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INTEGRATED NUMERICAL-EXPERIMENTAL APPROACH FOR
CHARACTERIZING DEFORMATION AND FRACTURE IN
SINGLE POINT INCREMENTAL FORMING

PHD THESES

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The author affirms that the research and findings presented in this thesis are entirely his own work. Proper acknowledgements and credit are given within the text whenever references are made to the work of other scholars. Additionally, all sources of information and data have been accurately cited to maintain academic integrity.

Miskolc, 29 April 2026

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Nomenclature

The most important and frequently used notations are listed here . While each notation is explained in the text upon its first appearance, this Nomenclature section may prove useful for quick reference.

Abbreviations:

ISF	Incremental Sheet Forming,
SMF	Sheet metal Forming,
ISMF	Incremental Sheet Metal Forming,
SPIF	Single Point Incremental Forming,
TPIF	Two Point Incremental Forming,
DSIF	Double-Sided Incremental Forming,
MPISF	Multi-Point Incremental Sheet Forming,
WJISF	Water Jet Incremental Sheet Forming,
LAISF	Laser-Assisted Incremental Sheet Forming,
FEM	Finite Element Method,
FE	Finite Element,
ALLIE	Total Internal Energy,
ALLKE	Total Kinetic Energy,
S4R	4-node, quadrilateral, reduced integration shell element,
C3D8R	8-node linear brick (hexahedral) solid element with reduced integration,
FEA	Finite Element Analysis,
CNC	Computer Numerical Control,
CAD	Computer-Aided Design,
ANN	Artificial Neural Network,
RD	Rolling direction,
DD	Diagonal direction,
TD	Transverse direction,
TC	Truncated cone,
TP	Truncated pyramid,
TCCWA	Truncated cone with constant wall angle,
TPCWA	Truncated pyramid with constant wall angle,
TCVWA	Truncated cone with variable wall angle,
HSS	High-speed steel,
PEEQ	Equivalent plastic strain,
P	Profile toolpath,
S	Spiral toolpath,
RS	Residual stresses,
GTN	Gurson-Tavraard-Needleman,

JC	Johnson-Cook,
IP5	Five integration points,
IP7	Seven integration points,
IP10	Ten integration points,

Greek Symbols, Superscripts, And Subscripts:

t_0	Initial sheet thickness,
t_f	final sheet thickness,
Δz	Step size,
Δx	Step over,
d	Tool diameter,
ψ	Drawing angle,
ψ_{max}	Maximum formable angle,
ψ_i	Initial wall angle,
ψ_f	Final wall angle,
F	Feed rate,
N	Spindle speed,
t	time,
F_x	Radial forming force,
F_y	Tangential forming force,
F_z	Axial forming force,
F_{zmax}	Maximum axial forming force,
μ	Friction coefficient,
σ	Stress,
ε	strain,
ε_0	Pre-strain,
τ_f	Shear stress,
σ_n	Normal stress,
YS/σ_y	Yield stress,
σ_m/σ_h	Mean stress/ hydrostatic stress,
$\Delta\sigma_{11}^{inner}$	Residual stresses in the inner surface element,
$\Delta\sigma_{11}^{outer}$	Residual stresses in the outer surface element,
S_{ij}	Deviatoric component of the Cauchy stress tensor,
Δ_{ij}	Kronecker delta,
η	Triaxiality,
η_{ave}	Average triaxiality,
$\bar{\theta}$	Normalized Lode angle parameter,
$\bar{\theta}_{ave}$	Average Lode angle parameter,
E	Young's modulus,

ν	Poisson's ratio,
A	Initial Elongation to break,
$\sigma_{vM} \bar{\sigma}$	von Mises stress,
σ_{ij}	Stress component,
$\sigma_u UTS$	The ultimate strength,
ε_p	Plastic strain,
$\bar{\varepsilon}_f$	Fracture strain,
x, y, z	Coordinate system
D	Damage variable,
D_{max}	Maximum degradation,
$D_1 - D_5$	Damage parameters,
$q_1 - q_5$	Tvergaard parameters,
f_0, f, f_f, f_c	Void volume fraction damage parameters,
f_N, ε_n, S_N	Void nucleation parameters,
$\dot{f}$	Total void volume fraction change,
$\dot{f}_G$	Void growth rate,
$\dot{f}_N$	, Void nucleation rate,
f_0	Initial Void Volume Fraction,
f	Void Volume Fraction(VVF),
f_c	Critical value of Void Volume Fraction (VVF),
f^*	Effective Void Volume Fraction (VVF),
ϕ	Yield function,
F, G, H, L, M, N	Hill's coefficients,
G_f	Fracture energy,
J_2	Second invariant stress tensor,
J_3	Third invariant stress tensor,
I	Identity tensor,
L	Element characteristic length,
k_s	Swift hardening coefficient,
n_s	Swift strain hardening exponent,
k_v	Voce hardening coefficient,
σ_s	Saturation stress,
n_v	Voce strain hardening exponent,
α	Weighting factor,
β	Thinning formulae,
R_{ij}	Yield stress ratios,
r_0, r_{45}, r_{90}	Planar anisotropy coefficients,
$\bar{r}$	Average anisotropy coefficient,
u_i	Displacement at the onset of fracture,
u_f	Displacement at the complete fracture,

$\bar{u}^{pl}$	Equivalent plastic displacement,
$\bar{u}_f^{pl}$	Equivalent plastic displacement at the complete failure,
ε^e	Elastic strain tensor,
ε^p	Plastic strain,
$\bar{\varepsilon}^p$	Equivalent Plastic strain,
$\bar{\varepsilon}_0^p$	Equivalent Plastic strain at the onset of damage,
$\bar{\varepsilon}_f^p$	Equivalent Plastic strain at the complete damage,
ξ	Normalized third deviatoric invariant,
$d\bar{\varepsilon}^p$	The plastic strain increment,

CHAPTER 1

Introduction

1.1. Motivation

The escalating global demand for enhanced fuel efficiency and reduced emissions has driven a significant imperative for lightweight materials, particularly within the automotive and aerospace sectors. This demand is directly correlated with vehicle and aircraft structural weight, prompting a notable transition from traditional heavier steels to lighter aluminum alloys. While aluminum alloys offer advantages in terms of lower material cost and weight, their inherent lower strength compared to advanced steels necessitates innovative manufacturing approaches to maintain structural integrity and performance.

Sheet metal forming (SMF) has long been a pillar of modern manufacturing technology, extensively employed for the mass production of diverse components across industries such as automotive, aerospace, and shipbuilding. Conventional SMF processes, including deep drawing, stamping, and hydroforming, have historically met high-volume production requirements. However, a primary limitation of these established methods lies in the substantial investment required for the design and fabrication of intricate, application-specific tooling. Such tooling is often complex and economically justifiable only for large-scale production runs, posing significant barriers for prototyping, small-batch manufacturing, or designs requiring frequent modifications.

In response to these challenges, Incremental Sheet Forming (ISF) has emerged as a highly promising, flexible manufacturing paradigm. As a free-form forming process, ISF has garnered considerable attention from researchers and engineers alike, offering a viable alternative to overcome the inherent shortcomings of traditional forming techniques. Its key advantages include a significantly reduced lead time and enhanced cost-effectiveness in tooling procurement, as it typically utilizes a simple, generic tool rather than expensive, custom dies. This characteristic positions ISF as a promising solution for the agile production of complex geometries and for applications demanding rapid prototyping or low-volume manufacturing, thereby addressing critical needs in modern industrial landscapes.

1.2. Incremental sheet forming

Incremental sheet metal forming (ISF) is a cutting-edge sheet metal forming technique classified as a die-less metal forming technology [1] that garnered significant attention from researchers and engineers across industries such as aerospace [2, 3], automotive [4, 5, 6], biomedical implants [7, 8, 9, 10], as well as in the electronics sector for enclosures and precision-formed housings [11, 12], and ship manufacturing [13]

The ISF process was patented as an apparatus by Leszak [14] in 1967 under patent number [US3342051A]. [15] invented a patent under the number [US3316745], which was more similar to the recent conventional incremental sheet metal forming. Moreover, it was indicated that the research conducted by Mason [16] in 1978 could be regarded as the root of ISF. Which followed by a research work about the sheet metal forming for small batches using sacrificial tooling by the same authors [17].

The ISF process is generally classified either based on the forming direction (negative vs. positive) forming [18, 19], process symmetry such symmetric or asymmetric forming [18, 19], or based on the assembly setup (i.e, conventional or hybrid, Incremental sheet free-forming (ISFF)) [20, 21], depending on the heat source (e.g., laser beam, friction stir, ultrasonic vibration) [18], or depend on the number of stages (e.g., single point incremental forming (SPIF), and multi-stage single point incremental forming (MSPIF) [22]. Other categorization was attributed based on various criteria [23] as listed below:

- Active forming tool: Punch, roller, water jet, laser, ball jet
- Active support element: without support element, with counterpunch, with roller, with mandrel, with active plate (partial or full die)
- force application: continuous/ intermittent
- clamping fixture: fixed/ mobile
- temperature: hot/ cold
- the number of contact point between the tool and the sheet: SPIF/ TPIF/ multi-point reconfigurable (MPISF) dies, etc.

Unlike conventional sheet metal forming processes such deep drawing, stamping, and hydroforming. The ISF requires a single generic and universal stylus forming tool featuring a hemispherical/ballpoint, flat, or roller-headed shape to form a firmly clamped sheet on its boundaries on a fixture and gradually, locally deformed into a complex three-dimensional geometries using generally a computer numerically controlled (CNC) machine enabling the punch to follow the target trajectory by producing local plastic deformation. This advantages makes ISF processes gain several credits over traditional forming categories by striving to meet the market demands in producing low-volume parts, rapid prototyping, and customized products, and process flexibility [24, 25] which has pioneered the high economic pay-off for the low-production parts by avoiding usage of costly dies and punches, with shorter lead time, despite the long processing time. In terms of feasibility, the shapes and the complexity of the parts play an important aspect in the lot size, which expected to be ranged from 200 till 1200 components [24].

This technology can be implemented on various platforms, including CNC machines, industrial robots, and dedicated ISF platforms [26]. Since its invention, a host of methods based on contact have been derived to shape various materials shape into useful product. Single-point incremental forming (SPIF) (see Fig. 1.1a), utilizes a simple forming tool to deform the sheet material incrementally. In contrast, two-point incremental forming (TPIF)

employs a partial or full die (see Fig. 1.1c, Fig. 1.1d), requiring simultaneous contact between the tool and the sheet on one side and between the sheet and the auxiliary (partial or full) die on the other side. Another alternative, double-sided incremental forming (DSIF) as shown in Fig. 1.1b, replaces the die with a counter-forming tool (see Fig. 1d), three opposite point incremental forming (TOPIF) represents an advanced variant and an extension of TPIF, distinguished by its tooling configuration. This method employs a primary forming tool alongside two sets of auxiliary, opposing tools. These supplementary tools function as dynamic support dies, strategically positioned on the reverse side of the workpiece, directly beneath the regions being formed by the main tool. Operating in synchronization with the primary forming action, these auxiliary tools are designed to move freely in the vertical direction. Their principal objective is to actively counteract and minimize undesirable secondary deformation and springback effects within the plastically formed areas, thereby enhancing dimensional accuracy and overall part quality [27].

The development of TPIF and TOPIF primarily address geometric inaccuracies in SPIF, which result from insufficient constraints in the assembly forming setup. These alternative have already implemented and practised in small workshops and private companies showing the capability to process a wide range of materials such aluminum [28, 29], grade steel [30], titanium [31], magnesium [32], copper [33], thermoplastics [5, 34], composite [35], shape memory alloys [36], etc.

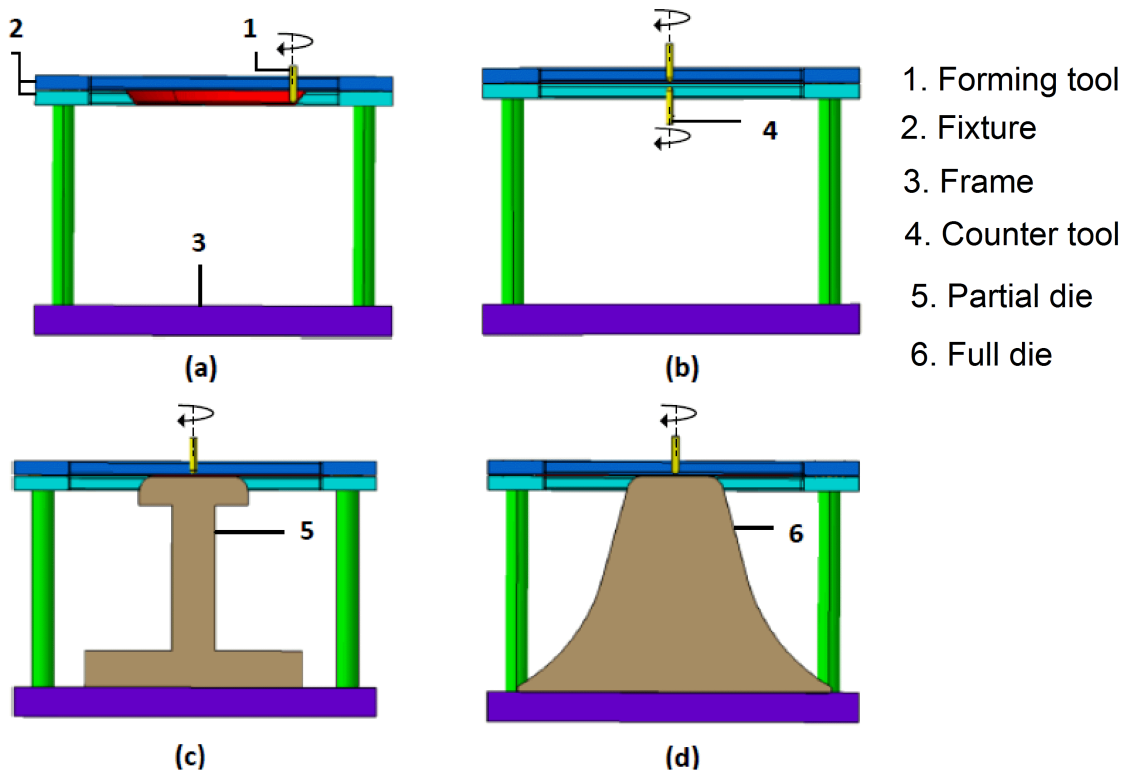


FIGURE 1.1. ISF variants: (a) SPIF, (b) DSIF, (c) TPIF with partial die, (d) TPIF with full die

Water Jet incremental forming (WJISF) is a cool hybrid forming process that mixes ISF with high-pressure water jet as presented in Fig. 1.2. It requires a multi-axial forming machine and needs a pressure pump with sufficient flow rate and pressure. It has witnessed an ongoing development since 2001 [37]. This alternative environmentally could be classified as green as there is no need for lubricant to reduce the friction at the interface, thereby eliminated the concept of tool wear and and better surface finish.

Laser-assisted incremental sheet metal forming (LAISF), is a hybrid manufacturing process that combines SPIF with localized laser heating. A laser beam is used to heat the sheet metal locally, just ahead of or at the contact point of the forming tool. This increases the material's ductility and reduces its yield strength, allowing for the forming of hard-to-deform materials, including titanium, magnesium alloys, and high-strength steels that would otherwise crack during cold forming [38, 39]. Tab. 1.1 summarizes the commonly used variants of ISF pointing on their applications, advantages, and substantial limitations.

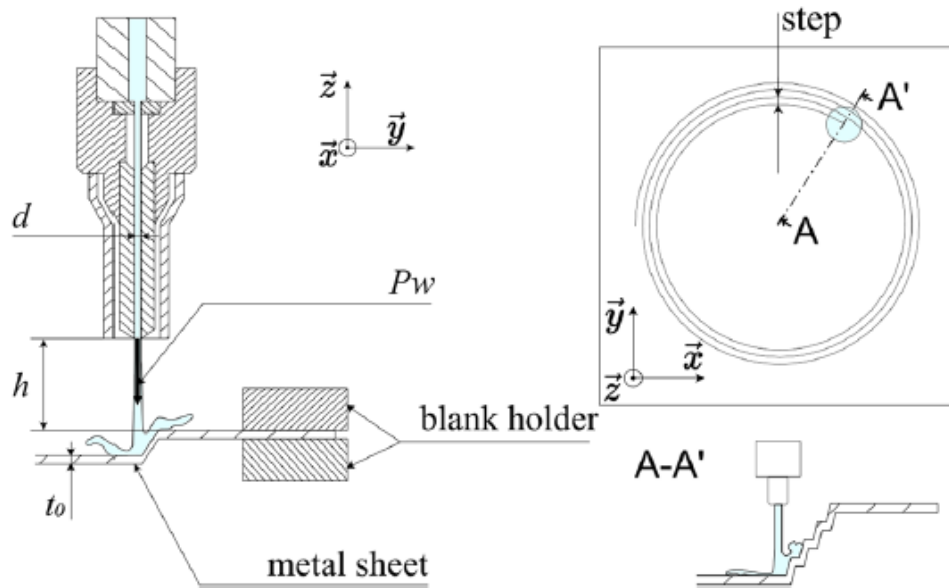


FIGURE 1.2. WJISF forming principle [37]

TABLE 1.1. ISF alternatives, characteristics, applications, advantages and disadvantages.

Alternative	Active element; Heat source	Process description	Applications	Advantages	Disadvantages
SPIF	hemispherical/ ball-ended tool	A clamped sheet deformed by a hemispherical-tipped (tool end-mill)	Rapid prototyping, small batch series	Short lead time, high flexibility, generic forming tool	less geometrical accuracy, high spring-back
TPIF	Forming tool, auxiliary die	Auxiliary support was added to enhance the process precision	High-precision components	Geometrical accuracy improvement	Extra tooling
WJISF	A high-pressure water jet	A water jet was injected at high pressure	Rapid prototyping	Environmentally green, tool-sheet contact removal, better surface finish	Limited formability, lower accuracy
LAISF	Laser beam (Fiber laser, diode laser)	A laser heats and deforms the material locally	Forming hard materials	Increase formability, better surface finish	High energy, more complicated, High equipment cost

1.3. Thesis outlines

This dissertation is structured into several chapters, each addressing a specific aspect of Single Point Incremental Forming (SPIF) research, from foundational literature to novel scientific contributions. The organization is as follows:

Chapter 2 "LITERATURE REVIEW": This chapter provides a comprehensive overview of the state-of-the-art in SPIF. It covers the main process mechanics, including forming force, material formability, surface roughness, and material fracture. The influence of various process parameters on these aspects is critically reviewed, incorporating the latest developments in the field. The chapter concludes by summarizing key findings and identifying existing research gaps that form the basis for this dissertation.

Chapter 3 "RESEARCH SCOPE AND OBJECTIVES": building upon the research gaps identified in Chapter 2, this chapter delineates the specific scope and objectives of the current research. It outlines the fundamental questions addressed and the contributions aimed for within the context of SPIF.

Chapter 4 "MATERIALS AND METHODS": this chapter details the experimental setup, materials, numerical methods, and evaluation techniques employed throughout this research. It is organized into several sections: Section 4.1 focuses on the constitutive model calibration for the investigated materials, namely commercial-purity aluminum (Al 1050) and high-strength aluminum (Al 5754). Section 4.2 describes the experimental procedures. Section 4.3 discusses the generation of commonly utilized toolpaths. Section 4.4 presents the adopted methods for thickness measurement. Section 4.5 introduces two benchmark geometries covering different stress states and varying degrees of complexity. Finally, Section 4.6 elaborates on the core Finite Element (FE) model, including its adopted framework, contact interactions, and underlying assumptions.

Chapter 5 "FORMING FORCE ANALYSIS": this chapter presents the results and discussions pertaining to the forming force behavior in SPIF, primarily based on an explicit finite element approach. It includes a detailed analysis of the effect of key process parameters and their interaction on forming force, examined both numerically and descriptively.

Chapter 6 "ANALYSIS OF THICKNESS EVOLUTION, PLASTIC STRAIN, AND STRESS STATE": dedicated to investigating the influence of key process parameters on thickness distribution, this chapter employs both experimental and numerical approaches. A special focus is given to the triple role of the wall angle in governing material formability, illustrating its impact on thinning, plastic strain evolution, and the resulting stress state. Additionally, the stress distribution, using two distinct element types, is compared in terms of damage prediction.

Chapter 7 "A FRACTURE-BASED APPROACH TO FORMABILITY ASSESSMENT": this chapter outlines a critical aspect of this dissertation: the damage assessment in SPIF. It covers the application of both coupled and uncoupled fracture criteria, following a successful calibration strategy and a thorough mesh-dependency study.

Chapter 8 "GEOMETRICAL ACCURACY AND SPRINGBACK": this chapter is dedicated to investigating geometrical inaccuracies in SPIF, particularly under various wall angles. A special focus is drawn to springback, presenting its origins and the key role of residual stresses in its occurrence.

Chapter 9 "THESES-NEW SCIENTIFIC RESULTS": this chapter, summarizes the important contributions and novel scientific results derived from this dissertation. Each claim is presented as a concise statement, followed by a list of related publications to finalize the dissertation.

CHAPTER 2

SINGLE POINT INCREMENTAL FORMING: A LITERATURE REVIEW

This chapter reviews the most up-to-date literature relevant to the proposed dissertation. It begins with an introduction to single point incremental forming, followed by an overview of the key process parameters. Additionally, the methodologies for predicting forming forces, as well as thinning, deformation, and fracture phenomena, are discussed in separate sections. Finally, a detailed description of damage and fracture mechanics in SPIF is presented, highlighting their correlation with material formability.

2.1. Introduction to single point incremental forming

The increasing demand for small-batch production and customized products has driven the development of specialized manufacturing technologies, among which Single Point Incremental Forming (SPIF) stands out. As a variant of Incremental Sheet Forming (ISF), SPIF (Fig. 2.1) has gained significant attention due to its simplicity and high flexibility. Initially adopted by car manufacturers as a rapid prototyping solution [40], its application has since expanded into diverse sectors, including aerospace [41] and biomedical implants [7, 42]. These attractive characteristics, coupled with its ability to form complex, asymmetric shapes, position SPIF as a cost-effective and promising alternative to traditional forming technologies. The growing interest in SPIF is reflected in the consistent increase in academic publications, with a notable rise from approximately 11,198 publications in 2005 to 69,670 in 2025, according to major scientific databases such as Scopus and Web of Science.

SPIF draws its conceptual foundation from traditional metal spinning, sharing the principle of using a moving tool to apply localized and progressive deformation to a metal sheet. However, SPIF distinguishes itself by operating without the constraints of rotational symmetry or the need for a specific mandrel. This enhanced flexibility, achieved through Computer Numerical Control (CNC)-guided toolpaths, enables the fabrication of intricate, non-axisymmetric geometries. Unlike conventional metal forming, SPIF is classified as a parametric process, meaning its outcomes are highly sensitive to various process parameters. Consequently, thorough investigation, precise control, and optimization of key variables such as step depth, tool diameter, wall angle, and toolpath strategy are crucial for ensuring component quality and reliability [43]. These parameters collectively influence critical aspects of the process, including forming forces, localized deformation, thinning ratios, formability, stress states, damage and fracture mechanisms. A detailed presentation of the forming mechanics and damage behavior in SPIF will be provided in subsequent sections, focusing on the influence of technological parameters (e.g., step depth, feed rate, tool rotational

speed, lubricant type, toolpath strategy) and geometrical parameters (e.g., tool diameter, wall angle, drawing depth, initial sheet thickness).

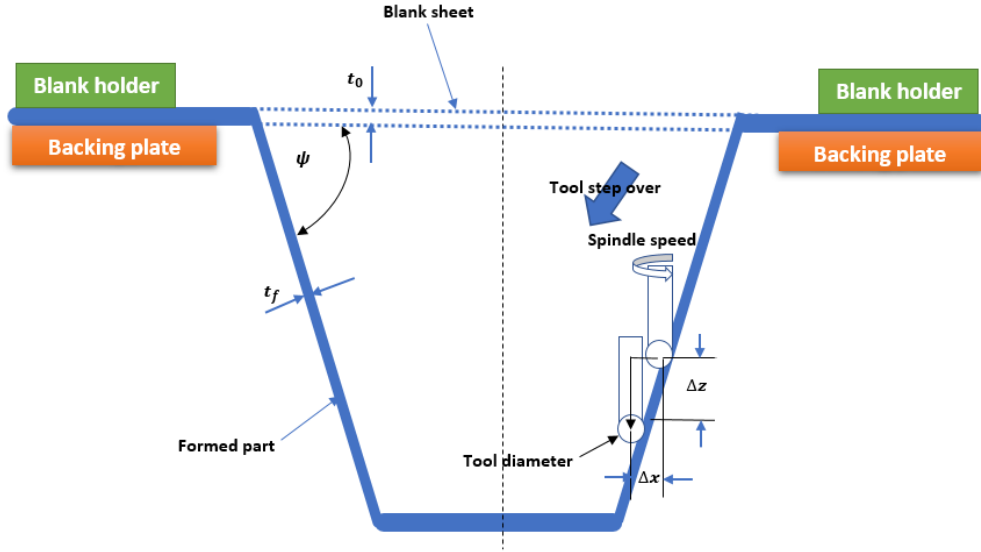


FIGURE 2.1. Schematic diagram of the SPIF principle

2.2. Process Parameters

A variety of process parameters, which directly impact the process response, are defined and labelled in the schematic diagram in Fig. 2.2 and are briefly introduced below:

- **Step depth (Δz):** Represents the vertical increment or jump between successive forming contours.
- **Tool diameter (d):** Refers to the diameter of the active element of the tool that contacts the sheet. Tools typically feature hemispherical, spherical, or flat ends.
- **Wall angle (ψ):** A characteristic geometric feature defining the inclination between the unformed flat sheet and the formed part.
- **Initial sheet thickness (t_0):** The thickness of the sheet material prior to deformation.
- **Feed rate (F):** Defined as the linear speed at which the forming tool deforms the material.
- **Friction coefficient (μ):** Governs the contact conditions at the tool-sheet interface.
- **Step over (Δx):** Corresponds to the horizontal increment between adjacent tool passes or layers.
- **Toolpath strategy:** The programmed trajectory, typically generated using CNC language, that controls the tool's movement over the sheet surface.

2.3. Forming force prediction

The forming force play an important role in the selection of designed tooling, fixtures, and the associated forming platforms particularly when dealing with, compliant structure

such arm robot platform (CNC is $2.5\times$, $4\times$, $11\times$ stiffer than arm robot in x , y , and z directions, respectively) [44]. Several research studies were devoted to investigate its components evolution experimentally and numerically. Duflou et al. [45] investigated experimentally the relationship between four process parameters (step size, tool diameter, initial sheet thickness, and wall angle). It was found that increasing the value of these factors lead drastically to the rise of the forming force magnitude. [46, 47] developed a regression models to predict the evolution of both peaks and the steady-state forming forces as a function of input process variables. Later, [48] has proposed empirical equations capable for predicting the force along the tool z -axis noted by axial force, and parallel to the tool movement, noted by tangential and radial force by taking into account various process parameters, including step down Δz , tool diameter d , sheet thickness t_0 , wall angle ψ based on truncated cone benchmark, consequently the model was generalized to different grade materials. Moreover, Li et al. [49] has presented an efficient strategy to predict the forming forces based on the use of a near-finished part as the starting point for the simulation. By averaging the sum of vector force, it was found similar results for both truncated cone and pyramid shape for the same process condition giving the reason to the localized deformation. This established strategy has shown a high efficiency for produced shape regardless of its complexity.

Prediction models for the forming force and contact area were established by [50]. It was found that the contribution of the process parameters is correlated with both the shape and size of the contact area, as well as with the equivalent thickness coefficient n , leading to a significant increase in the forming force in all three directions. Furthermore [51] presented a relevant an analytical force prediction model based on stress components and contact area in ISF process in polar coordinate system. Bahloul et al. [52] developed implicit and implemented a numerical model capable for predicting the forming force of both conical and pyramid shapes with a good agreement relative to experimental data. Additionally, a parametric form was set to systematically vary the initial sheet thickness. Outcomes indicate the proportionality between the trend of the sheet thickness and the forming force. Recently, Marwen et al. [19, 53] performed Finite element analysis using an explicit framework to examine four key process parameters and their effect on the average forming force magnitude. It was observed that increasing the increment size, wall angle, tool diameter, and sheet thickness systematically increases the forming force magnitude; however, their degrees of influence vary.

2.4. Thinning

Despite its notable advantages, SPIF inherently presents significant challenges, including severe thinning, geometric inaccuracies, and potential fracture occurrences. These phenomena directly impact product quality, limit applicability, and compromise the structural integrity of formed components [54]. Thinning, defined as the reduction in sheet thickness during the forming process, is a critical aspect that has been extensively investigated

through theoretical, experimental, and numerical approaches, focusing on its relationship with geometric complexity, material properties, and various process parameters [55, 56].

Severe thickness reduction is a particularly critical issue, as it not only affects product quality standards but also increases susceptibility to premature failure. The thinning ratios observed in SPIF can often exceed those encountered in conventional sheet metal forming processes like deep drawing, stamping, and bending, which can hinder its widespread industrial adoption [57]. The localized stresses and intense plastic deformation characteristic of SPIF lead to a non-uniform wall thickness distribution, thereby compromising structural integrity, geometric precision, and overall part quality.

Numerous studies have explored the influence of key process parameters on thinning behavior. The step depth has been identified as a significant factor affecting the thinning rate. For instance, finite element analysis (FEA) combined with response surface methodology (RSM) has demonstrated that employing a smaller step depth can reduce thinning, albeit at the cost of increased forming time [58].

Similarly, investigations into the combined effects of step size and wall angle on the thinning of AA 3003 have shown that while the impact of step size can be moderate, the wall angle exerts a substantial influence [59]. The interplay between these parameters is crucial, a combination of greater step depth and a steep wall angle typically leads to higher thinning due to intensified stretching and localized plastic deformation. Furthermore, the material flow characteristics, being freer at open edges and more restricted in other regions, often result in asymmetric thinning and subsequent thickness non-uniformity.

Research has also delved into the formability limits and strain characteristics in SPIF. Jeswiet et al. [60] examined the formability of Al3003-O and Al5754-O sheets across various geometries, revealing biaxial strain paths in domes and hyperbolas, and negative minor strain in five-lobe shapes. Their work demonstrated that SPIF can achieve strain levels up to 300%. However, the single-stage deformation inherent to many SPIF applications often restricts the ability to form complex geometries with steep wall angles due to high strain concentrations [61]. This can lead to non-uniform thickness, excessive thinning, localized defects, and geometric inaccuracies, particularly in high-aspect-ratio or intricate parts, thereby limiting material formability.

To mitigate these limitations, multi-stage incremental forming (MSIF) has been developed, involving sequential deformation steps to improve material flow control, reduce thinning, and enhance accuracy through strain redistribution. Young et al. [62] proposed a double-pass forming technique to achieve better thickness uniformity at steeper wall angles, proving its effectiveness in critical sections and for geometries unattainable via single-pass SPIF. Similarly, the introduction of intermediate shapes and optimized toolpath strategies in multi-stage SPIF has been shown to minimize thickness variation [63]. For example, studies evaluating different toolpath strategies, such as alternating inversion versus uniform press-point trajectories, have indicated that larger tool diameters, thinner sheets, and steep wall angles generally increase the thinning ratio [64]. However, specific toolpath designs,

like the uniform press-point trajectory, can offer superior thickness control. Other findings suggest that steeper wall angles consistently lead to higher thinning [65], while optimizing process variables, such as selecting larger step sizes and tool diameters, can achieve lower maximum thinning rates and improve formability [66].

Understanding the distribution of thinning across the formed part provides valuable insights into the underlying deformation mechanisms. Uneven thickness reduction can indicate areas prone to failure, guiding necessary modifications to tool paths or process strategies. Therefore, the ability to accurately predict and control thinning is a crucial measure of SPIF's capability to form complex shapes without material failure.

2.5. Formability and deformation mechanisms

Deformation mechanics in ISF are significantly more complex than in traditional die-based forming due to the localized, gradual, and incremental nature of the process. This complexity contributes to enhanced material formability, often characterized by a negative slope of the Forming Fracture Line (FFL) as depicted in Fig. 2.2 [67]. Compared to conventional methods [60, 68, 69], ISF formability is primarily governed by several distinct mechanisms: localized deformation, Bending Under Tension (BUT), stretching, and Through-Thickness Shear (TTS) [70]. Additionally, secondary mechanisms such as spinning and shear forming have been identified as contributing factors [71].

Centeno et al. [72] highlighted that the synergy between stretching and bending, combined with the localized nature of the deformation, is the primary driver for the superior formability observed in SPIF. Historically, formability has been assessed using the Forming Limit Curve (FLC), a concept introduced by Keeler [73] and typically evaluated through standard Nakazima [74] or Marciniak tests [75]. However, for ISF, Shim and Park [76] recommended utilizing the Fracture Forming Line (FFL) in the space of major and minor strains for more accurate predictions.

A common indicator of formability in benchmark geometries, such as conical and pyramidal frustums, is the maximum drawing angle achieved before fracture [60]. To determine this critical angle efficiently within a single test, Variable Wall Angle Conical Frustum (VWACF) and Pyramidal Frustum (VWAPF) geometries are employed [77]. Notably, constant-wall angle tests have been found to underestimate by approximately 4% compared to variable-angle tests [78].

Extensive research has investigated the impact of process parameters on formability. Early studies by Shim and Park [76] confirmed that localized deformation is confined to the instantaneous contact area. Kim and Park [79] found that a 10 mm diameter ball-end tool was more effective than hemispherical shapes, and that reducing feed rates and friction coefficients enhanced formability. However, the influence of tool diameter remains a subject of debate. While some researchers [80, 81, 82, 83, 84] argue that smaller tool diameters improve formability by concentrating deformation and reducing the contact area,

others [85] suggest that larger diameters and flat-end radii can lead to higher forming limits. Fig. 2.3 sum up the most commonly selected tool end-mill shapes in ISF process.

The effect of step down (vertical increment) is similarly controversial. While it is generally agreed that step size influences formability [86, 87], some studies report no visible effect on the FFL within specific ranges (e.g., 0.3 mm to 0.7 mm) [88]. The complexity of these interactions underscores the need for a deeper understanding of the underlying deformation mechanisms, such as BUT, shear stress, and cyclic loading, to accurately predict and delay the onset of fracture.

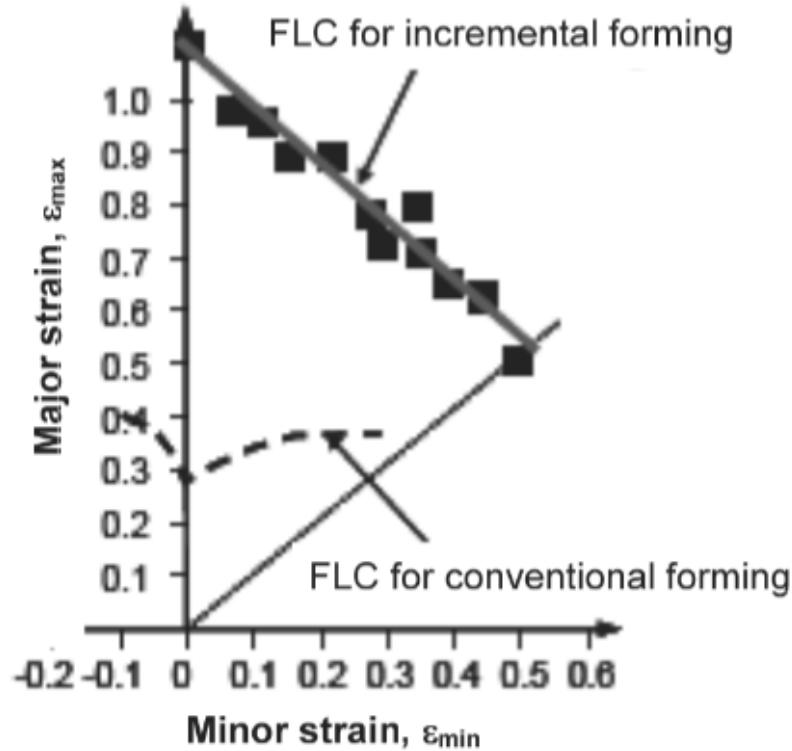


FIGURE 2.2. Schematic representation of FLC for conventional forming and incremental sheet forming [89]

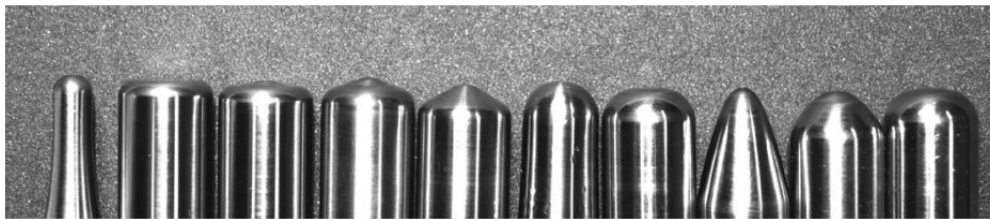


FIGURE 2.3. Various tool shapes proposed by [77]

2.6. Fracture mechanism prediction

Formability is defined as the maximum deformation a material can undergo before the onset of undesirable conditions such as cracking, necking, or buckling [90]. In ISF, predicting the transition from stable deformation to fracture is a critical area of research, involving

the definition of Forming Limit Diagrams (FLD) and Fracture Forming Lines (FFL) across various materials, including aluminum [79, 91], steel, and titanium [39].

2.6.1. Fracture based-formability assessment. Fracture assessment in ISF reveals that the deformation state typically fluctuates between plane strain and biaxial tension. Conical frustums generally exhibit plane strain conditions, whereas pyramidal shapes often experience equi-biaxial tensile strain at the corners. Theoretical models suggest that biaxial deformation leads to higher stress triaxiality, which accelerates thinning compared to plane strain regions [92]. The enhanced formability observed in ISF compared to conventional methods is frequently attributed to a combination of localized deformation, cyclic loading, and through-thickness normal stress [25, 93, 94, 95, 96]. Consequently, fracture-based FLCs are considered more suitable for the low-ductility conditions inherent in ISF than traditional necking-based FLCs [97].

Understanding the deformation mechanisms leading to strain localization and ductile damage is crucial for improving the quality of parts produced by ISF and SPIF [98, 99]. From a physical standpoint, ductile damage is initiated by the growth and coalescence of micro-cavities or micro-cracks, induced by high plastic strains in the vicinity of material inclusions [25, 100]. Metal forming processes are generally characterized by three fracture phenomena: ductile tensile fracture, ductile shear fracture, and necking [101, 102]. Localized and diffuse necking are two instabilities preceding the onset of fracture. Diffuse necking results from a reduction in waist over a certain length, while localized necking is due to thickness reduction in a narrow band [103]. Due to localized and incremental deformation, as well as altered tool-sheet contact, plastic deformation and damage mechanisms in ISF differ from those observed in traditional forming [97, 104, 105].

2.6.2. Ductile fracture criteria and prediction models. The reliable prediction of ductile fracture remains a major challenge in metal forming processes. Damage models are broadly classified into three main categories: empirical (phenomenological) models, porosity-based models, and continuum damage mechanics (CDM) models. Phenomenological models, developed through regression analysis, can be strain-based [106, 107, 108] or stress-based [109, 110]. The accuracy of these models strongly depends on the calibration of their parameters to reflect the material's response to deformation and damage evolution.

While coupled models, such as Gurson-Tvergaard-Needleman (GTN) [111, 112] and Lemaitre's Continuum Damage Mechanics (CDM) [113, 114], offer a micromechanical basis for damage evolution, a wide diversity of phenomenological (uncoupled) damage models are extensively used due to their computational efficiency and relative ease of calibration [105, 115]. These uncoupled models, which define a critical value of a damage indicator for fracture initiation, have significantly evolved to capture the complex stress states inherent to ISF, where through-thickness shear and varying stress triaxiality are dominant factors [116]. The fundamental distinction among these phenomenological models lies in the specific fracture mechanism they prioritize, which determines their predictive accuracy under different loading conditions.

Numerous studies have explored the impact of stress triaxiality on material fracture prediction. Stress triaxiality ($\eta = \sigma_m / \bar{\sigma}_{eq}$) is a dimensionless parameter that characterizes the growth of micro-voids in ductile fracture [117, 118]. Haque et al. [119] investigated the fracture mechanism in SPIF using conical and pyramidal frustums, finding that strain paths in the principal direction for both shapes significantly exceed necking and the forming limit. At low triaxialities, void growth is limited, delaying fracture initiation and thus enhancing material formability [118, 120]. The difference between FFL and FLC obtained by SPIF and conventional forming, respectively, can be attributed to different triaxiality levels [121].

However, models based solely on triaxiality often fail in shear-dominated scenarios. To address this, the Lode angle parameter ($\bar{\theta}$) is incorporated to account for the third invariant of the deviatoric stress tensor [122, 123]. The damage variable (D) is typically modeled as an integral of equivalent plastic strain relative to the fracture strain, which is a function of both triaxiality and the Lode angle:

$$D = \int_0^{\bar{\epsilon}_p} \frac{d\bar{\epsilon}_p}{\bar{\epsilon}_f(\eta, \bar{\theta})} \quad (2.6.1)$$

An adequate understanding of the fracture mechanism in SPIF, in addition to the impact of stress triaxiality and distinct deformation, is a challenge, as process parameters play a critical role in its occurrence. [124] proposed a new experimental methodology to determine the forming limit based on fracture and strain analysis. Anderson [125] stated that fracture occurrence must be determined by stress and strain states, whether necking is suppressed or not. Two types of failure modes have been observed: necking developing until fracture, or abrupt rupture occurring in the shear band in the absence of any sign of necking in the fracture regions [126, 127]. Malhotra et al. [94] showed that local bending and shear occur during ductile fracture in SPIF, explaining that stretching and local bending of the sheet near the contact region with the forming tool lead to high localization of plastic strain on the outer surface, making it more prone to fracture.

Phenomenological criteria are commonly preferred for predicting fracture in ISF and SPIF. Bharti et al. [128] successfully calibrated three damage models Bao-Wierzbicki (BW), Modified Mohr-Coulomb (MMC), and Hosford-Coulomb (HC)) using an inverse approach, observing that damage initiation occurs on the outer surface, attributed to the existence of a through-thickness strain gradient. Mirnia et al. [129, 130] integrated MMC3 combined with the Hill'48 criterion to evaluate single and two-stage SPIF of AA 6061-T6 aluminum alloy sheet, demonstrating MMC3's ability to capture fracture in truncated cones with variable wall angles with high accuracy. Recently, [131] calibrated the BW damage model using strains obtained from Digital Image Correlation (DIC). The model was integrated with isotropic von Mises and anisotropic Hill'48 yield functions to examine the formability of AA6061-T6, with the anisotropic model showing good accuracy compared to experimental data, with a deviation of about 8%. The Johnson-Cook (JC) damage model stands out among phenomenological criteria due to its inherent ability to couple the effects of strain, strain rate, and temperature on the material's failure strain ($\bar{\epsilon}_f$). This is particularly relevant

for ISF and SPIF, where high tool speed can induce significant frictional heating and high local strain rates, leading to a thermo-viscoplastic deformation regime [132].

2.7. Geometrical deviation

Despite its inherent flexibility, SPIF frequently struggles to meet stringent industrial geometric tolerance standards, typically ± 0.5 mm [24]. Common geometric defects observed include springback, bending, and the pillow effect, primarily stemming from insufficient constraint during the forming process. These inaccuracies limit SPIF applications to scenarios where precision is less critical, such as decorative components or prototype testing [26, 133]. It has been observed that frustum cone geometries generally exhibit superior geometrical accuracy compared to other formed shapes [60].

Geometrical errors are typically quantified by measuring the absolute distance between the nominal and formed profiles [134]. To mitigate these issues, TPIF, which incorporates additional support tools (either partial or full dies), has been explored to minimize elastic recovery. Furthermore, process parameters significantly influence geometric precision. Studies have shown that a small vertical step-down and a higher spindle speed are generally favorable for achieving greater geometrical accuracy [135, 136]. Similar findings indicate that improved precision can be obtained with higher spindle speeds across various materials, including aluminum sheets and titanium alloys [137]. Conversely, high feed rates can induce inaccuracy, although profile conformity tends to improve with higher tool RPM [138].

2.7.1. Addressing geometric errors and springback. Addressing geometric errors often involves optimization and compensation strategies. Early approaches, such as that proposed by Hirt et al. [57], focused on profile deviation compensation through trajectory adjustments, demonstrating that springback in truncated cones could be minimized with optimized toolpaths. More recently, multi-objective optimization strategies have been employed to assess the influence of process variables on both energy consumption and geometrical deviation, highlighting the linear effect of sheet thickness and the quadratic effect of wall angle as key determinants of accuracy [139]. While tool diameter and step depth are often considered to have minimal influence on geometric accuracy, parameters such as part geometry and material properties play a more significant role [140].

Specific strategies have been developed to reduce common defects. For instance, finite element simulations have been utilized to minimize pillow defects in AA 1060 during SPIF [141]. Increasing the tool diameter has also been targeted as a means to reduce springback [142]. The use of ultrasonic vibration tools during the forming of AA 1060 has shown a reduction in springback compared to traditional SPIF [143]. Compensation strategies based on tool deflection analysis [144] and the introduction of support tools that trail the main forming tool [145] have also been proposed to reduce deformation. Advanced techniques, such as a two-stage double-sided SPIF method employing circumferential hammering, have successfully improved profile accuracy by up to 43% in frustum-cone geometries [146].

Springback is recognized as the primary factor affecting geometrical precision in ISF. Two distinct variants occur: local springback, resulting from the cyclic loading and localized deformation that causes the sheet to partially return to its initial position as the tool moves away; and global springback, which arises from the elastic recovery of the material after the release of clamping plates due to residual stresses [147]. Studies have indicated that increasing factors such as sheet thickness, tool diameter, and feed rate, while decreasing step depth, can lead to a reduction in springback, independently [148]. Advanced modeling techniques, such as Gaussian process models relying on Radial Basis Functional kernels and multitask regression, have been proposed to optimize toolpaths for improved surface quality and reduced geometrical deviation [149].

For springback prediction, combined approaches like Finite Element Method (FEM) with Artificial Neural Networks (ANN) have demonstrated accurate results [150]. Recent optimization studies, employing methods such as Taguchi, have consistently identified the wall angle as the most significant parameter influencing springback in formed truncated cones, particularly those made of dissimilar metal joints. For instance, research by [151] reported that the wall angle contributed over 90% to the variation in springback. However, despite its established significance, there remains a notable controversy regarding the precise impact of increasing or decreasing the wall angle on springback magnitude, necessitating further clarification and investigation.

2.8. Problem and gap statement

Despite several research studies delved into the SPIF covering different perspective, the literature review studies point out gaps that worth it to be investigated for the advancement of metal forming and specifically in the field of incremental forming. The key gaps can be highlighted as follows.

1. Modeling techniques: There is a unclear studies that adopt explicit framework to investigate the full effect of process variables. Additionally, the element formulations need to be discovered regarding their adequacy for better accuracy.
2. Materials: This study focuses on covering a wide range of materials, namely high strength aluminum alloy finding applications in different areas, including aerospace and automotive.
3. Damage calibration: predicting the fracture initiation and evolution based on damage models is still undergoing approach and received less attention. Therefore, calibration damage model for material broadly used in aerospace, automotive, and beyond for damage prediction is with great interest.
4. Deformation and fracture mechanism: Despite the witnessed advancement in understanding the deformation under complex loading, there is a significant shortage in governing the deformation behavior under different stress state.

5. Geometrical accuracy: Analysis of geometrical errors, and more specifically the springback with the associative residual stresses is relatively new area which need to be explored.

CHAPTER 3

RESEARCH SCOPE AND OBJECTIVES

3.1. Introduction

This chapter, shortly provide a detailed description of the objective of this dissertation. The investigation of SPIF was carried out the based on the numerical-experimental focusing on the impact of the input parameters, more specifically on the critical role of the drawing angle giving its direct correlation with the material formability.

3.2. Objectives

The primary aim of this dissertation is to develop and implement a robust Finite Element (FE) model, capable of providing accurate and reliable results within the framework of Incremental Sheet Metal Forming (ISF) processes, with a specific focus on Single Point Incremental Forming (SPIF). This study is poised to make significant contributions to the advancement of metal forming processes, particularly within the sub-field of ISF.

This research seeks to bridge existing knowledge gaps by integrating a combined numerical-experimental approach. This approach aims to comprehensively investigate the SPIF process from various viewpoints, ranging from the analysis of forming force behavior to an in-depth exploration of deformation mechanisms and damage occurrence. Through this integrative framework, the overarching goals of this study are to analyze the forming force, thickness reduction, and damage mechanisms using an explicit integration scheme, thereby offering valuable insights for process optimization and industrial applications.

Specific Objectives

This dissertation aims to address several key aspects of Single Point Incremental Forming (SPIF) through a series of specific objectives:

Objective 1. This objective focuses on investigating the forming forces and their behavior in SPIF using an explicit Finite Element (FE) model. The specific aims are:

- To develop and implement an FE model capable of accurately predicting the trends of forming force components generated during the SPIF process. This model will be rigorously validated against experimental results.
- To assess the individual and interactive effects of key process parameters on forming force. Furthermore, the influence of the number of Gauss integration points through the sheet thickness on prediction accuracy will be evaluated.

Objective 2. This objective is dedicated to understanding thickness reduction and strain variation in SPIF, articulated as follows:

- To investigate the influence of various technological and geometrical process parameters on thickness reduction and strain variation. Numerical findings will be benchmarked against theoretical models (e.g., sine law) to quantify their limitations under large deformation.
- To conduct experimental validation using a CNC lathe machine to corroborate the numerical findings.
- To analyse the triple role of the drawing angle in governing sheet thickness, strain distribution, and stress state.

Objective 3. This objective aims to investigate the stress state in the SPIF process, comparing the performance of both shell and solid elements to gain insights into their adequacy for damage prediction:

- To develop and implement two distinct three-dimensional FE models (shell and solid-based) to evaluate their respective contributions to stress state and fracture mechanism prediction.
- To assess the role of compressive stresses in enhancing material formability limits.
- To evaluate the through-thickness strain gradient and stress distribution in SPIF-formed parts.

Objective 4. This objective focuses on the calibration of commonly used damage models to determine the material formability of two distinct aluminum alloys (commercial-purity and high-strength) using a combined numerical-experimental approach. The investigations have the following aims:

- To experimentally determine the maximum formable drawing angle in SPIF.
- To calibrate material constitutive models, as well as Gurson-Tvergaard-Needleman (GTN) and Johnson-Cook (JC) damage models. These models will then be evaluated for their effectiveness in predicting the onset of fracture in terms of depth and location, therefore, the material formability.

Objective 5. This final objective is designed to evaluate commonly reported geometric errors in SPIF for complex shapes:

- To develop and implement an FE model capable of predicting geometrical deviations relative to the designed profile.
- To assess the effect of the wall angle on springback in commonly adopted benchmark shapes.
- To qualitatively and quantitatively evaluate residual stresses, given their correlation with the amount of springback, leveraging the inherent flexibility of the SPIF process.

CHAPTER 4

MATERIALS AND METHODS

The SPIF has demonstrated the capability to produce complex, axisymmetric and non-axisymmetric shapes with short lead times and high cost-efficiency. However, the process remains under development. To better understand the formability, deformation mode, etc., numerous research studies have focused on a variety of materials including ductile metals (aluminum, steel, titanium, copper alloys) and polymers (PVC, PA, PC), with aluminum alloys being a strong candidate for automotive, aerospace, and biomedical applications due to their high strength-to-weight ratio. This chapter provides a comprehensive overview of the materials adopted for this research, detailing their characterization methods and their fundamental chemical and mechanical properties.

4.1. Materials

This section is dedicated to presenting the materials and the calibration procedure for their constitutive laws. Aluminum alloys have gained significant attention from researchers and engineers due to their high strength-to-weight ratio, making them highly sought after for major industrial applications. The third generation aluminum alloys Al-Li alloys has been identified as a promising material for use in aerospace applications regarding their combination of high strength, lower density, and controlled anisotropy.

Aluminum alloys are frequently employed in the automotive and aviation sectors owing to their favorable combination of high strength, low density, outstanding formability, good machinability, and excellent corrosion resistance, all at an affordable cost. In the aerospace industry, they are used for components such as rocket fuel tanks and the fuselage of space vehicles [152, 153]. In the other hand, for the automotive sector, they are utilized for shock absorbers, brake components, wheels, suspension systems, and chassis frames in various car models (Audi A8, Jaguar XE, Mercedes, BMW (MINI)) [154].

The density of aluminum is approximately one-third that of steel ($\rho_{\text{Al}} \approx 2.7 \text{ g/cm}^3$ vs. $\rho_{\text{Steel}} \approx 7.8 \text{ g/cm}^3$). Studies indicate that substituting one ton of steel with aluminum in a mid-size sedan can reduce greenhouse gas emissions by approximately 18 tons over the vehicle's life cycle [155]. The addition of alloying elements such as copper (Cu), manganese (Mn), silicon (Si), magnesium (Mg), and zinc (Zn) further enhances their performance, leading to increased specific strength, improved thermal and electrical conductivity, and superior corrosion resistance compared to pure aluminum.

According to the nomenclature proposed by the Aluminum Association, aluminum alloys are classified into different series (1000 to 9000), each corresponding to specific compositions and characteristics. For example, the 1000 series alloys (e.g., 1050, 1100) are valued for their

exceptional processability and corrosion resistance, making them suitable for applications like heat insulators [156]. The 5000 series alloys (e.g., 5083, 5754) are non-heat-treatable, magnesium-based alloys offering medium-to-high strength and high corrosion resistance. The 6000 series alloys (e.g., 6061, 6063) are magnesium and silicon-based, providing a good balance of formability, strength, and corrosion resistance, which makes them attractive for automotive body panels and structural components.

Within the scope of this dissertation, two specific aluminum alloys were employed to investigate the SPIF process: a commercial-purity aluminum alloy, Al 1050, medium strength aluminum alloy, Al 3003-O, and a high-strength aluminum alloy, Al 5754. The subsequent sections will detail the characterization and modeling of these materials.

4.1.1. Sheet materials.

4.1.1.1. *Aluminum alloy Al 1050.* The substrate AA 1050 is a commercial-purity aluminum alloy is characterized by high ductility, moderate strength for enhanced workability, and excellent resistance corrosion. Its chemical composition consists of 99.5% of aluminum, along of trace amounts of iron, copper, and other elements. Al 1050 belongs to 1xxx series. To characterize the aluminum sheets, a monotonic uniaxial tensile tests were conducted at room temperature with cross-head velocity $v = 6$ mm/min using dog-bone specimens machined from Al 1050A-H16 sheets with an initial thickness of 1 mm in accordance with ISO 6892-1. The H16 temper condition indicates that the material is strain-hardened. The average value of three tensile test specimens was recorded, enabling the determination of the mechanical properties. The material exhibits an elastic modulus $E = 69.5$ GPa, and a 0.02% offset yield strength $YS = 112.88$ MPa. The complete chemical composition and the mechanical properties are summarized in Tab. 4.1 and Tab. 4.2, respectively.

TABLE 4.1. Chemical composition of the Al 1050-H16

Composition (Weight (%))								
Material	Si	Fe	Cu	Mn	Mg	Zn	Ti	Al
Al 1050	0.25	0.4	0.05	0.05	0.05	0.07	0.05	99.5

TABLE 4.2. Mechanical properties of Al 1050 calibrated using tensile test

E (GPa)	YS at 0.2% offset (MPa)	UTS	Elongation at break (%)
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69.5	112.88	156	5.4%
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Fig. 4.1 presents the true stress-plastic strain curve, which characterizes the material's hardening behavior. It is noteworthy that the true stress-strain curve exhibits minor fluctuations, which can likely be attributed to inherent experimental noise and the sensitivity of the load cell during tensile loading, rather than to the Portevin-Le Châtelier (PLC) effect. The PLC effect is typically associated with solute dislocation interactions in more highly alloyed materials, including both substitutional and interstitial alloys, such as aluminum alloys, austenitic and mild steels, and copper alloys. These minor fluctuations were considered to have a negligible impact on the macroscopic material response and the subsequent finite element analysis. The constitutive model assumes isotropic hardening and obeys the

von Mises yield criterion noted by Eq. 4.1.1, relying on its ability to capture the general qualitative trends of the thickness distribution and the stress state.

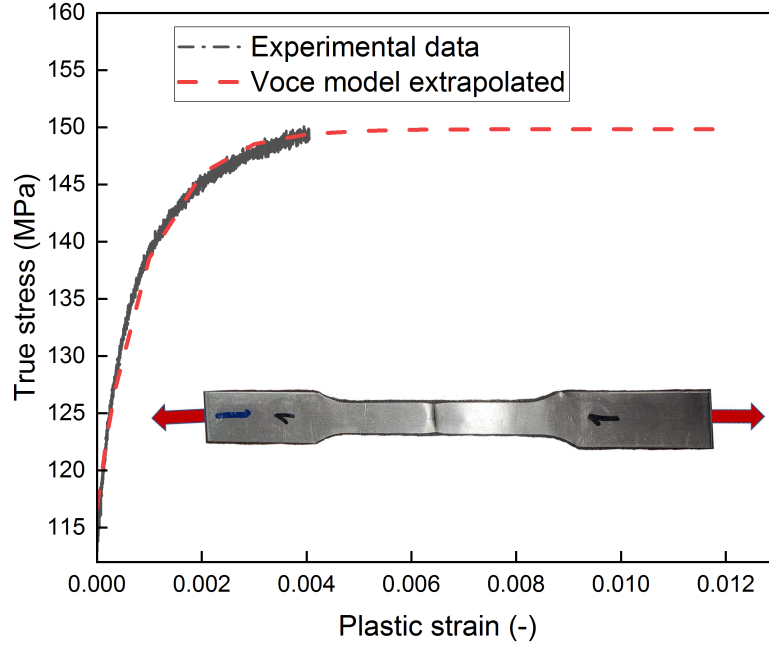


FIGURE 4.1. True stress-plastic strain aluminum alloy Al 1050-H16

$$\sigma_{vM} = \sqrt{\frac{1}{2} [(\sigma_{11} - \sigma_{22})^2 + (\sigma_{22} - \sigma_{33})^2 + (\sigma_{11} - \sigma_{33})^2 + 6(\sigma_{23}^2 + \sigma_{13}^2 + \sigma_{12}^2)]} \quad (4.1.1)$$

The Voce hardening model was adopted to accurately describe the material's strain hardening behavior beyond the instability point. Consequently, the true stress-strain relationship is defined by the following equation:

$$\sigma = \sigma_0 + Q [1 - e^{(-\beta \varepsilon_p)}] \quad (4.1.2)$$

where σ represents the flow stress, ε_p is the plastic strain, and σ_0 , Q , and β are material constants. This equation can be equivalently expressed as:

$$\sigma = \sigma_s - k_v [e^{(-n_v \varepsilon_p)}] \quad (4.1.3)$$

The material hardening parameters for the Voce model were meticulously calibrated by fitting the average experimental data to the chosen hardening model. The identified parameters are: $\sigma_s = 149.831$ MPa, $k_v = 33.136$ MPa, and $n_v = 1077.35$

4.1.1.2. *Aluminum alloy Al 3003-O*. A cold-rolled and annealed aluminum sheet Al 3003-O of 1.2 mm thick under fully annealed state was selected in the current proposal. Accordingly, the material's elastic modulus is $E = 70000$ MPa, the Poisson ratio is $\nu = 0.33$ MPa, the ultimate strength σ_u lies between 90 and 130 MPa, the yield strength is approximately.

The total elongation of the material at break is 19%, as specified by the supplier catalogue. The Swift power law expressed by Eq. 4.1.4 was used to describe the isotropic hardening of the material in the plastic zone [157]

$$\sigma = k_v(\varepsilon + \varepsilon_0)^{n_v} \quad (4.1.4)$$

Here k_v , ε_0 , and n_v denote the strength coefficient, the pre-strain, and the strain hardening. Eq. 4.1.3 explicitly transformed into Eq. 4.1.5

$$\sigma = 183(\varepsilon + 5.7 \times 10^{-4})^{0.229} \quad (4.1.5)$$

The Hill'48 yield criterion referred by Eq. 4.1.5 was adopted to present its anisotropic behavior in the three directions.

$$f(\sigma) = \sqrt{F(\sigma_{yy} - \sigma_{zz})^2 + G(\sigma_{zz} - \sigma_{xx})^2 + H(\sigma_{xx} - \sigma_{yy})^2 + 2L(\sigma_{yz})^2 + 2M(\sigma_{zx})^2 + 2N(\sigma_{xy})^2} \quad (4.1.6)$$

where σ_{ij} are the stress components, the coefficients F, G, H, L, M, and N are material constants.

Hence, the value of these constants is as follows: $F = 1.22$, $G = 1.19$, $H = 0.81$, and $L = M = N = 4.06$

4.1.1.3. *Aluminum alloy Al 5754*. High-strength aluminum alloy Al 5754 with a thickness of 1 mm was selected to investigate material formability and the adaptability of the Johnson-Cook damage model in capturing the onset of fracture in SPIF process. The chemical composition is presented in Tab. 4.3. Uniaxial tensile tests were conducted according to the ASTM-E8 standard using an MTS testing machine with dog-bone specimens, as illustrated in Fig. 4.3. To account for material anisotropy, specimens were extracted in three directions: rolling direction (RD, 0°), diagonal direction (DD, 45°), and transverse direction (TD, 90°). The Lankford anisotropy coefficients were determined in-plane, and the average anisotropy coefficient ($\bar{r}$) was calculated using Eq. 4.1.7.

TABLE 4.3. Chemical composition of the Al 5754-H22 aluminum alloy sheet (wt.%)

Material	Si	Fe	Cu	Mn	Mg	Zn	Ti	Al
Al 5754-H22	0.5	0.1	0.3	2.9	0.4	0.2	0.4	94.2

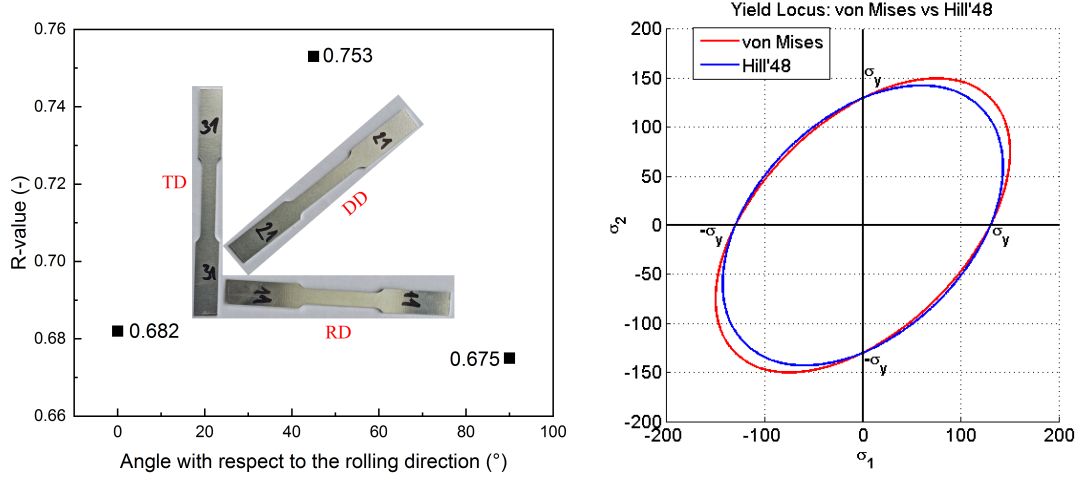


FIGURE 4.2. Anisotropy coefficients and yield surface comparison for Al 5754 aluminum alloy

The calibrated mechanical properties, including the elastic modulus ($E = 69$ GPa), the 0.2% offset yield strength ($\sigma_y \approx 134$ MPa), and the elongation to break ($A = 26.14\%$), are summarized in Tab. 4.4. Post-instability material behavior was modeled using the combined Swift-Voce hardening law (Eq. ??). The hardening parameters, including the weighting factor α , pre-strain ε_0 , hardening coefficients (k_s, k_v), saturation stress σ_s , and hardening exponents (n_s, n_v), are tabulated in Tab. 4.5. The weighting factor α balances the two hardening mechanisms: $\alpha = 1$ reduces the model to the Swift power law (continuous hardening), while $\alpha = 0$ yields the Voce law (stress saturation). Typically, α is calibrated to reflect the transition from power-law hardening at low strains to saturation at high strains. The resulting true stress-strain relationship is illustrated in Fig. ??.

TABLE 4.4. Mechanical properties of the Al 5754-H22 aluminum alloy

Material	E [GPa]	σ_y [MPa]	UTS [MPa]	A [%]	ν [-]
Al 5754-H22	69	134	292	26	0.33

$$\bar{r} = \frac{r_0 + 2r_{45} + r_{90}}{4} = 0.72 \quad (4.1.7)$$

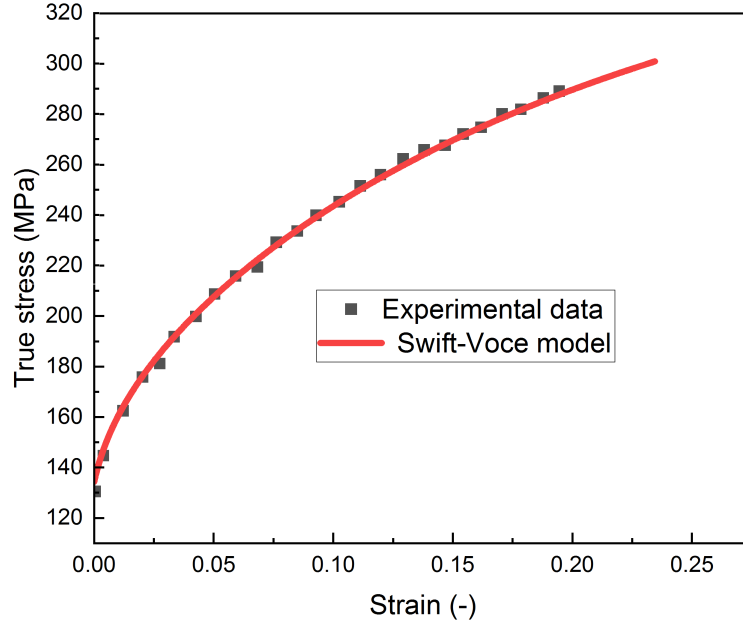


FIGURE 4.3. True stress-plastic strain curve of Al 5754-H22

$$\bar{\sigma} = \alpha \cdot k_s (\varepsilon^p + \varepsilon_0)^{n_s} + (1 - \alpha) \cdot (\sigma_s - k_v e^{-n_v \varepsilon^p}) \quad (4.1.8)$$

TABLE 4.5. Calibrated hardening parameters for the Al 5754-H22 aluminum alloy

Parameter	α	k_s	ε_0	n_s	k_v	σ_s	n_v
Value	0.5179	743.57	0.00455	0.05677	287.90	-21.76	4.9319

The constitutive model assumes isotropic hardening, while the yielding was described by the Hill'48 yield criterion. consequently, the material constants F, G, H, L, M , and N were determined from equation Eq. 4.1.9 based on the anisotropic stress ratios R_{ij} . By assuming the plane stress assumption, the latter can be expressed using the Lankford coefficients R_θ as detailed in equation Eq. 4.1.10.

$$F = \frac{1}{2} \left[\frac{1}{R_{22}^2} + \frac{1}{R_{33}^2} - \frac{1}{R_{11}^2} \right]; \quad G = \frac{1}{2} \left[\frac{1}{R_{33}^2} + \frac{1}{R_{11}^2} - \frac{1}{R_{22}^2} \right]; \quad H = \frac{1}{2} \left[\frac{1}{R_{11}^2} + \frac{1}{R_{22}^2} - \frac{1}{R_{33}^2} \right]$$

$$L = \frac{3}{2R_{23}^2}; \quad M = \frac{3}{2R_{13}^2}; \quad N = \frac{3}{2R_{12}^2} \quad (4.1.9)$$

Assuming $R_{11} = R_{13} = R_{23} = 1$, the remaining ratios are:

$$R_{22} = \sqrt{\frac{r_{90}(r_0 + 1)}{r_0(r_{90} + 1)}}; \quad R_{33} = \sqrt{\frac{r_{90}(r_0 + 1)}{r_0 + r_{90}}}; \quad R_{12} = \sqrt{\frac{3(r_0 + 1)}{r_0 + r_{90}}} \quad (4.1.10)$$

The calculated Hill'48 constants are: $F = 0.42, G = 0.42, H = 0.062, L = M = 1.5$, and $N = 0.4$.

4.1.1.4. *Tool material.* The stylus forming tool featuring a hemispherical end-mill shape is made of high speed steel (HSS) as depicted in Fig. 4.4, mostly used in cutting tools and

metal forming operations owing to its mechanical properties such as preserve a high hardness even at elevated temperature, high resistance to wear, and heat resistance.

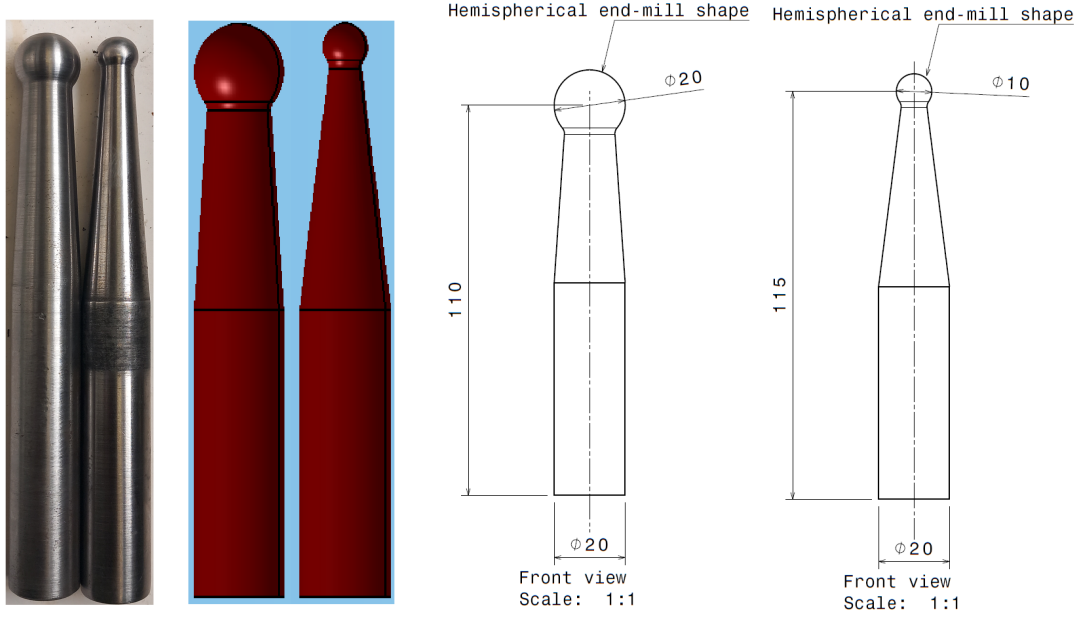


FIGURE 4.4. An overview of the employed forming tools

4.2. Experimental platform

The current investigation employed single-point incremental forming to produce axisymmetric geometries using a 3-axis CNC lathe machine. The experimental configuration consists of a 1 mm thick aluminum alloy sheet clamped between a blank holder and backing plate, with deformation induced by a 10 mm diameter hemispherical end-mill tool as shown in Fig. 4.5. Without any indication, the standard process parameters were maintained throughout the experiments, including step down $\Delta z = 0.6$ mm, wall angle $\psi = 60^\circ$, feed rate $F = 360$ mm/min, spindle speed $N = 600$ rpm, and drawing height $h = 43.3$ mm, utilizing the z -profile toolpath. The sheet was clamped on its boundaries by both a blank holder and a backing plate, each has an orifice with an opening diameter of $D = 110$ mm, rigidly secured with twelve CHC screws with 8 mm diameter. The same batch of material was used to carry on the experimental test; therefore, the sheets were trimmed to the size of 130×130 mm² with an initial sheet thickness of tensile test specimens $t_0 = 1$ mm. Deep drawing lubricant oil was employed to mitigate the friction effect at the contact area which, thanks to its good cooling ability, contributes to dissipate the heat generated during deformation.

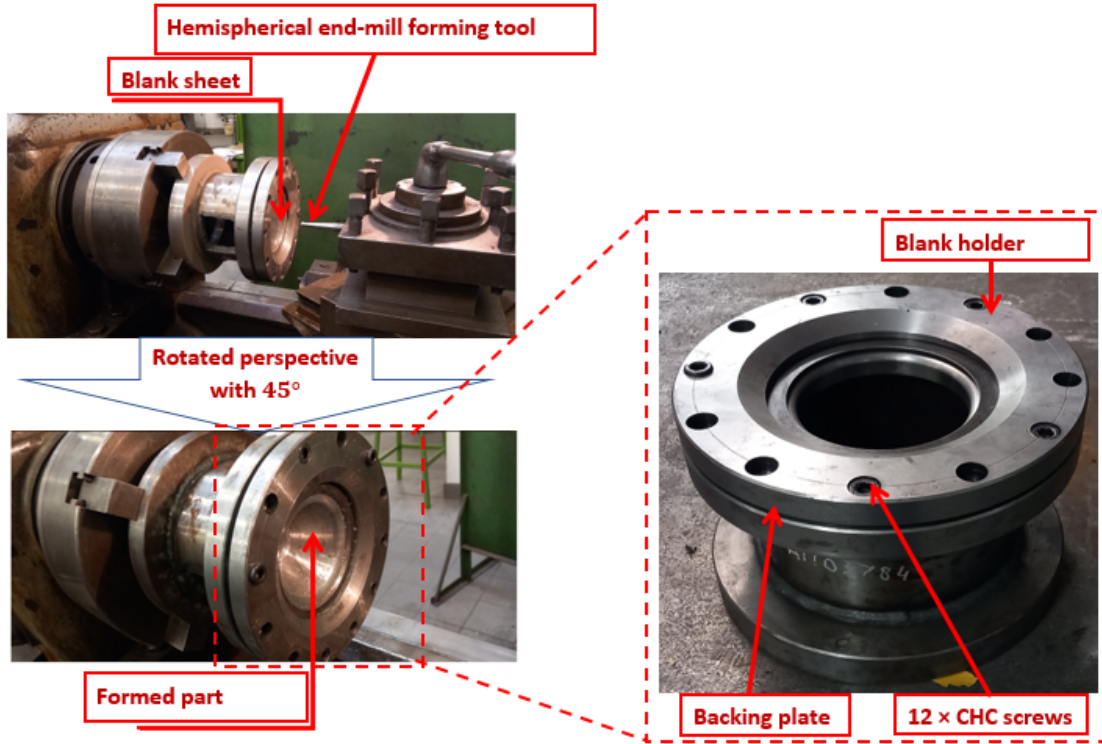


FIGURE 4.5. Experimental setup of the SPIF process.

4.3. Toolpath generation for tool movement

Several toolpath strategies were developed based on their application to machining processes. In the conventional toolpath methodologies, the toolpath is generated using sliced contours parallel to the horizontal plane. The SPIF process involves layer-by-layer deformation of the sheet metal through localized plastic deformation induced by a hemispherical punch following a prescribed trajectory. This incremental approach enables complex geometries to be formed using relatively simple tooling. The toolpath strategy (see Fig. 4.6) considerably influences the final part quality, dimensional accuracy, and process efficiency. Two predominant toolpath strategies are commonly employed, i.e., z-level profile and spiral toolpath. The contour strategy follows a stepwise, layer-by-layer path along the part's geometry (see Fig. 4.6a), making it particularly suitable for parts with sharp features or complex geometries. While this method offers shorter forming times, it may produce surface scarring due to discrete tool movements between layers. In contrast, the spiral toolpath (see Fig. 4.6c) employs a continuous, smooth trajectory that minimizes abrupt directional changes, thereby promoting superior surface finish at the expense of longer forming durations. Selection between these strategies depends on multiple factors, including product geometry, material properties, required surface quality, and production time constraints. In this study, both approaches are utilized to evaluate their respective merits. In the current dissertation, the toolpaths were programmed either by Power-mill software or Matlab code.

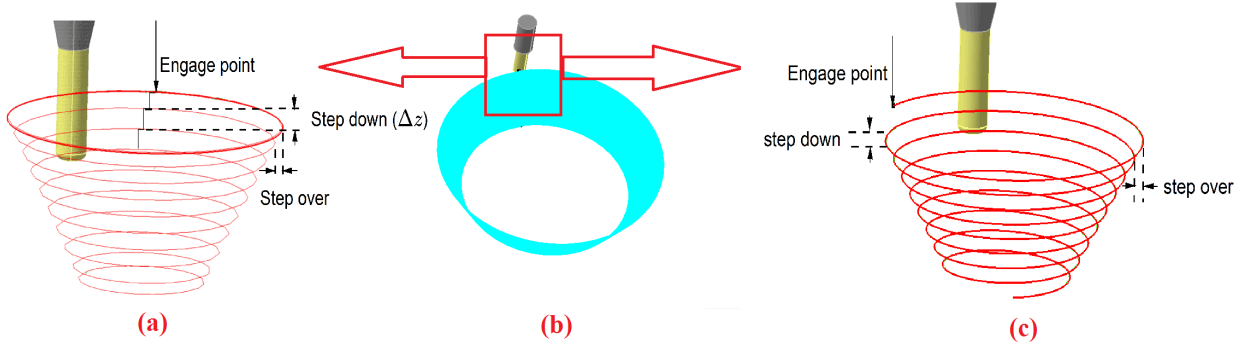


FIGURE 4.6. 3D representation of the toolpath strategies: (a) z-level profile, (b) Spiral toolpath

4.4. Thickness measurement

For dimensional analysis accuracy, the thickness distribution was measured using optical microscope with $6.5\times$ magnification as illustrated in Fig. 4.7. Images were captured and post-processed using AxioVision software for detailed measurement and analysis, the average cross-sectional measurements of three formed cones were obtained. The acquired image was processed through AxioVision software to quantify thickness distribution in critical regions of interest.

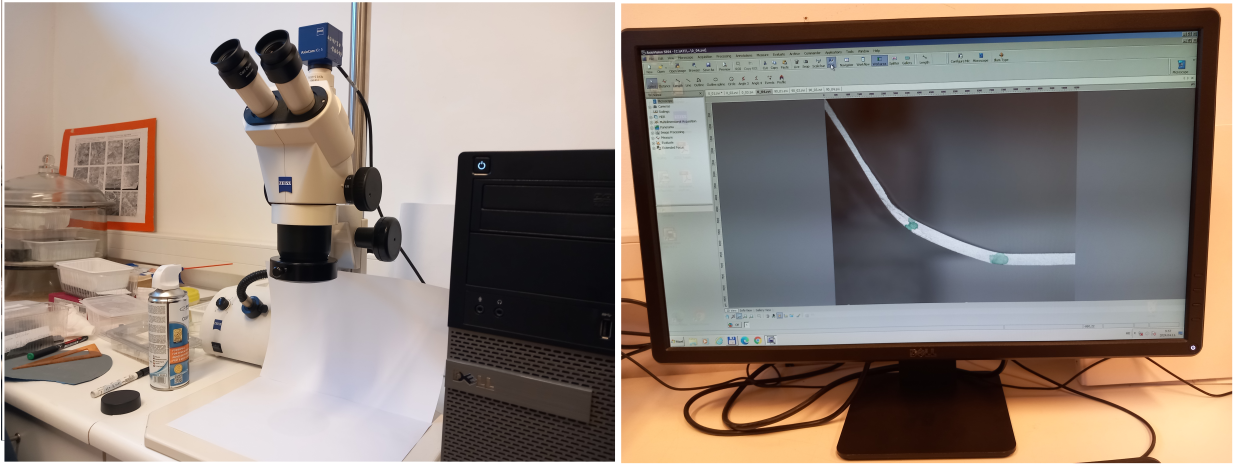


FIGURE 4.7. Device for thickness measurement.

4.5. Description of adopted geometries

Both truncated cone and pyramid shapes with constant-wall angle referred as (TCCWA) and (TPCWA) were used as the targeted shapes to perform the SPIF process with different materials. These shapes were denoted sometimes simply by (TC) and (TP) shapes for the sake of brevity. The selection of these parts rely on their ability to capture the general trends of the forming force, thickness distribution, geometrical accuracy, and damage mechanism. These benchmarks covered different range of the robustness and the complexity of the process. Without any indication, the geometrical dimension as detailed in Fig. 4.8 will be used

in the subsequent paragraphs.

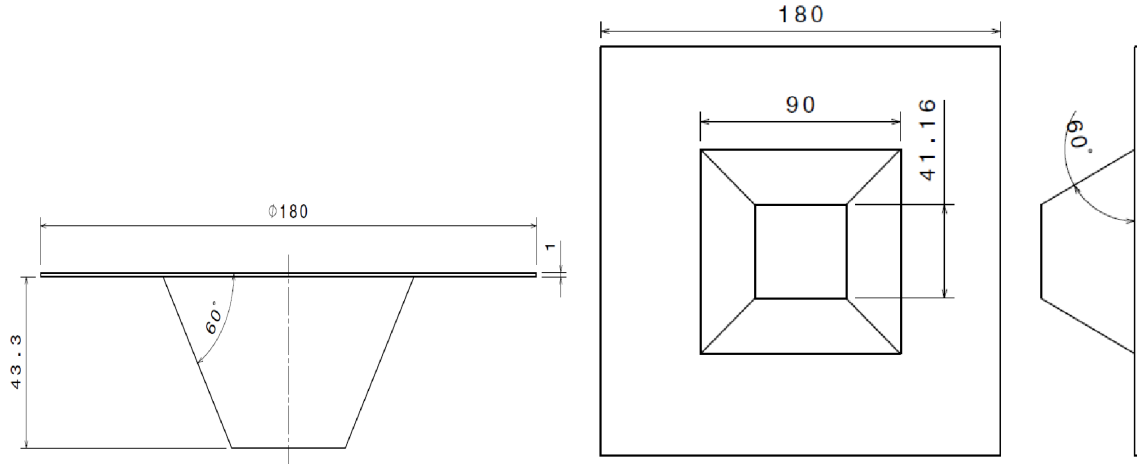


FIGURE 4.8. 2D drawing: (a) TC shape, (b) TP shape.

4.6. Finite element modeling of SPIF

Accurate and reliable finite element (FE) modeling of SPIF process is crucial, as it directly impacts computational time, convergence, and the accuracy of simulation results. A robust numerical model necessitates careful selection of integration schemes, contact formulations, element types, and boundary conditions (BCs).

4.6.1. Numerical integration schemes. Numerical integration methods are employed to solve the differential equations governing the deformation process by discretizing the continuous domain into discrete points. Two primary schemes are commonly utilized: implicit and explicit integration methods. The implicit approach relies on the Newton-Raphson algorithm to solve static equilibrium equations. While unconditionally stable and offering high accuracy, it often incurs significant computational time due to the iterative nature required for convergence at each time increment (Δt). Conversely, explicit frameworks, such as the forward Euler method, Runge-Kutta, or central difference method, are conditionally stable. Their stability is governed by the condition $\Delta t \leq L/c$, where L represents the smallest element length and c is the wave speed in the material. Failure to satisfy this condition can lead to divergence or inaccurate results. For SPIF, given its inherently long processing times, the explicit approach is generally recommended to reduce computational cost, often through techniques like mass scaling or time scaling.

4.6.2. Element formulation. Element formulation significantly influences the accuracy of simulation results. Both solid and shell elements are frequently employed in SPIF simulations, particularly due to the process's intermittent contact, high non-linearity, and large localized strain deformations. Solid elements with reduced integration points, combined with hourglass control, have demonstrated improved results, reduced computational time, and better handling of severe element distortion [158]. Shell elements, designed for in-plane

deformation under plane stress conditions, are also well-suited for SPIF [61]. Studies have shown that shell elements can provide better predictions for forming forces, though sometimes at the expense of thickness accuracy, whereas solid elements tend to offer more detailed thickness predictions. Ultimately, while both element types are adopted, shell elements are often preferred due to their lower computational cost.

4.6.3. Contact formulation. The complex contact conditions inherent to SPIF necessitate a carefully chosen contact formulation. The contact zone between the tool and the sheet is small and continuously changes as deformation progresses. Coulomb's friction law is widely prioritized to model the contact at the tool-sheet interface, effectively coupling tangential and normal pressures with a friction coefficient. However, some studies have explored simplified contact algorithms, such as those proposed by Robert et al. [159], which aim to reduce computational time by allowing larger tool displacement increments.

4.6.4. Finite element model setup. In this dissertation, an FE model was constructed using Abaqus/Explicit, as depicted in Fig. 4.9. In this setup, the punch and backing plate are treated as rigid, non-deformable bodies, while the sheet is modeled as a deformable material. Contact interactions are established using a surface-to-surface formulation, incorporating Coulomb's law with a friction coefficient of $\mu = 0.05$. The shear stress (τ_f) at the interface is thus related to the normal stress (σ_n) by:

$$\tau_f = \mu \sigma_n \quad (4.6.1)$$

To simulate the 3D toolpath movement, the toolpath was programmed using a computer-aided manufacturing (CAM) package after defining the desired geometry. Subsequently, prescribed displacements along the x , y , and z directions are applied to the punch, while the sheet is rigidly clamped along its ambient edges. Both element type, namely S4R shell element and C3D8R solid element meshes are adopted.

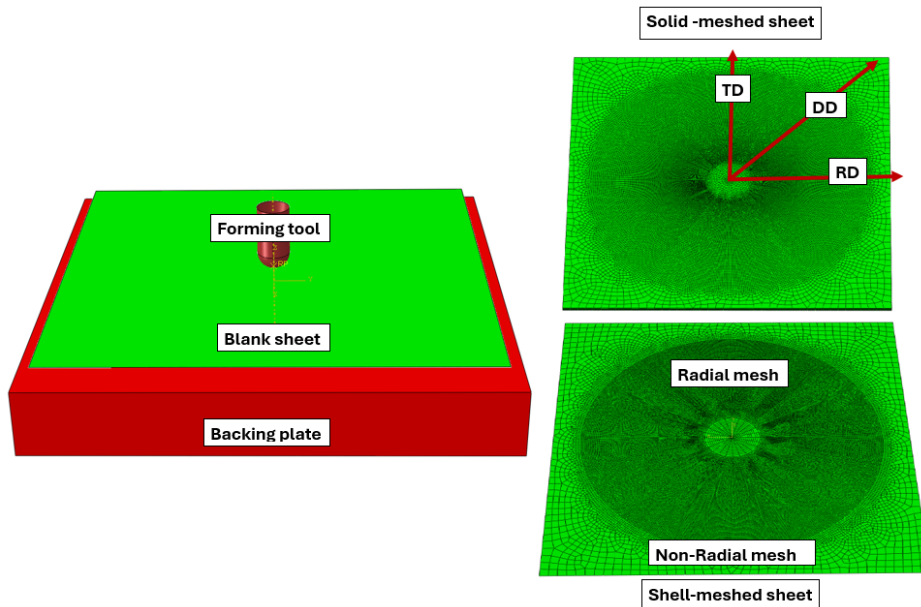


FIGURE 4.9. Assembly model of the SPIF process.

CHAPTER 5

FORMING FORCE ANALYSIS

5.1. Introduction

The accurate understanding and prediction of forming forces are of paramount importance in ISF processes. These forces must be precisely controlled: they need to be sufficient to successfully deform the material sheet to the desired geometry without defects, yet not so excessive as to induce unwanted flaws, such as thinning, tearing, or premature fracture. Conversely, insufficient forming forces can lead to incomplete deformation, resulting in parts that deviate from the designed shape. Consequently, the selection and interplay of various process parameters significantly influence both the magnitude and behavior of these forming forces.

This chapter is dedicated to an in-depth study of the forming forces in SPIF, utilizing an explicit finite element approach. The investigation encompasses the individual impact of critical process variables and subsequently analyses the interaction effects among the most relevant and significant parameters, aiming to provide comprehensive insights into their influence on the forming force characteristics.

5.2. Experimental platform and forming techniques

The experimental investigation of the SPIF process was conducted utilizing a 3-axis Computer Numerical Control (CNC) milling machine. To securely clamp the sheet material along its periphery, a robust steel fixture was employed, as depicted in exploded view in Fig. 5.1a.

Forming forces were precisely measured using a Kistler 9265B six-component force dynamometer (Fig. 5.1c), which was connected to a complementary Kistler 5017-A 8-channel amplifier. This system is capable of measuring the three force components (F_x , F_y , and F_z) within a range of -15 to 30 kN. The sensors were strategically mounted between the machine table and the steel fixture, enabling the accurate recording of forces exerted on the workpiece by the forming tool via a dedicated data acquisition system. The workpiece consisted of a square aluminum sheet, measuring 220 mm $\times$ 220 mm, made from aluminum alloy Al 3003-O, the properties of which are detailed in Section 4.1.1.2. This material was selected for producing a conical shape, with specific geometrical requirements to be elaborated in subsequent paragraphs.

The standard process parameters for these experiments included a vertical step size (Δz) of 0.5 mm, a tool diameter (d) of 10 mm, and a wall angle (ψ) of 50°. The initial thickness

of the worksheet utilized was $t_0 = 1.2$ mm.

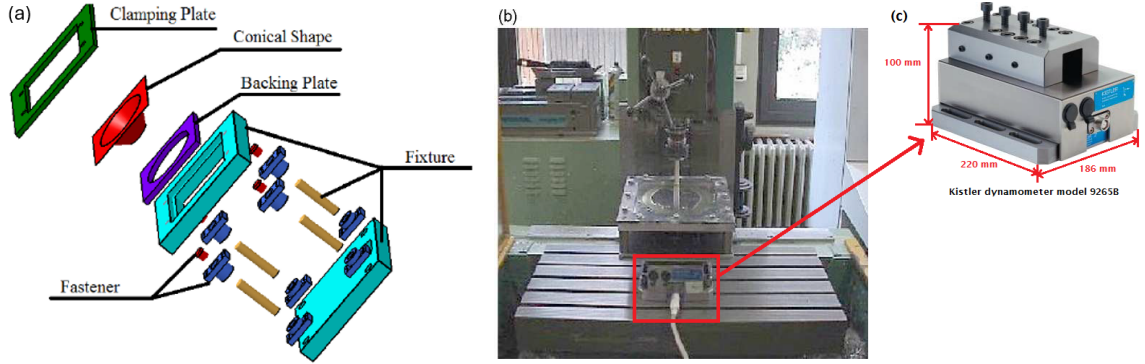


FIGURE 5.1. Experiment setup: (a) exploded view of the forming fixture with formed part, (b) the forming setup on CNC milling machine [45], (c) 3D illustration of the Kistler dynamometer model.

5.3. Forming forces based-model validation

The Fig. ?? presents a plot of the reaction forces on the y -axis versus the increment time on the x -axis during the SPIF of AA 3003-O with a continuous trajectory, which refers to the spiral toolpath strategy. Forces were measured experimentally and predicted numerically in three coordinate systems directions. These forces include F_x and F_y acting in the (x, y) plane, and F_z exerted in the z direction, which typically has the maximum value compared to the other two forces. These three force components are combined into a single force called Force Vector Sum and its Mean magnitude, respectively. which will be used for the next analysis. Fig. ??a and ??b depict the evolution of the three components, measured and predicted by experimental and numerical investigations, respectively. In this step, the tool diameter was set to $d = 12.7$ mm. Upon analysing these graphs, it is observed that as the forming operation initiates, the forces start at zero and gradually increase as the tool moves downward until they reach a stable value, remaining approximately constant. This behavior can be explained by the expansion of the plastic deformation zone (PDZ): as the tool presses along a few contours, the volume of material undergoing plastic deformation increases until the PDZ is fully established across the sheet thickness. While strain hardening increases the material's strength, the sheet thickness simultaneously decreases, leading to thinning. Consequently, stabilization occurs when the rise in strength is offset by the reduction in the cross-sectional area of the material being formed.

Both in-plane forces exhibit a sinusoidal behavior, varying between their minimum and maximum values as a function of increment time. Some small differences between F_x and F_y are observed due to the anisotropy factor and the non-symmetry of the deformation. However, the axial force F_z displays a higher amplitude of forming force, making it very important when selecting the machine. To the end, the observed variability in the force signals, particularly, the fluctuations around the mean F_z value and the oscillatory behavior

of F_x and F_y , is intrinsic to ISF. These variations are primarily attributed to intermittent tool-sheet interaction, local changes in material stiffness, and dynamic friction effects.

Aim to validate the above numerical results, a comparison was made with experimental results conducted by Duflou et al. [45] showing fairly good agreement with small fluctuations between the results, with a maximum standard error less than 5% as $\Delta F = (F_{Exp} - F_{Num})/F_{Exp} \times 100\% = ((600 - 545)/545) \times 100\% = 4.3\%$ when the mean value of the magnitude of the vector force sum and its mean is stabilized. (??c, and ??d). This error can be explained by the assumptions made in the model, including considering isotropic hardening to describe the material behavior in the plastic zone, while in reality, the material exhibits the Bauschinger effect. Furthermore, the model approximates contact to adhere to Coulomb's law, which differs from real forming processes where contact increases significantly as the tool moves deeper. Finally, all Figs. 5.2a, 5.2b, 5.2c, and 5.2d exhibit similarities and resemblance terms of shapes.

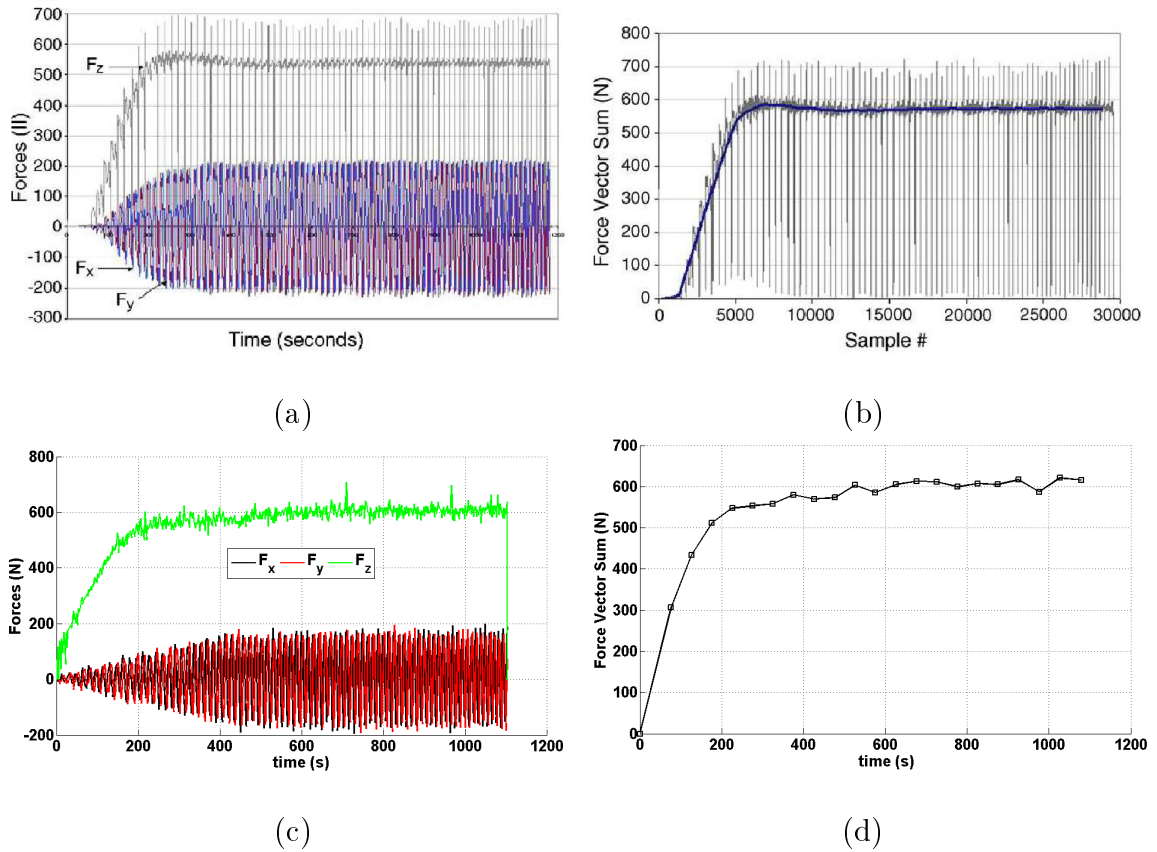


FIGURE 5.2. Experimental and numerical results for the forming forces during SPIF with standards parameters (a) Experimental data: Three force components F_x , F_y , and F_z ; Experimental data: (b) Magnitude of the total force and its mean, (c) Numerical data: Three force components F_x , F_y , and F_z , (d) numerical data: Magnitude of the total force and its mean.

5.4. Quantitative analysis of the effect of process parameters on forming force

5.4.1. Step size. The influence of the vertical step size (Δz) on the magnitude of the forming force was systematically evaluated. As depicted in Fig. 5.3, which illustrates the resultant forming force (vector sum and its mean) derived from both experimental data and numerical simulations, the force magnitude was assessed at defined intervals across the discrete step depth domain of $\Delta z \in [0.5, 0.75, 1.5, 2]$ mm. Two primary observations can be made regarding the increase in forming force with increasing step size. Firstly, this increase is directly attributable to the enhanced mechanical resistance of the workpiece, as a larger step size necessitates the deformation of a greater volume of material. Secondly, the numerical results demonstrated good agreement with the experimental data. Specifically, the relative errors in force amplitude between the two approaches were calculated to be 6.3%, 3.4%, and 1.6% for increment sizes of 0.5 mm, 0.75 mm, and 1 mm, respectively.

5.4.2. Sheet thickness. To comprehensively capture the general trend of the effect of initial sheet thickness (t_0) on forming forces, a range of thicknesses was investigated, specifically $t_0 \in [0.85, 1.2, 1.5, 2]$ mm. Fig. 5.3 presents the evolution of the maximum total force vector sum and its mean for these varying sheet thicknesses. While the figure may also show results for different wall angles, our current focus is specifically on the 50° wall angle. Both numerical and experimental data exhibit a generally good agreement. However, the standard error between the two results tends to increase with sheet thickness: 5.7% for the smallest sheet thickness ($t_0 = 0.85$ mm), 6.4% for $t_0 = 1.2$ mm, 13.26% for $t_0 = 1.5$ mm, and 14.7% for the largest sheet thickness ($t_0 = 2$ mm). This trend indicates that the numerical results progressively overestimate the resultant forming force compared to the experimental measurements, particularly for thicker sheets (in the range of 1.5 to 2 mm). This discrepancy might be attributed to an insufficient number of Gaussian integration points used through the sheet thickness, which could limit the accuracy of predictions as the sheet thickness increases. Fundamentally, increasing the sheet thickness leads to a rise in the resultant forming force, a phenomenon explained by the greater volume of metal subjected to deformation at each forming contour, thereby requiring a higher force to achieve the desired shape. This observed trend regarding sheet thickness aligns well with previous findings reported in the literature for a broad range of sheet thicknesses [52].

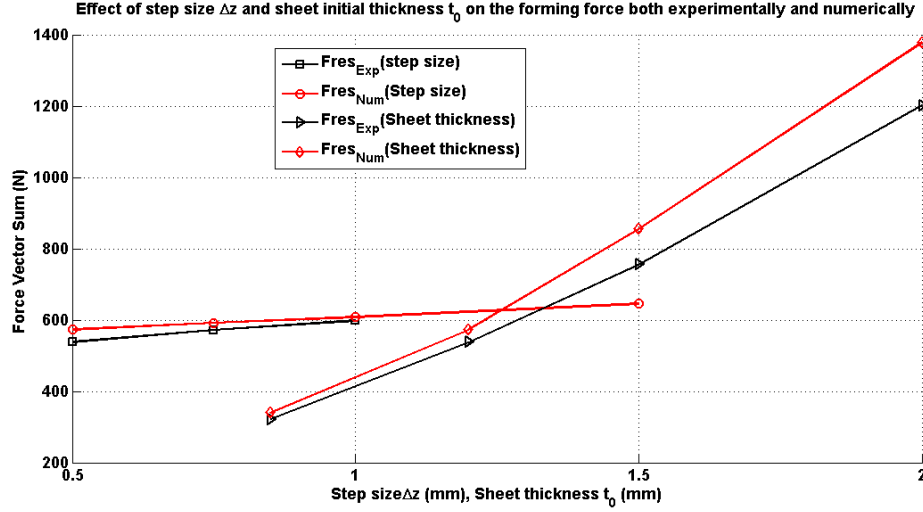


FIGURE 5.3. Effect of individual variables on the average magnitude of the forming force and its mean: Effect of the step depth Δz and the sheet initial thickness t_0 on the forming force.

5.4.3. Tool diameter. The influence of the tool end diameter on the forming force during SPIF was systematically investigated by varying the tool diameter (d) from 10 mm, 15 mm, to 20 mm. Fig. 5.4a illustrate the evolution of the total force vector sum and its mean magnitude over forming time for these different tool diameters. A comparison with numerical simulations was performed, as presented in Fig. 5.4b, revealing relative errors of 14.93% for the smallest tool diameter ($d = 10$ mm), 9.3% for $d = 15$ mm, and 5.67% for $d = 20$ mm. It was consistently observed that the numerical model tended to overestimate the resultant forming force compared to experimental measurements.

Two key observations emerged from this analysis. Firstly, the amplitude of the resultant forming force gradually increases at the initiation of the forming operation, reaching a maximum value before stabilizing. Secondly, an increase in tool diameter led to a proportional rise in the forming force for the selected tool sizes. This phenomenon can be attributed to the larger contact surface area between the tool and the blank at the interface. A greater contact area implies that more material is subjected to deformation during each revolution, thereby requiring a higher force for the tool to penetrate and form the sheet. Quantitatively, the force augmentation from a 10 mm to a 15 mm diameter tool was approximately 9.56%, while an increase from 10 mm to 20 mm resulted in an 18.16% augmentation. These findings are consistent with previous research [160]. Furthermore, employing a larger tool diameter was found to decrease the overall computation time, primarily due to the ability to utilize a larger increment depth.

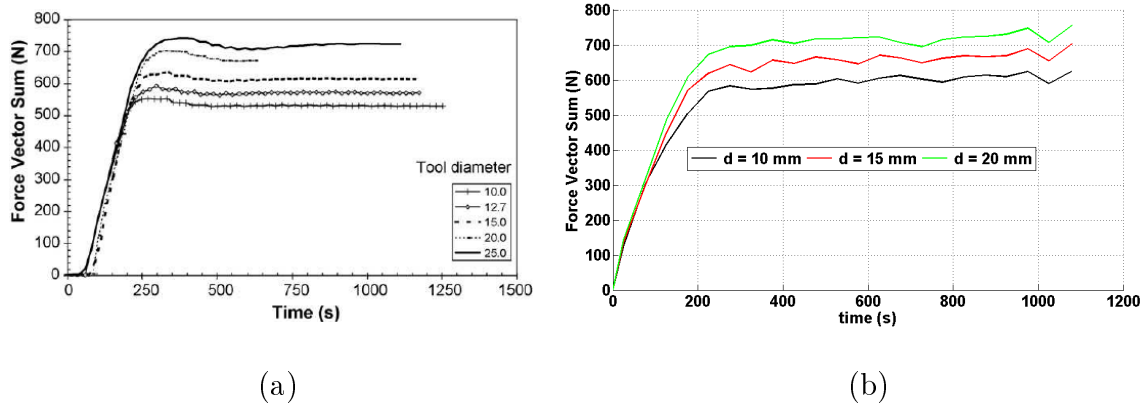


FIGURE 5.4. Effect of individual variables on the average magnitude of the forming force and its mean: Effect of tool diameter d .

5.4.4. Wall angle. The impact of the wall angle (ψ) on the forming force behavior was investigated by varying it from 40° to 60° . Figs 5.5a and 5.5b depict the total force vector sum and its mean magnitude for various wall angles. The experimental and numerical results showed generally small relative errors, although the numerical model consistently overestimated the resultant forming force.

Two distinct behaviors were observed regarding the forming force as the wall angle changed. For wall angles in the range of $20^\circ \leq \psi \leq 50^\circ$, the forming force exhibited a gradual increase until it reached a peak, after which it stabilized for the remainder of the operation. Conversely, for steeper wall angles where $\psi \geq 60^\circ$, the forming force demonstrated a drastic initial increase, reaching a maximum value. Subsequently, it experienced a slight decrease, followed by a gradual increase again, eventually stabilizing until the completion of the forming operation. This complex behavior can be explained by the interplay of severe thinning and subsequent strain hardening in the formed sheet. Initial thinning reduces the material's resistance, but as strain hardening occurs, the material's strength increases, requiring more force to continue deformation. Experimentally, the peak force augmentation from 20° to 30° was approximately 30.5%. Further increases in wall angle led to significant augmentations: 55.62% from 20° to 40° , 73.84% from 20° to 50° , and 89% from 20° to 60° . Numerically, the force increase was about 9.1% when the angle changed from 40° to 50° , and approximately 15.1% from 40° to 60° . In terms of computational efficiency, employing a steeper wall angle generally implies an augmentation of the forming time due to restrictions in achieving a large forming contour, in addition to a drastic increase in the peak forming force.

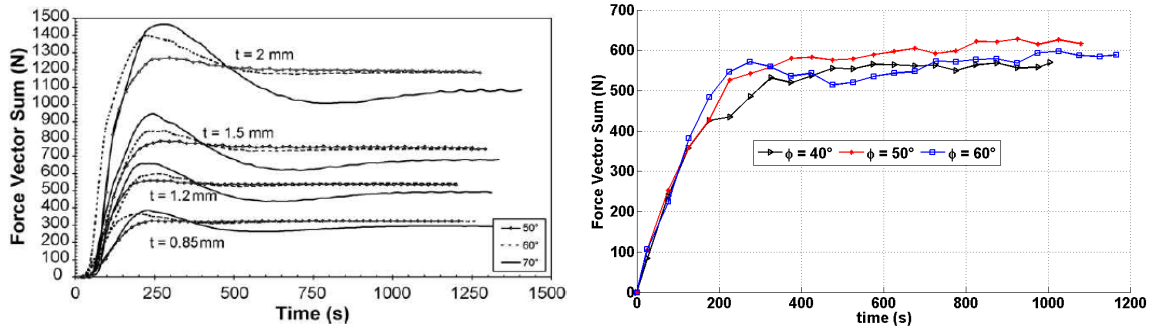


FIGURE 5.5. Effect of individual variables on the average magnitude of the forming force and its mean: Effect of the drawing angle ψ .

5.4.5. Toolpath strategies. The influence of toolpath trajectories on forming forces was investigated by comparing both profile (contour) and spiral strategies. For this analysis, the axial forming force (F_z) was adopted instead of the resultant forming force, primarily due to the inherent difficulty in distinguishing between the two toolpath types when considering the resultant vector. As illustrated in Fig.5.6, the forming force F_z consistently increases upon the initiation of the forming stage. Notably, the continuous engagement of the tool with the material in the spiral trajectory generally results in a greater forming force compared to the intermittent contact characteristic of the profile trajectory, particularly when evaluated across various tool diameters.

The difference in forming force between these two trajectories becomes more pronounced with increasing tool diameter, which can be attributed to the enlarged contact surface area between the forming tool and the blank. However, within the stabilization zone of the forming process, the axial forming forces extracted from both trajectories tend to converge for each respective tool diameter. A significant advantage of employing a spiral toolpath is the absence of force drop-downs, which are unfortunately characteristic of contour toolpaths. These force fluctuations in contour trajectories can have a detrimental impact on the surface roughness of the formed sheet, often leading to scarring and consequently compromising the overall quality of the product.

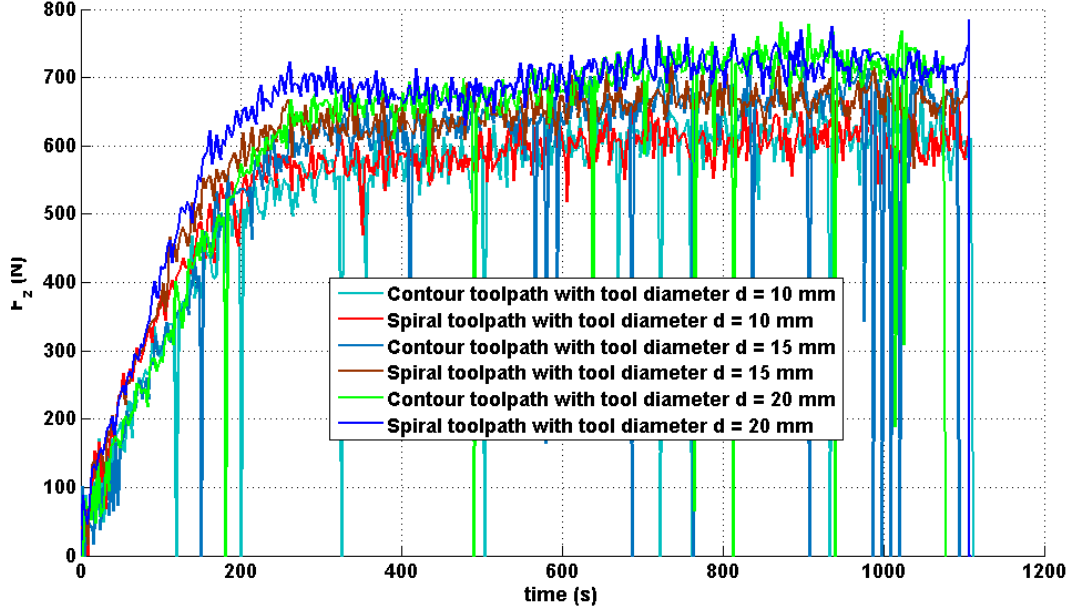


FIGURE 5.6. The axial forming force with different tool diameters and tool-path strategies

5.4.6. Geometrical shape. For the sake of the influence of the geometrical shape as well the interaction effect, the material employed is aluminum alloy Al 1050. It noteworthy that the standard parameters are slightly changed and set to the reference ones resented in Section. 4.2.

It can be observed that for the same working parameters but with a different geometry set, there is a small change in the forming force magnitude, which is consistent with the results presented in [45]. However, the forming time to manufacture the conical part is shorter compared to the truncated pyramid. Even though, the in-plane forces have demonstrated slight distinct behavior, the axial forces have shown strong similarities by attaining the same magnitude ≈ 600 N.

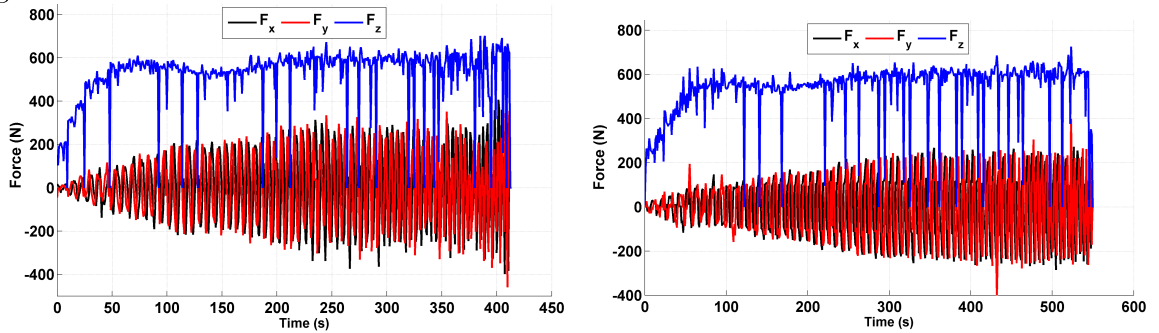


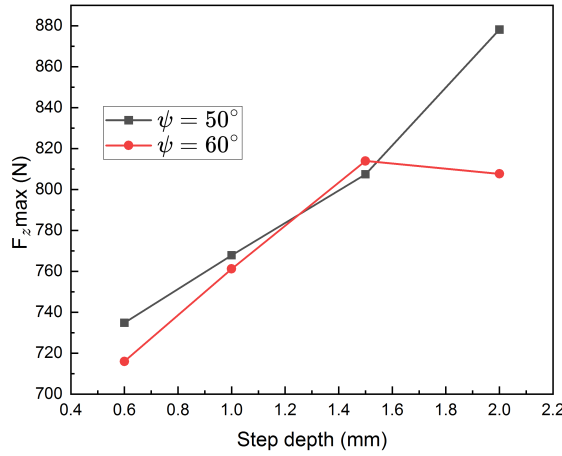
FIGURE 5.7. Forming forces components: (a) Truncated cone, (b) Truncated pyramid

5.4.7. Interaction effect. Given the importance of the previous factors on the forming force evaluated separately, an attempt was performed to investigate the interaction effects on the maximal axial forming force in the steady-state regime after filtering the unwanted noise signal. The influence of the interaction of step size Δz and wall angle ψ is depicted in

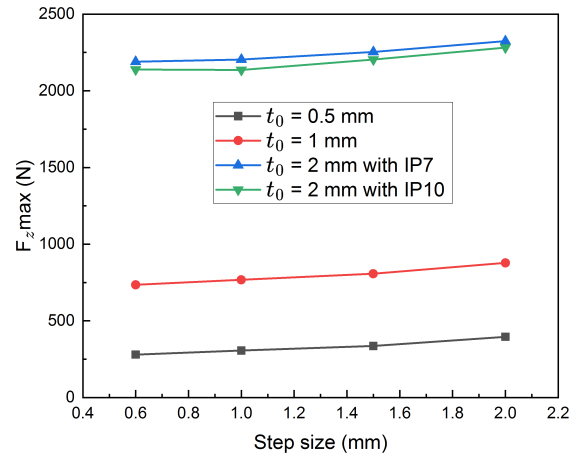
Fig. 5.8a. The maximal force F_{zmax} is increased drastically when the steepness of the slope angle increases due to the fact that larger step size causes a proportional rise on the forming inducing a large amount of material is deformed at that instant. However, a combination of these two factors results in a rapid increase and therefore render it to be a critical factor of the forming. Notably, F_{zmax} has shown a decrease when the wall angle reaches $\psi = 60^\circ$. Above that the forming under high drawing angle result in high risk of material fracture even before reaching the targeted forming height.

The combination of step size Δz and sheet thickness t_0 (Fig. 5.8b) shows a gradual increase following the same trend at different step depths. However, for thicker sheets $t_0 = 2$ mm, the forming force rapidly reaches an amplitude above 2 kN, regardless of the selected step size. This behavior can be attributed to the larger amount of material that must be formed at each contour. To ensure accurate results, the number of integration points through the sheet thickness was intentionally increased to seven and ten. Consequently, seven integration points (IP7) were selected, as the relative error compared to ten integration points (IP10) was approximately 3%.

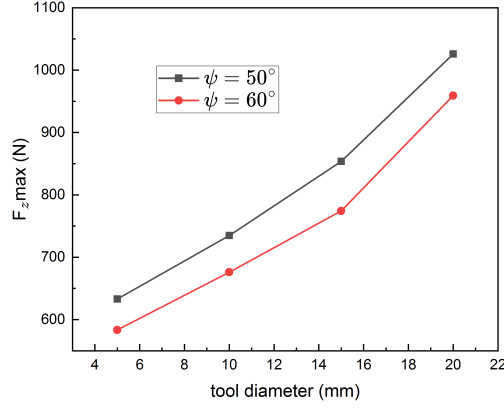
In Fig. 5.8c, the interaction between the tool diameter d and wall angle ψ was illustrated. As the the tool diameter increases, F_{zmax} rises accordingly, which is the result of the increase of the contact surface at the interface tool/ sheet. However, the increase of the wall angle close to the critical one has contributed significantly to the drop of the forming force owing to the appearance of the material thinning.



(a)



(b)



(c)

FIGURE 5.8. Interaction effects on $F_{z,max}$: (a) Step size/ wall angle, (b) Step size/ sheet thickness, (c) tool diameter/ wall angle

5.5. Summary

A summary campaign was prepared to ascertain the force augmentation of the individual parameters as well for the interaction effect as presented in Tabs. 5.1, 5.2, 5.3, and 5.4 summarizes the percentage variation of the forming force as a function of sheet thickness both numerically and experimentally.

Individually, for the same order of magnitude, the sheet thickness has shown a greater increase compared to the step depth reaching an augmentation approximately 305% with reference to set value. However, the tool diameter and the wall angle have demonstrated a close effect.

In terms of the interaction effect, outcomes revealed that combining a high step size with thicker sheet lead a drastic rise of the forming force magnitude. In the other hand, the interplay role of tool diameter with the wall angle has a reduction of the forming force magnitude when the upper bounds of these factors were interacted together.

TABLE 5.1. comparison of the individual contribution of the process parameters on the forming force average magnitude

Forming force [N] Step size [mm]	F_{resExp}	$\Delta F_{Exp}[\%]$	F_{resNum}	$\Delta F_{Num}[\%]$
0.5	538.59	-	572.99	-
0.75	572.24	6.25	591.43	3.22
1	598.39	11.10	608.55	6.21
1.5	-		645.59	11.25
Forming force [N] Sheet thickness [mm]	F_{resExp}	$\Delta F_{Exp}[\%]$	F_{resNum}	$\Delta F_{Num}[\%]$
0.83	322.147	-	340.456	-
1.2	538.590	67.187	572.994	73.905
1.5	755.033	134.375	855.150	151.178
2	1201.342	272.917	1378.026	304.759
Forming force [N] Tool diameter [mm]	F_{resExp}	$\Delta F_{Exp}[\%]$	F_{resNum}	$\Delta F_{Num}[\%]$
10	530.17	-	609.33	-
15	614.22	15.85	671.34	10.18
20	669.83	26.34	707.81	16.16
Forming force [N] Wall angle ($^{\circ}$)	F_{resExp}	$\Delta F_{Exp}[\%]$	F_{resNum}	$\Delta F_{Num}[\%]$
40	485.960	-	544.050	-
50	553.558	13.91	593.507	9.09
60	604.946	24.48	626.096	15.08

TABLE 5.2. Int.effect step size/ wall angle

Forming force [N] Inter. Step size/ wall angle $\Delta z/\psi$	F_{resNum}	$\Delta F_{Num}[\%]$
0.6/50	734.828	-
0.6/60	715.987	
1/50	767.823	
1/60	761.19	-
1.5/50	807.425	
1.5/60	813.925	
2/50	878.129	
2/60	807.626	

TABLE 5.3. Inter. effect Step size/sheet thickness

Inter. Step size/sheet thickness	Forming force [N]	F_{resNum}	$\Delta F_{Num}[\%]$
0.6/0.5		280.038	-
0.6/1		734.828	162.402
0.6/2		2189.35	681.805
1/0.5		306.198	-
1/1		767.823	150.760
1/2		2204.44	306.198
1.5/0.5		335.944	-
1.5/1		807.425	140.345
1.5/2		2253.29	570.733
2/0.5		396.259	-
2/1		878.129	121.604
2/2		2323.83	486.44

TABLE 5.4. Inter. effect Step size/sheet thickness

Inter. Tool diameter/ wall angle	Forming force [N]	F_{resNum}	$\Delta F_{Num}[\%]$
5/50		633.148	-
5/60		583.537	7.83
10/50		734.828	-
10/60		675.987	-8.70
15/50		853.855	-
15/60		774.197	-9.33
20/50		1025.96	-
20/60		959.191	-6.50

As a summary, this chapter, FEM followed by substantial experimental validation was carried out. Several impactful process parameters were undertaken including their interaction effect on the forming force magnitude denoted as the average vector sum and its mean amplitude. The main conclusions from this investigation are summarized as follows:

- A Matlab code was developed to generate a coordination between x , y , and z coordinates for each time increments to feed the tool movements.
- The forming force in the axial direction (perpendicular to the tool movement) demonstrated the highest magnitude compared to the in-plane forces parallel to the direction, which were shown an oscillatory pattern.
- The effects of critical process parameters ranging from technological to geometrical were investigated using combined numerical-experimental approach.
- The role was revealed through a combination of most influential key factors and therefore, presenting their significant contribution.

CHAPTER 6

ANALYSIS OF THICKNESS EVOLUTION, PLASTIC STRAIN, AND STRESS STATE

6.1. Introduction

Single Point Incremental Sheet Forming (SPIF) processes continue to garner widespread attention, particularly as efforts focus on scaling them for high-volume production. The unique characteristic of SPIF lies in forming parts through the accumulation of localized, incremental deformation, resulting from the intermittent contact between the tool and the sheet material. This stretching-based forming mechanism, however, inherently leads to significant thinning and can result in suboptimal mechanical properties. The severity of these issues can be amplified depending on the chosen geometrical and/or technological process parameters. Consequently, this chapter is dedicated to an in-depth study of the influence of critical process variables that contribute to severe thickness reduction. A central focus will be given to the comprehensive discussion of the triple role impact of the drawing angle on thinning, plastic strain distribution, and the evolving stress state, all of which are fundamental factors responsible for material fracture in SPIF.

6.2. Mesh independency study

To ensure both the accuracy and computational efficiency of the numerical simulation, a comprehensive mesh independence study was performed. This study focused on evaluating the convergence of the von Mises stress distribution, as illustrated in Fig. 6.1. Three distinct mesh configurations were investigated, comprising 26,807, 35,527, and 45,120 elements, referred to as Mesh 1, Mesh 2, and Mesh 3, respectively. The optimal mesh configuration was ultimately selected by balancing the requirements for computational cost and the convergence of the simulation results.

The final finite element model incorporates a total of 35,527 elements for the entire part. To accurately capture the high strain gradients and intricate contact mechanics characteristic of the process, a refined mesh with an average element size of 0.5 mm was strategically applied within the critical contact zone. Conversely, a coarser mesh was utilized in less critical regions of the model. This adaptive meshing strategy effectively ensures that the simulation results are independent of the mesh size, thereby enhancing their reliability, while simultaneously maintaining a manageable computational time.

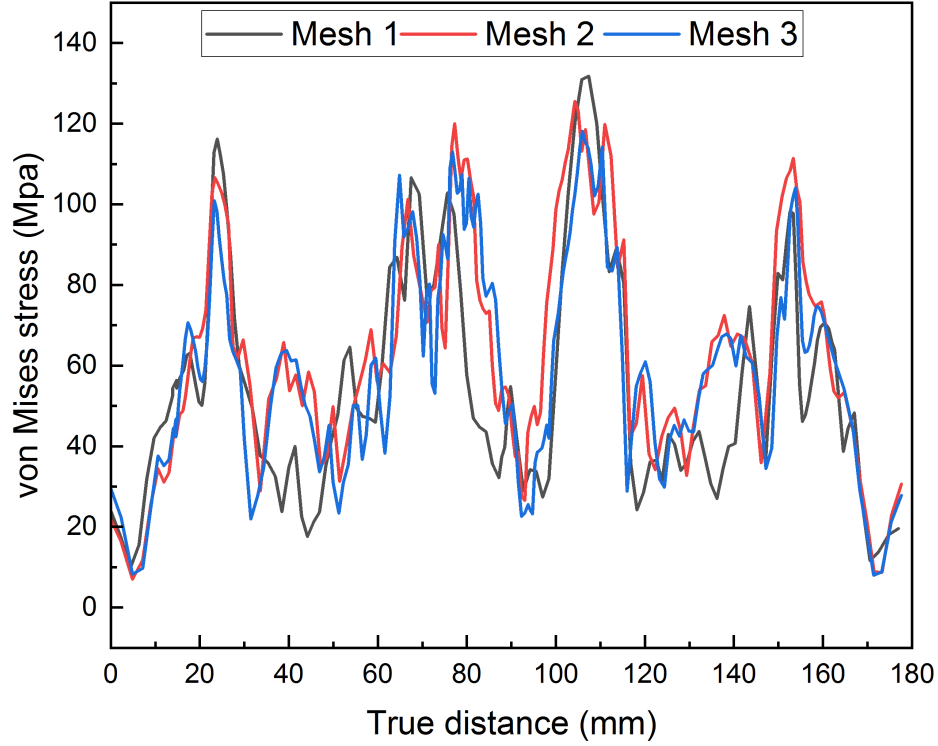


FIGURE 6.1. Grid mesh independency study

6.3. Validation of the FE model

A comparison between the experimental and numerical wall thickness distributions identified three distinct regions: steady-state, bending, and thinning as shown in Fig. 6.2a. For a more detailed quantitative assessment, a radial path was defined along the formed surface to include all relevant nodes from the pole to the flange (see Fig. 6.2b). In the clamping region, both experimental and numerical results exhibited close agreement, as this area largely retains the initial sheet thickness. Moving to the bending region, a slight reduction in thickness from the original value was observed. However, in the transition region, the numerical model predicted a more pronounced thickness reduction compared to the experimental measurements. Specifically, the numerical results indicated a minimum thickness value of approximately 0.38 mm, whereas experimental data yielded a value of 0.4 mm. To further assess these findings, the results were compared with an analytical rule, the so-called sine law, originally applied to the shear spinning process by Avitzur et al. [161] by assuming that (a) the deformation in shear spinning is a straight projection of the initial blank onto the mandrel, (b) the deformation of the sheet is pure shear deformation, and (c) the volume constancy holds. Therefore, Eq. (6.3.1) predicts the final thickness by presuming volume constancy and plane strain deformation mode.

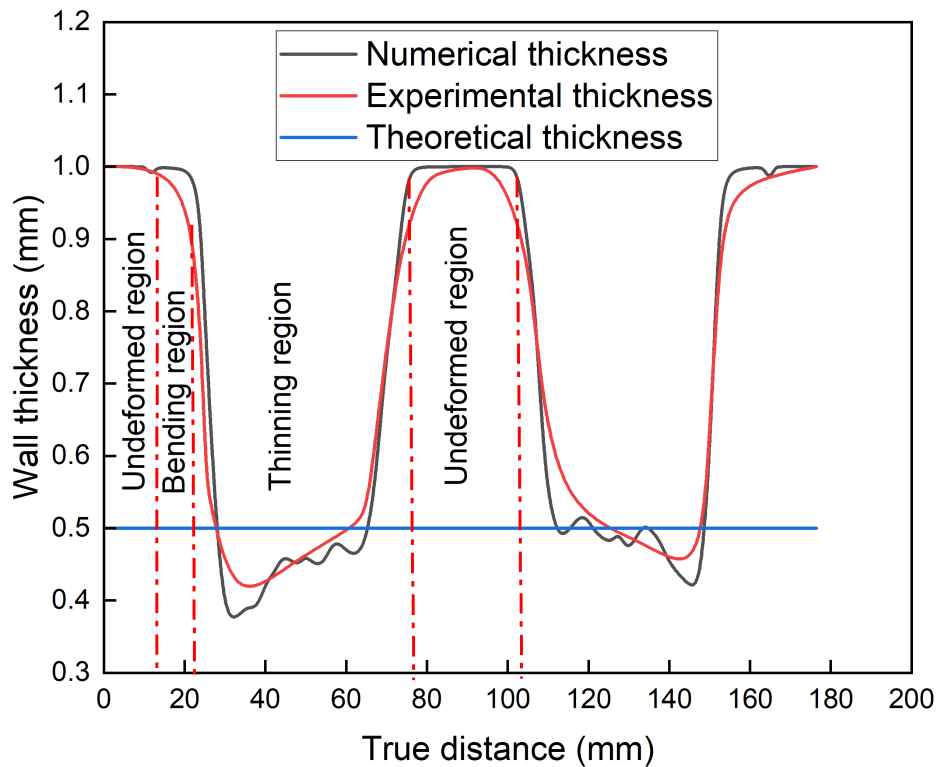
$$t_f = t_0 \sin\left(\frac{\pi}{2} - \psi\right) \quad (6.3.1)$$

where t_0 and t_f denote the initial and the final sheet thicknesses, respectively.

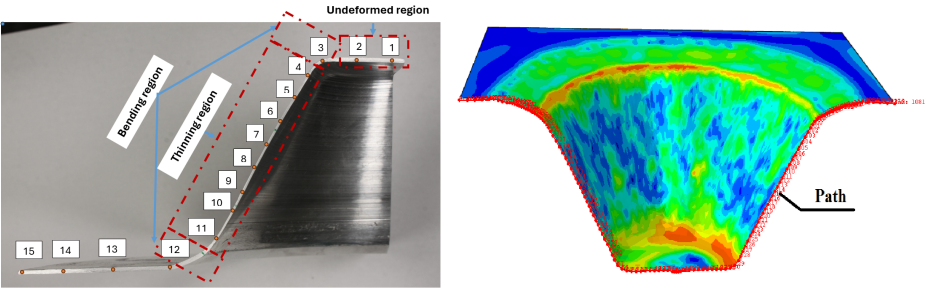
The comparison demonstrated experimental and numerical simulation results slightly overestimated the thickness reduction, particularly in regions dominated by stretching. Consequently both approaches presents minor deviations in high-strain areas.

To verify the quasi-static behavior of the model, the kinetic energy (ALLKE) is compared to the internal energy (ALLIE), as shown in Fig. 6.3. For quasi-static conditions, the contribution of the ALLKE should not exceed 5% of ALLIE. This demonstrates the negligible influence of the dynamic and inertia effects on the results, ensuring that the work applied by the external forces closely approximates the internal energy, thereby fulfilling the quasi-static condition.

A critical aspect for the industrial adoption and practical feasibility of ISF is their computational efficiency. While ISF is inherently a low-production-speed process, the efficiency of its numerical simulation remains paramount for demonstrating scalability and applicability. To ensure the reproducibility and transparency of our simulation results, the finite element simulations were performed on a high-performance workstation. The hardware configuration consisted of Dual Intel Xeon E5-2687W processors operating at 3.1 GHz, with 128 of RAM. The typical CPU time of 85 hours, 39 minutes, and 29 seconds was achieved by running the simulation in parallel using 32 threads. This computation time is considered reasonable and expected for an FEA simulation at this level of process complexity, localized deformation, cyclic loading, besides with the tool long travel inherent in the SPIF process, which require computationally and intensive model to accurately provide a detailed analysis of failure mechanisms based on stress triaxiality and Lode angle parameter.



(a)



(b)

FIGURE 6.2. Comparison of thickness distribution for truncated cone benchmark

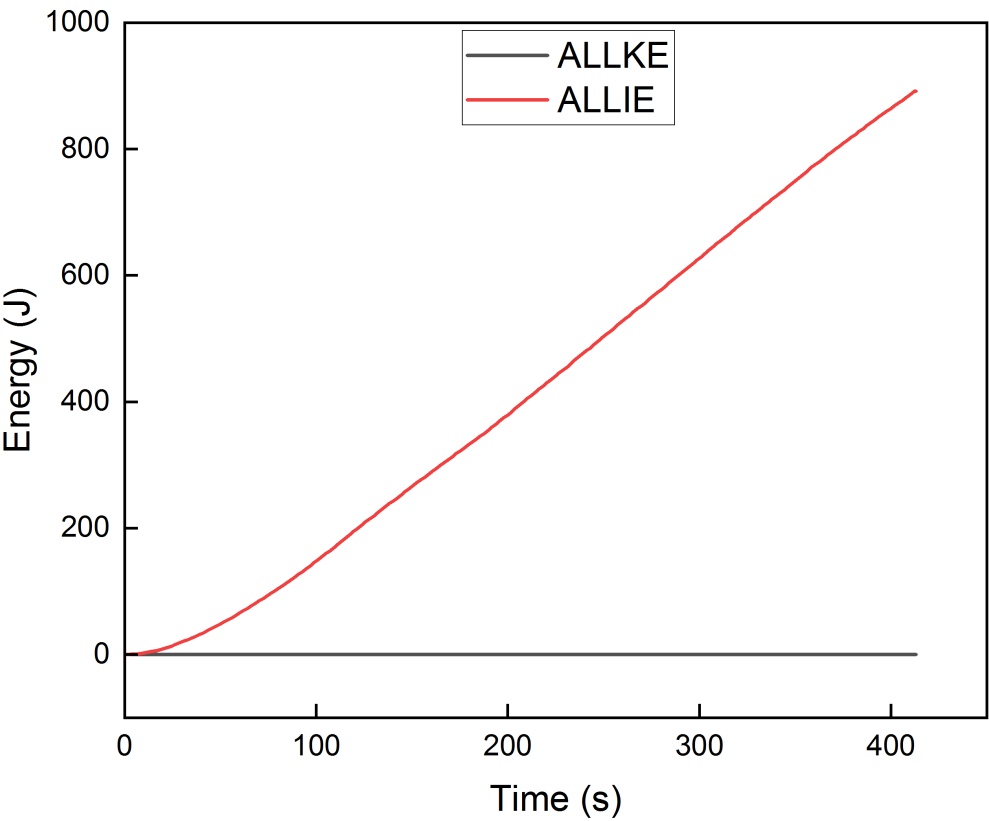


FIGURE 6.3. Comparison between internal and kinetic energy

6.4. Effects of process variables

To comprehensively evaluate qualitatively and quantitatively the impact of the working parameters on thickness distribution, as well equivalent plastic deformation, a preliminary set of six key factors were selected, each with specified levels, as outlined in Tab. 6.2. A total of 19 trials were then designed based on the number of factors and their respective levels as shown in Tab. 6.1, ensuring a systematic investigation of their effects.

TABLE 6.1. The designed parameters conditions of the SPIF .

Level	Step size (mm)	Wall angle (°)	Tool diameter (mm)	Feed rate (mm/min)	Sheet thickness (mm)	Friction coefficient
1	0.6	50	5	360	0.5	0.05
2	1.0	60	10	1000	1.0	0.10
3	1.5	70	15	1500	1.5	0.15
4	2.0	–	20	2000	2.0	0.20

6.4.1. Thinning. Numerical investigations into SPIF reveal significant influences of process input variables on thickness variation and material formability, particularly in truncated conical geometries Figs. (6.4a, 6.4b, 6.4c, 6.4d, 6.4e, 6.4f, 6.4g). A critical finding is that increasing the wall angle, while enhancing overall formability by promoting greater material stretchability, concurrently elevates the risk of necking and crack initiation. For instance, a wall angle increase from 50° to 70° resulted in a substantial escalation of the thinning quantified in Eq. 6.4.1 from (50.98% to 87.25%). This non-uniform deformation is characterized by minimal thickness change at the cone’s base and pronounced thinning in the sidewall regions, where material stretching predominates.

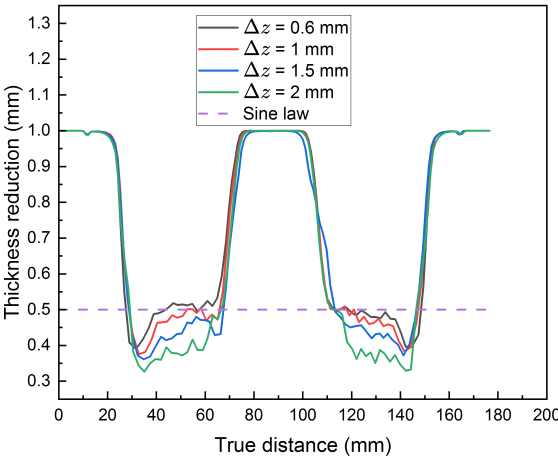
Further analysis demonstrates that step depth, tool diameter, and initial sheet thickness are crucial determinants of thickness distribution. A larger step depth intensifies thickness reduction, whereas a smaller step depth fosters a more homogeneous wall thickness. Similarly, smaller tool diameters lead to more uniform deformation, aligning closely with the sine law, while larger diameters induce significant localized thinning due to increased stresses. Thicker initial sheets exhibit greater resistance to thinning and less deviation from theoretical predictions, contrasting with thinner sheets that show higher susceptibility to elongation and substantial thinning. Conversely, the feed rate and friction exhibit only minimal to negligible influence on thickness variation. Regarding toolpaths, the spiral trajectory is shown to produce a smoother, more gradual thinning trend with less overall thinning compared to the profile trajectory, thereby mitigating the risk of material failure.

To further assess the reliability and general applicability of the developed model, a more complex geometry specifically, a truncated pyramid frustum with varying drawing angles investigated under identical process conditions. Consistent trends were observed: as the sidewall angle increased, the thinning ratio also increased drastically, reaching 39%, 57.12%, and 80.19% as depicted in Fig. 6.4h. Notably, despite the inherent lower thickness reduction exhibited by the pyramidal shape due to its distinct deformation state and strain paths, the overall thickness distribution trend remained consistent with that observed for the conical geometry. This comparative analysis demonstrates the model’s robustness and predictive capability across different complex and non-axisymmetric geometries, thereby confirming its broader applicability beyond simple conical forms.

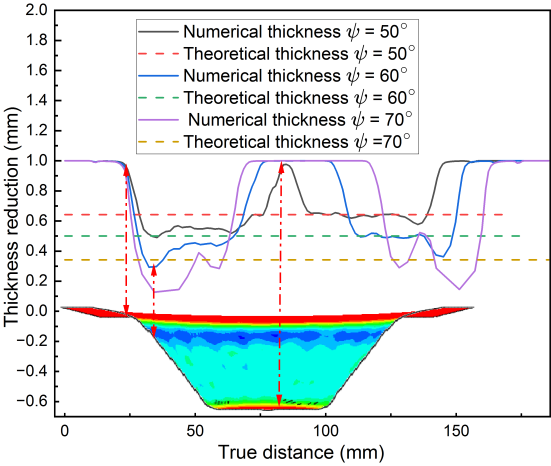
$$\beta = \frac{t_0 - t}{t_0} \times 100\% \quad (6.4.1)$$

TABLE 6.2. Designed process parameters and their level values.

Process Variables							Response Variables	
Δz	ψ	F	d	f	t_0	Toolpath	β (%)	max PEEQ
0.6	60	2000	10	0.05	1	P	61.40	3.242
1.0	60	2000	10	0.05	1	P	62.50	3.141
1.5	60	2000	10	0.05	1	P	63.90	2.861
2.0	60	2000	10	0.05	1	P	67.40	2.701
0.6	50	2000	10	0.05	1	P	50.98	2.377
0.6	70	2000	10	0.05	1	P	87.25	6.159
0.6	60	360	10	0.05	1	P	62.79	3.585
0.6	60	1000	10	0.05	1	P	65.82	3.428
0.6	60	1500	10	0.05	1	P	65.74	3.394
0.6	60	2000	5	0.05	1	P	56.32	3.836
0.6	60	2000	15	0.05	1	P	68.67	3.384
0.6	60	2000	20	0.05	1	P	75.16	3.179
0.6	60	2000	10	0.10	1	P	65.33	3.399
0.6	60	2000	10	0.15	1	P	64.65	3.382
0.6	60	2000	10	0.20	1	P	65.66	3.416
0.6	60	2000	10	0.05	0.5	P	65.84	3.693
0.6	60	2000	10	0.05	1.5	P	66.39	3.565
0.6	60	2000	10	0.05	2.0	P	60.49	2.150
0.6	60	2000	10	0.05	1	S	60.42	3.073



(a)



(b)

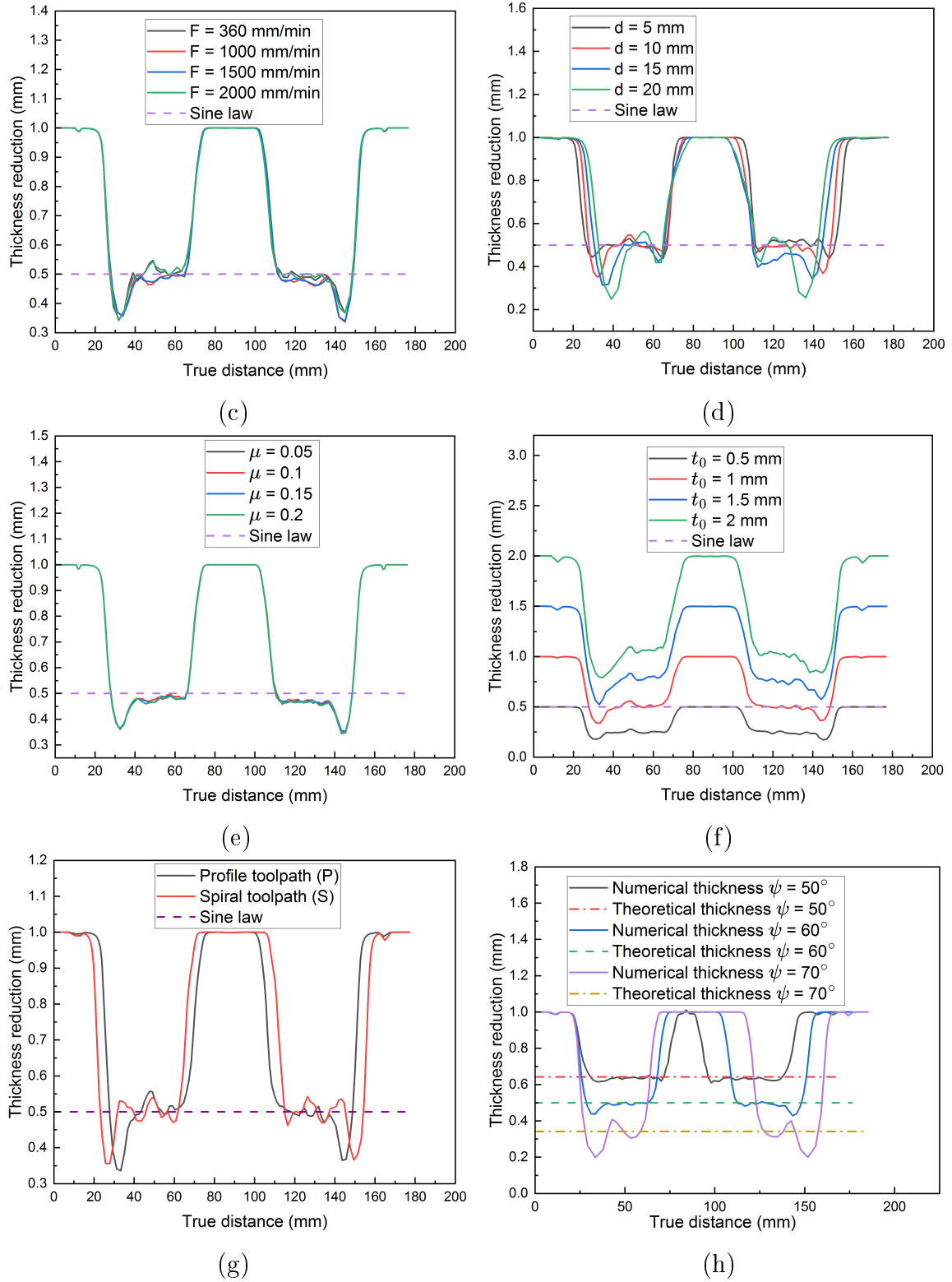


FIGURE 6.4. The impact of the process parameters on the thickness distribution: (a) the influence of the step size, (b) the influence of the wall angle, (c) the influence of the feed rate, (d) the influence of the tool diameter, (e) the influence of the friction coefficient, (f) the influence of the sheet's initial thickness, (g) the influence of toolpath strategy, (h) truncated pyramid shape

6.4.2. Stress state characterization. The stress state at any material point is described by a stress tensor σ , which depends on the chosen coordinate system. For a direction-independent representation, this state can be expressed either through the principal stress components $(\sigma_{11}, \sigma_{22}, \sigma_{33})$ or via stress invariants: the first stress invariant (I_1), and the second and third deviatoric stress invariants (J_2, J_3). The mean stress (σ_m) is calculated as follows:

$$\sigma_m = \frac{\sigma_{11} + \sigma_{22} + \sigma_{33}}{3} \quad (6.4.2)$$

The stress triaxiality (η) is a dimensionless parameter, which is the hydrostatic stress normalized by the von Mises equivalent stress ($\bar{\sigma}$), used to characterize ductile fracture:

$$\eta = \frac{\sigma_m}{\bar{\sigma}} = \frac{I_1}{3\sqrt{3}J_2} \quad (6.4.3)$$

Since the triaxial state alone is insufficient to uniquely define the stress state, the normalized Lode angle parameter ($\bar{\theta}$) is introduced. It is a function of the normalized third deviatoric invariant (ξ) and is defined as:

$$\begin{cases} \bar{\theta} = 1 - \frac{2}{\pi} \arccos(\xi) \\ \xi = \frac{27}{2} \frac{J_3}{(\sqrt{3}J_2)^3} \end{cases} \quad (6.4.4)$$

This parameter value lies between $-1 \leq \bar{\theta} \leq 1$. For an arbitrary loading direction, the stress vector can be fully described by the pair $(\eta, \bar{\theta})$. Under plane stress conditions, a correlation exists between these parameters [162], as expressed in Eq. 6.4.5:

$$\bar{\theta} = 1 - \frac{2}{\pi} \arccos \left[-\frac{27}{2} \eta \left(\eta^2 - \frac{1}{3} \right) \right] \quad (6.4.5)$$

Consequently, the stress state at a material point is fully defined in a spherical coordinate system by the triple $(\bar{\sigma}, \eta, \bar{\theta})$, providing a more suitable framework for characterizing ductile material behavior than the Cartesian triple $(\sigma_{11}, \sigma_{22}, \sigma_{33})$.

6.4.3. Accumulated plastic strain. As the wall angle is a critical factor governing wall thickness thinning during SPIF for a wide range of geometrical shapes as presented in the previous Section. 6.4.1. To comprehensively understand this influence, the evolution of the equivalent plastic strain (PEEQ) was numerically investigated across various drawing angles, as illustrated in Fig. 6.5. PEEQ accumulates incrementally with each successive tool pass, reflecting the discrete nature of the tool's interaction with the material. Numerical simulations consistently demonstrated a direct correlation between the maximum PEEQ values and the steepness of the drawing slope angle. Specifically, the maximum PEEQ values increased from 2.377 at 50° to 3.564 at 60°, and further to 6.159 at 70°. These peak strain concentrations are predominantly located within the transition region of the formed part, a localization attributed to the synergistic effects of stretching and in-plane shear deformation occurring parallel to the tool's trajectory.

Further analysis of PEEQ in the critical element, situated on the outer surface of the cone, was conducted as a function of stress triaxiality (η) and the normalized Lode angle parameter ($\bar{\theta}$). These parameters are fundamental in modeling damage and fracture in SPIF, as they comprehensively define the local stress state. As depicted in Fig. 6.6a, stress triaxiality, which quantifies the hydrostatic component of the stress state, plays a predominant role in the deformation behavior. For drawing angles of 50° and 60° , PEEQ values remained relatively low (PEEQ ≤ 3.5) and were primarily associated with negative triaxiality ($(\eta \leq 0)$). These compressive stress states are known to inhibit micro-void growth, thereby limiting their contribution to material damage. However, for a wall angle of 70° , the critical element experienced significantly more severe loading, with PEEQ reaching substantially higher maximum values ≈ 6.2 . The data plot for 70° exhibited the widest triaxiality range, spanning from high compression ($\eta \leq -0.66$) to moderate tension ($(\eta \leq) 0.66$). This broad dispersion and the characteristic shape of the data cloud indicate a complex cyclic loading path, alternating between states of high compression and tension. Under such conditions of cyclic loading and elevated triaxiality, void growth is accelerated, leading to a faster attainment of the material's formability limit and a reduction in deformation prior to failure [106].

Fig. 6.6b illustrates the evolution of PEEQ as a function of the normalized Lode angle parameter ($\bar{\theta}$). Contrary to initial assumptions, the critical element explores the entire spectrum of Lode stress states, bounded between $-1 \leq \bar{\theta} \leq 1$, encompassing triaxial compression, tension, and pure shear ($\bar{\theta} = 0$). This observation underscores the inherent complexity of the stress path within SPIF. Although the distribution of $\bar{\theta}$ values is qualitatively similar across all three wall angles, the profound impact of the wall angle (ψ) on the maximum PEEQ is evident. For $\psi = 70^\circ$, the PEEQ is markedly higher across the entire range of $\bar{\theta}$ compared to lower wall angles. Consequently, the wall angle strongly dictates the magnitude of accumulated plastic strain for a given Lode stress state. The most critical stress state, characterized by the maximum PEEQ, is achieved at $\psi = 70^\circ$ under complex cyclic loading involving a wide range of triaxialities and Lode angles.

Beyond wall angle, other process parameters also significantly influence strain accumulation. The observed strain accumulation can be attributed to the tool's abrupt displacement during transitions to new contour paths, which induces repeated material stretching and results in high localized strain. However, this effect is mitigated in the spiral toolpath configuration, where continuous tool motion prevents sudden transitions. The spiral trajectory promotes a more uniform distribution of accumulated equivalent plastic strain across the formed part by reducing strain concentration. In this case, the maximum strain limit is 3.073, which is significantly lower than that produced by a discontinuous profile toolpath [163].

Additionally, the tool diameter (d) plays a critical role in governing PEEQ. As the tool diameter increases from 5, mm, 10, mm, 15, mm, and 20, mm, the maximum PEEQ values attain 3.836, 3.564, 3.384, and 3.179, respectively. This trend suggests that smaller tools induce

more localized deformation due to their concentrated contact, leading to higher strain gradients. Conversely, larger tools distribute strain over a broader region, reducing peak strain concentrations by enhancing the contact area and promoting more uniform deformation.

Subsequently, the initial sheet thickness (t_0) also influences strain accumulation. Thicker sheets exhibit greater resistance to deformation, resulting in lower peak PEEQ values. Quantitatively, the maximum PEEQ decreases from 3.693 (for $t_0 = 0.5$, mm) to 3.242 (for $t_0 = 1$, mm), 2.665 (for $t_0 = 1.5$, mm), and 2.150 (for $t_0 = 2$, mm). This trend underscores the material's enhanced ability to withstand deformation without failure at higher thicknesses.

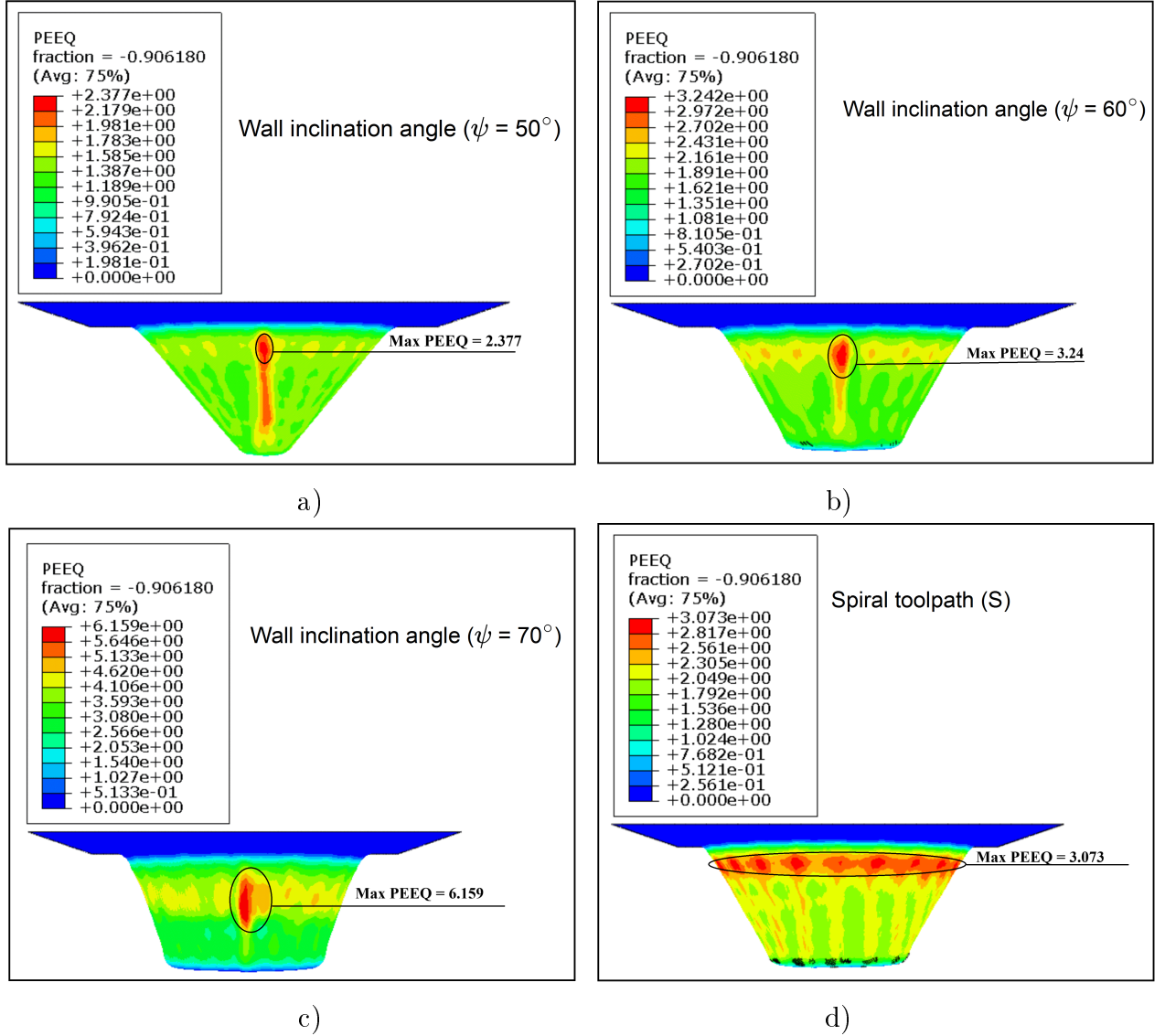


FIGURE 6.5. The impact of the process parameters on the maximum equivalent plastic strain, (a) wall angle $\psi = 50^\circ$, (b) wall angle $\psi = 60^\circ$, (c) wall angle $\psi = 70^\circ$, (d) wall angle $\psi = 60^\circ$ with spiral toolpath (S)

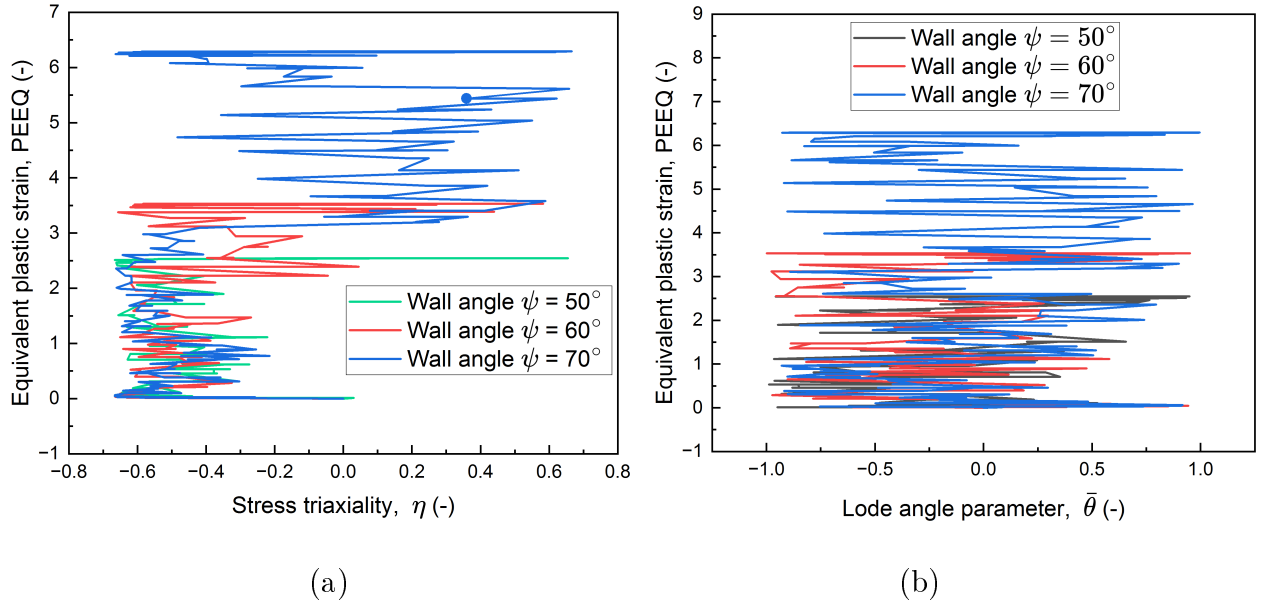


FIGURE 6.6. The evolution of equivalent plastic strain as a function of the (a) triaxiality (b) Lode angle parameter

6.4.4. Strain-gradient. Within the scope of strain distribution, three regions denoted as, A, B, and C labelled in the shell FE model was presented aiming to present the strain gradient in different areas of stretching zone as presented in Fig. 6.7. The element located in the regions labelled by "A" exhibited the highest deformation strain reaching 2.82, compared to the ones labelled as "B" and "C", respectively.

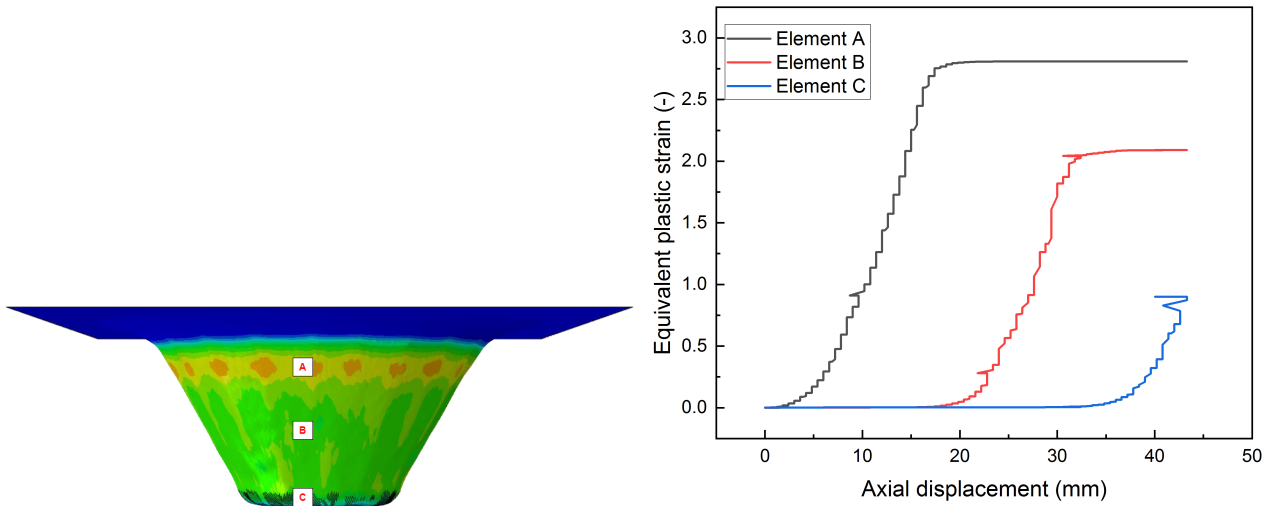


FIGURE 6.7. Strain gradient disposition in SPIF

6.4.5. Stress analysis. The stress analysis in SPIF is crucial for understanding the material behavior during the forming stage. Indeed, the localized and progressive nature of deformation in SPIF leads to a complex stress and strain state in the material. While the strain can be experimentally measured [164], the stress state is typically evaluated through of theoretical approaches [165] or finite element analysis. In the contest of sheet metal forming

simulations, shell elements are often employed due to their computational efficiency. However, the highly localized and three-dimensional nature of deformation in SPIF necessitates the use of solid elements to accurately capture the stress and strain state, particularly in the thickness direction. Although this approach increases the computational time, a trade-off between the accuracy and computational efficiency must be carefully considered when establishing the FE model.

Fig. 6.8 illustrates the evolution of the through-thickness normal stress component σ_{33} along the z -axis of the coordinate system, which corresponds to the thickness direction, recorded in three distinct element for both element types. Unlike shell elements, which commonly assume a plane stress or plane strain condition and provide an averaged through-thickness result, solid elements enable the direct calculation and visualization of stress variations across the sheet's thickness. Consequently, the stress state in the thickness direction is well-captured when utilizing solid elements such as C3D8R. The analysis reveals a significant variation in stress across the thickness of the sheet, indicating an oscillating behavior of the stress component. To analyse the stress distribution through the thickness in more details, σ_{33} was recorded for distinct elements at different thickness positions A and B (Fig. 6.8a): an inner surface element (EI), a middle element (EM), and an outer surface element (EO)(Fig. 6.8b). As depicted in Fig. 6.8c, the EI experiences cyclic loading between tensile and compressive stress with high fluctuations. This behavior continues until a certain forming time, after which the stress becomes approximately constant with minor oscillations. This stabilized stress primarily represents the remaining stress within the material, resulting from plastic deformation and uneven recovery. In contrast, the EM and the EO exhibit an oscillating pattern where the stress remains predominantly tensile.

Crucially, the compressive stress induced in the sheet thickness direction beneath the forming tool plays a vital role in enhancing formability [22]. These compressive stresses effectively suppress tensile instability and postpone the onset of localized necking, a common failure mode in sheet metal forming. By maintaining material integrity within the thickness direction, the material's ability to deform plastically without fracturing is significantly increased. As the forming tool indents the sheet, the material undergoes complex deformation, involving stretching, compression, and pushing, which forces it to flow along the toolpath to achieve the desired shape. This controlled material flow is crucial for attaining a more uniform thickness distribution and consequently, reducing excessive thinning. Quantitatively, the average remaining stresses are approximately -29.5 MPa for EI, 60.8 MPa for EM, and 96.6 MPa for EO. Comparing the two subplots (Fig. 6.8c and 6.8d), the right subplot demonstrates a completely inverse evolution of stress compared to the left subplot. Specifically, the stress components in right subplot starts from zero and only begin to rise once the forming tool reaches the concerned region, initiating the deformation process. This implies a significant increase in stress components in all three directions upon tool engagements.

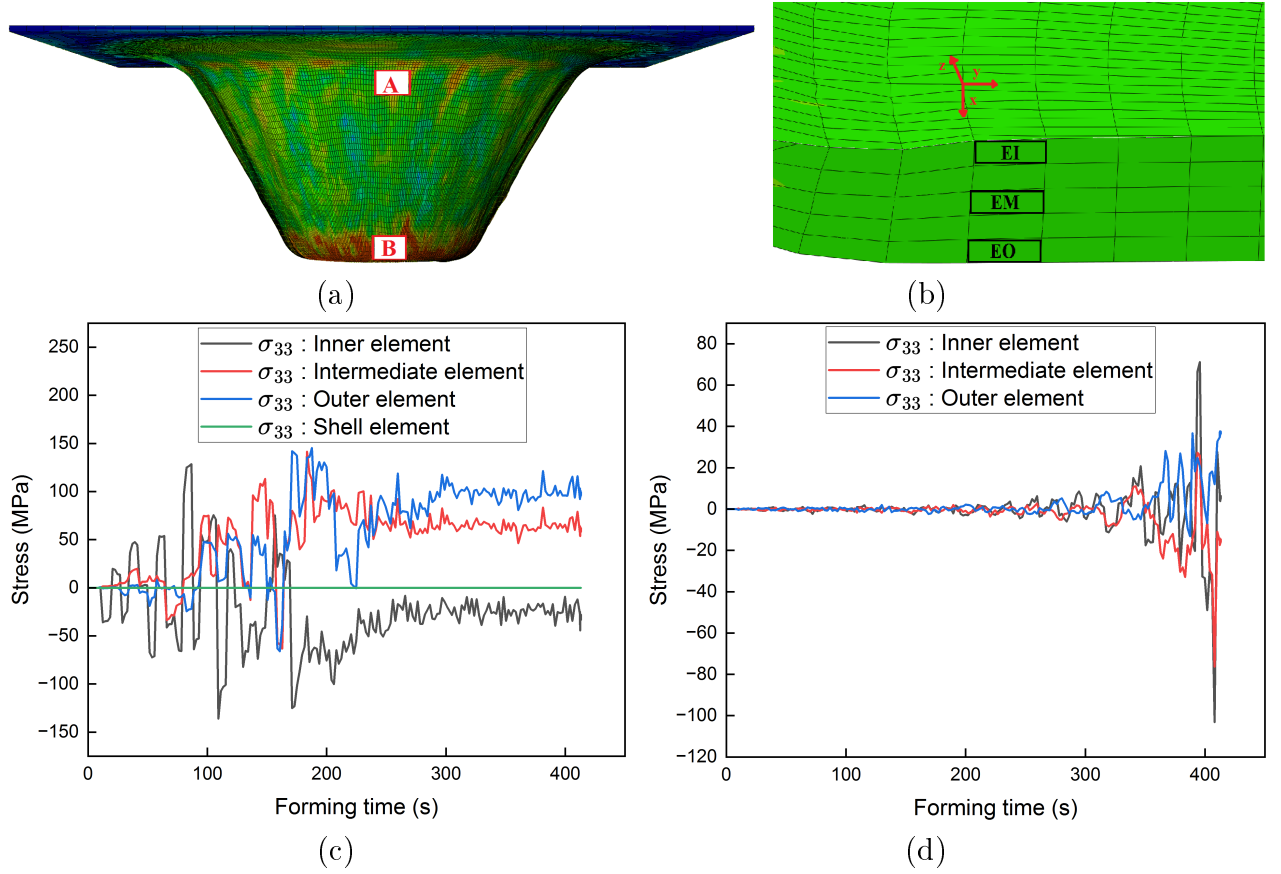


FIGURE 6.8. The stress distribution in thickness direction (c) region (A), (d) region (B)

To quantitatively justify the necessity of employing solid elements (C3D8R) for the accurate analysis of damage mechanisms, a comparative study was conducted against shell elements (S4R) under identical forming conditions. The comparison focuses on the evolution of the two critical stress state: η and $\bar{\theta}$ as a function of PEEQ at the critical element discussed in Section 6.4.3. As illustrated in Fig. 6.9a, the results of stress triaxiality show a dramatic difference. The solid element model (C3D8R) captures a significantly wider and more realistic range of η values, spanning from highly negative (compression, $\eta \approx -3$) to highly positive (tension, $\eta \approx 1$). This wide distribution is characteristic of the complex, non-linear stress path in SPIF, which includes significant through-thickness bending and stretching. In stark contrast, the shell element model restricts the η values to much narrower range (mostly between $-1 \leq \eta \leq 0.5$). This limitation arises because shell elements, by assuming a plane stress state, fail to fully capture the through-thickness stress component σ_{33} , which is essential for accurate calculation of the hydrostatic stress and consequently, η .

Similarly, the comparison of the normalized Lode angle parameter $\bar{\theta}$ was presented in Fig. 6.9b reveals that the shell element model fails to capture the full complexity of strain path. The solid element results show a broad distribution of $\bar{\theta}$ values, indicating a variety of stress states (from plane strain to shear) that influences the damage evolution as in addition to σ_{33} , they capture the shear stresses σ_{23} and σ_{13} , which are unavailable within shell element.

The restricted range of $\bar{\theta}$ in the shell element model would lead to an erroneous judgement of the local stress state and the subsequent damage initiation criterion. Since the core of this study relies on the accurate prediction of ductile damage based on the full stress state, this quantitative evidence confirms that the use of solid elements is a methodological imperative to ensure the physical validity of the analysis despite the high computational cost.

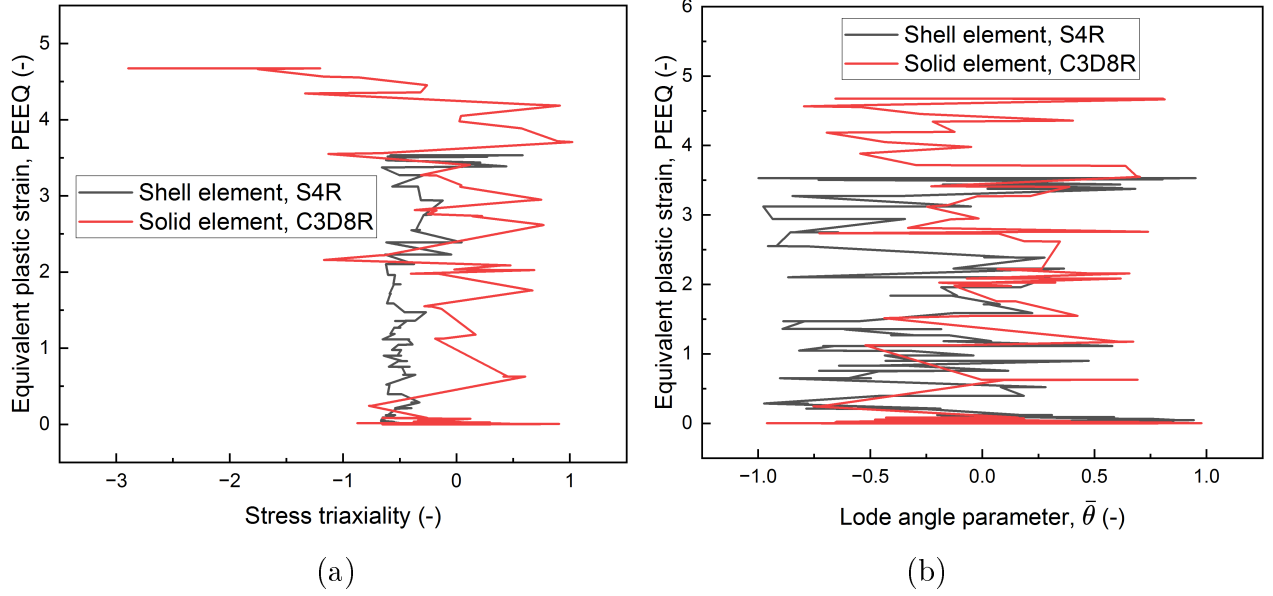


FIGURE 6.9. Comparison of the evolution of the stress state in both shell and solid element (a) the stress triaxiality, (b) the lode angle parameter

6.4.5.1. *Asymmetry of stress distribution.* The analysis of von Mises stress distribution is crucial for understanding deformation mechanisms and predicting the stress concentration level. It is evident from Fig. 6.10 that the von Mises stress distribution along the contour path exhibits an inherently non-symmetric behavior, characterized by significant fluctuations and localized peaks. This non-uniformity is a fundamental characteristic of SPIF, resulting from the localized and progressive interaction of the tool with the sheet metal.

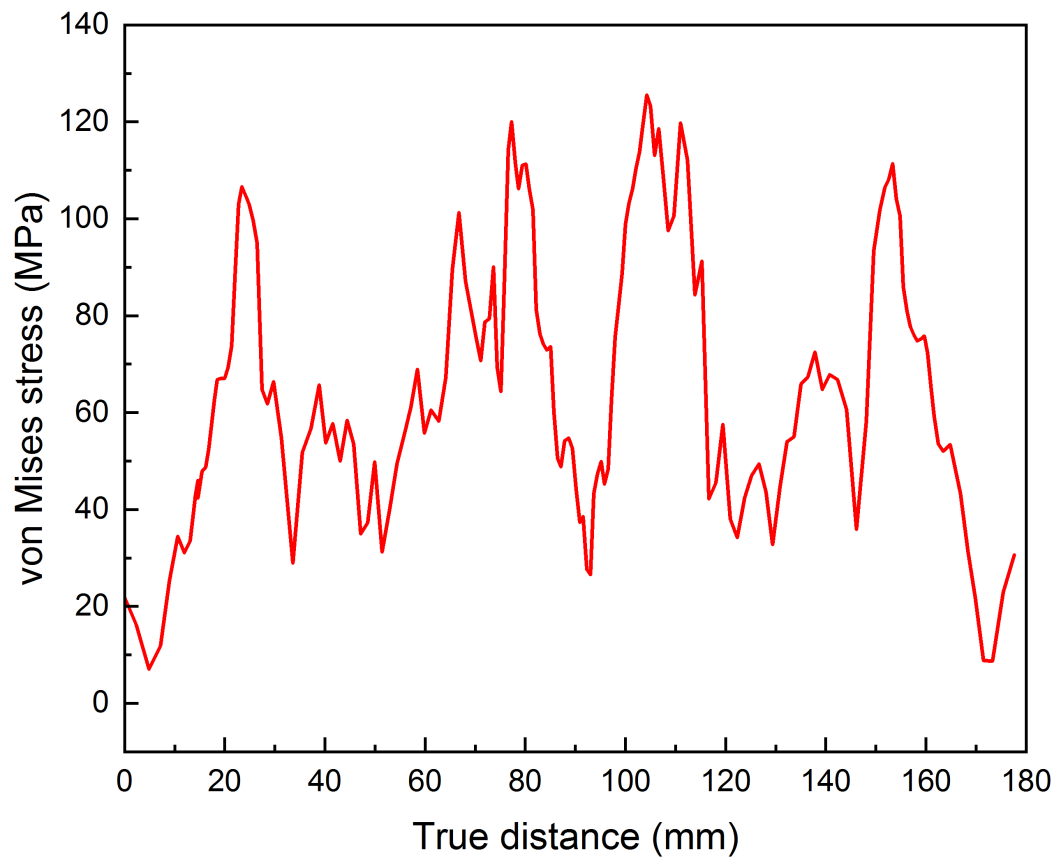


FIGURE 6.10. von Mises stress distribution

CHAPTER 7

A FRACTURE-BASED APPROACH TO FORMABILITY ASSESSMENT

7.1. Introduction

The thickness variation in SPIF has demonstrated a direct impact on the structural integrity of the formed part and therefore the material formability as presented in Chapter. 6. This concept further supported by [166]. Material formability and fracture mechanism can be characterized using damage models. Many research used both coupled and uncoupled damage models for fracture prediction in SPIF as they serve as a critical tools for predicting and postponing the failure occurrence. In addition, the prediction of the fracture FFLD method, whether the coupled or uncoupled damage frameworks are extensively used in the field of metal forming and ISF particularly. In the current chapter Both coupled and uncoupled damage models presented through the Gurson-Taveraard-Needleman (GTN) and Johnson-cook (JC) models were undertaken. Both Aluminum sheets Al 1050 and Al 5754 were adopted.

7.2. Theoretical background and framework

7.2.1. GTN damage model. Several failure models have been developed to predict material formability and the fracture initiation in ISF [131, 167, 168]. Among these, the GTN damage model has consistently demonstrated superior accuracy in capturing the transition from stable plastic deformation to ductile failure. The model accounts for the significant effect of hydrostatic stress on the yield locus, treating the material as a porous medium where damage originates from micro-cracks or inclusions [125].

The yield function, as extended by Tvergaard and Needleman [112] to account for void interaction and coalescence, is defined as follows: Eq. 7.2.1

$$\phi(\bar{\sigma}_m, \sigma_{eq}, f^*) = \left(\frac{\sigma_{eq}}{\bar{\sigma}_m}\right)^2 + 2q_1 f^* \cosh\left(-\frac{3q_2 \sigma_h}{2\bar{\sigma}_m}\right) - (1 + q_3 f^{*2}) = 0 \quad (7.2.1)$$

where σ_{eq} is the macroscopic von Mises equivalent stress (Eq. 7.2.2), σ_h is the hydrostatic stress (Eq. 7.2.3), and $\bar{\sigma}_m$ is the matrix material flow stress.

$$\bar{\sigma}_{eq} = \sqrt{\frac{3}{2} S_{ij} S_{ij}} = \sqrt{\frac{(\sigma_{11} - \sigma_{22})^2 + (\sigma_{22} - \sigma_{33})^2 + (\sigma_{33} - \sigma_{11})^2}{2}} \quad (7.2.2)$$

$$\sigma_h = \frac{\sigma_{kk}}{3} = \frac{\sigma_{11} + \sigma_{22} + \sigma_{33}}{3} \quad (7.2.3)$$

S_{ij} represent the deviatoric part of the Cauchy stress tensor, described as:

$$S_{ij} = \sigma_{ij} - \frac{1}{3}\sigma_{kk}\delta_{ij} \quad (7.2.4)$$

where δ_{ij} refers to the Kronecker delta (with $\delta_{ij} = 1$, if $i = j$ and $\delta_{ij} = 0$, if $i \neq j$), f^* is the total effective void volume friction, which accounts for the stress-carrying capacity's gradual loss and accelerated damage process after the coalescence phenomenon initiates. f^* can be given by the following piecewise function:

$$f^*(f) = \begin{cases} f & f \leq f_c \\ f_c + \frac{1/q_1 - f_c}{f_f - f_c}(f - f_c) & f_c < f \leq f_f \\ \frac{1}{q_1} & f > f_f \end{cases} \quad (7.2.5)$$

7.2.1.1. *Void evolution and parameter identification.* As the plastic deformation evolves and accumulate, the void volume fraction change is strongly dependent on strain values, the evolution of f is driven by the growth of pre-existent voids and the nucleation of new ones: $\dot{f} = \dot{f}_N + \dot{f}_G$. The void growth rate is considered to be dependent on the macroscopic accumulated plastic strain rate and the identity tensor I as shown in Eq. 7.2.6

$$\dot{f}_G = (1 - f)\dot{\varepsilon}_{kk}^p I \quad (7.2.6)$$

The porosity originated from strain nucleation is proportional to the plastic strain rate $\dot{\varepsilon}^p$:

$$\dot{f}_N = A_N \dot{\varepsilon}^p = \dot{\varepsilon}^p \begin{cases} 0 & \sigma_h < 0 \\ \frac{f_N}{S_N \sqrt{2\pi}} \exp(-\frac{1}{2}(\frac{\varepsilon_p - \varepsilon_N}{S_N})^2) & \sigma_h \geq 0 \end{cases} \quad (7.2.7)$$

where, $A_N = \frac{f_N}{S_N \sqrt{2\pi}} \exp(-\frac{1}{2}(\frac{\varepsilon_p - \varepsilon_N}{S_N})^2)$

To record the status of material losing stress capacity, a dimensionless damage parameter D is presented as follows:

$$D = \frac{f^* - f_0}{f_f - f_0} \quad (7.2.8)$$

The predictive capability of the GTN model relies heavily on the accurate identification of eight material parameters (q_1 , q_2 , f_0 , f_c , f_f , f_n , and ε_n , s_n). For this study, the GTN parameters for commercial-purity aluminum Al 1050 were determined based on the calibrated sets from the literature [169], as summarized in Tab. 7.1.

TABLE 7.1. GTN damage parameters of commercial-purity aluminum alloy

Al 1050								
q_1	q_2	q_3	f_0	ε_N	S_N	f_N	f_c	f_f
1.5	1	2.25	10^{-5}	0.3	0.1	2×10^{-4}	0.04	0.05

7.2.2. Johnson-Cook damage model. The phenomenological Johnson-Cook (JC) damage model, originally proposed by Johnson and Cook [106], accounts for the coupled effects of three independent terms, namely pressure, strain rate, and temperature. This criterion defines the equivalent plastic strain at fracture initiation (ε_f) resulting from the nucleation, growth, and coalescence of micro-voids:

$$\varepsilon_f = [D_1 + D_2 \exp(D_3 \eta)] [1 + D_4 \ln(\dot{\varepsilon}^*)] [1 + D_5 T^*] \quad (7.2.9)$$

where D_1 – D_5 are material-specific damage parameters, the triaxial effect controlled by D_2 and D_3 leading a decrease of the decrease of ε_f as η increase, expecting a positive value of D_3 . $\dot{\varepsilon}^*$ and T^* denote the normalized strain rate and the homologous temperature, expressed as $\dot{\varepsilon}^* = \dot{\varepsilon}/\dot{\varepsilon}_0$ and $T^* = (T - T_r)/(T_m - T_r)$ wherein $\dot{\varepsilon}_0$, T_r , and T_m refer to the reference strain rate, room temperature, and reference temperature, respectively. This phenomenological damage model reflects the effect of the tensile and compressive stresses on the onset of fracture compared with commonly used failure criterion based of plastic strain at failure included in the first term. The second term, reflects the impact of the strain rate. The last terms contribute to the effect of the temperature [170]. Determining these parameters is a critical step for accurate failure prediction.

Various calibration strategies exist in the literature. A purely experimental approach involves a comprehensive series of mechanical tests (tensile, compression, or plane strain) under varying conditions, with parameters extracted through direct fitting of experimental data using optimization algorithms [171]. While straightforward, this method can be time-consuming and requires extensive testing to cover the full range of loading conditions. Alternatively, numerical-only approaches utilize inverse analysis, where finite element (FE) simulations are iteratively adjusted until the predicted response matches experimental observations [172]. This is particularly useful for complex loading scenarios, such as cutting or forming, where direct measurement is difficult.

A more robust strategy is the hybrid experimental-numerical approach [173, 174]. In this methodology, controlled tests are performed on specimens with specifically designed geometries (e.g., smooth and notched specimens) to achieve diverse stress triaxiality states. Simultaneously, FE simulations of these tests are conducted, and parameters are refined through iterative optimization to minimize the discrepancy between experimental measurements (e.g., load-displacement curves or DIC strain fields) and numerical predictions. This hybrid strategy accounts for complex stress states and boundary effects that are difficult to characterize analytically. Recent implementations have demonstrated high accuracy, with prediction errors as low as 3–4% for fracture [173].

Once the damage initiation criterion is met, damage evolution is triggered, leading to material softening and a reduction in load-carrying capacity until final failure. The evolution law can be described based on fracture energy (G_f) or equivalent plastic displacement (u^{pl}). The evolution toward fracture can follow either a linear or an exponential law expressed by Eq. 7.2.10 and Eq. 7.2.11:

$$\dot{d}_L = \frac{L \dot{\varepsilon}^{pl}}{\bar{u}_f^{pl}} = \frac{\dot{u}^{pl}}{\bar{u}_f^{pl}} \quad (7.2.10)$$

$$\dot{d}_E = 1 - \exp\left(-\alpha \frac{\dot{u}^{pl}}{\bar{u}_f^{pl}}\right) \quad (7.2.11)$$

where L is the element characteristic length. The damage variable d ranges from 0 (pristine material) to 1 (fully degraded). In this investigation, the JC damage model is implemented in conjunction with both von Mises and Hill'48 plasticity models.

7.2.2.1. Calibration methodology and FEM of tension tests. Calibration of the fracture loci for the Al 5754-H22 aluminum alloy under various stress states was performed using an inverse method based on the load-displacement history of tensile tests. In addition to the uniaxial tensile tests used for constitutive model calibration, in-plane shear and notched specimens (see Fig. 7.1) were subjected to tensile loading at ambient temperature and strain rate of 0.004 s^{-1} . A grid pattern was etched onto the surface of each geometry to accurately capture the fracture localization zone and measure the fracture strain, as detailed in Fig. 7.2.

In parallel, a finite element (FE) model for each test was implemented in Abaqus/Implicit solver. The specimens were fully modeled without assuming geometric symmetry (Fig. 7.3) and meshed using linear-brick solid elements (C3D8R). A fine, regular mesh of 0.1 mm was employed in regions where necking and fracture were expected, while a coarser mesh was used elsewhere. This mesh size was selected following a convergence study, which showed a minimal relative error between 0.1 mm and 0.05 mm, thus balancing accuracy and computational cost (Tab. 7.2) tested for NUT4 specimen. Eight layers through the thickness were chosen to ensure convergence of the fracture strain. For consistency, a J2-plasticity model with an associated flow rule and non-linear isotropic hardening (Swift-Voce law) was defined. Planar anisotropy was neglected during the damage calibration procedure.

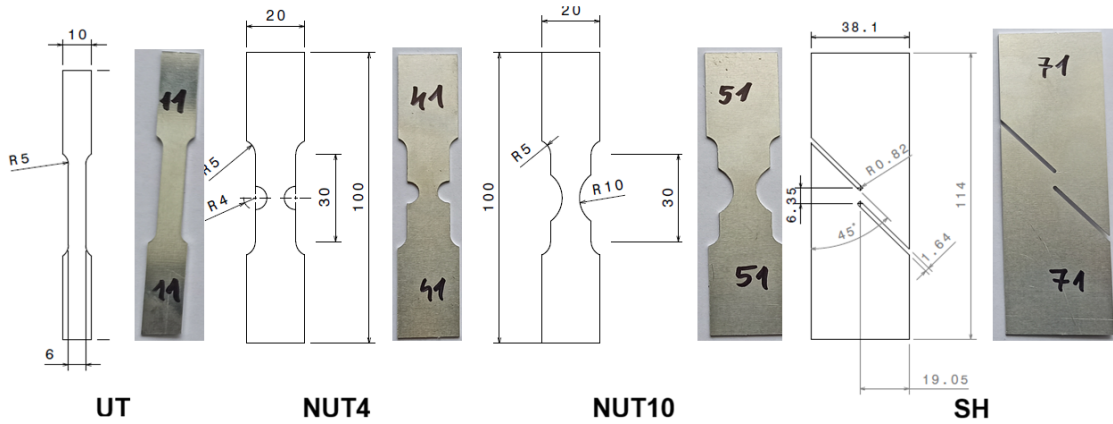


FIGURE 7.1. Schematic and actual images of test specimens: (UT) uniaxial tensile, (NUT4) notched radius 4 mm, (NUT10) notched radius 10 mm, and (SH) in-plane shear

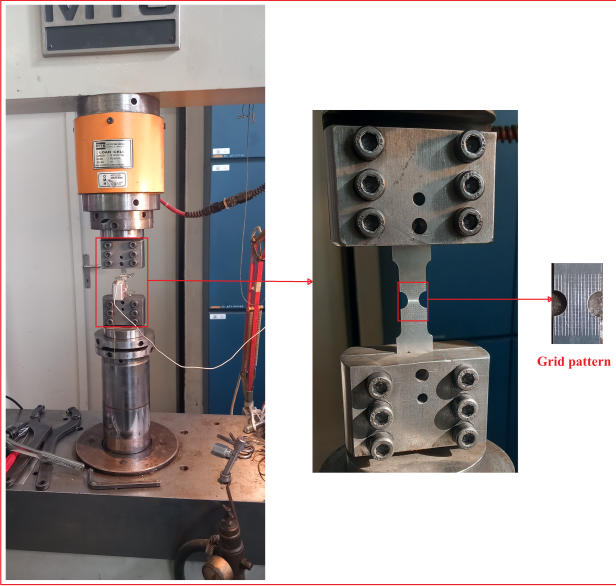


FIGURE 7.2. Experimental setup and grid pattern shown for NUT4

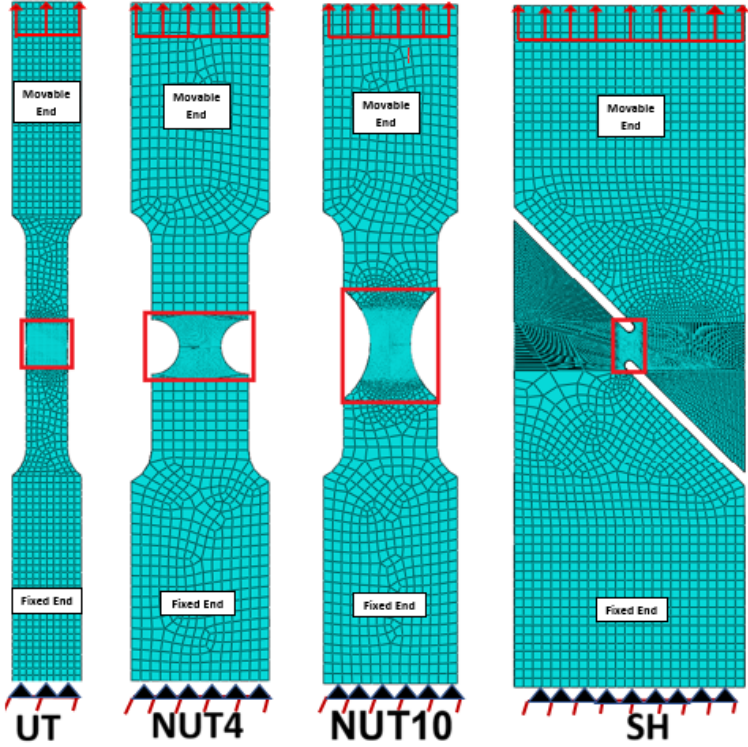


FIGURE 7.3. Schematic of the mesh divisions of various tensile specimens: (UT) uniaxial tensile sample, (NUT4) Notched radius 4 mm flat tensile specimen, (NTR10) Notched radius 10 mm flat tensile specimen, (SH) In-plane shear test sample

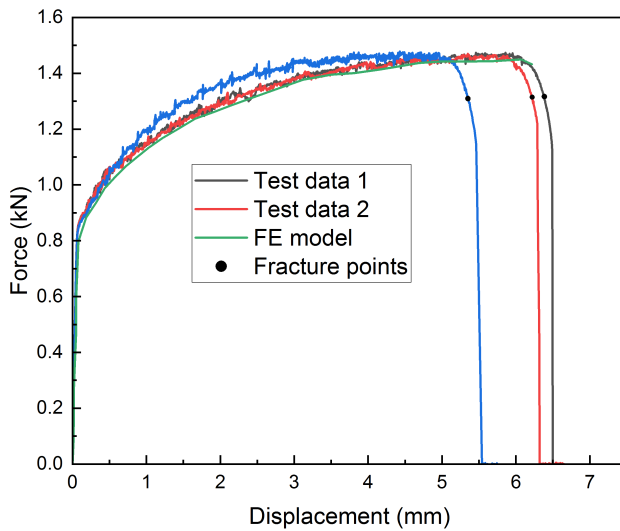
TABLE 7.2. Mesh convergence and computational time analysis for the NUT4 specimen

Element size [mm]	Elements	Triaxiality (η)	Rel. Error [%]	$\bar{\varepsilon}_f^p$	Time [s]
0.25	10,755	0.345	-	0.3110	165
0.10	47,280	0.491	42.32	0.3118	1,246
0.05	271,040	0.498	1.40	0.3213	27,897

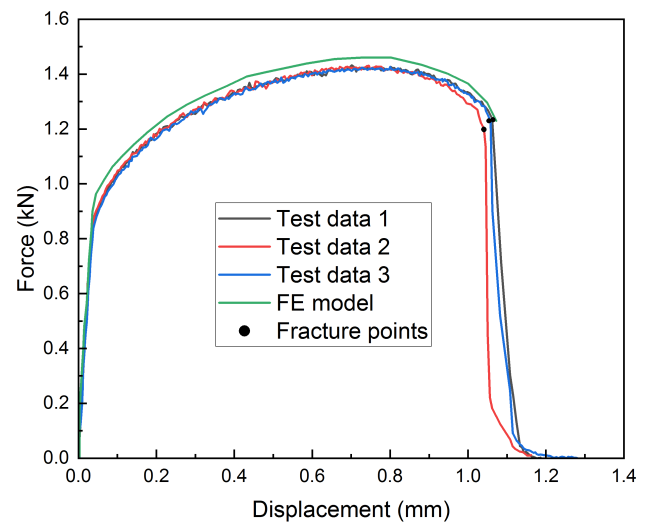
The numerical and experimental load-displacement curves for the UT, NUT4, NUT10, and SH configurations are plotted in Fig. 7.4. The FE results were validated against the average of experimental tests, showing high level of consistency. A hybrid inverse method was adopted to determine the fracture strain ($\bar{\varepsilon}_f$) at each configuration, assuming that the onset of fracture coincides with the point of maximum plastic strain at the experimental fracture displacement [128].

The onset of fracture was identified using two primary indicators:

- **Macroscopic Load Drop:** A sudden drop in the load-displacement curve serves as a clear indicator of fracture initiation. This method is applicable to all loading conditions (shear, plane strain, etc.) and can be supplemented by high-speed imaging or acoustic emission (AE) sensors.
- **Surface Crack Initiation:** In cases where a load drop is less pronounced, direct observation of crack appearance on the specimen surface is used. Techniques such as Digital Image Correlation (DIC) and post-mortem measurements are suggested for this purpose.



(a)



(b)

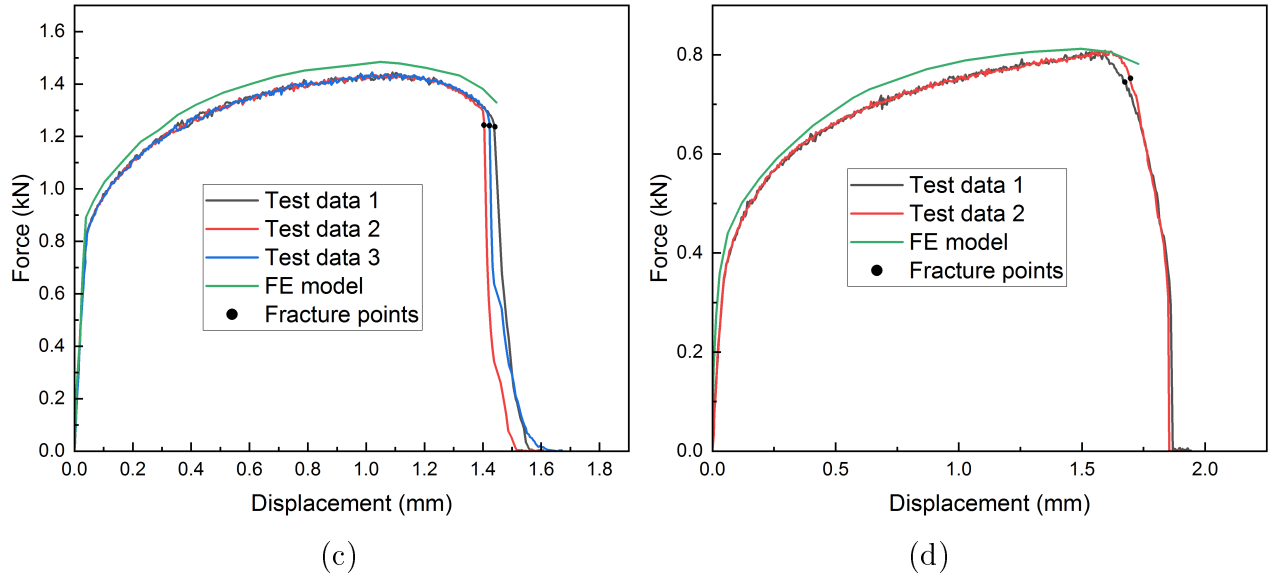


FIGURE 7.4. Experimental-numerical load-displacement curve: (a):UT, (b): NUT04, (c): NUT10, (d): SH

Since the stress state in the critical element varies during the deformation process (non-proportional loading), an average stress state scheme is used to account for the cumulative nature of ductile failure [175]. The average triaxiality (η_{ave}) and average Lode parameter ($\bar{\theta}_{ave}$) are evaluated over the plastic strain history up to the fracture strain ($\bar{\epsilon}_f$):

$$\eta_{ave} = \frac{1}{\bar{\epsilon}_f} \int_0^{\bar{\epsilon}_f} \eta(\bar{\epsilon}^p) d\bar{\epsilon}^p; \quad \bar{\theta}_{ave} = \frac{1}{\bar{\epsilon}_f} \int_0^{\bar{\epsilon}_f} \bar{\theta}(\bar{\epsilon}^p) d\bar{\epsilon}^p \quad (7.2.12)$$

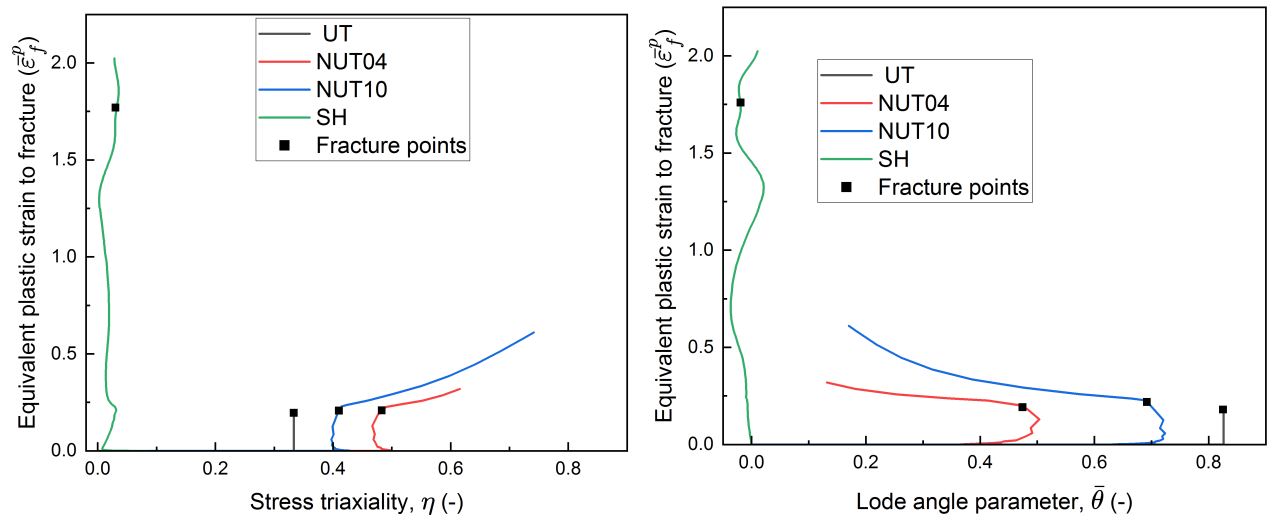


FIGURE 7.5. Evolution of stress triaxiality and Lode angle parameter for different test specimens

7.2.2.2. *Fracture locus.* As discussed in Section 7.2.2.1, the effects of temperature and strain rate were neglected based on the quasi-static nature of the calibration tests. Consequently, the Johnson-Cook damage model (Eq. 7.2.9) simplifies to a stress-state-dependent form, as expressed in Eq. 7.2.13 wherein the value of the damage parameters were presented

in Tab. 7.3. The resulting fracture locus of the Al 5754-H22 alloy, representing the equivalent fracture strain as a function of stress triaxiality, is illustrated in Fig. 7.6.

$$\varepsilon_f = D_1 + D_2 \exp(D_3 \eta) \quad (7.2.13)$$

TABLE 7.3. Damage parameters of aluminum alloy sheet Al 5754-H22

Material	JC damage model				
	D_1	D_2	D_3	D_4	D_5
Al 5754-H22	0.21	0.08	-175	-	-

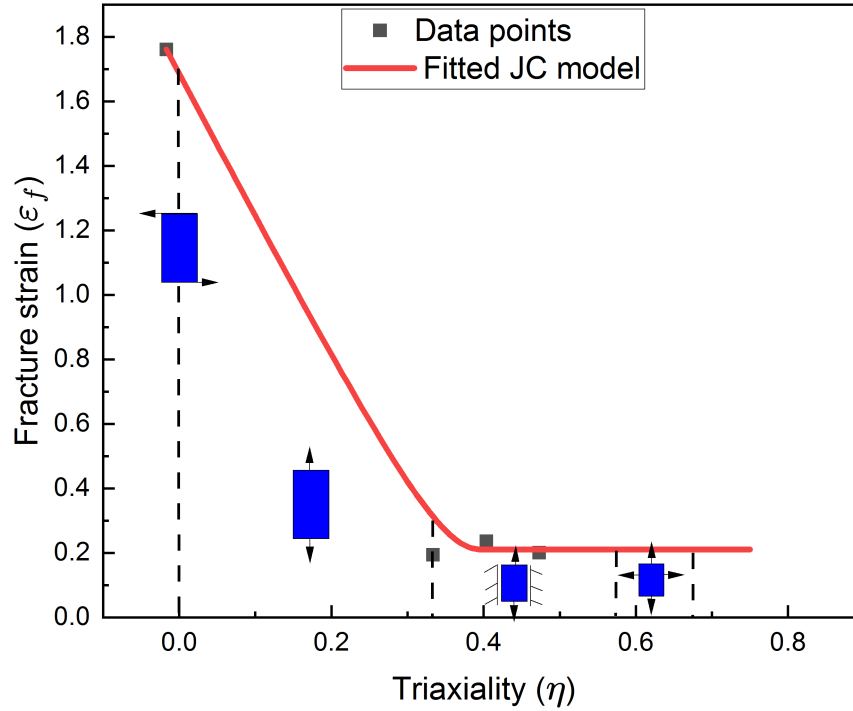


FIGURE 7.6. Fracture locus of the Al 5754-H22 aluminum alloy as a function of stress triaxiality.

7.3. Application of damage models in SPIF

7.3.1. Mesh sensitivity analysis. Recognizing that mesh density significantly influences the accuracy of fracture location and height in both coupled and uncoupled fracture criterion, a comprehensive grid independency study was performed. Three element lengths were evaluated: 0.5 mm (M1), 0.25 mm (M2), and 0.1 mm (M3). The results, illustrated in Fig. 7.7, show that while the coarse mesh (M1) failed to capture localized gradients, the M2 mesh (0.25 mm) provided a convergence in maximum PEEQ with a relative error of only 3.38% compared to the finest mesh (M3). Consequently, the M2 mesh was selected to optimize the balance between computational efficiency and predictive precision for fracture assessment. It is important to note that these numerical simulations were conducted on a high-performance workstation as described in Section.6.3. The computational times recorded

for M1, M2, and M3 were 48.60 hours, 103.19 hours, and 141.94 hours, respectively, underscoring the trade-off between mesh refinement and computational cost.

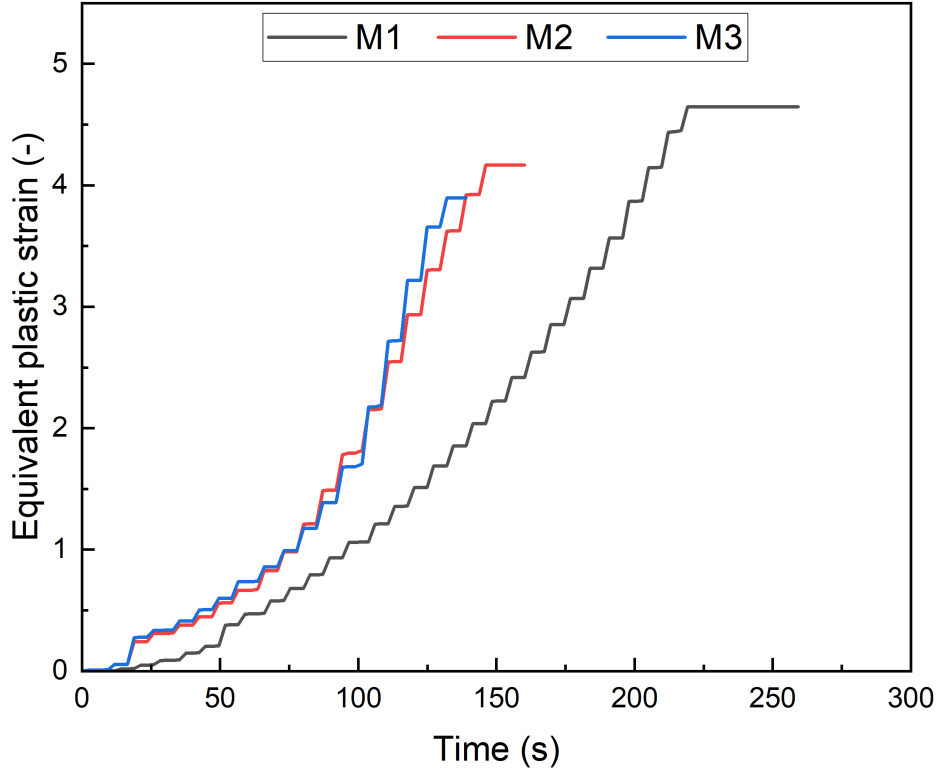


FIGURE 7.7. Mesh independency study

7.3.2. Formability evaluation and comparative analysis.

7.3.2.1. Commercial-purity aluminum alloy. The GTN damage model was employed to rigorously evaluate the formability of the TC and TP geometries at the critical wall angle of $\psi = 70^\circ$. This analysis focused on understanding the micromechanical damage evolution and predicting fracture initiation.

A key observation from the GTN simulations is the higher damage parameter D recorded on the inner surface of the formed parts, despite the equivalent plastic strain PEEQ often peaking on the outer surface (Fig. 7.8). While the outer surface experiences more severe stretching and bending-induced elongation, leading to higher PEEQ, the elevated damage parameter D at the inner surface is primarily driven by the localized stress triaxiality and hydrostatic tension. These conditions are generated by the tool's compressive-tensile cycles and the continuous bending-under-tension (CBT) mechanism inherent to the SPIF process. This critical interpretation suggests that ductile damage evolution in this configuration is governed more by the triaxiality-driven void growth and subsequent damage accumulation at the tool-contact interface than by the magnitude of plastic strain on the non-contact surface. Consequently, fracture initiation is highly sensitive to the localized constraints at the inner surface, which accelerate damage accumulation even in regions of relatively lower equivalent strain.

The numerical model accurately predicted the onset of fracture at depths of 11.4 mm for the TC and 9.0 mm for the TP (Fig. 7.9). These predictions were rigorously validated against experimental data obtained from manufactured conical-frustum specimens as depicted in Fig. 7.10, showing excellent agreement with the observed fracture specimens. Specifically, the average experimental fracture depth for the truncated cone shape was found to be 12.35 mm. Restricted by the machine peculiarities, just axisymmetric shapes were produced and analysed. The minor relative error between numerical and experimental results can be attributed to a combination of factors, including inherent variability in experimental measurements and potential limitations of the GTN model, particularly when the significant shear effects prevalent in the ISF process are not fully accounted for in the model's formulation [120]. This robust validation confirms the GTN model's suitability for assessing formability limits in SPIF and underscores the critical impact of part geometry on the material's structural integrity during forming.

Further analysis of the strain paths was conducted to elucidate the deformation mechanisms leading to fracture. Three critical points (labelled A, B, and C in Fig. 7.9) were selected in the TP and TC geometries, respectively, corresponding to regions where fracture typically initiates. The strain paths were evaluated against the Forming Limit Curve at Fracture (FLCF), which was approximated using a linear curve fitting based on engineering strains [176], as expressed in Eq. 7.3.1:

$$\varepsilon_{\max} = -1.040 \times \varepsilon_{\min} + 108.3 \quad (7.3.1)$$

being $\varepsilon_{\max}$ and $\varepsilon_{\min}$ the major and the minor strain, respectively. This investigation revealed distinctive material behaviors under the non-proportional loading conditions inherent to the SPIF process as depicted in Fig. ???. The analysis demonstrated that both shapes exhibit a strain state lying between plane strain and biaxial tension. Crucially, in contrast to the TC shape, the TP shape presented a more pronounced biaxial strain state, particularly recorded in the corners of the formed part (point B). This highlights how the part geometry significantly influences the forming limit by generating different strain paths, with the biaxial state in the TP corners contributing to its lower formability.

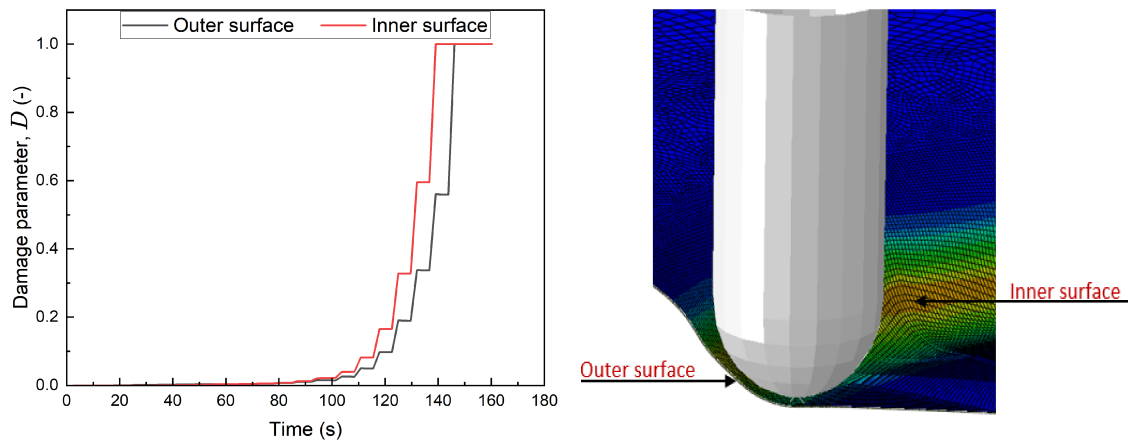


FIGURE 7.8. Damage degradation evolution with outer and inner surface

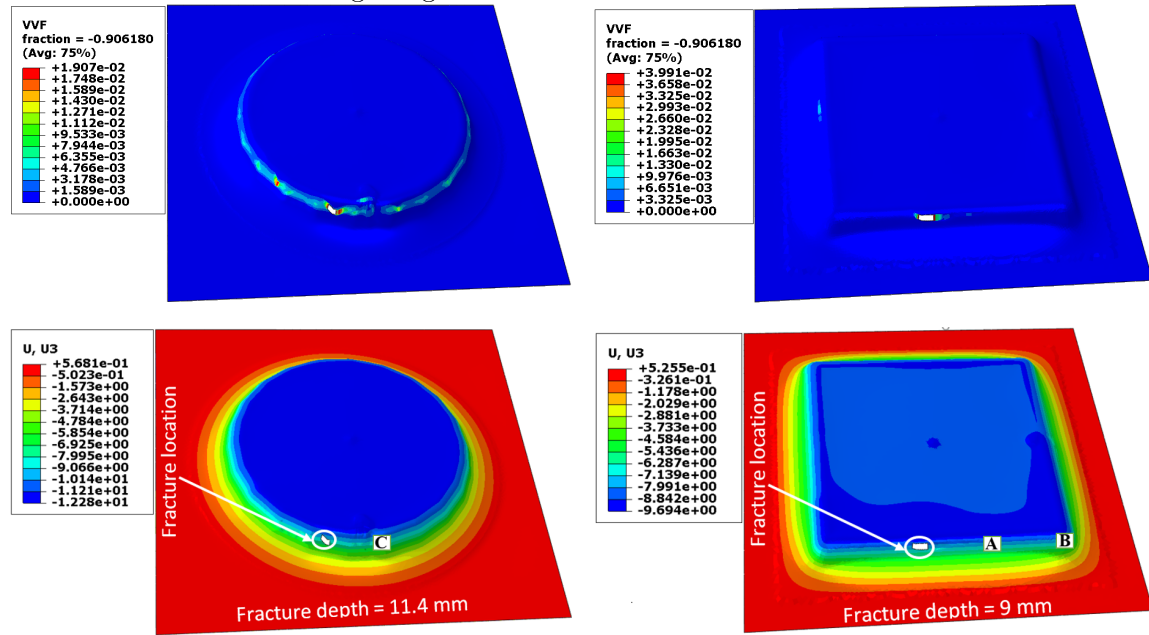


FIGURE 7.9. Distribution of void volume fraction and displacement at fracture initiation in a SPIF

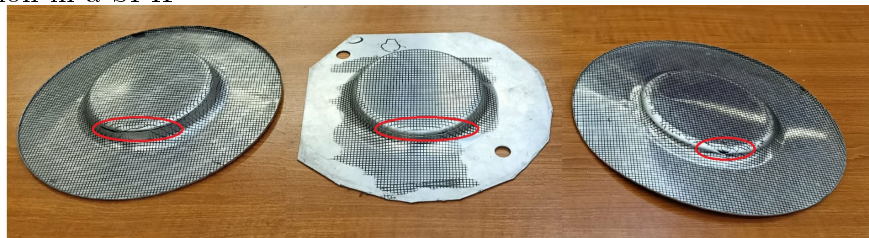


FIGURE 7.10. Experimental fracture specimens at 70°

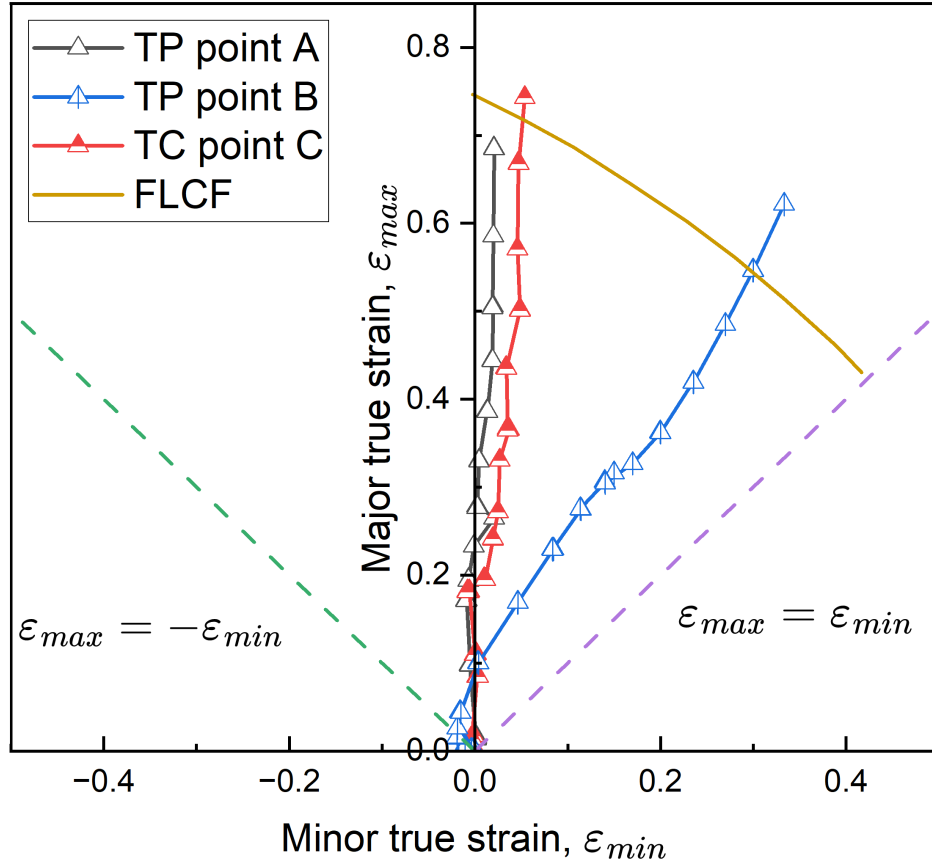


FIGURE 7.11. Non-proportional loading strain path evolution up to material failure, compared with FLC curve.

7.3.2.2. *High-strength aluminum alloy.* To evaluate the material formability of Al 5754-H22 sheets experimentally, a truncated cone with a variable wall angle (TCVWA) was initially proposed, where the drawing angle varies from shallow to steep. This geometry allows for the precise determination of the maximum formable wall angle using a minimal number of specimens. The geometric details of the continuously changing wall angle are shown in Fig. 7.12. The design involves a curved-line generatrix to create the revolved surface, defined by the major opening diameter (d_1), the designed depth (h), the generatrix radius, and the minor diameter (d_2). Experimental results indicate that the maximum achievable wall angle is $\psi = 66^\circ$. Subsequently, conical frustums with a constant wall angle (TCCWA) were tested to determine the corresponding fracture depths.

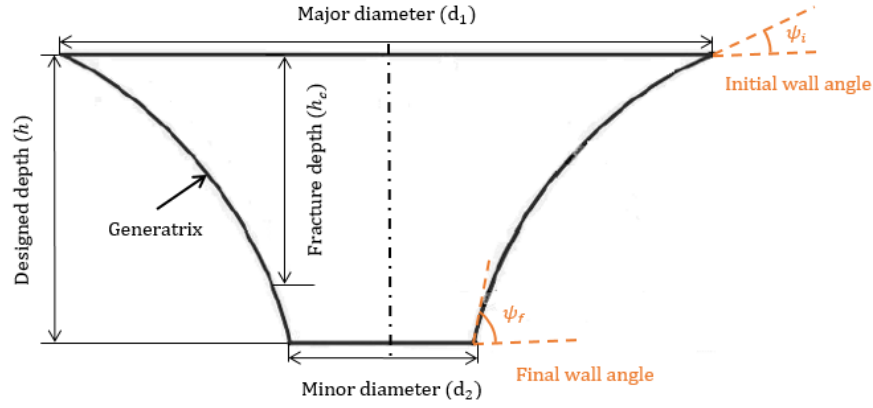


FIGURE 7.12. Generatrix and revolved surface for the TCVWA geometry.

For more detailed analysis, the JC damage model was implemented in the numerical model. Therefore a comparison of both experimental and numerical data are provided. The isotropic and its extended version for anisotropy consideration denoted as Hill'48 yield criterion were compared. Fig. 7.13 illustrates the tested specimens, including the fractured constant-wall angle and funnel part. In the first experimental set, a series of parts with continuously increasing wall angles were formed, starting from $\psi_i = 60^\circ$ and increasing at an interval of 2° up to a final angle of $\psi_f = 70^\circ$. Experimental results showed that the maximum safe wall angle achieved without fracture was $\psi = 66^\circ$. For the constant-wall angle test, fracture occurred at $\psi = 67^\circ$ at a critical depth of $h_c = 19.37$ mm. Correspondingly, the FE model predicted fracture depths of approximately 16.15 mm and 17.87 mm for the isotropic and anisotropic yield functions, respectively (Fig. 7.14a and Fig. 7.14b). Notably, the simulation using the Hill'48 yield function predicted the fracture locations with higher accuracy, yielding a relative error of 7.74%, compared to 16.62% for the isotropic model. Furthermore, the predicted fracture locations showed excellent agreement with experimental observations when using the anisotropic approach. The discrepancy between the FE model and experimental observations can be attributed to the absence of the contribution of the shear effect through the exclusion of the Lode angle parameter.

The evolution of equivalent plastic strain and the material damage variable during the forming process are tracked and plotted in Figs. 7.15a and 7.15b, correspondingly. The damage model relies on a damage index (D) to represent the accumulation of material degradation and the subsequent loss of load-bearing capacity, for uncoupled fracture criterion, this quantity is linearly defined by Eq. 7.3.2:

$$D = \int_0^{\bar{\epsilon}_f} f(\eta, \bar{\theta}) d\bar{\epsilon}^p \quad (7.3.2)$$

In case of the JC model, Eq. 7.3.2 is simplified to the summation of the incremental plastic deformation as referred in Eq. 7.3.3

$$D = \sum \frac{\Delta \bar{\epsilon}^p}{\bar{\epsilon}_f} \quad (7.3.3)$$

where $f(\eta, \bar{\theta})$, $\Delta\bar{\epsilon}^p$ and $\bar{\epsilon}_f$ denote the function incorporating the stress state dependency, the increment of equivalent plastic strain, and the strain at failure, respectively. In the FEA framework, an element is deleted when the damage index D reaches 1. Both graphs exhibit an incremental, stepwise increase, which is inherent to the localized deformation and cyclic loading characteristic of the SPIF process. Elements on the outer surface exhibit higher deformation levels and a higher damage index, as indicated by the steeper damage rate (slope). Consequently, damage initiation is more likely to occur on the outer surface of the conical frustum.

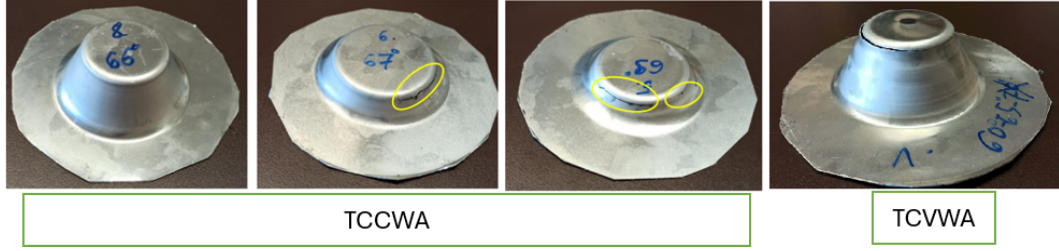


FIGURE 7.13. Experimental fractured specimens

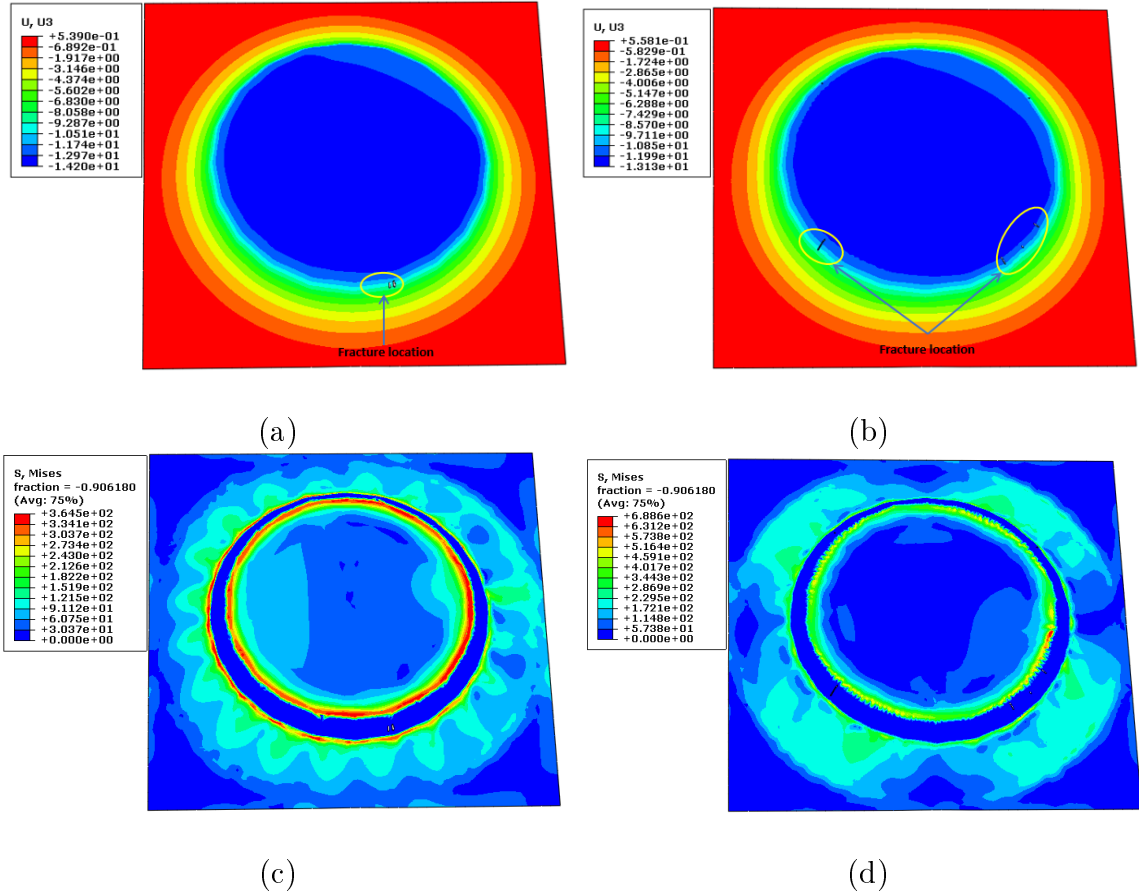


FIGURE 7.14. Predicted fractured specimens based on JC damage model

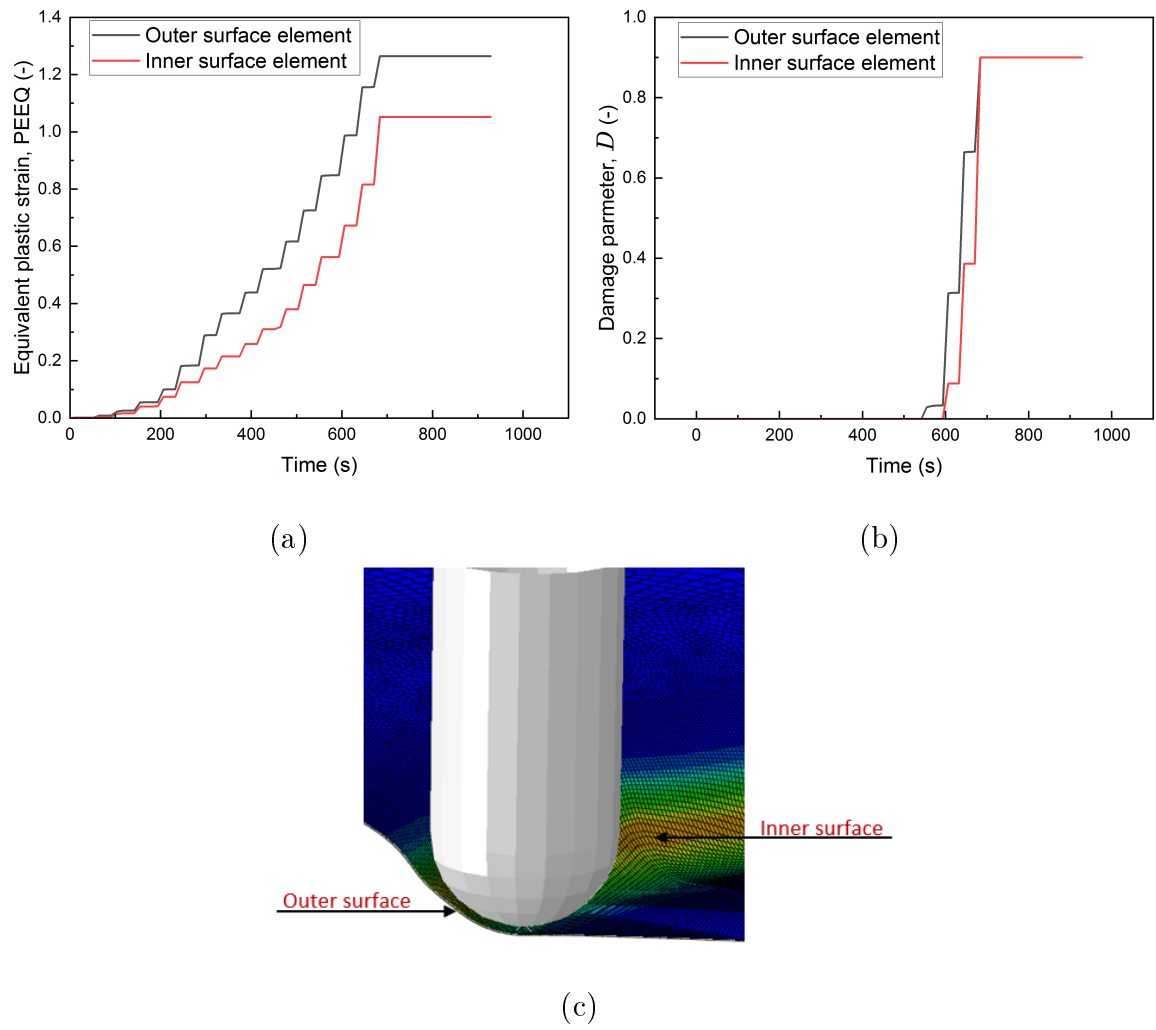


FIGURE 7.15. Evolution of (a) plastic strain, (b) damage index, (c) illustration of outer and inner surface element

GEOMETRICAL ACCURACY AND SPRINGBACK

8.1. Introduction

In ISF and particularly SPIF, the punch gradually shapes the sheet into the designed part following a predetermined path. Despite its flexibility and free-to-form constraints, this leads to geometrical errors as illustrated in Fig. 8.1, which include commonly found and agreed phenomena such as unwanted sheet bending, springback, which occurs due to elastic recovery. Lastly, is the pillow effect, which is a concave surface that takes place at the bottom of the formed shape where the area is undeformed. Given the significance of the wall angle in governing the thinning, formability, and dimensional accuracy as indicated in Chapter. 6 and Section. 2.7 in the literature review. This chapter is aimed to provide a qualitative and quantitative assessment of the drawing angle impact on both geometrical inaccuracy. Additionally, a detailed analysis is given on the role of the residual on causing springback phenomenon. In this chapter, the reason behind the distinguish springback quantity in truncated cone and pyramid shapes was provided.

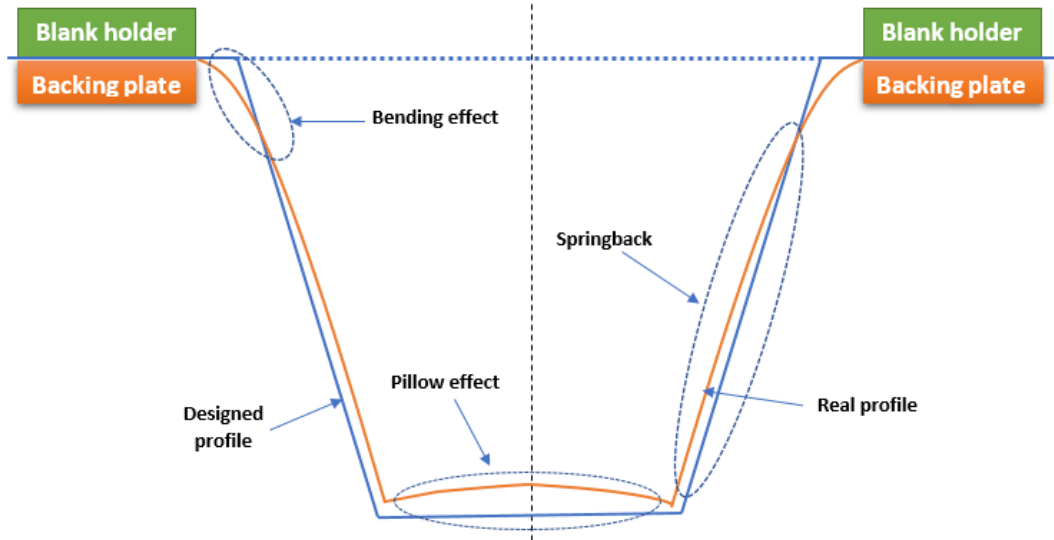


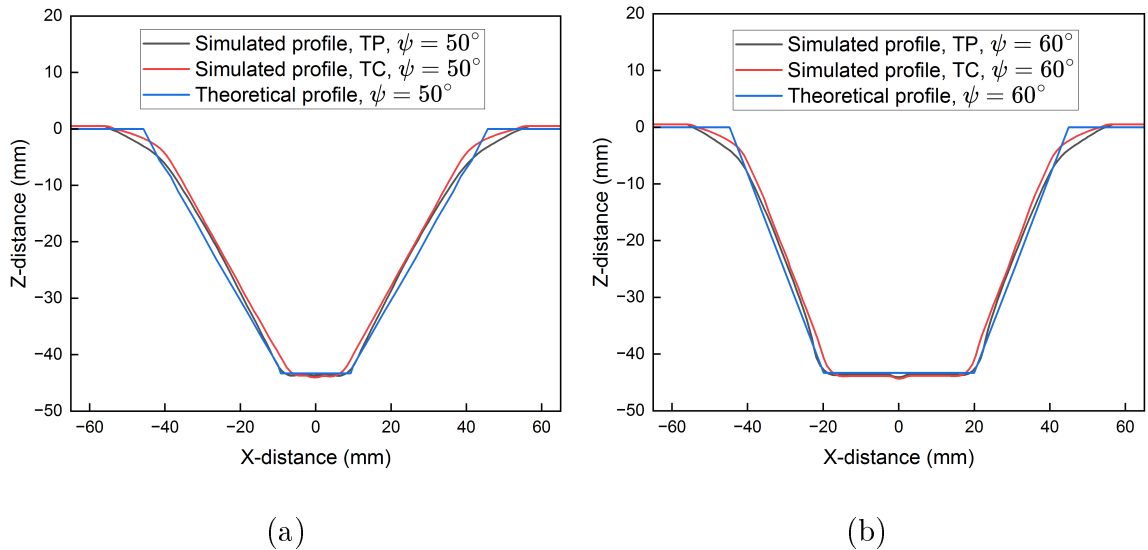
FIGURE 8.1. Commonly found geometrical errors in SPIF.

8.2. Geometrical deviation

The same range of the slope angle adopted in chapter 6, i.e., 50° , 60° , and 70° were carried out to investigate their impact on the geometrical deviation between both nominal (theoretical contour) and simulated. At shallow wall angle $\psi \approx 50^\circ$, a good conformity to the theoretical contour was achieved. However, upon close inspection, the TC profile exhibited

a greater deviation from the nominal line compared with the TP form. As the drawing angle increased to a moderate value $\psi \approx 60^\circ$, a noticeable increase in geometrical inaccuracy and deviation was observed, particularly in the bending zone close to the clamping plate at the major corner and the minor radius at the bottom (Figs .8.2a, 8.2b, and 8.2c). At the steep wall angle $\psi \approx 70^\circ$, the discrepancy between the compared profiles became more apparent and visible, specifically in the sidewall where springback occurs and in the bottom of the part where the pillow effect takes place. Overall, the evidence suggests that the frustum-cone shape is inherently more challenging to form with higher geometrical accuracy in SPIF, especially as the drawing angle becomes steeper. This can be explained by the continuous curvature of the cone, which may lead to more complex and less predictable material flow and springback compared to the more planar faces of the pyramid shape. While specific process optimization and material choices can significantly minimize the geometrical deviation of both shapes, the truncated pyramid appears to offer a more stable and accurate forming behavior under general SPIF conditions, particularly at higher wall angles. A slight difference between the two simulated curves is observed in the part sidewall, where springback prevails. However, a greater difference occurs mainly in the corners where bending is taking place. Furthermore, the amount of the pillow effect located at the bottom of the part increases with the increase of the drawing angle.

8.2.1. Springback analysis. The simulated parts obtained from Abaqus/Explicit were imported into Abaqus/Standard, and a static analysis was performed under specific conditions correspond to the relaxation of the formed part to calculate the springback for both TC and TP shapes, as shown in Figs .8.2d and 8.2e. To provide a quantitative assessment, the maximum deviation between the simulated profile before and after the springback was recorded in Tab. 8.1. Notably, the elastic recovery increased with increasing the wall angle, reaching a 1.24 mm and 1.198 mm for $\psi = 70^\circ$. Despite the similar trend, the TC shape exhibited a higher springback compared to the TP parts.



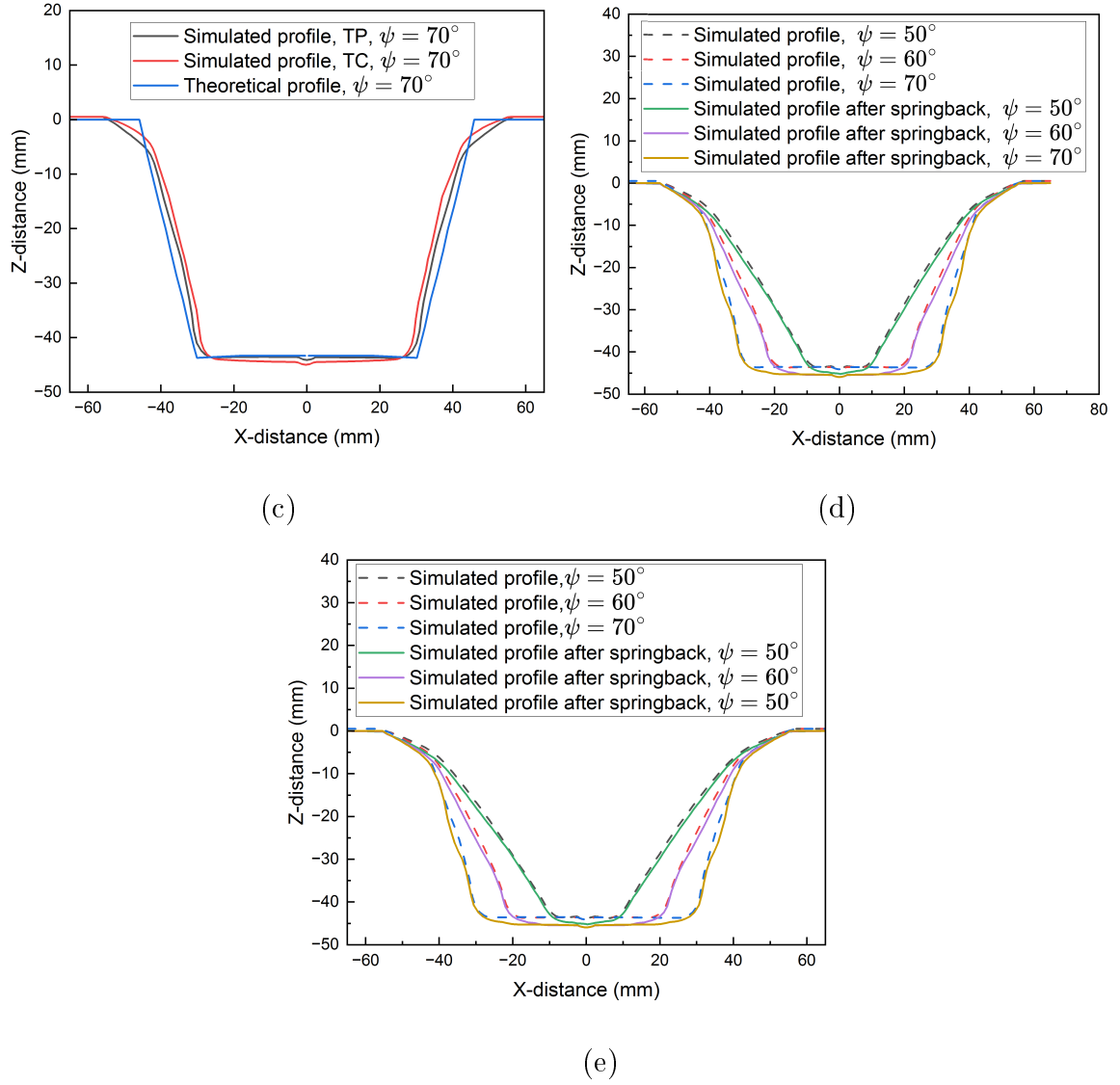


FIGURE 8.2. The impact of wall angle on the geometrical deviation: (a) geometrical profiles comparison at $\psi = 50^\circ$), (b) geometrical profiles comparison at $\psi = 60^\circ$), (c) geometrical profiles comparison at $\psi = 70^\circ$), (d) geometrical profiles comparison before and after springback in TC shape, (e) geometrical comparison profiles before and after springback in TP shape

TABLE 8.1. The effect of the wall angle on springback

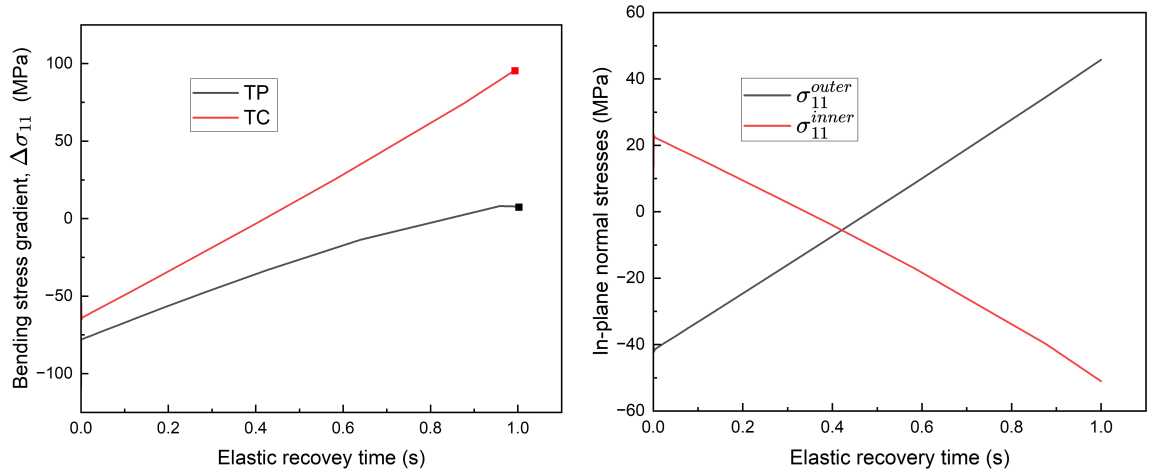
Wall angle $\psi(^{\circ})$	Springback in TC (mm)	Springback in TP (mm)
50	0.5448	0.4979
60	0.9447	0.8932
70	1.2437	1.1987

Residual stresses (RS) are "locked-in" stresses in the manufacturing, occurred even when there is no additional load was applied, which originated from the difference or the non-uniform volumetric change relative to other locations regardless of the manufacturing technique used (Mechanical deformation, heat treatment, casting, welding). The stresses beyond

yielding point induce plastic deformation leading to residual stresses [177]. It is well noted that the highest rate of the residual remain lower to the elastic recovery of the material [178]. These stresses are induced during forming processes are the key factors that influence the springback and the mechanical properties of the formed part. RS are defined as the internal stresses that remain within a body after the original cause of the stresses (e.g., forming, heating) has been removed, maintaining a state of balanced equilibrium. In SPIF, the characteristics of residual stresses (RS) are distinctive, primarily due to the absence of rigid support tools and the additional assembly constraints. Therefore, the residual stresses in both the truncated cone (TC) and truncated pyramid (TP) parts were investigated along the in-plane direction, specifically focusing on the region exhibiting higher springback within the formed part's sidewall. For brevity, only the component along the x-direction is presented, as depicted in Fig.8.3a.

To quantify the bending stress state, a residual bending indicator, $\Delta\sigma_{11}$, was defined as the difference between the in-plane residual stresses at the inner and outer surface element: $\Delta\sigma_{11} = \sigma_{11}^{inner} - \sigma_{11}^{outer}$. Here, σ_{11}^{inner} and σ_{11}^{outer} represent the stresses determined at the inner and outer surfaces of the part during elastic recovery. Quantitatively, both produced parts, at their equilibrium state, exhibited remaining tensile stresses of approximately 7.81 MPa for the TP and 96.88 MPa for the TC. This significant difference directly correlates with the higher amount of springback observed in the TC. This approach allows for a direct assessment of the residual bending stresses primarily responsible for springback.

The distribution of through-thickness residual stresses was further characterized for the TC shape, as illustrated in Fig.8.3b, unequivocally demonstrate the non-uniformity of the residual stresses in the thickness direction. The residual stresses were found to be more uniformly distributed across the TC shape Fig.8.3c, Fig.8.3d, in stark contrast to the TP shape (Fig.8.3e, Fig.8.3f) where they were predominantly localized in the corners. This localization in the TP is likely associated with the stiffness discrepancy between the corner and flat surfaces [179,180], leading to a differential response to elastic deformations in distinct regions, consequently affecting the magnitude and distribution of residual stresses.



(a)

(b)

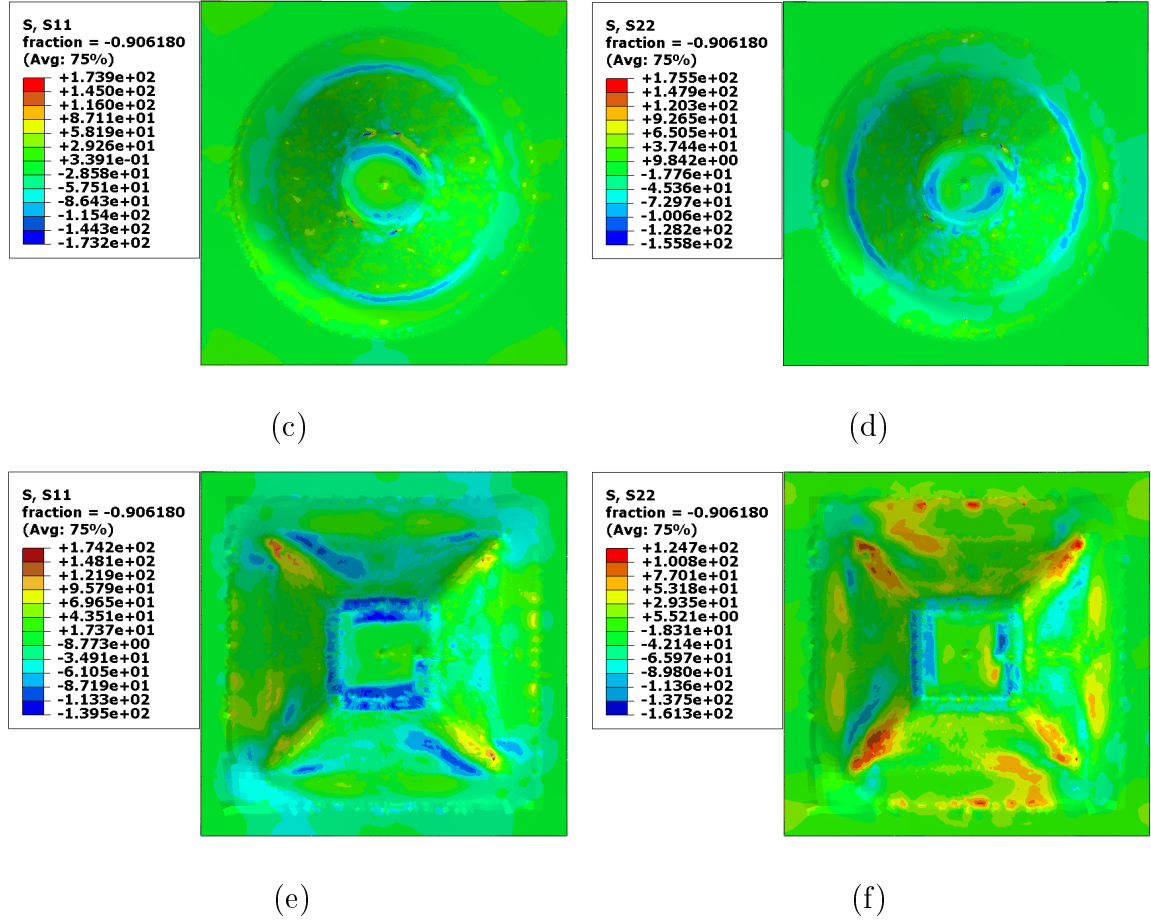


FIGURE 8.3. Comparison of Residual Stresses: (a) Bending stress gradient, (b) Through-thickness distribution of normal stresses, (c) Normal residual stresses in the TC, (d) Normal residual stresses in the TP, (e) Transverse residual stresses in the TC, (f) Transverse residual stresses in the TP.

8.2.2. Comparison with existing literature. Only five research investigations were identified in the literature concerning the impact of the wall angle on geometrical accuracy as will be detailed here. Several studies support the current findings, which demonstrate that increasing the wall angle negatively impacts the accuracy in truncated cone shapes made of pure aluminum alloy Al 1050A-H16. Specifically, Bârsan et al. [181] supported this observation, demonstrating that an increased inclination angle leads to a substantial rise in deviation from the nominal geometrical profile. For instance, they reported that a 10° increase in the tilt angle resulted in an almost 100% rise in dimensional deviation in truncated cone shapes made of deep-drawing steel DC04. Furthermore, the effect of the slope angle on springback has been confirmed by Patel et al. [182], who investigated angles of 40°, 50°, and 60° in numerical and experimental studies on a benchmark cone made of AA8011-H14 aluminum alloy. Similarly, Singh et al. [183] supported the notion that the drawing angle has a major effect on the local springback, based on tests conducted on Al 5052 sheets. However, the literature also presents contradictory findings. Jain et al. [184] reported that springback decreases with an increase in the steepness of the drawing angle for polypropylene

sheets. In a contrasting finding, Tang et al. [185] suggested that increasing the slope angle could actually improve geometrical accuracy. These contradictions are likely attributable to the differences in the adopted materials, such as the polypropylene used by [184] and the aluminum-lithium alloy (Al-Li) adopted in [185], highlighting the material-dependency of the process.

CHAPTER 9

THESES-NEW SCIENTIFIC RESULTS

Thesis 1.

I developed an advanced FE model based on explicit framework capable for predicting the forming forces in incremental forming. The model serves a critical tool for analysing the contributions of individual process parameters and their interplay-role on the forming force magnitude after model validation was presented. The results related of the geometrical and complexity of the part were argued. Two aluminum sheets were utilized during this investigation, namely, Al 1050, and Al 3003-O (5,6).

Thesis 2. A systematic study were carried out to illustrate qualitatively and quantitatively assess the significance impact of the drawing angle ψ , tool diameter d , and Δz after optimization was performed. Given its importance on both thickness reduction and formability, the effect of the wall angle on the stress state (Triaxiality (η), Lode angle parameter ($\bar{\theta}$)) were analysed correlating it with the material failure and therefore the formability limit (3).

Thesis 3.

I investigated the impact of the element formulations on the damage prediction accuracy by developing and implementing two damage models (Shell FE model and Solid FE model) through elucidating their influence on the stress state evolution. The C3D8R elements capture a realistic and a wide-ranging evolution of both (η) and ($\bar{\theta}$)) compared to the S4R, which confirm the necessity to adopt solid element accurate judgement on ductile damage mechanism (3).

Thesis 4.

I analysed the stress and strain along the formed truncated cone shape resulted in demonstrating the asymmetric of the von Mises stress distribution. The deformation exhibited a distinct strain gradient along the stretching zone.

C3D8R elements were used to capture the evolution of the through-thickness normal stress referred as contact stress and denoted as σ_{33} . The dominance of the compressive normal to the surface of the sheet contributing to the localization, stabilization, and consequently to the formability enhancement (2,4).

Thesis 5.

I calibrated a phenomenological damage model using a hybrid experimental-numerical approach on carefully selected designed geometries to cover a wide range of the normalized

hydrostatic stress η with the associative fracture strain $\bar{\epsilon}_f$. The Johnson-Cook damage model is simplified to be stress-state-dependent by neglecting the strain rate and temperature effects ().

Thesis 6.

I assessed the formability limit based on the maximum formable wall angle of two commonly used materials Al 1050 and Al 5754 aluminum sheets based on experimental data and numerical simulation through the incorporation of coupled and uncoupled damage models through the assessment of different geometries shapes such TCCWA, TPCWA, and TCVWA.

The TCCWA exhibits a higher material formability relative to the TPCWA. This material behavior was associated with non-proportional strain path and strain state ranging from plane-strain to biaxial-loading.

I evaluated the capability of the calibrated Johnson-Cook damage model as built-in model with the incorporation of both isotropic and the anisotropic plasticity theory demonstrating a relative error of 7.5% and 16.62% for the isotropic and the anisotropic models, respectively. The outer surface element exhibited the highest damage rate D relative to the inner surface demonstrating its susceptibility to failure and proving the through-thickness strain gradient ().

Thesis 7 .

I investigated the impact of the wall angle on the geometrical deviation of two commonly used benchmarks shapes (TCCWA, TPCWA). The geometrical inaccuracy has shown a direct correlation with the increase of the wall angle. The quantification of springback revealed a slight difference between the two shapes was observed. A bending stress indicator was defined as the difference between the in-plane residual stresses at the inner and outer surface element $\Delta\sigma = \sigma_{11}^{inner} - \sigma_{11}^{outer}$ was served to correlate the difference amount of springback in both shapes ().

Related publications by the author in the topics of the Thesis

- (1) Habbachi, M., Kovács, P. Z., & Baksa, A. (2025). Evaluation of thickness distribution during single point incremental forming of pure aluminum alloy Al1050. *Key Engineering Materials*, 1019, 21-27. <http://doi.org/10.4028/p-WmDG1q>
- (2) Habbachi, M., & Baksa, A. (2024). Numerical modeling of single-point incremental sheet metal forming of 3003-O aluminum alloy. *Multidiszciplináris Tudományok*, 14(3), 168-176. <http://doi.org/10.35925/j.multi.2024.3.15>
- (3) Habbachi, M., Baksa, A., & Kovács, P. Z. (2025). Influence of process parameters on thickness distribution and damage mechanisms in single point incremental forming. *Materials Today Communications*, 114357. <http://doi.org/10.1016/j.mtcomm.2025.114357>
- (4) Habbachi, M., & Baksa, A. (2025). An investigation into the evolution of the deformation state in single point incremental forming. *Multidiszciplináris Tudományok*, 15(4), 32-40. <http://doi.org/10.35925/j.multi.2025.4.3>
- (5) Habbachi, M., & Baksa, A. (2024, September). Numerical modelling and experimental investigation of incremental sheet metal forming of Aluminum Alloy Al 3003-O. In *Journal of Physics: Conference Series* (Vol. 2848, No. 1, p. 012006). IOP Publishing. <http://doi.org/10.1088/1742-6596/2848/1/012006>
- (6) Habbachi, M., & Baksa, A. (2024, November). The effect of the drawing angle and the tool diameter on forming force prediction in single point incremental forming. In *International Congress of Automotive and Transport Engineering* (pp. 341-353). Cham: Springer Nature Switzerland. http://doi.org/10.1007/978-3-031-77631-1_29

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