forming.

5.2.3 Effect of Weld Line Position

TWBs used in commercial forming operations can be cut from a large welded sheet in any specific location, which does not necessarily guarantee that the weld line of the TWB is in a position aligned with the punch symmetry axis. In this study, the weld line was also modelled in different locations, as shown in Figure 5.11, using 1.6 to 1.0 mm blanks.

The predicted profiles in the XY and XZ planes for 1.6/1.0 mm TWBs with the weld line being at various different positions are presented in Figure 5.12. It can be seen that there is a substantial difference in springback behaviour in the XY-plane caused by weld line placement, whereas the curls in XZ-plane remain small and similar. The profiles of the TWBs with the weld line off the tooling symmetry axis demonstrate a larger springback at both the flange and the wall, particularly at the flange area, than that with the weld line at the tooling symmetry line. Also, the channel shape becomes more and more symmetrical (like monolithic blanks after springback) when the weld line position shifts gradually from the tooling symmetry axis to around the die radius, despite the increase in the degree of springback itself.

Different weld line positions affect the contact forces between the tooling and workpiece interfaces, which, in turn, determine the strain distribution within the TWB and the final shape and the degree of springback. Figure 5.13 is a fringe plot of the effective plastic strain in the entire U-channel for different weld line placements. It shows that the

average strain level is approximately 15% and 12% at the wall area for thick and thin side, respectively, which leads to more springback at thick sides than at thin sides for these models. However, the strain distribution appears to be more symmetrical about the tooling symmetry axis when the weld line shifts from at the tooling symmetry to around the die radius. The plots also show that with the shift of the weld line, the average strain level at the wall and the strain peak values at the tooling profile radii areas have a slight but gradual increase, which corresponds to the change of the final channel shape and the increase of springback in these models. It is logical that when the weld line is eventually around the die radius, the final channel shape of this type of TWB is more symmetrical because the same thickness sheet is the only material in the die cavity.

5.2.4 Effect of Binder Force (BHF)

In the U-channel forming process, the BHF acts to control the flow of the material into the die cavity, therefore its effect on the springback behaviour of TWBs is significant. To examine this effect, several different BHF's were utilized from a relative low value of 6 kN to a quite high value of 50 kN (without material failure), for a 1.6/1.0 mm TWB with the weld line initially located at the tooling symmetry axis.

Several representative XY and XZ profiles of the channel are plotted in Figure 5.14. It is observed that with the increase of the BHF's within a low range, the springback will decrease, which is consistent with the trend exhibited by the monolithic blanks (see Figure 5.4). However, when the BHF approaches a value of 50 kN for the model, the mode of the springback is changed; the flange at the thick side springs back upward (a negative angle as defined in subsection 3.1), which occurs rarely in the current investigation. In current investigation, the final shape of the channel will remain "normal" until a 45 kN BHF. beyond this level the BHF is too high and extremely uneven draw-in causes the majority of the thin side to be drawn into the cavity, while much of the thick side is left out of the die cavity. The

sidewall curl is much reduced when the BHF increases, since more stretching stress component is involved in the sidewall area, undermining the bending effect.

When the BHF is too small, deformation is limited and often the desired final part shape cannot be achieved. When the BHF is too large, excessive draw-in results and the deformable blank can fail. This phenomenon suggests that for a given material, tooling geometry, TWB size and friction, there may exist an optimal BHF to balance springback against failure.

5.2.5 Effect of Clearance

The effect of clearance was also investigated by comparing predictions using various punch widths. The 1.6/1.0 mm TWB model was considered, with the weld line being initially positioned at the tooling symmetry axis. Only the 100 and 102 mm wide punches were modelled, the 104 mm wide punch being too wide to accommodate a 1.6 mm thickness.

The XY and XZ profiles of the channel are plotted in Figure 5.15. It is observed that the overall final shape of the U-channel using the 102 mm punch (2.5 mm clearance) is closer to the ideal shape, compared to that of the 100 mm punch model. Therefore the springback angles will reduce with the narrower clearance, especially in the flange area. Also the XZ curl, particularly at the thick side of the TWB, is smaller for a narrower clearance, as expected. The deviation of the weld line position off the tooling symmetry axis is decreased also. However, due to the narrower tooling clearance combined the friction around the punch radius, the material at the channel bottom is not stretched adequately to ensure that the welded sheet wraps tightly around the punch nose.

From the strain and stress distributions, presented in Figure 5.16, it can be seen that the decrease of the die gap has the effect of lowering the overall strain level along the channel profile. Also, the effective stress at the end of the forming decreases considerably with the narrower clearance, resulting in a decreasing springback, as seen from Figure 5.15.

This springback result seems to contradict the predictions for monolithic blanks, where a narrower clearance definitely causes a larger springback (see Figure 5.2). However, given the fact that in TWBs the controlling factor influencing springback is the dissimilar thickness, which generates a strain distribution different from that in monolithic blanks, the difference in results between monolithic blank and TWBs are reasonable.

5.3 Summary of Predicted TWB Springback Trends

This section presents a summary of predictions of springback angles and sidewall curvatures in the XY-plane, versus a number of forming parameters and TWB properties. The XZ-plane sidewall curl is not considered in this section because it occurs during forming, as is not affected by springback, as described previously.

5.3.1 Predicted Springback versus Thickness Ratio and Weld Line Position

Figure 5.17 shows the plots of springback angles and curvatures against the thickness ratio. A 2.0 ratio represents the 2.0/1.0 mm TWB, 1.6 the 1.6/1.0 mm TWB, and 1.25 the 2.0/1.6 mm TWB. When the weld line is initially located at the tooling axis, it shows that an increase of the thickness ratio leads to a decrease in the predicted springback angles, θ_1 and θ_2 , on the thick side of 5°, while the angle at the thin side do not have obvious variation.

The predicted sidewall curvatures in the XY-plane generally increase with increasing thickness ratio, regardless of the weld line position.

Overall, it is difficult to find an apparent relation between predicted springback angles and TWB thickness ratios when various weld line positions are considered. This is most likely because different weld line placements need different sizes and locations for the shims; their interaction with dissimilar thickness complicates the U-channel forming process such that the draw-in from the two flanges differs considerably when the thickness ratio

changes, thus leading to an uneven springback. However, for a given thickness ratio and other forming conditions, the gradual shift of the weld line position has a straightforward correlation with springback angles, as shown in Figure 5.18. The shift of the weld line position off the geometric symmetry results in an incremental trend for both θ_1 and θ_2 for both the thick and thin sides of the TWB.

5.3.2 Predicted Springback versus Binder Force (BHF)

The BHF has a significant influence on the springback behaviour of TWBs as well as on monolithic blanks. Figure 5.19 is a demonstration of the effect of BHF on predicted TWB springback. It is shown that for a given forming condition, springback angles of TWBs will decrease with the increase of the BHF, which is consistent with the results obtained from monolithic blanks.

5.3.3 Predicted Springback versus Die Gap

Figure 5.20 is a plot of the effect of die gap on predicted springback. It is shown that a narrower die gap leads to smaller springback angles at both the thick and thin sides of the TWB and for different weld line positions. However, the sensitivity at the two sides varies, with the thin side exhibiting relatively smaller changes of springback angles brought by die gap adjustment. The general trend of TWB springback with die gap is opposite to that for monolithic blanks, as discussed at the end of subsection 5.2.5. This difference is attributed to the lower predicted residual elastic strains in the TWBs for a narrower die gap.

5.4 Comparison between Predicted and Measured Springback Angles

This section compares the experimental results presented in Chapter 4 with the

numerical predictions of springback behaviour for both monolithic and tailor welded blanks (TWBs). The comparison is made mainly between the experimental springback angles and punch forces and the numerical predicted results for AA5754. Due to the fact that the experimental conditions do not fully match the numerical simulation conditions in every testing case, not all of the cases can be compared.

5.4.1 Comparison between Monolithic Blank Results

Figure 5.21 is a comparative plot of measured and predicted springback angles versus BHF, using the 1.0 mm x 75 mm x 400 mm AA5754 blank with the 100 mm punch and 107 mm die. The BHF used for the comparison ranges from 2 kN to 16 kN, which is a reasonable BHF range to ensure that the bottom of the blank can appropriately wrap around the punch radii without failure or uneven draw-in. The lubricant utilized in the experiments was Teflon film, which was modelled using a COF of 0.06. It is shown that the general predicted trend within this BHF range agrees well with the experimental results; that is, for a given forming condition, both the flange θ_1 and the sidewall θ_2 will decrease with an increase of the BHF. There is, however, a general underprediction of the springback angle. There is a difference of about 6° for θ_1 and 3° to 6° for θ_2 between the numerical and experimental results.

It should be noted that the springback results are very sensitive to the combined application of the BHF and the COF. In Figure 5.5, Even small changes in COF have large change in predicted springback. These small errors in the COF for Teflon could account for these differences.

Figure 5.22 compares the measured and predicted effect of blank thickness on springback. 75 mm x 400 mm AA5754 blanks with 1.0 mm, 1.6 mm and 2.0 mm thickness were examined. Both the experimental and the numerical results clearly show that both the flange θ_1 and sidewall θ_2 will decrease with increased blank thickness. As seen from the

figure for the 1.0 mm blank, the predicted value of θ_1 is 14° , compared to a measured value of about 21° . Similarly, the predicted value of θ_2 is about 9° , compared to about 15° for the experiments.

5.4.2 Comparison between TWB Results

For the experimental validation of the U-channel springback predictions for tailor welded blanks (TWBs), the effects of dissimilar thickness (thickness ratio), BHF, and die gap are considered. As explained in Section 4.2, due to the limited availability of various TWBs, all experiments were conducted using TWBs with weld lines initially positioned at the tooling symmetry plane.

Figure 5.23 compares the predicted and measured punch force time histories for AA5754 TWBs with different thickness ratios using the 100 mm punch and 107 mm die. It shows that the numerical predictions agree very well with the experimental results, including the capture of the maximum punch force and some fine variations of the punch force.

Figure 5.24 serves to compare the effect of thickness ratio on the springback of TWBs. Three dissimilar thickness combinations, 1.6 mm with 1.0 mm, 2.0 mm with 1.0 mm, and 2.0 mm with 1.6 mm of AA5754 (note that the 2.0/1.0 mm TWB is of AA5754 welded to AA5182), were used under the conditions of 40 kN BHF with the 100 mm punch and 107 mm die. For the 2.0/1.0 mm and 2.0/1.6 mm TWBs, the numerical results give good agreement with the experiments, while the predicted and experimental results disagree for the 1.6/1.0 mm TWB. The numerical predictions can be considered to be reasonable nonetheless because the simulations do predict that the larger springback usually occurs at the thick side of the TWB. In addition, the absolute error between the numerical and experimental results is no greater than that observed for the monolithic blanks.

The predicted and measured effect of BHF on the springback of the 1.6/1.0 mm

AA5754 TWBs is presented in Figure 5.25. Within the range of applied BHF in the experiments, the numerical predictions of the sidewall angle θ_2 at both the thick and thin sides of the TWB demonstrate moderate agreement with the experimental results, while the numerical simulations underestimate the flange angle θ_1 at both the thick and thin sides of the TWB, with an absolute error of up to 6.5° at the thin side for a 20kN BHF. As described previously, the effect of BHF on the springback of TWBs is sensitive to and interactive with other process parameters. The numerical-experimental error of θ_1 at the thin side could be due to the complex lubrication condition and the re-distribution of the imposed BHF caused by the use of the shim.

Figure 5.26 presents an assessment of the effect of die gap on the springback of 1.6/1.0 mm AA5754 TWBs under a 40 kN BHF. Only the 100 mm and 102 mm punches were used. It is shown that both the experimental and numerical values for θ_1 and θ_2 increase when the die gap increases from 2.5 mm to 3.5 mm. The numerical underprediction, however, still exists, as seen from the figure. The largest error between the numerical and experimental results occurs for the θ_1 at both the thick and thin sides of the TWB, with a difference of roughly 7° when the die gap is 2.5 mm (102 mm punch).

Chapter 6

6. Discussion and Conclusions

6.1 Discussion

The experimental work has served to demonstrate the effectiveness of using an explicit finite element code, such as LS-DYNA, to predict material performance in a sheet metal forming process. The results also support the combined use of explicit and implicit FEM programs, such as LS-NIKE3D, to analyze springback behaviour after forming. The predicted springback results using the implicit FEM code LS-NIKE3D are somewhat ambivalent. Based upon the results presented in the previous chapters, the current model has been shown to provide a reasonable and effective representation of the U-channel forming experiments and the springback behaviour of both monolithic sheet and tailor welded blanks. There are, however, a great number of factors which can substantially influence the simulation of sheet metal forming processes, including the choice of an appropriate constitutive model, the selection of a proper finite element formulation, and numerous process variables such as friction and lubrication, binder force, and contact conditions. Their

effects on springback of sheet metals after forming are complex and significant. The current investigation focuses on the effects of forming process variables on the springback.

Several significant experimental parameters, such as the punch force/time history, were found to have very good correlation with the numerical results. In addition, the general trends of the springback behaviour for both monolithic blanks and TWBs have reasonably good qualitative agreement with the numerical predictions. However, the numerical models tend to under-predict springback in terms of the magnitude of springback angles. This becomes more apparent when wider or thicker blanks are compared. In the numerical simulations, the channel profiles are extracted from the XY symmetry lines, while in the experiments, the channel profiles are taken from the outside edges of the formed parts. In the numerical simulations the XY symmetry line is under a plane strain condition, while the outer areas of the deformed parts are subject to a 3D strain condition due to the XZ sidewall curl. Also, in the experiments, it was difficult to obtain a symmetrical channel profile for the monolithic blanks as in the numerical simulations, which could account for some of the differences with the model.

In the parametric study of monolithic blanks, the numerical simulations did give results that were in good agreement with experiments, in terms of the general trends of springback versus a variety of forming variables. When Teflon films were used as a lubricant, the effect of varied BHF was clearly captured by the current model, showing a decreasing springback when the BHF increases. The current numerical model also captured the springback trend quite well for blanks with different thickness, showing a decreasing springback for thicker monolithic blanks.

Overall, the current numerical models did not accurately predict the magnitudes of springback angles for the monolithic blank. The numerically predicted springback angle was approximately 3° to 7° smaller than the experimentally measured values. This consistent error is largely attributed to the error in the friction coefficient (COF).

The numerical investigation into TWB springback, which behaves in a more complex

manner, has not been considered by others to date. The current numerical models have been shown to provide reasonable predictions of springback for TWBs of dissimilar thickness and of different weld line positions. It must be noted that although the parametric correlation between the springback angles and process parameters, such as BHF, still holds true, results have shown that TWBs demonstrate different springback trends from that exhibited by monolithic blanks. The current numerical models did predict the unloaded profiles of TWBs with reasonable fidelity, including the effects of dissimilar thickness and different weld line positions on springback. However, as for the numerical simulations of monolithic blanks, the predicted springback in TWBs was also lower than the experimental results in most cases.

Several possible factors contribute to these numerical-experimental discrepancies. In the current model, the “regular” Coulomb frictional formulation was utilized and thus the appropriate selection of the COF μ becomes critical. Based upon previous simulation experience, a COF of 0.06 was adopted in an attempt to represent the frictional behaviour of Teflon films. Experimental validation of this value is quite difficult, and the frictional nature of Teflon films may change within different deformation zones as the forming process progresses.

Numerically, some improvements could be made. Although the FEM mesh used for the current investigation can be deemed fine enough to supply consistent results, the shell formulation used in the current simulations may not be sufficient to precisely capture the subtle bending effect around the tooling profile radii. Despite of the fact that seven integration points were used through the shell thickness, it is still possible that the behaviour of sheet metals in the cross-sections were not fully captured by current models.

Another possible source of the variability of TWB springback results is the determination of the location of the pin points in the springback analysis, which requires a manual modification each time in assigning the nodal springback constraints to account for the deviation of the weld line caused by the non-symmetrical draw-in. Although it is

appropriate to locate the pinned nodes at the tooling symmetry position, it is not possible to ensure that the locations of the pinned nodes are exactly aligned with the tooling symmetry axis.

In general, the test facility at the University of Waterloo does provide good results, however, several experimental aspects require improvement. As discussed previously, the drag existing in the sliding movement of the posts of the die set may lead to a draw-in that is not symmetrical and has significant influence on the final shape of deformed parts. The numerical-experimental disagreement may be due to such problems in the experiments. A certain extent of mechanical misalignment is more or less always a problem for presses.

6.2 Conclusions

From the current research the following conclusions can be drawn:

1. FEM simulations of sheet metal forming using an explicit finite element code have provided reasonably accurate predictions of the U-channel forming operation, and process history parameters such as punch force versus displacement histories.
2. The coupled use of explicit and implicit FEM codes can drastically reduce computational costs and provide a reasonable prediction of the general trends of the springback behaviour of aluminum alloys.
3. For a given combination of tooling geometry, blank size, binder force and lubrication condition, a thinner monolithic blank has a larger degree of springback than a thicker blank.
4. Springback will increase with increased width of monolithic blanks, when other

forming parameters are unchanged.

5. Increasing the binder force (BHF) has a similar effect on springback as increasing the friction coefficient (COF). In both cases, the springback is reduced, provided that the BHF or COF is within an appropriate range. It must be noted that the combination of BHF and COF is also the most significant and sensitive factor in this study of springback. Although the typical range of the COF is 0.05 to 0.10, springback will change considerably with even a small variation of the COF.
6. Tailor welded blanks (TWBs) demonstrate a different trend for springback both in the numerical simulations and in the experimental tests. Namely, the larger springback tends to occur at the thicker side of the TWB.
7. The thickness ratio and different weld line positions of a TWB of dissimilar thickness affect springback considerably; when weld line placements shift gradually from the tooling symmetry position to around the die radius, the springback increases, but the final shapes of parts are more symmetrical.
8. TWBs have the a similar response as do monolithic blanks to the variation of process parameters, such as BHF and COFs.
9. AA5182 sheet demonstrates a larger extent of springback than AA5754 does for both monolithic blanks and TWBs, both numerically and experimentally, due to a higher ratio of yield strength to elastic modulus of AA5182.

6.3 Future Work

An attempt has been made in the current investigation to accurately simulate the springback behaviour of monolithic blanks and tailor welded blanks (TWBs) of aluminum alloys, via the coupled use of both explicit and implicit finite element codes.

It is recommended that future work include investigation of alternative friction models in order to better capture the real lubrication conditions at different locations of the deformation zones and those at different deformation stages, since the friction coefficient (COF) is one of the most significant factors that influence springback behaviour.

The current investigation considered sheet metals of different thicknesses and punches of different widths, however, the tooling radii, r_p and r_d remain unchanged. It is thus desirable to modify the punch and die profile radii in the future to better assess the springback behaviour of aluminum alloys under different bending ratios, especially when the relative bending radius is very small.

A number of forming parameters that affect springback of sheet metals are interactive during the forming process; thus, it is highly likely that there exists an optimal combination of these variables which is able to minimize springback so that a better net shape of the deformed part can be acquired. Therefore future work should consider the optimization of sheet metal forming processes to control and minimize springback.

In addition, concern exists over the alignment of the press used in this experiment. Further work should consider improvements to the clamping system of this press.

Finally, further experiments should be considered for more aluminum alloys, and more experiments should be conducted, particularly on tailor welded blanks with different weld line positions. The extraction of the profiles of deformed parts should use digital means so that the springback angles and sidewall curl can be measured more accurately. Strain measurements of deformed parts should also be included in future work.

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Table 2.1 Summary of tooling geometries and blank sizes of monolithic blanks (M) and TWBs (W) considered for numerical simulations (N), and for experiments (E) at University of Waterloo

	100 mm punch	102 mm punch	104 mm punch
25 mm blank	MN, ME	MN, ME	MN, ME
50 mm blank	MN, ME	MN, ME	MN
75 mm blank	MN, WN, ME	MN, WN, ME	MN
100 mm blank *	MN, WN, ME,	MN, WN, ME, WE	MN

* TWBs have three thickness combinations: 1.6/1.0 mm, 2.0/1.0 mm, and 2.0/1.6 mm.

Table 2.2 Uniaxial material properties of AA5754-O and AA5182-O

Property	AA5754-O	AA5182-O
Thickness (mm)	1.49	1.49
Yield Stress (MPa)	90~110	110~130
UTS (MPa)	215~230	255~290
Lankford Coefficient, r	0.7~0.9	0.6~0.7
Uniform Elongation	18%~24%	-
Total Elongation	21%~27%	28%~31%
Strain Hardening Exponent, n	0.28~0.31	0.25~0.28
Minimum Radius/Thickness	0.0~0.3	0.2~0.5

Table 2.3 Chemical compositions of AA5754-O and 5182-O

Chemical Compositions (weight%)									
Alloy	Mg	Si	Cu	Fe	Mn	Cr	Ti	Zn	other
AA5754-O	2.6~ 3.6	≤0.4	≤0.1	≤0.4	≤0.5	≤0.3	≤0.1	-	-
AA5182-O	4.0~ 5.0	0.2	0.15	0.35	0.2~ 0.5	0.1	0.1	0.25	0.15

Table 2.4 Uniaxial tensile test data for AA5754-O and AA5182-O* [47]

Specimen	Yield Stress (0.2%) (MPa)	UTS (MPa)	<i>n</i> Value	<i>r</i> Value (10%)	Uniform Elongation	Total 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%
5182 (0°)	119.6	287.7	0.29	0.75	18.94%	20.93%
5182 (45°)	124.7	291.3	0.28	0.84	23.44%	25.26%
5182 (90°)	121.4	288.7	0.29	0.85	21.24%	24.35%

* Sheet thickness 1.49 mm

Table 2.5 Average uniaxial material properties of AA5754-O and 5182-O alloys [47]

Property	AA5754-O	AA5182-O
Thickness (mm)	1.49	1.49
Yield Stress (0.2%) (MPa)	104	122
UTS (MPa)	225	289
Anisotropy Parameter, $\bar{r}$ * (10%)	0.747	0.82
Δr *	0.046	0.04
Uniform Elongation	19%~24%	18%~24%
Total Elongation	22%~27%	20%~26%
Strain Hardening Exponent, n	0.31	0.287
Young's Modulus (GPa)	70.3	70.3

$$* \quad \bar{r} = \frac{r_0 + 2r_{45} + r_{90}}{4} \quad \Delta r = \frac{r_0 - 2r_{45} + r_{90}}{2}$$

Table 2.6 Summary of blank sizes of monolithic and TWBs used in experiments *

Blank Type	Blank Thickness (mm)	Blank Width (mm)
monolithic	1.0, 1.6, 2.0	25, 50, 75, 100
TWB	1.6/1.0, 2.0/1.0, 2.0/1.6, 1.5/0.8 **	100

* All specimens tested are 400 mm long

** 1.6/1.0 etc. indicates the dissimilar thickness at the two sides of the weld line

Table 2.7 Summary of U-channel experiments for monolithic and TWBs at the University of Waterloo 250 kN Press Facility *

Blank Dimension (mm x mm)	Material	BHF's (kN)	Lubricant	Punch Width (mm)	Punch Stroke (mm)	Punch Speed (mm/s)
Monolithic Blanks (all 400 mm long)						
1.0 x 25	5754	2,4,6,8,12,16	Teflon	100	65	1.083
1.0 x 50	5754	2, 8, 12, 16	Teflon	100	65	1.083
1.0 x 75	5754	4,5,7,8,9,12,16	Teflon	100	65	1.083
1.0 x 100	5754	4, 8, 12, 16	Teflon	100	65	1.083
1.0 x 75	5754	1, 2, 4, 8	Pre-waxed	100	65	1.083
1.0 x 75	5754	2, 4, 8, 12, 16	Cromac oil	100	65	1.083
1.6 x 25	5754	4, 8, 12	Teflon	100	65	1.083
1.6 x 75	5754	4, 6, 8, 12	Teflon	100	65	1.083
2.0 x 25	5754	8, 12	Teflon	100	65	1.083
2.0 x 75	5754	4, 8, 12, 16	Teflon	100	65	1.083
1.6 x 75	5754	2, 4, 8, 12, 16	Teflon	102	65	1.083
1.0 x 25	5754	2, 4, 8, 12	Teflon	104	65	1.083
1.0 x 75	5182	4, 8, 12, 16	Teflon	100	65	1.083
TWBs (all 100 mm wide, 400 mm long)						
1.6/1.0	5754	10,20,30,40,50	Teflon	100	70	1.167
2.0/1.0	5754 **	20, 40	Teflon	100	70	1.167
2.0/1.6	5754	20, 40	Teflon	100	70	1.167
1.5/0.8	5182	20, 40	Teflon	100	70	1.167
1.6/1.0	5754	10,20,30,40,50	Teflon	102	70	1.167

* Die throat width 107 mm, punch and die profile radii 10 mm are fixed

** Only the 2.0 mm side is of AA5754, the 1.0 mm side is of AA5182

Table 3.1 Summary of FEM representations of the quarter monolithic blanks used in U-channel forming (LS-DYNA) and springback (LS-NIKE3D) simulations *

Blank Width (mm)	25	50	75	100
# of Elements	1600	3200	4800	6400
# of Nodes	1771	3381	4991	6601

* Models of 1.0 mm, 1.6 mm and 2.0 mm thick blanks have the same number of elements and nodes, though they represent three different models

Table 3.2 Summary of FEM representations of the quarter tooling finite element meshes and various combinations of tooling and blanks used in U-channel forming (LS-DYNA)

# of Elements and # of Nodes *	Punch**	Die	Binder	Total
25 mm models	330	540	300	2770
	392	637	357	3157
50 mm models	660	1080	600	5540
	728	1183	663	5955
75 mm models	990	1620	900	8310
	1064	1729	969	8753
100 mm models	1320	2160	1200	11080
	1400	2275	1275	11511

* For each blank width, the first row is the number of elements, the second row is the number of nodes

** Models of 100 mm, 102 mm and 104 mm punches have the same number of elements and nodes, though they are three different models

Table 3.3 Summary of FEM representations of the half TWB and tooling finite element meshes and various combinations of tooling and blanks used in U-channel forming (LS-DYNA)

Single Weld TWB Models	# of Elements *		# of Nodes
	thick	thin	
TWBs, weld at tooling symmetry (1)	4050	4050	8456
TWBs, weld around r_p (2)	5346	2754	8456
TWBs, weld at mid-wall (3)	5751	2349	8456
TWBs, weld around r_d (4)	6480	1620	8456
Shim for (1)	1000		1071
Shim for (2)	1100		1176
Shim for (3)	1000		1071
Shim for (4)	600		651
100 mm (5), 102 mm (6) Punches Each	2400		2541
107 mm Die (7)	2800		2982
Binder (8)	1600		1722
Total of (1) + (5) + (7) + (8) + Shim for (1)	15928		16772
Total of (1) + (6) + (7) + (8) + Shim for (1)	15928		16772
Total of (2) + (5) + (7) + (8) + Shim for (2)	16028		16877
Total of (2) + (6) + (7) + (8) + Shim for (2)	16028		16877
Total of (3) + (5) + (7) + (8) + Shim for (3)	15928		16772
Total of (3) + (6) + (7) + (8) + Shim for (3)	15928		16772
Total of (4) + (5) + (7) + (8) + Shim for (4)	15528		16352
Total of (4) + (6) + (7) + (8) + Shim for (4)	15528		16352

* The 28 rigid link elements for welds not included in the number of TWB models

Table 3.4 Summary of load curves used in U-channel forming stage (LS-DYNA)

Punch Stroke (mm)	Run Time (ms)	Maximum Velocity (mm/ms)
60	10.1, 14.1, 30.1	9.33, 6.68, 3.13
65	10.1, 14.1, 30.1	10.11, 7.25, 3.39
70	8.1, 10.1, 14.1, 30.1	12.61, 10.88, 7.80, 3.65
75	10.1, 14.1, 30.1	11.66, 8.36, 3.91

Table 3.5 Summary of material properties used in LS-DYNA and LS-NIKE3D simulations

Property in LS-DYNA	AA5754-O	AA5182-O *
Density, ρ (g/mm ³)	2.643×10^{-3}	2.643×10^{-3}
Poisson Ratio, ν	0.33	0.33
Young's Modulus, E (GPa)	70.23	70.23
Strength Constant, k (MPa)	416.52	-
r_0	0.705	0.75
r_{45}	0.765	0.84
r_{90}	0.906	0.85
Strain Hardening Exponent, n	0.2939	-
Barlat Exponent, M	8.0	8.0
Property in LS-NIKE3D	AA5754-O	AA5182-O
Density, ρ (g/mm ³)	2.643×10^{-3}	2.643×10^{-3}
Poisson Ratio, ν	0.33	0.33
Young's Modulus, E (GPa)	70.23	70.23

* All LS-DYNA simulations of AA5182 performed using "load curve hardening" mode, thus no k or n presented here

Table 3.6 Experimental data of COFs of several lubricants on aluminum sheet [91]

Sheet Material	6111 (0°)	6111 (90°)	6111 (0°)	6111 (90°)
Lubricant	AL070, 31°C	AL070, 31°C	AL070, 21°C	AL070, 21°C
μ	0.054	0.066	0.077	0.041
Sheet Material	Ni plated Al	Ni plated Al	Ni plated Al	Ni plated Al
Lubricant	Cromac 1049	Lamson oil	vanishing oil	no lube
μ	0.052-0.068	0.076-0.08	0.127-0.155	0.205

Figure 1.1 Plot of a Typical FLD (AA5754-O)

Figure 1.2 Geometry of the Test Tooling Showing the Dependency of Springback on BHF [28]

Figure 1.3 Correlation of Springback with the Binder Restraining Force [28]

Figure 1.4 Relation of Maximum Bending Moment with Material r, n Values [40]

Figure 1.5 Nonlinear (True) Strain Distribution across the Sheet Thickness [55]

Figure 1.6 Schematic of the "Double-Bend" Technique for Springback Compensation [58]

Figure 1.7 Springback as a Function of Die Gap [58]

Figure 1.8 Schematic of the "Spanking" Operation for Springback Compensation [59]

Figure 1.9 Results of Springback Changes with Values of R_1 and R_2 in "Spanking" Operation [59]

Figure 1.10 Tensile Tests for TWB Specimen in Both Longitudinal and Transverse Directions

Figure 1.11 Schematic of Springback Analysis of the Benchmark Problem of Numisheet 93

Figure 1.12 Schematic of the Deep Drawing of S-rail and Its Springback (not to scale)

Figure 2.1 Orientation of Tensile Test Specimen Relative to Sheet Rolling Direction

Figure 2.2 True Stress-Strain Curves for AA5754 and AA5182

Figure 2.3 Schematic of the Tooling Geometries for Experiments at the University of Waterloo

Figure 2.4 (a) Photograph of the 250 kN Formability Press Facility at the University of Waterloo

Figure 2.4 (b) Photograph of the Controllers and the Data Acquisition System of the 250 kN Formability Press Facility

Figure 2.4 (c) Photograph of the Die Set and the Tooling of the 250kN Formability Press Facility with a U-channel Shown after Springback

Figure 2.4 (d) Photograph of a Deformed Monolithic Blank U-Channel after Springback

Figure 2.4 (e) Photograph of a Deformed TWB U-Channel of Dissimilar Thickness after Springback, with the Weld Line Initially Located at the Tooling Symmetry

Figure 2.5 Schematic of Definitions of Flange Springback Angle θ_1 and Sidewall Springback Angle θ_2

Figure 3.1 Definitions of Springback Angles and Sidewall Curl in XY Plane in Numerical Models

Figure 3.2 Definitions of Sidewall Curl in XZ Plane and Descriptions of Measured Points

Figure 3.3 **Exploded View of FEM Mesh of 25mm Monolithic Blank and Tooling**

Figure 3.4 **Exploded View of FEM Mesh of 50mm Monolithic Blank and Tooling**

Figure 3.5 Exploded View of FEM Mesh of 75mm Monolithic Blank and Tooling

Figure 3.6 **Exploded View of FEM Mesh of 100mm Monolithic Blank and Tooling**

Figure 3.7 Exploded View of FE Mesh of One of the TWB Models, with the weld positioned at the tooling symmetry

Figure 3.8 (a) TWB and Shim Meshes, with weld being positioned at the tooling symmetry

Figure 3.8 (b) TWB and Shim Meshes, with weld being positioned around punch radius

Figure 3.8 (c) TWB and Shim Meshes, with weld being positioned at the sidewall

Figure 3.8 (d) TWB and Shim Meshes, with weld being positioned at the die radius

Figure 3.9 Close-up View of the Weld Line of TWB Models

Figure 3.10 (a) Tooling for Monolithic Blank Models

Weld line positions:

- (1) initially at the tooling symmetry
- (2) around punch radius at one side
- (3) at the sidewall area
- (4) around the die radius at one side

Figure 3.10 (b) Tooling for TWB Models, showing initial weld line positions

Figure 3.10 Schematic of Various Tooling Models, showing the initial blank and tooling positions and the movement direction

Figure 3.11 Plot of a Typical Punch Velocity Time History (Load Curve) for U-Channel Simulations

Figure 3.12 Plot of a Typical Load Curve for Binder Force in Simulations

Definition of the contact interfaces during forming for TWBs with weld lines initially

at the tooling axis or
around the punch radius at on side or
at the middle area of the sidewall or
around the die radius at one side

- A: thick and thin sheets to punch
- B: thick sheet to die
- C: thin sheet to die
- D: thick and thin sheet to binder
- E: thin sheet to shim
- F: shim to die

Figure 3.13 Schematic of Contact Interfaces for TWBs with Different Weld Line Positions

Figure 4.1 Scatter in Measured Springback Angles, using 1 mm x 75 mm x 400 mm Monolithic Blanks of AA5754 under 7 kN BHF

Figure 4.2 Scatter in Measured Springback Angles, using 1 mm x 75 mm x 400 mm Monolithic Blanks of AA5754 under 12 kN BHF

Figure 4.3 (b) Flange Springback Angles θ_1 versus BHF

Figure 4.3 (b) Sidewall Springback Angles θ_2 versus BHF

Figure 4.3 Plot of Measured Flange Angle θ_1 and Sidewall Angle θ_2 versus BHF for 1 mm x 400 mm Monolithic Blanks of AA5754 with different widths

Figure 4.4 (a) Flange Springback Angles θ_1

Figure 4.4 (b) Sidewall Springback Angles θ_2

Figure 4.4 Effect of Lubricant Selection on Measured Springback Angles

Figure 4.5 (a) Flange Springback Angles θ_1 versus Blank Width

Figure 4.5 (b) Sidewall Springback Angles θ_2 versus Blank Width

Figure 4.5 Plot of Measured θ_1 and θ_2 versus Blank Width for 1 mm x 400 mm Monolithic Blanks of AA5754 under different BHF

Figure 4.6 (a) Flange Springback Angles θ_1 versus Blank Thickness

Figure 4.6 (b) Sidewall Springback Angles θ_2 versus Blank Thickness

Figure 4.6 Plot of Measured θ_1 and θ_2 versus Blank Thickness for 75 mm x 400 mm Monolithic Blanks of AA5754 under different BHFs

Figure 4.7 (a) Flange Springback Angles θ_1 versus Die Gap

Figure 4.7 (b) Sidewall Springback Angles θ_2 versus Die Gap

Figure 4.7 Effect of Die Gap on Measured Springback Angles, using 1.6 mm x 75 mm x 400 mm Monolithic Blanks of AA5754 for several BHF

Figure 4.8 (a) Flange Springback Angles θ_1 versus Die Gap

Figure 4.8 (b) Sidewall Springback Angles θ_2 versus Die Gap

Figure 4.8 Effect of Die Gap on Measured Springback Angles, using 1.0 mm x 25 mm x 400 mm Monolithic Blanks of AA5754 for several BHF

Figure 4.9 (a) Flange Springback Angles θ_1 versus BHF

Figure 4.9 (b) Sidewall Springback Angles θ_2 versus BHF

Figure 4.9 Comparison of Measured Springback Angles of AA5182 and AA5754, using 1.0 mm x 75 mm x 400 mm Monolithic Blanks for several BHF's

Figure 4.10 (a) Flange Springback Angles θ_1 versus Thickness ratio

Figure 4.10 (b) Sidewall Springback Angles θ_2 versus Thickness Ratio

Figure 4.10 Effect of Thickness Ratio on Measured Springback in TWBs, under 20 kN BHF

Figure 4.11 (b) Flange Springback Angles θ_1 versus Thickness Ratio

Figure 4.11 (b) Sidewall Springback Angles θ_2 versus Thickness Ratio

Figure 4.11 Effect of Thickness Ratio on Measured Springback in TWBs, under 40 kN BHF

Figure 4.12 (a) Flange Springback Angles θ_1 versus BHF

Figure 4.12 (b) Sidewall Springback Angles θ_2 versus BHF

Figure 4.12 Effect of BHF on Measured Springback in TWBs , using 1.6:1.0 AA5754 TWBs

Figure 5.1 (a) Flange Springback Angles θ_1 versus Blank Width

Figure 5.1 (b) Sidewall Springback Angles θ_2 versus Blank Width

Figure 5.1 Effect of Blank Width on Predicted Springback, 1.0mm Monolithic Blanks Deformed under the Conditions of 10kN BHF, 70mm Punch Stroke with a 107mm Wide Die

Figure 5.2 (a) Flange Springback Angles θ_1 versus Die Gap

Figure 5.2 (b) Sidewall Springback Angles θ_2 versus Die Gap

Figure 5.2

Effect of Die Gap on Predicted Springback, 1.0mm Monolithic Blanks Deformed under the Conditions of 10 kN BHF, 70mm Punch Stroke with a 107mm Wide Die

Figure 5.3 (a) Predicted Channel Profiles for a Range of Blank Thicknesses

Figure 5.3 (b) Springback Angles versus Blank Thickness

Figure 5.3 Effect of Blank Thickness on Predicted Springback, 1.0mm Monolithic Blanks Deformed under the Conditions of 10kN BHF, 70mm Punch Stroke with a 107mm Wide Die

Figure 5.4 (a) Predicted Channel Profiles for a Range of BHFs

Figure 5.4 (b) Springback Angles versus BHF

Figure 5.4 Effect of BHF on Predicted Springback, 1.0mm Monolithic Blanks Deformed under the Conditions of 10kN BHF, 70mm Punch Stroke with a 107mm Wide Die

Figure 5.5 (a) Predicted Channel Profiles for a range of Friction Coefficients

Figure 5.5 (b) Springback Angles versus COF

Figure 5.5 Effect of COF on Predicted Springback, 1.0mm Monolithic Blanks Deformed under the Conditions of 10kN BHF, 70mm Punch Stroke with a 107mm Wide Die

Figure 5.6 Effect of Element Remeshing on Predicted Channel Profiles of 1mm Monolithic Blank

Figure 5.7 Plot of Predicted Final Shape of 1.6/1.0mm TWB with Weld Line Initially Positioned at the Tooling Symmetry

Figure 5.8 (a) XY Profiles

Figure 5.8 (b) XZ Profiles

Figure 5.8 Typical TWB U-Channel Profiles in XY and XZ Planes, Indicating the Springback and the Sidewall Curls in XY and XZ Plane

Figure 5.9 (a) XY Profiles

Figure 5.9 (b) XZ Profiles

Figure 5.9 Predicted Final Profiles of TWBs Having Dissimilar Thickness, with the Weld Line Initially Located at the Tooling Symmetry Axis, under the Conditions of BHF=20 kN and $\mu=0.06$

Figure 5.10 Predicted Distribution of the Effective Stresses along the Channel Profiles, under the Conditions of BHF=20 kN and $\mu=0.06$

Figure 5.11 Arrangement of Weld Line Positions in TWB Model Simulations

Figure 5.12 (a) XY Profiles

Figure 5.12 (b) XZ Profiles

Figure 5.12 Comparison of Predicted Final Profiles of TWBs in XY and XZ Planes

Figure 5.13 (a) thick side, weld at tooling symmetry

Figure 5.13 (b) thin side, weld at tooling symmetry

Figure 5.13 (c) thick side, weld around punch

Figure 5.13 (d) thin side, weld around punch

Figure 5.13 (e) thick side, weld at mid-wall

Figure 5.13 (f) thin side, weld at mid-wall

Figure 5.13 (g) thick side, weld around die

Figure 5.13 (h) thin side, weld around die

Figure 5.13 Fringe Plots of Effective Plastic Strains of TWBs with Different Weld Line Positions

Figure 5.14 (a) Effect of BHF on Predicted Final XY Profiles

Figure 5.14 (b) Effect of BHF on Predicted Final XZ Profiles

Figure 5.14 Effect of BHF on Predicted Final Profiles of TWBs, with Weld Lines Initially Located at the Punch Axis, under the Condition of $\mu=0.06$

Figure 5.15 (a) Effects of Die Gap (Clearance) on XY Profiles

Figure 5.15 (b) Effects of Die Gap (Clearance) on XZ Profiles

Figure 5.15 Effect of Die Gap (Clearance) on Predicted Final Profiles of TWBs

Figure 5.16 (a) Effects of Die Gap on the Distribution of Effective Plastic Strains

Figure 5.16 (b) Effects of Die Gap on the Distribution of Effective Stresses

Figure 5.16 Effect of Die Gap on Predicted Distribution of Effective Plastic Strains and Stresses along Channel Profiles of TWBs

Figure 5.17 (a) Springback Angles versus Thickness Ratios, Weld at Symmetry

Figure 5.17 (b) Sidewall Curl in XY Plane versus Thickness Ratios, Weld at Symmetry

Figure 5.17 (c) Springback Angles versus Thickness Ratios, Weld at Mid-Wall

Figure 5.17 (d) Sidewall Curl in XY Plane versus Thickness Ratios, Weld at Mid-Wall

Figure 5.17 (e) Weld Line Deviation off the Tooling Axis versus Thickness Ratios

Figure 5.17 Predicted Effect of Thickness Ratio and Weld Line Positions, with Weld Lines Initially Positioned at the Tooling Axis and at the Mid-wall, Deformed under the Conditions of 20 kN BHF, $\mu=0.06$, and 70mm Punch Stroke

Figure 5.18 Comparison of Predicted Springback Angles versus Weld Line Positions

Figure 5.19 Predicted Effect of BHF, All 1.6/1.0mm TWBs with Weld Lines Initially Positioned at Tooling Axis and Deformed under the Conditions of $\mu=0.06$, and 70mm Punch Stroke

Figure 5.20 (a) Flange Springback Angles θ_1 versus Die Gap

Figure 5.20 (b) Sidewall Springback Angles θ_2 versus Die Gap

Figure 5.20 Predicted Effect of Die Gap, All 1.6/1.0mm TWBs with Weld Lines Positioned at Tooling Axis or around Tooling Radii, Deformed under the Conditions of $\mu=0.06$, and 70mm Punch Stroke

Figure 5.21 (a) Flange Springback Angles θ_1 versus BHF

Figure 5.21 (b) Sidewall Springback Angles θ_2 versus BHF

Figure 5.21 Experimental and Numerical Comparison of the Effect of BHF on Springback Angles of AA5754 , using 1.0mm x 75mm x 400mm Monolithic Blanks

Figure 5.22 (a) Flange Springback Angles θ_1 versus Blank Thickness

Figure 5.22 (b) Sidewall Springback Angles θ_2 versus Blank Thickness

Figure 5.22 Experimental and Numerical Comparison of the Effect of Blank Thickness on Springback Angles of AA5754, using 75mm x 400mm Monolithic Blanks

Figure 5.23 Experimental and Numerical Comparison of the Punch Force History for AA5754 TWBs, with different thickness ratios, using 100 mm punch and 107 mm die

Figure 5.24 (a) Flange Springback Angles θ_1 versus Thickness ratio

Figure 5.24 (b) Sidewall Springback Angles θ_2 versus Thickness Ratio

Figure 5.24 Experimental and Numerical Comparison of the Effect of Thickness Ratio on Springback Angles of AA5754 TWBs under 40kN BHF, using 100mm x 400mm TWBs

Figure 5.25 (a) Flange Springback Angles θ_1 versus BHF

Figure 5.25 (b) Sidewall Springback Angles θ_2 versus BHF

Figure 5.25 Experimental and Numerical Comparison of the Effect of BHF on Springback Angles of AA5754 TWBs, using 1.6/1.0mm x 100mm x 400mm TWBs

Figure 5.26 (a) Flange Springback Angles θ_1 versus Die Gap

Figure 5.26 (b) Sidewall Springback Angles θ_2 versus Die Gap

Figure 5.26 Experimental and Numerical Comparison of the Effect of Die Gap on Springback Angles of AA5754 TWBs, using 1.6/1.0mm x 100mm x 400mm TWBs under 40kN BHF