IMPROVEMENTS ON SINGLE POINT INCREMENTAL FORMING
THROUGH ELECTRICALLY ASSISTED FORMING, CONTACT AREA
PREDICTION AND TOOL DEVELOPMENT

by

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Abstract

Single Point Incremental Forming (SPIF) is a die-less sheet metal forming method. Because SPIF does not use custom tooling, this process allows for parts to be made at low cost and short lead times. In this thesis electric current is applied through the tool to alter the formability of samples formed with SPIF. The research goal of this work is to determine if formability is effected by resistive heating alone or if there is some formability change due to the current interacting with the material.

An apparatus that allows electrical current to be applied through the tool during forming is designed and implemented. A method is also developed to allow the contact area between the tool and sheet to be estimated, with particular focus on developing a method that allows for experimental measurement.

The effect of applied current on formability is estimated by evaluating the maximum wall angle that can be formed in a single pass, using a variety of tool sizes and current settings. Using the contact area model to estimate current density, a significant increase in formability is found at a current density range that agrees with previously published literature on electrically assisted forming of the same material. The results show that across multiple tool sizes, a significant increase in formability is observed when applying a current density (A/mm\(^2\)) larger than the current threshold density published in the literature.

A study is also performed to test the performance of a set of novel tool shapes. By using parabolic tools, it was found that formability can be improved while maintaining low surface roughness.

Finally, a series of case studies are presented documenting the production several parts for a variety of design groups and researchers at Queen’s University. These case studies provide examples for the uses of SPIF, as well as document the methods used to produce these parts in greater detail than is present in the literature.
Acknowledgments

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Thanks go to the Queen’s Baja SAE and Formula SAE teams for being enthusiastic clients for parts made with SPIF. The members of both teams have been an excellent sounding board for my ideas throughout the entire process, and have given lots of great design ideas. To Dave Cerantolla, thank you for cooperating so well with me in the lab and giving me the opportunity to make parts that get use.

I would also like to thank my parents for inspiring me toward a life of curiosity and enthusiasm for the world. I would like to thank my Dad for getting me interested in machining and design, and I would like to thank my Mom for giving me the fortitude and patience to finish a project of this size.

Finally, I would like to thank Miranda for listening even when I won’t stop talking about SPIF, and providing me with moral support and keeping me sane.
Co-Authorship

I would like to thank the various authors that have helped prepare the papers that are presented in this thesis. In all of the papers, Dr. Jack Jeswiet is listed as a co-author, as he helped with idea generation as well as writing and editing the manuscripts for publication.

For all of the papers presented I am listed as the first author as I conducted the research and wrote the manuscript and subsequent edits. Similarly I am listed as the corresponding author on all of the papers presented.

I would like to thank Brendan Cawley for his excellent work in performing the testing of the tools presented in Chapter 7. Brendan is listed as a co-author on the tools paper, as the work started as a MECH 461 project under my supervision. Idea generation was a joint effort by Brendan and I, and Brendan performed the physical testing of the tools. Brendan is listed as the first author on the earlier version of the paper that was presented to NAMRC, though the presentation was given by me with the approval of Professor Jeswiet. The results from the tools research were subsequently re-written into a manuscript format by me and submitted to IMECE Part B: Journal of Engineering Manufacture.

I would also like to thank Chris Carrick for contributing the section about forming polymers for the Niagra Foot team in Chapter 8.
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Chapter 1

General Introduction

1.1 Background

Single Point Incremental Forming (SPIF) is a dieless sheet metal forming method capable of producing complex asymmetrical parts. In SPIF a small tool is moved in a series of passes around the outer periphery of the part, stepping down between each pass. Because it does not require dies it is an ideal process for prototyping, custom fabrication and low volume production.

To improve formability in SPIF, particularly with exotic materials, many methods have been presented that focus on heating the sheet metal using electric current through the tool [1–3]. Little work has been carried out, however, to determine the direct effect of electric current as opposed to simple resistive heating on formability.

By creating a model of the contact area to estimate current density, comparisons can be drawn to previous literature on electric forming of similar materials. By understanding of the effects of electric current and temperature of formability, it may be possible to realize formability gains without the challenges associated with high temperature forming.
1.2 Objectives

The central goal of this work is to make SPIF a more appealing process to industry through improving forming limits and fundamental understanding of the process. These goals are achieved through presenting the following research:

- Create a system that safely allows electric current to be applied through the tool during SPIF. This design is detailed in Chapter 4. Currently this design is not published, however it was submitted to the Electric Safety Authority of Canada as part of the safety approval process.

- Create a method of determining contact area between the tool and sheet that allows for direct measurements to be taken, and use it to estimate current density during electrically assisted SPIF. This method has been submitted for consideration to the *IMECHE Journal of Engineering Manufacture*. See Chapter 6.

- Determine the effects of current density on formability, and determine if direct current effects are significant as opposed to simple resistive heating. This study has been accepted for publication in the *IMECHE Journal of Engineering Manufacture*, and can be found in Chapter 5.

- Study the effects of tool shape on formability and surface roughness in SPIF. The results of this study have been presented at the NAMRC conference in Madison, WI and has been reformatted and submitted for consideration to the *IMECHE Journal of Engineering Manufacture*. This study can be found in Chapter 7.

- Demonstrate application of SPIF through a series of case studies documenting the process of making a series of parts. This set of case studies has been submitted for consideration to the *CIRP Journal of Manufacturing Science and Technology*. See Chapter 8.
1.3 Scope

The scope of this research is:

- Design an apparatus that is capable of replicating previously published EAF conditions during forming with SPIF (see Chapter 4). This requires the following:
  1. Design and acquire equipment capable of supplying up to 900 A through the tool.
  2. Acquire safety approval from the Electric Safety Authority.
  3. Design and implement a system that allows for programmable current control according to position data from the mill. This is used to maintain constant current density and improve repeatability.

- Create a method of estimating the contact area to determine the current density during forming (see Chapter 6) which consists of the following:
  1. Create a method of estimating contact area based on directly measurable features.
  2. Using a series of experimental measurements, construct a simple linear predictive model for each of the parameters used to construct the area model.

- Study relative formability changes in 6061-T6 Aluminium while current is applied using the following:
  1. Using the variable wall angle conical frustum test, evaluate wall angle of failure for a range of applied currents and tools.
  2. Using the contact area model, estimate current density during electrically assisted forming. Comparing forming results at varying current densities, attempt to determine the mechanism by which formability is raised.

- Study relative formability changes due to various tool shapes (see Chapter 7).

2. Compare tools in terms of surface roughness, wall angle and qualitative performance criteria.

3. Demonstrate applications of SPIF by documenting the process of making several parts (see Chapter 8).

1.4 Thesis Organization

- Chapter 2 is an introduction to SPIF in general. This chapter provides background on SPIF as well as recent efforts regarding improving formability, particularly through thermally assisted means.

- Chapter 3 provides further background on the field of Electrically Assisted Forming. This focuses on samples that are formed using simple loading cases, such as tensile and compression testing.

- Chapter 4 documents the design of the electrically assisted SPIF apparatus that was used for this work.

- Chapter 5 is a paper accepted for publication in the IMECE Journal of Engineering Manufacture that studies the effects of direct electrical current on relative formability changes in 6061-T6.

- Chapter 6 is a paper that presents a method of determining contact area based on measurable features. Based on measured data, a predictive model for contact area is presented. This paper has been submitted for consideration to the IMECE Journal of Engineering Manufacture.

- Chapter 7 is a paper comparing the relative formability and surface roughness effects of several non-standard tools. This paper is current submitted to the IMECE Journal of Engineering Manufacture, and was presented at the North American Manufacturing Research Conference 2013 in Madison, WI.
• Chapter 8 is a paper presenting the application of SPIF to make a variety of parts for various student design teams and research groups. This paper has been submitted for consideration to the *CIRP Journal of Manufacturing Science and Technology*.

• Chapter 9 is a discussion of the results and limitations of the research performed.

• Chapter 10 presents the main conclusions that can be reached from this body of research, as well as recommendations for future areas of study.

1.5 Paper Introductions and Co-authorship

• Chapter 5 has been accepted for publication in the *Institution of Mechanical Engineers Part B: Journal of Engineering Manufacture*. It documents an initial investigation into determining the principal mechanism by which formability is affected by electric current. The experimentation was performed solely by David Adams, and co-authored by David Adams and Dr. Jack Jeswiet.

• Chapter 6 has been submitted to the IMECE for consideration for publication in the *Journal of Engineering Manufacture*. It documents the creation of a method that allows contact area to be experimentally measured, and presents a simple empirical model for contact area. The experimental and theoretical development was performed by David Adams, and co-authored by David Adams and Dr. Jack Jeswiet.

• Chapter 7 has been submitted to the *Journal of Engineering Manufacture*. The physical testing was performed by Brendan Cawley as part of a MECH 461 project supervised by David Adams. Idea generation was jointly performed by David Adams and Brendan Cawley. The results were presented at NAMRC 41 in Madison, WI. The paper was jointly written by David Adams, Brendan Cawley and Dr. Jack Jeswiet.
• Chapter 8 has been submitted to the *CIRP Journal of Manufacturing Science and Technology* for consideration for publication. It is a review of a series of parts made using SPIF. It was co-authored by David Adams, and Dr. Jack Jeswiet, with contributions from Chris Carrick.

**References**


Chapter 2

Single Point Incremental Forming: Background

2.1 Single Point Incremental Forming

Single Point Incremental Forming (SPIF) is a sheet metal forming technique wherein a sheet is formed using a single, small tool as opposed to a large die.

Figure 2.1 shows a typical SPIF configuration. In SPIF, the tool makes a series of 2d (x-y) contour passes around the periphery of the part, stepping down in the third (z) axis between each pass. The sheet is thus formed into the desired shape based on the toolpath. Unlike conventional sheet metal forming techniques such as stamping

![Figure 2.1: Configuration of the SPIF process for a circular toolpath](image)
or spinning, SPIF is able to form complex asymmetrical parts, without the need for a die. As a result, SPIF offers a much larger degree of flexibility than stamping or spinning, making it ideal for small production runs and custom parts, as the tooling cost associated with a given part is small.

SPIF has shown considerable potential as an industrial process because it allows for custom parts to be made rapidly and for a low cost. SPIF is also readily implemented in any conventional Computer Numerically Controlled (CNC) milling machine with very little modification, making it easy to incorporate into any existing machine shop. By allowing existing machines to be re-purposed, SPIF offers a major cost advantage over rapid prototyping (RP) technologies. Parts made with SPIF are also suitable for immediate use as opposed to parts made using RP technologies that are made from plastics.

### 2.2 Metalforming and the case for SPIF

For mass production, stamping is one of the most commonly used processes to form complex parts from sheet metal due to the low cost and short cycle times. A disadvantage of stamping, however, is the large tooling cost associated with producing a die. If a large production run is made, the tooling cost per part becomes small and parts can be made very economically. If small runs or custom parts are desired, however, the cost of the die becomes large in comparison to the part itself. As a result of the expensive and time-consuming die-making process, stamping is rarely used for small productions and custom parts. These advantages and disadvantages are summarized in Table 2.1.

<table>
<thead>
<tr>
<th></th>
<th>SPIF</th>
<th>Stamping</th>
<th>Spinning</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mass Production</td>
<td>Poor</td>
<td>Excellent</td>
<td>Good to Excellent</td>
</tr>
<tr>
<td>Asymmetric Shapes</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Not Possible</td>
</tr>
<tr>
<td>Lead Time</td>
<td>Very Short</td>
<td>Long</td>
<td>Medium</td>
</tr>
<tr>
<td>Tooling cost</td>
<td>Very low</td>
<td>Very high</td>
<td>Medium</td>
</tr>
</tbody>
</table>

Table 2.1: Comparison of advantages and disadvantages of metalforming methods
Because SPIF is capable of producing parts with generic tooling, the cost per part to produce small batches is very low in comparison to stamping. The cycle time with SPIF is, however, considerably larger than with stamping and as the number of parts desired increases stamping becomes the more economical choice.

2.3 Development of SPIF

SPIF is a member of the class of forming techniques known as Asymmetrical Incremental Sheet Forming (AISF) [1], which trace their roots to metal spinning and shear forming. Spinning is a forming method that involves placing a blank on the end of a rotating mandrel and forming the sheet into the shape of the mandrel by using a series of sweeping passes.

Shear forming is a similar method to spinning, however uses only a single pass to form the sheet as opposed to a series of passes as with spinning. The main difference between spinning and shear forming is that shear forming is done in a single pass, meaning that the dominant deformation mode is through-thickness shear in shear forming, resulting in corresponding thinning of the walls during forming [2].

Similar to shear forming, SPIF exhibits thinning in the wall of the formed part as a function of the wall angle. The thinning is caused by a combination of stretching and shear of the sheet in the forming areas [3], with the edges and centre of the part left undeformed. The wall thickness is typically predicted as a function of the initial thickness $t_i$ and the wall angle $\phi$ according to the cosine law, shown in Equation 2.1.

$$\text{Most often, parts formed with SPIF will fail at a given maximum wall angle } \phi_{\text{max}} \text{ due to thinning limits [4]. Raising the maximum wall angle that can be formed therefore allows a wider variety of parts to be formed successfully. Each combination of tool, material and initial thickness will have a unique } \phi_{\text{max}} \text{ due to varying strain distribution and material properties.}$$
Figure 2.2: Cosine law for wall thickness changes in SPIF, assuming pure shear within the walls.

$$tf = tj \cos \phi$$  \hspace{1cm} (2.1)

All AISF techniques involve a small tool or tools forming only a part of the sheet at a given time \([1]\). Because all AISF processes are performed on machines capable of moving in 3 or more axes at a time (such as a CNC mill), complex asymmetric parts can be made as easily as simple rotational ones. Adding additional axes can allow further complexity in the parts that can be made by allowing the tool to access the backside of the part, or position the tool to form wall angles steeper than 90°.
2.4 SPIF workflow

As with any machining or manufacturing process, the first step in forming a part with SPIF is determining if the part is suitable to be made using this process. As the most common failure method of parts made with SPIF is fracture due to thinning limits [4], the maximum wall angle is generally the most useful method of determining if the part will fail during forming.

If the walls of a part exceed $\phi_{max}$ (see Figure 2.2 for a definition of $\phi$), intermediate steps can be generated in the CAD model to allow for multi-pass forming. Multi-pass forming has been shown to allow wall angles of up to 90° [5, 6] by more uniformly straining the material in the sheet.

With the CAD model prepared, the toolpath can be generated using Computer Aided Manufacturing (CAM) software. The toolpath used in SPIF is typically a series of 2d contours around the outer periphery of the part, stepping down by a
constant distance between each pass. This toolpath can be easily created by most commercially available CAM packages, so implementation of SPIF does not require custom software.

For initial forming from a flat sheet, the ideal toolpath is a spiralling shape of with a constant pitch. While the spiral method creates more even stresses on the part, it is not always easily generated with commercially available CAM software. For non-rotational parts the spiral path can be difficult to convert into a series of smooth arc motions, resulting in the machine moving in a series of short line segment moves. Using line segments instead of arc segments results in a longer program and can reduce cycle time and increase wear on the machine due to high peak accelerations between motions. Detail on toolpath strategies can be found in [7].

2.5 Important Parameters in SPIF

This section aims to highlight the various factors in SPIF and their importance to part quality, formability and cycle time.
2.5.1 Tools used in SPIF

The tools used in SPIF are most often a solid hemispherical shape. Tool diameters commonly range from less than 6 mm to 25 mm or greater. Developments on tooling have included flat-ended designs, non-cylindrical tools and a tool with a free rolling spherical end to reduce friction. It has been shown that the diameter of the tool has a considerable effect on the forming limits of a given part and material, with smaller tools forming higher wall angles.

The increased formability of small tools is generally explained by the more localized strain distribution, resulting in suppressed neck formation.

While smaller tools can offer improved formability due to suppressed neck formation, smaller tools can also produce higher surface friction, as evidenced by spalling and high surface roughness. Studies by Cavalier et al. found that the increased friction may be due to a larger angle of wrap around the tool due to indentation.

Using a tool that is too small can result in degradation of the inner surface due to spalling and adhesion, as well as reduced maximum wall angle due to the increased tangential stress. In one extreme case during the work performed in this thesis a very small tool caused a buildup of material, forming a solid wall that starved the tool of lubrication and eventually caused failure of the tool, shown in Figure. Using a very small tool results in conditions approaching that of cutting with an extremely dull tool. Tool design is investigated further in Chapter.

Some work has been done to investigate the performance of non-hemispherical tools
in SPIF. Ziran et al. [8] studied the effects of using flat-end and hemispherical tools for SPIF and found that by using flat tools with radiused corners, both formability and shape retention could be improved. Particularly, the bottom surfaces of parts could be made flatter than with hemispherical tools as the tools supported the sheet better than a hemispherical shape.

Work is presented in this thesis in Chapter 7, investigating the forming performance of non-standard tools such as parabolic and angled ends.

### 2.5.2 Sheet Material and Forming Limits

For most sheet metal forming operations, formability is often represented by a forming limit diagram (FLD) that expresses the maximum strains that can be achieved before necking begins. Because neck formation is suppressed in SPIF, parts can be formed to much higher strains than are predicted by conventional FLDs [13][14]. Formability limits with SPIF can therefore be expressed with fracture forming limit diagrams [3][4][15]. Because wall thinning is related to wall angle, the geometry that can be formed is often represented by a maximum wall angle $\phi_{\text{max}}$.

One method for easily determining the maximum permissible wall angle is the
Variable Wall angle Conical Frustum (VWACF) test. This test, proposed by Hussain and Gao [16] and in the 2005 CIRP keynote by Jeswiet et al. [1], tests the thinning limits of parts by forming a shape with increasing wall angle with depth. The part is formed until fracture occurs within the wall, and based on the depth of fracture the angle can be calculated from the CAD model [16].

Through studying the effects of several factors, Fratini et al. determined that the strain hardening coefficient and percentage elongation had the largest effects on formability for SPIF [17]. Sheet thickness, according to Ham and Jeswiet, was also found to have a direct effect on the achievable wall angles in SPIF [10].

2.5.3 Feedrate and Spindle Speed

Because there is no chip load as in conventional machining, the spindle speed and feedrate do not have the same large effects in SPIF as they do in conventional machining. They do, however, have an effect on the energy efficiency and carbon footprint of the process. Branker et al. [18] found that increasing the feedrate and step size for a given part reduced the total energy consumed by 69% as well as a substantial reduction in the net CO$_2$ production.

The spindle speed does, however, affect the amount of friction at the tool/sheet interface. In general more friction leads to a worse surface finish and lower forming limits, in addition to higher energy consumption. It has been shown, however, that high spindle speeds may result in an increase in formability [1,10], speculated to be as the result of heat from friction reducing yield stress of the material. Experimentation by Palumbo and Brandizzi [19] revealed that high spindle rotation speed had a stabilizing effect on necking.

2.5.4 Lubrication

The importance of lubrication in SPIF cannot be understated, particularly in the case of the electrically assisted work done in this thesis. Lubrication reduces friction at
the tool/sheet interface, and to some extent cools the process. The impact of various lubricants and tribological conditions is an ongoing area of study within SPIF.

The performance of a lubricant is therefore perhaps best described by its ability to stay within the contact area. Adams and Jeswiet [20] found that while using a variety of gear oils of slightly different grades there was very little difference in the power consumed by the mill. When using a grease based lubricant, however, the grease was squeezed out of the contact area and was not able to flow back into place. The result was a drastic increase in power consumption (visible in Figure 2.7 as the tool became starved of lubricant, and premature failure of the part as well as very poor surface quality.

Hussain et al. found that while forming commercially pure Titanium, the lubricant and application method used had a large effect on interior surface quality of a part [21]. The best method was found to be micro-arc oxidization and anodic oxidation to create pores in the surface of the sheet to retain MoS$_2$ lubricant. A similar lubrication method was employed by Fan et al. [22].

It has also been observed that the tool shape has a direct impact on the lubricant
Figure 2.8: Failed part made with grease lubrication. Note the chips created by friction, the poor surface quality, and the forming area where all of the lubricant has been squeezed out.

performance. Cawley [11] observed that angled tools produce a lubricant trail that corresponds to a non-constant contact patch. Further, Adams and Jeswiet [20] found that small tools can create a buildup of chips that certain lubricants are not able to flow back over, eventually starving the tool of lubricant. The viscosity of a lubricant can therefore become important in SPIF not just due to the lubrication properties but the ability to flow back into the contact area after it has been pushed out of place by the tool.

2.6 Limitations of SPIF

2.6.1 Dimensional Accuracy

Like any manufacturing operation, parts produced by SPIF are never exactly the shape that was specified. After forming, residual stresses within the sheet material result in the part deforming slightly, reducing shape accuracy. Ham and Jeswiet [23] performed an analysis of geometrical accuracy for a variety of parts made with SPIF and found that for most parts the deviation was between 0 and 1 mm, however deviations up to as much as 4 mm were seen in some parts. An example of the shape accuracy is shown in Figure 2.9. A study of shape deviation was performed as part of this thesis and can be found in Section 9.3.
Micari et al. [24] found that the shape accuracy of parts is dependent on the distance from the fixture to the forming area, part shape and selection of process parameters such as tool size and stepdown. Careful selection of process parameters and toolpath generation may help improve some of these issues. In general there are several mechanisms that contribute to a loss of shape adherence.

- Elastic recovery. An extreme case is shown in Figure [2.11].
- Deformation outside the forming zone, shown in Figure [2.10].
- Over-forming resulting in walls that are thinner than expected by the cosine law.
- Non-flat bottoms, referred to as “pillow effect” by Ziran et al. [8].
- For multi-pass forming strategies, geometry may be pushed down the part further than expected, resulting in a conical protrusion from the expected end of the part [5].
2.6.2 Exotic material formability

Many materials are very difficult to form due to a combination of high yield strength, springback and surface properties affecting friction. Natural microstructure can be an important factor in these properties, such as HCP structure seen in Ti alloys. Materials with high yield strength can be difficult to form with SPIF because high process forces can result in high friction between the tool and sheet and failure of the tools due to high stresses. In some cases the high stresses can result in severe tool degradation (see Figure 9.10 on page 158). Similarly, certain materials exhibit low formability before fracture, resulting in a very small range of possible shapes.

One material that is very difficult to form is Ti-6Al-4V. Ti-6Al-4V is a high performance alloy of Titanium, with an HCP microstructure, widely used in the medical, automotive and aerospace industries. Due to the high degree of elastic recovery exhibited by this material, early attempts to form this (shown in Figure 2.11) were unsuccessful.
2.7 Current methods of improving SPIF

Since the inception of SPIF, considerable work has been done to overcome the challenges outlined in Section 2.6. Some of the methods that are being used to improve formability with hard to form materials are outlined in this section.

2.7.1 Warm forming methods

Metal forming is often made easier by increasing the temperature of the material during forming. Increasing the temperature both reduces yield and flow stress, and the amount of elastic recovery, resulting in a cheaper and more reliable process. Furthermore, deforming a material at high temperature results in significantly higher maximum deformation before material failure. Materials such as Titanium and Magnesium require warm forming due to their HCP microstructure.

The simplest method of warm forming in SPIF is to heat the entire sheet. This method uses a heated blank holder (an example is shown in Figure 2.12) or inductive heating elements that allow for the entire sheet temperature to be increased.
This method has been employed to successfully form Magnesium AZ31 alloy as well as Titanium alloys. A downside to the heated blankholder, however, is a reduction in working volume and increase in cost of the machine. Further, heating the entire sheet results in reduced sheet stiffness at all points, increasing the risk of undesirable deformation occurring at other parts of the sheet than the forming point. At this time, however, the direct relation between temperature and shape tolerance is not well characterized.

2.7.2 Laser Assisted local heating

To overcome the shortcomings of heating the entire sheet, it is possible to heat only the area around the tool/sheet interface. Locally heating the sheet reduces the chances of unwanted deformation while reducing the energy requirements from the process.

Figure 2.13 shows a laser assisted local heating system created by Duflou et al. with the goal of aiding formability and reducing springback in SPIF. A 500 W Nd:YAG laser was delivered to the back side of the sheet via a 3-axis positioning system. The laser was used to heat the sheet directly in front of the tool position, and coolant was used to ensure that the rest of the sheet remained at low temperature.

While the laser assisted method showed substantial improvements in performance, the 3-axis beam delivery system adds considerably to the cost of the machine. Additionally, the counter beam system is difficult to implement on a conventional CNC
Figure 2.13: Laser assisted SPIF system developed by Duflou et al. [27]

Figure 2.14: Left: Titanium alloy sheet after unclamping without laser heating. Right: With laser heating. Source: [27]
milling machine due to the large space requirements, thereby reducing the ease of implementation for most conventional machine shops.

To eliminate the need for a beam delivery gantry on the far side of the sheet, Gottman et al. developed a beam delivery method that supplies the beam on the same side as the tool [28].

### 2.7.3 Electric Hot Incremental Forming

As an alternative to laser heating, Fan et al. [29] implemented an electric heating system that passes direct current through the tool into the sheet while forming. The resistive heating from the current results in a heated patch around the contact area.

The goal of the electric hot incremental forming method (EHIF) was to address the shortcomings of the laser system. In particular, EHIF does not require a beam delivery system, drastically reducing the cost and making it suitable for implementation in a conventional CNC mill.

EHIF has been shown to drastically improve the wall angles possible with AZ31 Magnesium as well as TiAl$_2$Mn$_{1.5}$ Titanium [29]. Further work showed that Ti-6-Al-4V could be formed with this method [22].

In general, EHIF showed that increasing the current, and thus temperature, increased the formability of a sheet. The limit to the improvement, however, is reached when the temperature becomes high enough to begin burning the sheet [22, 29].

EHIF can also present a challenge to lubrication due to the high temperatures

![Figure 2.15: Electrically assisted hot forming method created by Fan et al.](image)
involved. Due to the elevated temperatures, conventional liquid lubricants begin to break down. Similar results are presented in this thesis, and one of the central goals of applying EAF theory to SPIF is to improve tribological conditions. Fan et al. used solid lubricants such as MoS$_2$ and graphite. To retain these lubricants a micro-arc oxidation process was employed to create pores in the surface. A study was published by Meier et al. that tested the wear on several tools during EHIF. The electric resistance heating method was also employed by Shi et al. to improve the accuracy of parts formed with EHIF.

### 2.8 The electroplastic effect

As far back as 1959 it has been documented that application of high density current can reduce the yield and flow stress in crystalline materials. The reduction in flow stress has been shown to be significantly greater than would be predicted by resistive heating alone.

The electroplastic effect has been exploited to improve the forming limits in a process referred to as Electrically Assisted Forming (EAF). EAF has been shown to improve forming limits and reduce elastic recovery for compression, tension and deep drawing. EAF is an attractive addition to metalforming because it may be able to greatly increase the performance of a forming operation without the need for heating of the part. This will be tested in this work.
2.9 Conclusion and proposal for work

This chapter has highlighted SPIF as well as the state of the art in overcoming the limitations of the process. As has been mentioned in the sections above, the current methods of improving the performance of SPIF often focus on raising the temperature of the sheet. While the laser heating system \[27\] proved effective, it is difficult and expensive to implement. The electric heating system designed in \[29\] proved simpler to implement but the temperatures created a challenge for lubrication \[22\].

While previous studies on electric resistance heating during SPIF have shown that formability can be improved \[22, 29, 31, 36\], these studies have not considered that there may be electroplastic effects present. The proposed work for this thesis is therefore to determine if results from EAF literature can be used to predict forming characteristics to help understanding the mechanism by which formability is altered by applied current. By understanding the mechanism by which formability is raised, better process controls can be developed, while minimizing tradeoffs such as surface friction. Finally, SPIF performance can be further raised by developing a greater understanding of the effects of tool design on forming characteristics during SPIF.

References


Chapter 3

Electrically Assisted Forming

3.1 Introduction

During any metal forming operation, a common method of improving the process limits is through warm or hot forming. By deforming a metal at an elevated temperature, the process forces can be greatly reduced. Shape retention can also be improved through the use of hot forming.

Drawbacks to hot forming include less than favourable tribological conditions as well as wasted energy from heating and subsequently cooling the entire part. As a result of the additional heating and cooling steps that must be added to the process, cycle time is also increased and in many cases accuracy may be reduced due to the need to re-fixture parts between heating and cooling cycles.

Electrically Assisted Forming (EAF) is a process wherein electric current is applied through a metal during forming. While electric current is applied, there is a drop in yield and flow stress of a material being formed, a phenomenon referred to as the Electroplastic Effect [1–3]. An example of this reduction in process force can be seen in Figure 3.1.

During EAF, high density Direct Current is applied across the part. As will be explained in greater detail in section 3.2, the electrons moving through the crystal structure interact with the dislocations in such a way as to aid dislocation motion [1–4].
Most importantly, however, temperature rise due to resistive heating has been shown to account for only a small portion of the formability increase \[5\],[6].

The exact contribution of electric current versus resistive heating is still a matter of dispute. Magargee et al \[8\] found that when cooling samples directly, very little formability change was observed compared to the non electrified baseline, and suggested that formability may be explained through local temperature effects at the microstructural level. Okazaki et al \[5\], however, found that temperature, skin and pinch effects account for only a small percentage of formability change.

### 3.2 Electrical Theory

Though the exact mechanism for EAF is still a matter of some debate, most theories revolve around the electrons from a high density current interacting with the dislocations in a material such as to aid dislocation motion. There are three mechanisms by which it is thought that electrons interact with dislocations to aid motion, with formability change likely as a result of a combination of each of these. It should be noted that these are only the current theories of the mechanism, and more work is yet to be done in confirming these theories and to what extent each one is significant.
1: Drift electron-dislocation interaction force  The first mechanism by which current density affects forming parameters is through electron-dislocation interaction. As described by Conrad [1] the electrons moving through a crystal structure exert an “electron wind” on dislocations. Because the electrons move relatively freely through the crystal structure but are redirected around dislocations, the result is a force specifically centered on dislocations. The force predicted by this electron wind is predicted by equation (3.1) [1], where \( \rho_d/N_d \) is the specific resistivity due to unit length of dislocation, \( N_d \) is the dislocation density, \( e \) is the electron charge, \( n_e \) is the electron density and \( j \) is the current density.

\[
F_{ew} = (\rho_d/N_d)en_cj \tag{3.1}
\]

2: Local heating due to electron redirection  The second mechanism by which electrons aid dislocation motion is kinetic energy dissipation. As electrons move through a crystalline structure, they are scattered around the dislocations. As the number of dislocations increases, the resistivity of a material increases [9].

The result of the electron scattering is that as electrons are scattered they release kinetic energy in the form of heat. This heat is released in the areas near each dislocation, resulting in a locally heated zone around the dislocation [9]. The temperature rise in these local areas creates expansion of the lattice around dislocations, allowing for easier forming and breaking of bonds.

3: Excess of Electrons  The final theory to explain the electroplastic effect is the large quantity of electrons present in the crystal structure [9]. Due to the excess of electrons in the lattice, bonds can be more easily broken and re-formed. As a result of the ease of bond forming, the stress required to move a dislocation line becomes lower.
3.2.1 Current density threshold

During electrically assisted forming many materials exhibit a threshold current density, below which little change in formability or flow stress is observed, and above which flow stress and limiting strain rapidly change \cite{2}. The exact cause for this phenomenon is not well understood at this time.

3.3 Applications of EAF

EAF has been shown to be a useful method for improving the capabilities of a wide variety of processes. This section will serve to highlight to capabilities of EAF for a variety of aspects of forming.

3.3.1 Reduction of process forces

The reduction in flow stress caused by EAF can have considerable effect on the process forces involved in forming metals. Specifically, the flow stress has been shown to be reduced considerably for a variety of materials \cite{2,10}, as shown in Figure 3.2. It should also be noted that the decrease in flow stress correlates with the current density, meaning that the change in flow stress can be easily controlled by varying the current.

![Figure 3.2: Flow stress reductions in Cu260 formed in a compression test with various applied current densities \cite{10}](image)

Figure 3.2: Flow stress reductions in Cu260 formed in a compression test with various applied current densities \cite{10}
The same results can be seen for a forging operating. Perkins et al [2] found that by using a DC current in a compression test, the flow stress of several materials could be drastically reduced.

Collins et al [11] tested a one-dimensional deep drawing method that employed EAF. As with the results from tensile testing and compression, the process force showed a considerable reduction, increasing with current density.

EAF has also been employed to reduce roll separating force for rolling AZ31 Mg [12]. Similarly, high currents have been employed to increase the maximum draw and reduce forces in wire drawing [13, 14].

### 3.3.2 Deformation limit increases

One of the most drastic effects of EAF is the increases possible in deformation limits. Because the reduction in bond forming energy increases the recrystallization rate, the effects of cold work can be reduced [1]. By using direct current, the deformation limits in compression have been shown to be increased by close to 50% [2], as seen in figure 3.3.

When in tension, however, reduction in cross sectional area results in an increase
in current density throughout the test. As a result the deformation limit with direct current can be less than expected due to the increase in heating at the thinnest part.

To help further increase the elongation limits in tension, electrical pulsing has been used \cite{15,16}. By pulsing the high density current, the heat buildup in the part can be reduced, allowing higher peak current densities. Using this method, Salandro et al \cite{15} were able to produce an elongation of more than 400% of the non-electrified baseline test. Stress-strain curves for a pulsed-current test can be seen in Figure 3.5. Distinct stress drops can be seen at the application of each pulse. Much work on EAF has also been performed using using very high current densities on the order of $10^3$ A/mm$^2$ for very short duration, on the order of $10^{-3}$ s \cite{4,17,18}.

### 3.3.3 Shape retention

EAF has been used to improve the shape retention of materials after forming. Because applied current can reduce the stress required for dislocation motion, EAF can be useful to reduce post-deformation stresses without additional deflection. The result is that by deforming a part and passing large current densities through the part after
being formed but while still held in place, springback can be significantly reduced or even eliminated [11,19].

Green et al. [19] found that by using a short duration pulse and a high current density, a springback reduction of 100% could be achieved. Examples of parts with reduced springback can be found in Figures 3.6 and 3.7. Furthermore, Green et al. found that higher density current and shorter pulse duration could result in a lower specimen temperature while still achieving reduction of springback.

Collins et al. [11] compared the effects of using continuous dc current during forming versus using post-deformation current on springback, shown in Figure 3.8. Their conclusion was that while it is possible to eliminate springback during deformation by using very high current densities, significant resistive heating can be observed. The best method of reducing the effects of resistive heating is to use a higher die speed with a high current density. The springback can then be eliminated by using post-deformation current pulses.
Figure 3.6: Shape retention tests performed by Green et al. [19].

Figure 3.7: Flattening test performed by Green et al. [19]
3.4 Conclusions

EAF is a very powerful process that allows metal forming processes to be improved dramatically. By applying high density direct current through a metal during deformation, the yield and flow stress can be significantly lowered. The resistive heating effects have been shown to have only a very small effect on the formability increases that have been observed.

When applied properly, EAF can be used to reduce process forces thereby improving energy efficiency of a process. Additionally, the deformation limits can have been shown to be increased significantly, [15]. Finally, EAF has been shown to reduce the springback in sheet metal forming operations by relieving residual stresses. Applying this to a production process could mean the elimination of annealing steps in addition to improved process performance.

3.5 Applying EAF to SPIF

While electric hot incremental forming has been shown to increase the forming limits of exotic materials in SPIF [20–22], the high temperatures involved produce challenges for lubrication. Based on the theory of EAF, however, it may be possible to obtain the same results or better results as Fan et al by focusing on increasing the current density instead of higher temperature.
Methods of increasing the current density without increasing the temperature could include cooling of the tool/sheet interface and changing the power profile to the tool tip. By making electrically assisted incremental forming a cold or cooler process, the lubricants used could be standard lubricants rather than the exotic lubricants used by Fan et al [21] which can be difficult to obtain and apply. Tool wear may also be reduced below that seen in [23] and [24]. The next few chapters of this thesis will therefore seek to determine the mechanism by which formability is improved during electrically assisted SPIF. By understanding the contribution of electrical current to formability change in SPIF, process controls could be designed that optimize the trade-offs between formability and frictional increase.

References


Chapter 4

Design of an electrically assisted SPIF apparatus

Prelude

The following documents the design process for an electrically assisted SPIF apparatus. This document was presented to the Electric Safety Authority (ESA) as part of the electrical safety approval process.

4.1 Introduction

This chapter highlights the design process involved in creating a slip ring apparatus that is capable of carrying large currents through the tip of a SPIF tool and into a sheet of metal while being formed. This apparatus was designed to be a retrofit to a conventional CNC milling machine, with the goal of having as little permanent impact on the mill as possible. In the design of this system there was concern about the effects that the high currents (900 A) may have on the electronics and control systems of the mill, so special attention was paid to ensure no current leakage into the mill through the spindle or table could occur.

The design includes a DC power supply feeding current through a slip ring into
the rotating tool. The current is carried through the tool, into the workpiece and into the blankholder. Both the blankholder and tool are insulated from the mill itself in order to ensure the safety of the operator and of the machine. An overview of the design is shown in Figure 4.1.

4.2 Functional Requirements

Before any design work was done, a list of the functional requirements was created. These design goals helped focus the design process and ensure that the apparatus design was useful and capable of carrying out everything that the process might require with enough excess capacity for any unforeseen requirements. Additionally, these functional requirements helped guide the design towards a more user friendly, reliable design.
4.2.1 Safety

The first and utmost requirement of this design is safety. Particularly because this design deals with very high current, it is vital that there is no risk of fire, injury to the operator, or damage to the machine. Additionally, the design was subject to the approval of the Electrical Safety Authority of Canada (ESA).

Safety factors for current were calculated based on relative cross sectional areas of conductors in comparison to those recommended by the Electrical Wiring code of Canada. All conductors in this apparatus have a factor of safety of at least 1.19. Additionally, all insulation was specified to have a minimum factor of safety of greater than 100 based on dielectric strength and voltage requirements.

Another aspect that was considered was the possibility of the slip ring locking. In this case, permanent damage could be caused to the wires and supporting hardware. To ensure safety, quick-disconnectors were added to the system near the slip ring. In the case of locking of the slip ring, the disconnectors are designed to pull away, both breaking the electrical circuit and ensuring that no damage can occur to the wiring and mechanical components.

The final safety consideration is that there should be no machine configuration that allows contact between the conductive components of the circuit and the machine itself. This was accomplished by using an insulated tool and blankholder. The design of the insulation on the blankholder ensures that no conduction is possible between the tool and the machine table even in the event of a tool crash. The routing of cables was also of particular importance, to ensure that it is impossible for cables to become entangled in the machine.

4.2.2 Current Capacity

The goal of this thesis is to replicate the electrical conditions previously published in EAF literature during forming with SPIF. Many of the conditions will be similar to electric hot SPIF \[1,2\] with possibly even higher current values.
To provide the very high currents and low voltages required, a DC power supply was specified from Magna Power Electronics that was rated with an output of $V = 0-5$ VDC and $I = 0-900$ ADC. This power supply was selected because it was the highest current capacity model in their 5kw line of power supplies, as well as compatibility with line power available in the lab. Additionally, adding further current capacity was both expensive and difficult to package within the mill.

The critical current densities for a variety of materials have been published by Perkins et al [5]. The critical current density is the minimum current at which EAF effects begin to dominate the process, and is specific to each material. The critical current density can be as low as 7.9 A/mm$^2$ DC for A2 steel, [5] and as high as 60.8 A/mm$^2$ DC for 6061-T6 [5]. By pulsing current on for short durations, however, current densities of as high as 90 A/mm$^2$ have been used [6]. Using the contact area model presented in Chapter [6] a maximum current of 900 A was deemed sufficient.

As the power supply is incapable of providing more than its rated maximum values, and has internal safety features to prevent exceeding these values, 5V and 900A were selected as the working values for specifying components.

### 4.2.3 Maintenance and Serviceability

While it may not be easy to apply quantitative goals to serviceability, the system should be easily maintained. In order to achieve serviceability goals, all components were designed such that they can be assembled and disassembled easily. The design is also left open as much as possible to allow access for lubrication and service, as well as air flow for cooling. An exploded view of the tool is shown in Figure 4.2.

Electrical quick-disconnectors were used to facilitate easy removal of electrical components. To reconfigure the mill for conventional SPIF or machining, the tool need only be removed from the spindle and disconnected from the electrical circuit.
4.2.4 Control

As this machine is designed for a research environment, and it is not known which settings will be best, it was desired that the user have as much control over all parameters as possible. The ability to closely control current both manually and through software control was a major factor in selecting the power supply used.

The machine is designed for safety and ease of use by integration of control to both the mill and power supply. As the mill is able to output commands and coordinates through a serial port, and the power supply is able to receive instructions through a similar port, the system has the capacity for integrated control using MATLAB on an external computer. Later versions of this system could include a safety system that automatically stops current should the machine axes stop moving, indicating a tool crash or a stoppage by the operator.

4.3 The Electrical Circuit

Based on the design goals above, an electrical system was designed as a retrofit for an existing Bridgeport GX-480 CNC milling machine. The system was designed such that it could be implemented in the mill with as little modification as possible. The electrical circuit is shown in Figure 4.1.
In the circuit shown in Figure 4.1, DC current up to 5V and 900A is generated by a DC power supply. The current is fed through cables to a pair of quick-disconnectors that are mounted on the z-axis of the mill. The current is then fed through a slip ring and into the SPIF tool. As the tool is in direct contact with the sheet, current is passed through the tip of the tool, through the workpiece and into the blankholder. The blankholder is grounded back to the power supply, and insulated from the mill.

4.4 Mechanical Systems

While the electrical components of this system present a challenge in their design, the system is further complicated by the fact that there is mechanical motion in nearly all components.

To facilitate the motion of these components while avoiding tangling of the cables, careful cable management was very important. An energy chain cable guide was used to ensure safe, repeatable motion of the z-axis cables through the mill enclosure. Additionally, a slip ring was designed as a retrofit to a standard toolholder, as can be seen in Figure 4.2. The ground cables drop from the top of the mill and are strain relieved at two points. A full set of drawings for all parts that were designed can be found in Appendix A.

4.5 Component Design

With the requirements of the design outlined above, this section will now outline each component, its role in the design and, when applicable, all calculations related to factors of safety and performance criteria. This section will begin at the power supply, and follow the path of the current.
4.5.1 DC power supply

As mentioned previously, the power supply selected was a Magna Power TSA5-900. This power supply was selected because it fulfilled the criteria for capacity and fine-grain control. Additionally, this power supply allows for integrated control between the mill and power supply, as well as custom power profiles.

The power supply was purchased from the United States and did not come with a CSA approval. In order to be connected to power it first required approval from the Electrical Safety Authority (ESA). The approval required inspection of the power supply internal components, as well as an approval of the safety of the installed apparatus.

4.5.2 Terminal Enclosures

As per the request of the ESA, custom terminal enclosures were designed and fabricated for both the input and output terminals on the power supply. These enclosures were designed to ensure that hands or tools are not able to touch live components. All enclosures were made from 3.175 mm thick Aluminium and all cables were strain relieved as they passed through the enclosures.
The output terminal enclosures (shown in Figure 4.4) required substantially more design than the input side due to the large size of the conductors. Because a single cable with the required current capacity was not available, the output side wires were split into three conductors. While 3-conductor wires made cable management easier, it meant a much larger terminal enclosure. Each terminal was fitted with a long bolt with 3 connectors attached to it. Each connector was separated by an aluminum spacer. To ensure the mechanical strength of the connectors, the far side of each bolt was secured to the enclosure via a nylon block. An exploded view of the terminal assembly is shown in Figure 4.5.

**Spacers**

While the connectors were rated by the CSA, the spacers are custom designed for this application. The design goal with these spacers is to ensure that they will not provide so much resistance that they may become a fire hazard or cause the wires to become unbalanced in load. The final design was subject to the approval of the ESA inspector.

The spacers are made from Aluminum and have an outer diameter of 25.4 mm and
Figure 4.5: Unexploded view of the output terminals. Outer enclosure is hidden for clarity

an inner diameter of 9.53 mm. The goal of this section is to show that the resistance added by them is negligible compared to the resistance from the wires. The area of the spacers is calculated in equation 4.1,

\[ A_{\text{spacer}} = \pi/4(d_{OD}^2 - d_{ID}^2) = \pi/4((25.4mm)^2 - (9.525mm)^2) = 435.45mm^2 \] (4.1)

Aluminum has an electrical conductivity of \(2.82 \times 10^{-6}\Omega m\) [7]. Since the furthest cable from the terminal will have all of its current travelling to it through both spacers, the worst case scenario is assumed to be if two of the three cables stop conducting and only the furthest one remains. In this case, the two spacers will effectively become a single electrical resistor, of length 69.85 mm. The resistance for this worst case spacer is given by equation 4.2,

\[ R_{\text{spacer}} = \text{Resistivity}_{Al} \frac{\text{length}}{\text{area}} = 2.82 \times 10^{-8}\Omega m \frac{69.85mm}{435.45m^2} \] (4.2)

\[ R_{\text{spacer}} = 4.52 \times 10^{-9}\Omega \]
From the spec sheet provided by the manufacturer (see Appendix B) the resistance of each Royal Excelene 4/0 cable is 0.052Ω/m. Each conductor segment is made up of two segments of cable, 5 feet and 10 feet in length, for the input and return wire runs, respectively. The total cable resistance is therefore based on a length of 15 feet or 4.572 m. The resistance of the cable is therefore given by equation 4.3.

\[ R_{\text{cable}} = 0.052 \frac{\Omega}{m} \times 4.572m = 0.238\Omega \]  

(4.3)

From equations 4.2 and 4.3, the resistance of each cable is more than 6 orders of magnitude greater than the resistance of even the worst case spacer. The spacers are therefore of negligible resistance in comparison to the wires themselves and fully capable of carrying the full current from the power supply, and are therefore safe to operate. Contact resistance was minimized by using large contact areas, and machining the spacers to as smooth of a surface as possible. As both materials are soft, there is also some conformity between spacers and connectors under the compressive load from the bolt.

4.5.3 Wires

All components in the circuit are connected via 3 parallel gauge 4/0 (also referred to as gauge 0000) Royal Excelene welding cables (see Appendix B for specifications). Typically, wire gauges are selected based on the Canadian Electrical Code. For conductors of this ampacity, however, there is no standard in the Canadian Electrical Code. Additionally, the wires are not sold with an ampacity rated by the CSA.

To ensure the safety of the design, the cables were judged in conductivity against a similar product made by the different manufacturer that is rated by the CSA. X-FLEX 4/0 cables made by Cobra Wire & cable inc. are rated by the CSA for 452 A in continuous service (see Appendix B).

Each cable is made up from a series of small strands. For the Royal Excelene Cables, the effective conductor cross-sectional area is based on the following:
\[ d_{\text{strand}} = 0.23\text{mm (measured)} \]

\[ A_{\text{strand}} = \pi \frac{d_{\text{strand}}^2}{2} = \pi \frac{0.23\text{mm}^2}{2} = 0.0415\text{mm}^2 \quad (4.4) \]

\[ n_{\text{strands}} = \frac{2052 \text{strands}}{\text{cable}} \]

\[ A_{RE\text{cable}} = A_{\text{strand}} n_{\text{strands}} = 0.0415\text{mm} \times 2052\text{strands} = 84.22\text{mm}^2 \quad (4.5) \]

The XFLEX cables use 2109 gauge 30 strands. Gauge 30 strands have a diameter of 0.254 mm. Using equations [4.4] and [4.5], the XFLEX cross-sectional area is calculated to be 106.86 mm\(^2\). Assuming that the current capacity is linear with cross-sectional area, the current capacity of each Royal Excelene cable \(I_{RE}\) and safety factor \(SF_{RE}\) are therefore determined to be:

\[ I_{RE} = \frac{A_{RE}}{A_{XFLEX}} I_{XFLEX} = \frac{84.22\text{mm}^2}{106.86\text{mm}^2} \times 452A = 356A \quad (4.6) \]

\[ SF_{RE} = \frac{I_{RE}}{I_{Load}} = \frac{356A}{300A} = 1.19 \quad (4.7) \]

The wires are also carefully arranged to ensure that no wires can contact or rub on mechanical systems. To ensure predictable and safe wire movement, a hard cable carrier was fitted to the Z-axis of the mill.
4.5.4 Quick Disconnectors

The quick disconnectors were added to increase the servicableity of the system. They fit on to the z-axis of the mill, and allow the tool to be disconnected easily from the electrical system. They ensure a clean environment when not using the electrical system. They are SMH brand 4/0 gauge quick disconnects, and rated by the CSA for 350 A of continuous current per connection.

4.5.5 Slip Ring

The slip ring is a custom designed part that fits onto a standard CAT 40 milling toolholder. It is designed to carry current to the tip of the rotating tool while keeping the machine isolated and safe from stray current. The assembled design can be seen in Figure 4.7. A full set of drawings can be found in Appendix A. It has been very carefully designed to ensure that the all conductors have been sized appropriately, and the machine is completely safe. Below is a component-by-component breakdown of the parts and their respective factors of safety.

The general design of the slip ring is two brushes running on a cylindrical ring, which rotates with the tool. Current enters the system via a conductor bar that acts
as a mechanical support for the brushes. The brushes are sprung against the surface to ensure good contact. From there the current travels down the ring, to a transfer block that carries the current to the tool via bridge wires. All electrical components are made from copper. The toolholder is isolated electrically via tapered standoffs, and the tool is isolated via a non-conductive collet.

In this section, all factors of safety for ampacity are calculated based on the minimum cross-sectional area as compared to the cross sectional area of three Royal Excelene 4/0 welding cables.

\[ A_{conductor} = 3 \times A_{REcable} = 3 \times 84.22mm^2 = 253mm^2 \]  

**Input conductor bar**

The conductor bar (see figure 4.8) acts as the input terminal for the tool as well as a mechanical support for the brushes. The minimum diameter of the conductor bar is 19.05 mm, sized to fit the ring terminals from the input wires. The cross-sectional area and factor of safety are:

\[ A_{bar} = \frac{\pi}{4} d_{conductor}^2 = \frac{\pi}{4} (19.05mm)^2 = 285mm^2 \]
Figure 4.8: Cutaway view showing the input conductor bar and brushes. The ends are left protruding from the base of the tool to leave room for terminals.

\[
SF = \frac{A_{\text{bar}}}{A_{\text{conductor}}} = \frac{285mm^2}{253mm^2} = 1.13 \quad (4.9)
\]

**Brushes**

The brushes are the components that slide on and transmit current to the ring. The contact area of the brushes is designed to be as large as possible to reduce the current density through the moving contact surface. In order to fit two brush pivots on to the input conductor bar, the minimum cross-sectional area occurs in the arm, and is 544 mm\(^2\).

\[
SF = \frac{A_{\text{brush}}}{A_{\text{conductor}}} = \frac{544mm^2}{252mm^2} = 2.15 \quad (4.10)
\]

**Slip Ring**

The slip ring carries current from the stationary brushes to the rotating transfer block. The lowest cross-sectional area that carries current occurs along the centre of the ring. The outer diameter is 55 mm, and the inner diameter is 47.63 mm. The safety factor for the ring is therefore:
\[ A_{\text{ring}} = \frac{\pi}{4}(ID^2 - OD^2) = \frac{\pi}{4}((55\text{mm})^2 - (47.63\text{mm})^2) = 594\text{mm}^2 \]

\[ SF = \frac{A_{\text{ring}}}{A_{\text{conductor}}} = \frac{594\text{mm}^2}{253\text{mm}^2} = 2.35 \]  

**Transfer Blocks**

The transfer blocks work by creating a bridge between the slip ring and the tool while keeping the conductors themselves isolated from the toolholder, as visible in figure 4.2. The bridge wires use 8 Royal Excelene gauge 1 wires in parallel. The diameter of a gauge 1 wire is 7.34 mm. The factor of safety for the wires is:

\[ A_{\text{wire}} = \frac{\pi}{4} * d^2 = \frac{\pi}{4}7.34\text{mm}^2 = 339\text{mm}^2 \]

\[ SF = \frac{A_{\text{wire}}}{A_{\text{conductor}}} = \frac{339\text{mm}^2}{253\text{mm}^2} = 1.34 \]  

**Insulation**

There are two major points of insulation on the toolholder. The first is tapered insulators that hold the slip ring in place, and the second is a collet that holds the tool. The collet can be seen surrounding the tool in figure 4.7.

**Slip ring insulation** The slip ring is both mechanically supported and insulated by two tapered components, one at the top and one at the bottom of the ring. These each mate with a matching taper in the slip ring, ensuring a strong fit that is self-centering and resistant to machining defects. During assembly, the ring slides over the threads at the end of the toolholder, and mates with the taper that is placed at the top of the toolholder. The lower taper is then threaded on to the toolholder, locking the ring in place. The ring is then isolated via the two tapers, and a 1.59 mm wide air gap, as shown in figure 4.7.

The tapers themselves are made from Ultem, a high temperature high strength
plastic. Ultem has a rated dielectric strength of 830 V/0.0254 mm (see Appendix C), or 32677.17 V/mm. This material is also recommended by the manufacturer for use in automotive powertrain applications in part because of the high temperature range and compatibility with lubricants. At the thinnest, there is 1.59 mm of Ultem between the ring and the tool. The factor of safety for 5V is calculated in equation 4.13

\[
SF = \frac{\text{dielectric} \times \text{distance}}{\text{voltage}} = \frac{32677.17 \text{V/mm} \times 1.59 \text{mm}}{5 \text{V}} = 10391 \quad (4.13)
\]

Based on a dielectric strength for air of 3000 V/mm, the factor of safety for the 1.57 mm air gap is

\[
SF = \frac{\text{dielectric} \times \text{distance}}{\text{voltage}} = \frac{3000 \text{V/mm} \times 1.57 \text{mm}}{5 \text{V}} = 88.9 \quad (4.14)
\]

**Insulating Collet**  The tool is held in place by a custom made collet, visible in the lower part of figure 4.7. The collet must be both electrically insulating, and capable of withstanding the temperatures and pressures exerted on it without deforming. In practice, temperatures of greater than 150-200°C may be experienced in the tool. Any small deformation within the collet will result in drastic runout at the tool tip, so a high performance plastic, *Torlon*, was selected as the material of choice.

Torlon is rated to 250°C before softening, and the manufacturer reports a dielectric strength of 22.8 kV/mm. The technical specifications for this material can be found in Appendix C under the page marked *Duratron*. The wall thickness of the collet is 2.7 mm. Equation 4.15 shows the safety factor for the collet.

\[
SF = \frac{\text{dielectric} \times \text{distance}}{\text{voltage}} = \frac{22800 \text{V/mm} \times 2.7 \text{mm}}{5 \text{V}} = 12312 \quad (4.15)
\]
4.5.6 Blank Holder

The blank holder used in the apparatus is a modification of the design that was previously installed in the machine by Kelvin Hamilton [8]. The blank holder (shown in figure 4.9) is a modular design that allows for a variety of sized blanks to be used very easily.

To ensure the safety of the design, the blankholder was modified to electrically isolate the workpiece from the milling machine. Figure 4.10 shows the insulation that was applied to the bottom of the blankholder.

A series of insulation collars were made that fit around the support posts for the blankholder. These collars had a conical inside and serrated edges to ensure that any stray lubricants could not form a conductive coating around the collar. The serrated edges act as drop formation points, as smooth edges resulted in a solid ring that did
not drip off easily. The mounting bolts were then placed in insulating sleeves.

References


Chapter 5

Single Point Incremental Forming of 6061-T6 using Electrically Assisted Forming Methods

Prelude

The following is a copy of a paper accepted for publication in IMECH Engineering Manufacture. Some figures may therefore be repeated from other chapters.

Abstract

In this paper large direct current is applied through the tool to improve formability while forming 6061-T6 Al using Single Point Incremental Forming. Special attention is paid to the direct effect of current density, as opposed to bulk resistive heating, to determine if the electroplastic effect is significant in raising the formability without requiring temperature rise. Tests are performed to determine the maximum wall angle that can be formed for a variety of current and tool settings. The area of contact between the tool and sheet is modelled and a control system is proposed
and tested to vary the current to maintain a constant current density during tests. 
The phenomenon of current threshold density is observed at a current density range 
agreeing with previous studies forming the same material in different loading cases. 
For both the 6.35 mm and 9.57 mm tool diameters, the maximum wall angle that 
could be formed was achieved at a current density of slightly above 60.8 A/mm$^2$. 
Measurements of surface roughness showed a similar trend toward increasing surface 
roughness and spalling with current density, and a reversibility in surface roughness, 
suggesting the mechanism of surface roughness increase may not entirely be high 
temperatures resulting in reduced lubricant effectiveness.

5.1 Nomenclature

$h_1$ Depth of indentation into sheet

$h$ Horizontal stepover

$q$ Half-width of intersection between tool and previous toolpath

$t_f$ Final through-wall thickness

$s$ Radius of the tool projection in the XY plane

$t_i$ Initial sheet thickness

$r_t$ Radius of a trough from a previous tool pass

$\phi$ Wall angle from horizontal

$\phi_{max}$ Maximum achievable wall angle

$\psi$ Angle from tool axis

$M$ Mean

$SD$ Standard Deviation

$p$ Probability
5.2 Introduction

Single Point Incremental Forming (SPIF) is a dieless technique for forming complex shapes from sheet metal. SPIF, outlined in Figure 5.1, uses a simple, small tool making a series of passes around the outer periphery of a part to form the final shape. Unlike conventional sheet metal forming techniques, SPIF does not require specialized tooling for each part [1], instead using the motion of the tool to form the desired shape. While traditional stamping is typically only economically feasible for large production runs due to the capital cost of tooling, the dieless nature of SPIF allows custom parts to be made for low cost.

In recent years, much effort has been spent on improving the ability of SPIF to form steep walls and hard to form materials. Babu and Kumar employed SPIF to form 304 Stainless steel [2]. Ambrogio et al [3] used a heated blankholder to successfully raise the maximum wall angle achievable in AZ31 magnesium. Local heating of the sheet near the forming area with a laser has also been used by Duflou et al [4] and Gottman et al [5] to successfully form titanium alloys.

Both the heated blankholders and laser heating methods increase the complexity
and initial cost of the forming system, and potentially reduce the usable volume of the machine. To produce a lower cost alternative which requires fewer modifications to an existing CNC mill, Fan et al passed electric current through the forming tool, increasing the sheet temperature through resistive heating \[6,7\]. This method was successfully used to increase the formability of AZ31 magnesium and Ti-6Al-4V. A similar system has been implemented by Ambrogio \[8\].

A common problem with forming at elevated temperatures is reduced lubricant effectiveness and tool wear \[9\]. One potential method of improving the formability without directly relying on increasing the temperature of materials is Electrically Assisted Forming (EAF). EAF is a process where the yield and flow stress of a material are reduced by electron interaction with dislocations while passing high density current through the material during forming \[10,13\]. Unlike resistive heating, however, the temperature rise accounts for only a small portion of the formability increase observed \[14\].

By recognizing that formability gains with applied current may be a result of electric current, rather than resistive heating, it may be possible to realize formability gains at lower temperature, or otherwise find the optimal forming parameters that maximize formability increase while reducing unnecessary temperature rise. Reducing the forming temperature could reduce lubricant failure and tool wear. To evaluate the effects of current density in SPIF as proposed by Roth \[15\], an electrified toolholder was designed and used to test the maximum wall angle of several samples of Al 6061-T6 at varying current values. The goal of this work is therefore to establish that electric current has a direct effect of formability conditions in Electrically Assisted SPIF (EASPIF).

Many materials formed with EAF exhibit a threshold current density value, below which little effect is seen \[11\]. This study also seeks to establish if a similar threshold current density phenomenon is present and if it can be used to predict formability increases in EASPIF. While the method used for electrically assisted forming is similar
to previous work on resistive heating, the goal of this study is to determine if electrical, rather than thermal effects dominate in increasing formability.

For this study, the maximum achievable wall angle for 6061-T6 Aluminium is tested for a range of current (constant current magnitude) and current density (varying current as contact area changes) values for a variety of tools. The results of the formability response can be compared to literature on forming 6061-T6 with EAF [11].

5.3 Background

5.3.1 Single Point Incremental Forming

During SPIF, thinning of the walls occurs due to stretching and through-thickness shear of the material. The final wall thickness $t_f$ is often approximated as a function of the initial sheet thickness $t_i$ and the wall angle $\phi$ [16], as shown in Equation 5.1:

$$t_f = t_i \cos \phi$$  \hspace{1cm} (5.1)

As the wall angle is increased, the wall thickness decreases, resulting in a practical wall angle maximum for every material and thickness combination. Multiple passes can be used to extend forming limits well beyond that of single passes [17] by redistributing material from the centre of the part to the walls, however at the cost of drastically increased cycle time.

5.3.2 Electrically Assisted Forming

In EAF, high density direct current is applied through a metal during deformation. While the current is flowing, the yield and flow stresses are significantly reduced, and the deformation limits are raised [12,18]. EAF has been shown to improve the forming limits and reduce the process forces of tensile tests [12], compression tests [11] and deep drawing [19].

As electrons pass through the material, it is theorized that they aid dislocation
motion in three ways [20]: by locally heating the area around dislocations, by increasing the kinetic energy deposited on the dislocation creating an electron “wind force”, and by increasing the number of electrons, allowing bonds to be broken and formed more easily. In tensile tests, EAF has been shown to increase the forming limit before fracture by as much as 200% of the non-electrified baseline [12].

Of particular interest is that the formability gains that have been achieved are significantly higher than can be attributed to temperature alone [11, 14]. EAF has also been used to reduce, and in some cases eliminate, springback after forming [21].

Many materials also exhibit a “threshold” current density, below which little formability improvement is seen. The cause of this effect is still not well understood, however threshold current densities have been observed in several materials [11]. For 6061-T6511 Aluminium in compression, the current density threshold as observed to be between 54.6 and 60.8 A/mm^2 [11].

5.4 Experimental Method

In EASPIF, current is passed through the tip of the SPIF tool into the sheet. To test EASPIF, a custom forming apparatus was designed, capable of carrying high currents (up to 900 A) to the rotating tool, while insulating the mill spindle from any stray current. Figure 5.2 shows the electric circuit formed by the machine.

Current is carried to the rotating tool by means of a custom toolholder, shown in Figure 5.3. The toolholder has copper slip rings that allow for high currents to be transferred to the rotating tool with minimal resistance.

Tests were performed in a Bridgeport GX-480 CNC milling machine. Current was supplied by a Magna-Power TSA5-900 programmable DC power supply with an output range of 0-5 V and 0-900 A. Prior to forming, samples were lubricated with 75W-90 synthetic gear oil. Sheet temperature was measured by thermocouples attached to the underside of the sheet: one at the edge near the toolpath and one
Figure 5.2: Electrical circuit used for tests

Figure 5.3: Custom slip-ring toolholder
near the centre. Temperatures were recorded with an Omega HHM290 temperature sensor.

Maximum wall angles were determined by using a Variable Wall Angle Conical Frustum (VWACF) test, as described by Hussain and Gao [16]. The VWACF is a test shape of increasing wall angle with depth. Tests are stopped upon sheet fracture, and the tool depth is recorded at that point. The maximum wall angle can be determined from the depth of the tool at the point of fracture. The test shape used in this study varied in wall angle from 40° to 90° from horizontal, with a radius of wall curvature of 50 mm and an upper diameter of 178 mm. The VWACF test was favoured as a method of determining formability due to the ability to directly and rapidly compare relative formability changes from one setting to another.

Tests were performed for three diameters of tool: 6.35 mm, 9.57 mm and 12.70 mm. For the 9.57 and 12.7 mm tools, a series of tests were run at currents of 0, 50, 100, 150, 200, 300, 400, 500, 600 and 700 A. A similar set of tests was run with the 6.35 mm tool, however with a maximum current of 500 A to protect the equipment against excessive tool heating. For each set point, three replicates were performed. Further replicates of the 0 and 500 A conditions were performed with the 6.35 mm tool to establish greater significance for each, with a total of 5 replicates at each condition. The number of additional replicates was determined based on estimations of the variance of the samples from the first three tests.

Tests were first performed in order of increasing current, and no cleaning operations were performed on the tool between tests. A first run-through of these tests revealed decreasing wall angle with increasing current. Inspection of the tool tip revealed a degradation of the surface of the tool at the end of the test, with material being deposited on the surface of the tool. To ensure that the tool degradation effects were not producing a false negative result, the second and third repetitions of each set point were performed in random order, and the tool surface was re-machined and polished to a surface roughness $R_a$ of 0.7 µm between each test.
Tests were performed at a feedrate of 1270 mm/min. For the three tools, the spindle speed used was 50, 33 and 25 RPM respectively. These speeds were selected to minimize relative speed between tool rotation and feedrate. The stepdown between each pass was 0.25 mm. To determine the effects of temperature on the process, an additional set of tests were performed with cold air applied to the underside of the sheet. The cold air was supplied by a vortex tube compressed air chiller, and applied to the underside of the sheet so as not to disturb the lubricant.

5.4.1 Constant current density

For a constant current test, the current density varies considerably through the test due to the contact patch increasing in area with wall angle. Variable current density leads to excessively high current densities near the top of the part, and densities that may be too low near the steep sections of the test part.

To better isolate the effects of current density, a simple control system was created in MATLAB to vary the current throughout the duration of the test, ensuring constant current density. The control system reads the z-axis position from the mill, allowing the wall angle and contact patch area to be estimated. The current magnitude for that position is then calculated and used as the power supply set point until a new z-position is read.

5.5 Modelling contact patch area

In order to determine the current density during SPIF, a highly simplified model was created to determine the area of contact between the tool and sheet. While models have been previously presented [22], the model presented in this section aims to minimize computation time, allowing in-process determination of contact area for control. The following model is also designed to use geometry that can be directly measured, allowing experimental validation. Experimental validation of the following
model is an ongoing area of study.

The model presented in this section is an extension of the model created by Hamilton [23]. For the purposes of simplification, the model assumes no springback of the sheet directly behind the tool and no radial deflection of the bulk part, therefore assuming perfect adherence to the tool and path shape.

To calculate area, the depth to which the tool indents into the sheet is first estimated. Measurements of through-sheet thickness taken by Jackson and Allwood [24] showed that the through-sheet thickness of the part directly under the tool is approximately proportional to the final thickness of the wall. The height of indentation $h_1$, is then modeled as the difference in height due to thinning from wall angle $\phi$, as calculated in Equation 5.2. An experimental measurement of this value and creation of an empirical model from measurements is discussed in Chapter 6.

$$h_1 = t_i (1 - \cos \phi) \quad (5.2)$$

The tool is also assumed to not contact the sheet in areas that have been previously formed by earlier tool passes. To account for previous forming passes, the intersection is determined between the tool and previous forming pass. Removing the area due to the previous pass allows tool wrap due to scallop to be easily accounted for. The location of the previous tool pass can be determined using the vertical stepdown, $v$ and using the wall angle to determine the horizontal stepover, $h$, at that point. For simplicity, the previous contact patch is presented in this section as straight, however by modelling the intersection with a curved previous toolpath, the effects of part curvature on contact area can be accounted for.

For a given angle $\psi$ from the axis of the tool, the tool projects a circle of radius $s$ in the horizontal plane. The previous toolpath projects a rectangular section of width $2q$, as shown in Figure 5.5 intersecting the tool circle. Because the centre of the previous toolpath is offset by the horizontal stepover $h$, two angles are used to describe the angle of intersection, $a$ and $b$. 

71
Figure 5.4: Intersection of the tool and the trough left by the previous toolpath. Tool motion is into the page.

For an arbitrary value of the angle $\psi$, the angles $a$ and $b$ are given by Equation 5.3.

$$
\begin{align*}
a &= \sin^{-1} \left( \frac{q - h}{s} \right) \\
b &= \sin^{-1} \left( \frac{q + h}{s} \right)
\end{align*}
$$

(5.3)

Where $q$ is the half-width of the intersecting trough, given by Equation 5.4.

$$
q = \sqrt{r^2 - (r\cos\psi + v)^2}
$$

(5.4)

The area is then calculated for each point in the test according to Equation 5.5. The three discrete components represent the area of the tool below the bottom of the trough where the full half circle of the tool is swept, the area above the trough and below the bottom of the sheet where contact occurs on both sides of the tool, and finally the area above the bottom of the sheet where contact only occurs on the outer side of the tool.
Figure 5.5: Top-down view of the tool at angle $\psi$, with the intersection of the previous toolpath shown. Angle $\psi$ is indicated in Figure 5.4.
Figure 5.6: Creation of a contact patch model

\[ A = \int_0^{\cos^{-1}\left(\frac{r-v}{r}\right)} \pi r s d\psi + \int_{\cos^{-1}\left(\frac{r-h_1}{r}\right)}^{\cos^{-1}\left(\frac{r-h_1}{r}\right)} (\pi - (a + b)) s r d\psi + \int_{\cos^{-1}\left(\frac{r-h_1}{r}\right)}^{\pi/2} \left(\frac{\pi}{2} - b\right) s r d\psi \]  
\[ (5.5) \]

5.6 Results

The wall angle for each test is shown in Figure 5.8. At sheet failure, tool depths were recorded and used to calculate wall angle based on the CAD model and tool diameter. A typical part shape and fracture pattern are shown in Figure 5.7. The current density at that point was calculated using the method in section 5.5. While
current was the independent variable for this test, current density at wall fracture is reported because it allows the results to be normalized for tool diameter, and allows comparison the EAF literature.

To ensure greater statistical significance, further tests were repeated with the 6.35 mm tool at 0 and 400 A (visible in Figure 5.8a as the highest peak for the tools that had been re-machined between tests, near 60 A/mm²). The wall angles from these repeated tests are shown below. An additional set of tests was performed with cold air applied to the underside of the sheet to isolate the effects of the current from resistive heating.

To determine if there is a significant increase in maximum wall angle attributable to sheet temperature, a pair of two-tailed t-tests were performed, each comparing the cooled and non-cooled 400 A tests to the non-electrified (0 A) baseline. Because two separate hypotheses are tested using the baseline data, a Bonferroni correction is applied to the confidence interval, requiring a P-value of 0.025 for rejection at 95% confidence.

Within the confidence bounds described above, a statistically significant difference
Figure 5.8: Wall angle as a function of current density at fracture for tool diameter. Note the effect of regularly remachining the surface of the tool between forming passes.
was found between the non-cooled 400 A tests (M = 69.1, SD = 1.23) and the non-electrified baseline (M = 66.3, SD = 1.41); t(8) = -3.25, p = 0.01. Between the cooled 400 A tests (M = 66.6, SD = 0.99) and the non-electrified baseline tests, however, no significant difference was found; t(8) = -0.31, p = 0.76).

5.6.1 Surface roughness

Among the effects of current is the ability have an effect on the roughness of the inside surface of the part. Figure 5.9 shows surface roughness $R_a$ values for the samples, as a function of current density.

5.6.2 Constant current density

Two sets of tests were performed with constant current density, at 60 A/mm$^2$ and 70 A/mm$^2$, both with a 6.35 mm tool, allowing the results to be compared to the non-electrified baseline. Five repetitions of each set point were carried out to ensure significance of the results, and results were compared to the non-electrified results.
Tests were performed in random order, and the tool was remachined between tests. Figure 5.10 shows the wall angle results for tests at 0, 60 and 70 A/mm².

5.7 Discussion

During constant current tests, the best forming performance from both the 6.35 and 9.53 mm tools occurred when the current density was near 60 A/mm² at fracture. In compression testing, Perkins et al [11] found the threshold current density for 6061-T6511 to be between 54.6-60.8 A/mm². Since for a constant current test the current density decreases with increasing wall angle, this fracture point could be where the current density passed below the threshold value.

To determine if there is indeed a threshold current density effect occurring, constant current density tests were compared to the non-electrified tests using the same statistical method as in section 5.6. Using a two-tailed T-test, no significant difference was found between the 60 A/mm² tests (M = 66.1, SD = 2.49) and non-electrified
baseline wall angle results; $t(6) = -1.42, p = 0.204)$. A significant difference, however, was found between the 70 A/mm$^2$ tests and the non-electrified baseline results; $t(8) = -4.05, p = 0.004$. This difference is visible in Figure 5.10.

The greater variability of the 60 A/mm$^2$ tests is also worth noting. The 60 A/mm$^2$ tests had a standard deviation of 2.49, compared to 1.70 for the 70 A/mm$^2$ tests. Because 60 A/mm$^2$ lies within the threshold range published for this material [11], it is possible that small variations within the current density magnitude result in the actual current density varying from one side to the other of some more exact threshold.

The simplifying assumptions within the contact patch model result in a simulated contact patch area that is smaller than the true area. The actual current density value is therefore slightly lower than the reported current density.

### 5.7.1 Tool fouling

As mentioned earlier, the first set of tests (blue points in Figure 5.8) showed lower wall angles and higher surface roughnesses than subsequent tests. For the first tests, the experiments were performed in ascending order of current magnitude with no remachining of the tool between tests. For subsequent tests, the surface of the tool was remachined between runs, and tests were done in random order to ensure that no tool effects were carried between tests.

The decreased performance from first tests where the tool was not re-machined between tests suggests that the tool surface became degraded in some way from previous tests, a result that is reflected in the work by Meier et al [9]. Figure 5.11 shows the tip of the 6.35 mm tool after forming with applied current, showing material deposited on the surface of the tool. As a result of tool surface degradation, the increased friction between the tool and sheet appears to negate any positive effects from the applied current.
5.7.2 Current and surface roughness

In all tests with current, the roughness was raised above the non electrified baseline, as seen in Figure 5.9. Spalling of the surface also increases with current. To determine if spalling is directly caused by current as opposed to lubricant breakdown due to heating, a test was performed with a constant wall angle (a drawing of the part can be found in Appendix E) and current alternated between 0 and 500 A. The inside surface of the test is shown in Figure 5.12. Spalling at the surface occurred only in the test regions with current applied.

The increase in surface roughness could be indicative of increased tool friction. It is likely that the additional friction acts to negate the formability benefits, resulting in some optimal point for best forming and surface roughness performance. The surface roughness of parts formed with larger tools appeared to be less affected by the current than smaller tools, however larger tools produced a lower initial wall angle.
5.8 Conclusions and future work

An apparatus was constructed to carry large currents to the rotating tool, and the maximum wall angle was tested for several current settings and tool diameters. Formability gains are seen at the same current density for different sizes of tools, suggesting that the current density is the driving factor and not current magnitude. The importance of current density agrees with EAF theory, as resistive heating in the sheet would be largely dictated by current magnitude. Tests with current switched from 0 to 500 A showed a very rapid response in inside surface texture, suggesting surface quality can correlate with current rather than pure thermal effects. Tests with cooling applied to the sheet were inconclusive in determining if the process is independent of temperature.

Samples were formed at constant current density. The formability increase seen between 60 and 70 A/mm² agrees with the published current threshold density for 6061-T6.

Future work in this area will investigate the suitability of this method for forming
exotic materials such as Titanium alloys. Direct experimental measurement of contact area will also be a continued focus, allowing the model above to be validated. Tool fouling will also be investigated, with the eventual goal of producing tool coatings and procedures that prevent tool fouling. The effect of current on surface friction (adhesion and abrasion) and on grain boundaries can also be studied. Finally, strategies to mitigate the frictional increase due to applied current are of great interest. These include lubrication methods and new lubricants, tool materials and modeling of through-thickness current density distribution.

References


Chapter 6

Experimental measurement of contact area in Single Point Incremental Forming

Prelude

The following is a copy of a paper currently submitted for consideration in IMECH\(E\) Part B: Journal of Engineering Manufacture. As this is a standalone paper, some figures may be repeated from other chapters.

Abstract

The contact zone between the forming tool and sheet is of great importance to Single Point Incremental Forming. While many previous models have been proposed, no direct measurement of the contact patch geometry with experimental results has been published. The following paper presents a method of calculating the contact area based on directly measured features. Direct measurements of geometry surrounding the contact zone are taken, allowing contact area to be inferred. An empirical model is presented for contact area based on the measurements taken, returning contact
area as a function of tool diameter, wall angle and step size.

6.1 Nomenclature

\( h_1 \) Depth of indentation into sheet  
\( h \) Horizontal stepover  
\( q \) Half-width of intersection between tool and previous toolpath  
\( t_f \) Final through-wall thickness  
\( s \) Radius of the tool projection in the XY plane  
\( t_i \) Initial sheet thickness  
\( r_t \) Radius of a trough from a previous tool pass  
\( \phi \) Wall angle from horizontal  
\( \phi_{max} \) Maximum achievable wall angle  
\( \psi \) Angle from tool axis  
\( P \) Perimeter of contact area

6.2 Introduction

Single Point Incremental Forming (SPIF) is a sheet metal forming method that allows for complex custom and low production parts to be formed at low cost, and to very high strains [1]. Unlike conventional forming methods such as stamping, deep drawing and spinning, SPIF does not require a die, forming the part instead based on the motion of a small, generic tool [2]. SPIF is ideal as a process for prototyping and custom work because it can make complex parts with very short lead times and at low cost. SPIF also has a low startup cost because it can be easily implemented in a commercially available CNC mill with very little modification.
Because deformation in SPIF occurs in a small area around the tool, formability and tribological factors are dependent on the contact conditions between the tool and sheet. Understanding and being able to model the size and shape of the contact patch is therefore useful for modeling and predicting the performance of SPIF. While models have been proposed previously to describe the contact geometry [3–6], no direct measurements of the contact area have been published to validate these models.

Recently, electric resistance heating has been employed to improve formability in SPIF by passing large electrical current through the tool during forming [7–10]. Because the amount of heating that can be done by a given current depends heavily on the cross sectional area that the current flows through, modeling contact area is very important to predict the amount of formability change due to current. While it may be possible to detect relative changes in contact area due to resistance change, this does not allow for an absolute reference value of contact area to be established.

Similar methods have been proposed [11] and tested [12] that rely on direct electron interaction with dislocations to improve the formability beyond what is observed by thermal heating alone. Because electrically assisted forming methods rely heavily on current density, understanding the contact area is vital to forming at a material’s optimal forming parameters.

In the following work a method is proposed to determine the area of contact using measurable features from the surrounding geometry. A series of measurements are taken, and an empirical model is presented allowing contact area and shape to be estimated based on tool diameter, wall angle and stepdown.

6.3 Background

Modeling the area of contact represents a challenge due to the combination of large deformations of the material including elastic recovery in the area around the tool and thickness variations of the sheet due to a wide variety of factors [13]. Additionally,
there is no direct method of measuring area on a non-planar shape such as the contact patch.

Methods have been presented to determine contact area and force distribution by means of Finite Element Analysis \[4,6\], as well as the upper bound approach \[14\]. These methods, however, are computationally expensive, and have not yet been directly validated through experimental measurements.

A simplified model of contact area based purely on geometrical considerations was created by Hamilton in his Master’s thesis at Queen’s University \[15\]. The model accounted for indentation as well as wrap on the side of the tool due to wall angle. A similar model for indentation that accounts for tool wrap on the inside of the toolpath was proposed by Aerens et al \[5\] and used to model the contact zone with minimal computational expense. The wrap around the inside of the tool, as published by Aerens et al \[5\], visible in Figure 6.1 as $\gamma$, is modeled empirically by equation 6.1

$$\gamma = 17.2 \left(\frac{d_t}{10}\right)^{-c}$$

Where $c = 2.54$ for aluminium alloys and $c = 1.20$ for AISI 304. Both Hamilton’s and Aerens’ models present the contact area as a ribbon lying directly in the tool path. As a result of this assumption, the models do not account for tool curvature in the direction of the toolpath. To produce the most accurate model possible, a new geometrical model is presented, created by removing segments known to not be in contact from a hemisphere representing the tool.
6.4 Modeling the contact area

The following model is developed to create an accurate, easy to compute model for contact area. Rather than focus on directly modeling area, several features are used to construct an area model, each of which can be independently measured and modeled. The overall process of creating this contact surface is outlined in Figure 6.2.

During forming, the tip of the tool is indented slightly into the sheet. To make measurement easier, indentation is stated in this paper, as in Hamilton’s work [15], as depth of indentation, \( h \), as shown in Figure 6.3. Above the plane of the sheet no contact occurs on the in-side (negative r-direction) of the toolpath.

As the tool moves along the part, a cylindrical trough is left behind the tool from where the material has been formed into shape. During fully established forming a trough is also present in front of the tool, left from the previous forming path. Because the tool cannot be in contact with the sheet inside these troughs, the cylindrical section may be removed from the tool hemisphere, leaving a ribbon-shaped section of the hemisphere where it contacts the sheet, visible in Figure 6.3. The remaining shape is the contact patch, accounting for wall angle, scallop due to stepdown as well
Figure 6.2: Development of the contact area by removing areas that are not in contact with a tool hemisphere
as springback effects. By using a curved trough intersecting the tool, part curvature can easily be accounted for.

6.5 Numerical Derivation

The contact area is calculated by integrating the perimeter of the tool at angle $\psi$ (see Figure 6.4 for a definition of $\psi$) from the tool axis. At a given angle $\psi$, the tool projects a circle of radius $s$ in the plane of the sheet, as shown in Figures 6.4 and 6.5. Calculation of area is split into two halves: one half in front of the tool axis, and the other half behind the tool axis. These are done separately as they are intersected by different troughs, representing the previous and current toolpaths. To calculate the total area, the integral is performed twice, once for the leading edge and once for the trailing edge. For each half, the tool is intersected by a cylindrical shape in the r-z plane of radius $r_t$ and offset from the tool centre by $h$ horizontally and $v$ vertically.

For a given angle $\psi$, the trough projects a straight-walled cut through the tool in
the horizontal plane, producing a rectangular projection of width $2q$. The perimeter $P$ of the tool intersection for a given step is therefore calculated using Equation 6.2 the following function:

$$P = s(\pi - (a + b))$$  \hspace{1cm} (6.2)$$

The angles $a$ and $b$ represent the angle from the path of the tool to either edge of the trough where it intersects the periphery of the tool. For the section of the tool below the bottom of the trough, $a$ and $b$ are both equal to 0. The angles $a$ and $b$ are given by Equation 6.3

$$a = \sin^{-1}\left(\frac{q - h}{s}\right) \hspace{1cm} b = \sin^{-1}\left(\frac{q + h}{s}\right)$$  \hspace{1cm} (6.3)$$

The angles $a$ and $b$ are a function of the width $q$ of the trough at a given $\psi$ as well as the horizontal distance $h$ between the centre of the tool to the centre of the
trough. The instantaneous radius $q$ of the intersecting trough is calculated as:

$$q = \sqrt{r_{\text{trough}}^2 - (r_{\text{trough}} - (r_{\text{trough}} - r\cos\psi + z_t))^2}$$  \hspace{1cm} (6.4)$$

In the above equation, $z_t$, shown in Figure 6.4, is the height of the bottom of the trough above the bottom of the tool. Area is determined in three discrete steps: below the trough, above the trough but below the plane of the top of the sheet, and above the sheet. Below the bottom of the trough the area is calculated by the following integral:

$$A = \int \frac{\cos^{-1}(r-v)}{r} \pi s r d\psi$$  \hspace{1cm} (6.5)$$

Above the bottom of the trough, the area is calculated as:
Finally, above the plane of the sheet bottom, only the side nearest the wall is in contact. This is accounted by reducing the angle to which the instantaneous radius $s$ is swept. Correspondingly only one side of the trough is intersected. The final stage of the area integral is:

$$A = \int_{\cos^{-1}\left(\frac{r-a}{r}\right)}^{\cos^{-1}\left(\frac{r-b}{r}\right)} \left(\pi - (a + b)\right) s r d\psi$$  \hspace{1cm} (6.6)

It should be noted that while the upper limit to this integral is listed as $\psi = \pi/2$, the area added by a section will become zero prior to that when $b = \pi/2$. This represents the point at which the wall no longer contacts the tool.

### 6.6 Experimental Method

To evaluate the contact area, a $2^3$ full factorial experimental design was implemented, varying the tool diameter, wall angle and stepdown size at two levels each. The test shape was a four-sided pyramidal frustum, chosen because motion along the sides is directly in line with the machine axes. During forming the tool was stopped at a known position, and measurements were taken to establish the plane of the bottom area of the part, and the location and diameter of the leading and trailing troughs. Measurements were taken with the tool in place to ensure that the conditions during measurement are as close as possible to those during forming.

Tests were performed in a Bridgeport GX480 CNC mill, fitted with a custom blankholding fixture. Measurements of the troughs and sheet were then taken using a FARO Platinum measurement arm (shown in Figure 6.6) fitted with a point probe and Cam 2 Measure 10 software.

For each part, measurements were first taken to establish the blank holder coordinate system, then measurements of the bottom plane and troughs ahead and
Figure 6.6: FARO measurement arm

Figure 6.7: Treatments for a full factorial $3^2$ design of experiment
behind the tool are measured after forming. Features such as circles and planes were constructed based on a best fit to a series of points taken from the arm. The measurements taken are outlined in Figure 6.8. Measurements of contact area features are taken with the tool in place on the part, ensuring that the measurements taken most accurately reflect the shape of the contact area during forming.

For this study, measurements with a hard probe were favoured over using a laser line scanner to produce a point cloud, because a point cloud would require defining clear boundaries of the contact patch, introducing an additional source of error. Additionally, using the feature based method above allows several simple models to be created and independently measured and tested, rather than only having a single value for area.

Before forming, the fixture coordinate system was established by holding the probe on the fixture and moving the mill x and y axes to establish a plane. Next, the tool is moved to its final position in the program, and the x and y axes are located. Hence,
all measurements are reported in the coordinate system of the part fixture, allowing the tool position as reported by the mill to be directly compared to position values from the FARO arm.

To establish the $z$-position of values accurately, tools are first machined in the mill spindle against a turning tool fixed to the forming rig. A flat ended reference tool is then machined against the same tool. The reference tool is then moved to a known distance above the tool compensation plane, allowing a plane to be measured with a known offset to the tool compensation plane.

A set of tests were performed, varying tool diameter, stepdown and wall angle. A full factorial experiment was designed, with two replicates at each point for a total of 16 tests. Tests were performed in random order. Tool diameter was varied as either 6.35 mm or 12.7 mm, as these are within the range of most commonly seen tool diameters. Stepdown was either 0.127 mm or 0.635 mm. Finally, wall angle was varied between 40 and 60 degrees. Tests were performed on AA 3003-O with an initial thickness of 1.57 mm. Sheet material and thickness was kept constant in order to reduce the samples required while having greater sample accuracy.

6.7 Results and Analysis

6.7.1 Total Area

The area was calculated from the measured geometry using the method described earlier. The final calculated contact area for each test is summarized in Table 6.1.

6.7.2 Indentation Depth

Because both the height of the bottom plane and the tip of the tool are known with respect to the tool compensation plane, the depth of tool indentation below the sheet can be calculated using the following equation:
Table 6.1: Area results for the samples tested

<table>
<thead>
<tr>
<th>Test</th>
<th>Tool Diameter (mm)</th>
<th>Wall Angle</th>
<th>Stepdown (mm)</th>
<th>Contact Area (mm²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>6.35</td>
<td>40</td>
<td>0.127</td>
<td>3.64</td>
</tr>
<tr>
<td>2</td>
<td>12.7</td>
<td>40</td>
<td>0.127</td>
<td>4.89</td>
</tr>
<tr>
<td>3</td>
<td>6.35</td>
<td>60</td>
<td>0.127</td>
<td>1.18</td>
</tr>
<tr>
<td>4</td>
<td>12.7</td>
<td>60</td>
<td>0.127</td>
<td>14.99</td>
</tr>
<tr>
<td>5</td>
<td>6.35</td>
<td>40</td>
<td>0.635</td>
<td>2.49</td>
</tr>
<tr>
<td>6</td>
<td>12.7</td>
<td>40</td>
<td>0.635</td>
<td>12.43</td>
</tr>
<tr>
<td>7</td>
<td>6.35</td>
<td>60</td>
<td>0.635</td>
<td>3.83</td>
</tr>
<tr>
<td>8</td>
<td>12.7</td>
<td>60</td>
<td>0.635</td>
<td>9.9</td>
</tr>
<tr>
<td>9</td>
<td>6.35</td>
<td>40</td>
<td>0.127</td>
<td>3.52</td>
</tr>
<tr>
<td>10</td>
<td>12.7</td>
<td>40</td>
<td>0.127</td>
<td>8.40</td>
</tr>
<tr>
<td>11</td>
<td>6.35</td>
<td>60</td>
<td>0.127</td>
<td>1.87</td>
</tr>
<tr>
<td>12</td>
<td>12.7</td>
<td>60</td>
<td>0.127</td>
<td>2.58</td>
</tr>
<tr>
<td>13</td>
<td>6.35</td>
<td>40</td>
<td>0.635</td>
<td>8.91</td>
</tr>
<tr>
<td>14</td>
<td>12.7</td>
<td>40</td>
<td>0.635</td>
<td>14.87</td>
</tr>
<tr>
<td>15</td>
<td>6.35</td>
<td>60</td>
<td>0.635</td>
<td>6.86</td>
</tr>
<tr>
<td>16</td>
<td>12.7</td>
<td>60</td>
<td>0.635</td>
<td>12.68</td>
</tr>
</tbody>
</table>

Table 6.2: ANOVA results for indentation depth.

<table>
<thead>
<tr>
<th>Source</th>
<th>Sum Sq.</th>
<th>d.f.</th>
<th>Mean Sq.</th>
<th>F</th>
<th>Prob &gt; F</th>
</tr>
</thead>
<tbody>
<tr>
<td>diameter</td>
<td>0.04007</td>
<td>1</td>
<td>0.04007</td>
<td>1.94</td>
<td>0.1716</td>
</tr>
<tr>
<td>angle</td>
<td>0.21153</td>
<td>1</td>
<td>0.21153</td>
<td>10.25</td>
<td>0.0108</td>
</tr>
<tr>
<td>stepdown</td>
<td>0.32526</td>
<td>1</td>
<td>0.32526</td>
<td>15.76</td>
<td>0.0033</td>
</tr>
<tr>
<td>diameter*angle</td>
<td>0.07517</td>
<td>1</td>
<td>0.07517</td>
<td>3.64</td>
<td>0.0887</td>
</tr>
<tr>
<td>diameter*stepdown</td>
<td>0.01197</td>
<td>1</td>
<td>0.01197</td>
<td>0.58</td>
<td>0.4657</td>
</tr>
<tr>
<td>angle*stepdown</td>
<td>0.00021</td>
<td>1</td>
<td>0.00021</td>
<td>0.01</td>
<td>0.9228</td>
</tr>
<tr>
<td>Error</td>
<td>0.18573</td>
<td>9</td>
<td>0.02064</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>0.84995</td>
<td>15</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

\[ h_1 = \text{bottom plane depth} - \text{final tool depth} \]  \tag{6.8}

To determine the contribution of each factor to the depth of indentation, a 2-way analysis of variance (ANOVA) is used, using coded factors for the variables. The results from this ANOVA are shown in Table 6.2.

The ANOVA results show that that for a confidence interval of 95% there is a significant dependence on wall angle and stepdown, while there are no significant interactions or dependence on diameter. Based on these results an empirical model was created using a least squares fit. The indentation depth is approximated based on the wall angle \( \phi \) and stepdown \( v \) according to equation 6.9.
By restating Equation 6.9 to express the tool wrap due to indentation $\gamma$ allows a direct comparison to the model presented by Aerens et al [5]. Figure 6.9 shows the indentation height as predicted by both the current model and the one presented by Aerens et al, as compared to the measured values.

\[
h = 0.8494 - 0.0115\phi + 0.5913v \tag{6.9}
\]

\[
\gamma = \cos^{-1}\left(\frac{d/2 - (0.8494 - 0.0115\phi + 0.5913v)}{d/2}\right) \tag{6.10}
\]

### 6.7.3 Trough Position

To determine the effect of the variables on springback, the amount of recovery in the z-direction of the centre of each trough was examined. Two way ANOVA tests were performed for the trailing edge trough on all parts to get vertical recovery as well as diameter change. The results are shown in Tables 6.3 and 6.4.
The height of the leading trough correlates most strongly with the stepdown, as this variable determines the main position of the previous tool pass. A correlation was also found with tool diameter, perhaps due to the wider force distribution pulling the leading material down.

For height recovery of the trailing trough, departure from tool position was found to correlate only with diameter. This effect could once again be due to the larger tools producing a wider stress distribution.

Using a least-squares fit, the height of the leading and trailing troughs were modeled, given in equations 6.11 and 6.12:

\[ h_{\text{leading}} = -0.1247 + 0.0198d + 0.5888v \]  \hspace{1cm} (6.11)

\[ h_{\text{trailing}} = -0.3705 + 0.0263d \]  \hspace{1cm} (6.12)
Table 6.5: ANOVA results for the difference between leading trough diameter and tool diameter

<table>
<thead>
<tr>
<th>Source</th>
<th>Sum Sq.</th>
<th>d.f.</th>
<th>Mean Sq.</th>
<th>F</th>
<th>Prob &gt; F</th>
</tr>
</thead>
<tbody>
<tr>
<td>tool diameter</td>
<td>0.0118</td>
<td>1</td>
<td>0.0118</td>
<td>0.07</td>
<td>0.7995</td>
</tr>
<tr>
<td>wall angle</td>
<td>11.0484</td>
<td>1</td>
<td>11.0484</td>
<td>64.12</td>
<td>0</td>
</tr>
<tr>
<td>stepdown</td>
<td>0.9401</td>
<td>1</td>
<td>0.9401</td>
<td>5.46</td>
<td>0.0443</td>
</tr>
<tr>
<td>diameter*angle</td>
<td>1.2252</td>
<td>1</td>
<td>1.2252</td>
<td>7.11</td>
<td>0.0258</td>
</tr>
<tr>
<td>diameter*stepdown</td>
<td>0.1495</td>
<td>1</td>
<td>0.1495</td>
<td>0.87</td>
<td>0.376</td>
</tr>
<tr>
<td>angle*stepdown</td>
<td>0.1441</td>
<td>1</td>
<td>0.1441</td>
<td>0.84</td>
<td>0.3843</td>
</tr>
<tr>
<td>Error</td>
<td>1.5508</td>
<td>9</td>
<td>0.1723</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>15.0698</td>
<td>15</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 6.6: ANOVA results for trough diameter of the trailing trough

<table>
<thead>
<tr>
<th>Source</th>
<th>Sum Sq.</th>
<th>d.f.</th>
<th>Mean Sq.</th>
<th>F</th>
<th>Prob &gt; F</th>
</tr>
</thead>
<tbody>
<tr>
<td>tool diameter</td>
<td>0.008</td>
<td>1</td>
<td>0.00805</td>
<td>0.02</td>
<td>0.8911</td>
</tr>
<tr>
<td>wall angle</td>
<td>7.872</td>
<td>1</td>
<td>7.87201</td>
<td>19.42</td>
<td>0.0017</td>
</tr>
<tr>
<td>stepdown</td>
<td>1.6035</td>
<td>1</td>
<td>1.60349</td>
<td>3.96</td>
<td>0.0779</td>
</tr>
<tr>
<td>diameter*angle</td>
<td>2.4939</td>
<td>1</td>
<td>2.4939</td>
<td>6.15</td>
<td>0.035</td>
</tr>
<tr>
<td>diameter*stepdown</td>
<td>0.0243</td>
<td>1</td>
<td>0.02427</td>
<td>0.06</td>
<td>0.8122</td>
</tr>
<tr>
<td>angle*stepdown</td>
<td>0.2676</td>
<td>1</td>
<td>0.26759</td>
<td>0.66</td>
<td>0.4375</td>
</tr>
<tr>
<td>Error</td>
<td>3.6481</td>
<td>9</td>
<td>0.40534</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>15.9174</td>
<td>15</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

6.7.4 Trough Diameter

To investigate trough diameters, a two-way ANOVA test was performed, comparing the difference between the measured trough diameter and measured tool diameter for each test. The results of the test and diameter model are shown in tables 6.5 and 6.6.

Both leading and trailing troughs had diameters larger than the tool that formed them. Likely the diameter increase is due to springback in the material, causing the pillow effect in the bottom of the sheet, as explained in [16]. It is worth noting, however, that the troughs measured did generally retain a circular shape, having an average form deviation of 0.049 mm, approximately 0.8% of the diameter of the smallest tool used. The leading and trailing trough diameters are modeled by equations 6.13 and 6.14.
\[ D_{\text{leading}} = 0.5713 - 0.0272 \left( \frac{d_t - 9.525}{3.175} \right) - 0.8310 \left( \frac{\phi - 50}{10} \right) - 0.2424 \left( \frac{v - 0.3810}{0.2540} \right) \]
\[ \quad - 0.2767 \left( \frac{d_r - 0.9525}{3.175} \right) \left( \frac{\phi - 50}{10} \right) \] (6.13)

\[ D_{\text{trailing}} = 0.5275 - 0.7015 \left( \frac{\phi - 50}{10} \right) - 0.3948 \left( \frac{d_t - 9.525}{3.175} \right) \] (6.14)

6.8 Discussion

Due to the relatively small features measured, a sharp probe was used on the FARO arm to maximize the distance traveled relative to the measurement error. The volumetric accuracy of the arm is stated as ± 0.073 mm. While the probe was made from AISI 4140 and hardened to 60 RHC, wear was still present on the probe tip after several measurements. To prevent error from accumulating, the tip was ground sharp between each test and re-calibrated using a spherical calibration target.

Prior to forming parts, measurements were taken of the blankholder reference geometry at the final tool position in the program. Doing so allows a datum to be established, allowing tool position from the mill to be aligned with measurements taken from the FARO arm. The assumption in this method, however, is that the coordinate system used by the mill is perfectly aligned with the blankholder. A more ideal system for future versions of this work may be to use the mill to cut reference features into the blankholder, ensuring perfect alignment with the machine.

6.8.1 Trailing trough contribution to area

Upon comparing the height of the trailing trough bottom to the bottom of the tool, it is noticeable that in nearly all cases the bottom of the trough is below the bottom of the tool. When this is coupled with a trough diameter that is larger than the tool, the area contribution is seen to be zero. Images of the contact area (see Figure
6.3) seem to agree with this finding, as the leading edge of the contact area tends to be curved with wrap around the front of the tool while the trailing edge is straight. The straight edge is indicative of the sheet remaining in place, and not wrapping around the tool.

6.9 Conclusion

A method has been devised to calculate contact area based on measurable geometry. Using a full factorial experimental design with three factors at two levels each, tests were made on a series of samples of AA 3003-O with an initial thickness of 1.57 mm. Using measurements taken with a probe, the area of contact was calculated for each part.

Contact behind the tool was found to be zero or nearly zero in all cases, suggesting little recovery occurs behind the sheet. The lack of contact is visible in Figure 6.3 as the straight line in the trailing direction. Additionally, the diameters of the trough left by the tool are larger than the tool. The depth of indentation of the tool below the sheet was found to be a function of wall angle, and predominantly vertical step size.

References


Chapter 7

Tool shapes in Single Point Incremental Forming: an Experimental study

Prelude

The following is a paper that has been provisionally accepted for publication to the IMEچE Part B: Journal of Engineering Manufacture. The work was originally presented at the North American Manufacturing Research Conference 2013.

Abstract

To improve the forming limits and expand the range of potential applications of SPIF, testing was performed on a variety of tool heads, with emphasis on non-standard tool profiles. Ten tool designs have been investigated by determining the maximum wall angle that can be formed from AA 3003-O with an initial thickness of 1.57 mm. The results have been compared to previous results using conventional tool shapes. Among the non-standard shapes tested, three shapes of parabolic tool were tested, with the most pointed tool showing wall angle performance comparable to that of
a hemispherical tool but lower interior surface roughness. Tool with flat sides at 60°, 70° and 80° from the tool axis were also tested and found to have performance approaching that of flat-ended tools as angle increases.

7.1 Introduction

Single Point Incremental Forming (SPIF) is a highly flexible sheet metal forming process whereby parts are formed by the motion of a generic, small tool [1]. Unlike conventional sheet metal forming processes such as stamping, spinning and shear forming, SPIF does not require a die. Because SPIF is a dieless process, it is ideally suited for forming bespoke complex parts for low cost and with short lead times.

The diameter and shape of the tool used in SPIF has long been known to have a large effect on the formability [1,2] and surface roughness. Despite this information, however, nearly all SPIF literature has used hemispherical tools for all forming operations, the notable exception being a study by Ziran et al. [3] using flat-ended tools, and a study by Allwood et al. [4] employing a paddle-shaped tool.

The objective of this research is to further develop the understanding of how SPIF tool profiles affect the forming characteristics by experimentally testing several tool shapes. Special focus has been given to tool profiles which have not yet been demonstrated. A thorough understanding of the effects of tool shape on formability and surface quality will allow end-users of SPIF to select the best tool for each particular set of forming conditions and requirements.

This investigation compares maximum wall angles, measured by forming a test shape of gradually increasing wall angle until fracture occurs in the wall, for several tool designs. Tools are also compared in terms of surface roughness, and qualitative effects such as ridge formation on the lower surface.
7.2 Background

7.2.1 Single Point Incremental Forming

Toolpaths for SPIF are generated using commercially available CAM software designed for machining toolpaths, and consist of a series of 2-dimensional contours around the outer periphery of the part. Between each contour, the tool is moved downward by a constant depth, \( v \). This toolpath is executed by a 3+axis CNC mill equipped with a tool designed to form material rather than remove it. A backing plate is used to support the underformed exterior of the sheet in order to improve shape retention. The final part is thus formed as a result of many small deformations occurring near the point of contact.

During forming, the wall thickness is decreased due to a combination of biaxial stretching and through-thickness shear. Most often the wall thickness is approximated as a function of the cosine of the wall angle, \( \phi \), as shown in Equation 7.1.

\[
t_f = t_i \cos \phi
\]  

While SPIF has been shown to have a higher formability than traditional stamping, the forming limit remains present as a maximum wall angle that can be formed in a single pass due to the thinning limits mentioned above. While it is possible to form parts past the maximum wall angle using multi-stage toolpaths, significant cycle time and complexity is added to the process. Optimizing the tooling to produce the highest possible wall angle in a single pass therefore allows high wall angle parts to be made with greater confidence and in the shortest time frame possible.

7.2.2 Deformation modes

Studies investigating the plastic deformation occurring at the contact point indicate deformation takes the form of uniform stretching without necking until the fracture limit is reached. It was also found that for high angles, the longitudinal stress
was far higher than the hoop stress, while at lower angles hoop stress has a larger impact [11].

Fracture patterns within the part typically have three main shapes: vertical fracture, horizontal fracture, and zig-zag fracture [11]. While vertical fracture relates to hoop stress exclusively and horizontal fracture relates to longitudinal stress, zig-zag fracture is a combination thereof. By analyzing the pattern of failure of the sheets as a result of each tool, inferences may be made as to the dominating stress state.

7.2.3 Variable Wall angle Conical Frustum Test

The Variable wall angle conical frustum test (VWACF) uses a gradually increasing wall angle to determine the maximum wall angle due to thinning limits [12]. Parts are then compared in terms of the wall angle at which failure occurs in the sheet. Because neck formation is suppressed during forming with SPIF [6,7], the wall angle at which failure occurs is a useful comparison of relative changes in forming limits.

7.3 Experimental Procedure

To evaluate the performance of a variety of tool profiles, parts were formed from AA 3003-O with an initial thickness of 1.59 mm. Tools were machined from ASTM A681 tool steel, following the profiles in Figure 7.1. Tools were machined in place in the mill spindle to ensure concentricity, and polished to an average surface roughness $R_a$ of 0.7 $\mu$m. The lubricant used was 75w90 synthetic gearbox oil: kinematic viscosity 193.2 Pa s @ 40 °C, (ASTM D-445) viscosity index 180 (ASTM-2270); density 0.87 kg/m$^3$ @ 15°C (ASTM D 1298). Tools of various shapes (shown in Figure 7.1) were used to form a modified conical frustum test and the maximum wall angle was determined from the failure point.

A series of three trials per profile were conducted, each with a spindle speed of 200 RPM, feed rate of 4064 mm/min, and step-down of 0.254 mm. While formability
Table 7.1: Tool profiles tested

<table>
<thead>
<tr>
<th>Tool Type</th>
<th>Parameter/Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Angle r = 2.54 mm</td>
<td>( \phi = 60^\circ )</td>
</tr>
<tr>
<td></td>
<td>( \phi = 70^\circ )</td>
</tr>
<tr>
<td></td>
<td>( \phi = 80^\circ )</td>
</tr>
<tr>
<td>Flat D = 12.7 mm</td>
<td>( r = 5.08 \text{ mm} )</td>
</tr>
<tr>
<td></td>
<td>( r = 2.54 \text{ mm} )</td>
</tr>
<tr>
<td>Hemispherical</td>
<td>( D = 5.08 \text{ mm} )</td>
</tr>
<tr>
<td></td>
<td>( D = 10.16 \text{ mm} )</td>
</tr>
<tr>
<td>Parabolic D = 12.7 mm</td>
<td>( y = x^2 )</td>
</tr>
<tr>
<td></td>
<td>( y = 5x^2 )</td>
</tr>
<tr>
<td></td>
<td>( y = 10x^2 )</td>
</tr>
</tbody>
</table>

increases with decreasing feedrate [2], the effect is small, so the feedrate was selected to minimize process time. Step size has been shown to affect surface roughness [13]. The step size was therefore selected to minimize surface roughness due to scallop from tool stepover, allowing friction-based surface roughness effects to more easily be observed.

7.3.1 Tool shapes tested

The shape of the tools that were tested is shown in Figure 7.1. The hemispherical and flat-ended tools were used to allow comparison to previously published results with similarly shaped tools. For all tools tested, the parameters used are summarized in Table 7.1.

Angled tools (far left in Figure 7.1) were created to determine the importance of the centre part of the tool versus the outer section. By reducing the angle \( \phi \), the central protrusion becomes more prominent, shifting contact toward the centre of the tool.

Parabolic tools were tested because they have a constantly varying curvature with distance from the tool axis. Varying the curvature along the tool allows tools to be made that have localized deformation in small areas supporting the sheet in other areas.
7.3.2 Formability Test

When toolpaths are generated, the CAM software generates tool positions based on placing the tool tangent to the target surface, a process called tool compensation. The software used (MasterCAM™) was not able to perform tool compensation on shapes other than the flat and hemispherical tools. To allow non-standard tools to be tested, a new test shape was created, using discrete sections of constant wall angle, referred to as the Variable Wall Angle Step Test (VWAST). The test part shape is shown in Figure 7.2.

During this test a 0.254 mm step down was used, and the length of each angle section is 6.35 mm. The tool therefore makes 25 passes at each prescribed angle,
eliminating doubt as to the failure point, at the cost of a maximum wall angle resolution of $\pm 2.5^\circ$.

Failure depths were recorded from the mill as the z-position of the tool when the program was stopped. Failure can be both heard and observed visually. Failure does not occur at the tip of the tool, rather at the tangent contact point. The effect of this on failure angle calculation is negated through the use of the VWAST, as the angle at which the tip travels is equivalent to the angle at which any point on the profile will travel for constant angles. As such, failure angles recorded using this method are accurate to within $\pm 2.5^\circ$.

### 7.4 Results and Discussion

#### 7.4.1 Forming limits

Figure 7.3 shows the wall angle results for the two flat-ended tools tested, as compared to the results published by Ziran et al. [3]. The results show a similar trend toward higher wall angles with a tighter connecting radius, however because only two tool shapes were tested the decrease in forming with very small connecting radius was not observed. Differences in the actual values of wall angle are attributable to the different initial thickness of the material formed by Ziran et al. as well as the resolution of the VWAST used in this study.

Failure angles recorded for each tool are shown in Figure 7.4. Note that the coloured sections in the background of Figure 7.4 reflect the boundaries of the constant wall angle sections present in the VWAST. Results within a constant colour bar in Figure 7.4 are therefore considered to be equivalent due to the resolution of the VWAST.

In initial tests of the parabolic tools, some spalled material stayed within the lubricant, resulting in a dramatic reduction of maximum wall angle due to increased friction. To reduce the risk of this issue, subsequent tests were performed where
Figure 7.3: Hemispherical and flat-ended tools compared with the results of Ziran et al. [3]

Figure 7.4: Wall angle results. Coloured regions indicate the discrete regions of constant wall angle. Height of bars indicate actual depth of the tool at sheet fracture.
spalled material was removed before continuing the test.

7.4.2 Surface Finish

The surface roughness of the inner wall of formed samples was measured with a Hommel T 500 surface roughness tester. The surface roughnesses of the samples are shown in Figure 7.5. The smoothest profiles were produced by the 10x² tool, shown as the parabolic tool furthest from the left in Figure 7.1. A general trend was observed toward lower surface roughness with tools that favoured large, low curvature tangential contact surface. Early passes with the 10x² profile were very rough while later passes ran smoothly with even lubricant distribution. The hemispherical 5.08 mm profile fared slightly better than the x² and 5x².

7.4.3 Pitting

Pitting, visible in Figures 7.6 and 7.7, was observed in the walls of several samples. Though no quantitative measurements were taken for pit density or severity, the amount of pitting appears to increase with a smaller starting contact area. One possible explanation for the pitting could be that fragments of aluminium join the lubricant and periodically pass into the contact area. Pitting appeared more prevalent
with the tools with smaller initial contact area, possibly because they create a larger ridge and more chips at entry, producing more material to foul the lubricant.

Parts formed with the 60° angled tool showed vertical stretch marks on the inner surface, increasing in intensity as the wall angle increases. These are shown in Figure 7.8. For samples formed with the 70°, 80°, flat-end, and hemispherical 2.54 mm tools, patterns of horizontal fissures emerge at approximately one third of the depth and continue until failure point. These marks are most pronounced in samples formed with the 70° tool.

Pitting was also observed on the flat-end 5.08 mm, hemispherical 5.08 mm, $x^2$, $5x^2$, and $10x^2$ profiles. The most severe incidence occurred on hemispherical 2.54 mm and 10x² samples.

7.4.4 Ridging

The build-up of residual material, visible in Figure 7.9, at the centre of the SPIF sample was observed when forming with tools with tight curvature near the tip, as they produce large forces in the negative radial direction.
Figure 7.7: Close-up view of pits in the wall of a sample.

Figure 7.8: Vertical fissures observed on the inner wall of a sample formed with the 60° tool.
The degree of ridging observed decreases as the tool profile approaches flat-ended, such that the hemispherical, 10x², and 60° samples have the most severe, the x² and 80° have the least, and the flat-ended profiles do not exhibit any immediately measurable ridging. Among these, the 10x² profile caused the highest degree of ridging, detaching a large ridge section from the sample during the test, shown in Figure 7.9. Likely this ridging has to do with the amount of indentation into the sheet, but do not seem to be substantially linked to formability.

7.4.5 Failure Pattern

Among samples tested, failure occurred in all three of the previously mentioned manners, and are shown in Figure 7.10. The variable fracture shapes indicate that the tool may have some effect on the stress distribution within the formed sample. Additionally, while friction may not be the sole cause for premature fracture, it could be a strong contributor. The fracture patterns observed for each tool are summarized in Table. By far the zig-zag fracture shape was the most common, suggesting combined axial and radial loading. Vertical fractures were observed with the highly pointed 10x² tool, while horizontal fractures were seen most often with the flat and
Figure 7.10: Fracture patterns observed: A: flat, b: zig-zag and c: longitudinal.

Table 7.2: Fracture patterns observed in samples after forming

<table>
<thead>
<tr>
<th>Hemispherical</th>
<th>Flat</th>
<th>Angled</th>
<th>Parablic</th>
</tr>
</thead>
<tbody>
<tr>
<td>5 mm</td>
<td>10 mm</td>
<td>2.54 mm</td>
<td>5.08 mm</td>
</tr>
<tr>
<td>zig-zag</td>
<td>vertical</td>
<td>zig-zag</td>
<td>horizontal</td>
</tr>
<tr>
<td>zig-zag</td>
<td>vertical</td>
<td>horizontal</td>
<td>zig-zag</td>
</tr>
<tr>
<td>zig-zag</td>
<td>horizontal</td>
<td>zig-zag</td>
<td>horizontal</td>
</tr>
<tr>
<td>zig-zag</td>
<td>vertical</td>
<td>horizontal</td>
<td>zig-zag</td>
</tr>
<tr>
<td>zig-zag</td>
<td>horizontal</td>
<td>zig-zag</td>
<td>horizontal</td>
</tr>
</tbody>
</table>
| low angle tools. The flat fracture shape seen with the flatter tools suggests that the flat shape contributes to larger axial stress, while the pointed shape of the 10x2 tool contributes to larger meridional stress. As the corner radius of the flat ended tool is increased, the fracture pattern appears to trend toward a zig-zag pattern, suggesting that the larger radius is contributing to a higher degree of radial stress within the wall.

7.5 Conclusion

Ten tool shapes for SPIF were tested on AA 3003 sheet. From the tests performed, the following results were found:
• The shape of the tool can effect both the formability and surface finish of a part made with SPIF.

• Tools with tight curvature and therefore small contact surfaces were shown to increase forming limit while decreasing the surface quality. Particularly, curvature near the tip of the tool has a larger response on formability than tight curvature near the tool edge.

• Hemispherical profiles resulted in further polarization in this regard, while flat-ended profiles resulted in reduced forming limit with increased surface quality. Using these results as a baseline, it was found that the formability of angular profile tools improves with increased angle until 90°, with a slight reduction in inner surface quality.

• Parabolic tools appear to yield an excellent combination of high wall angle and low surface roughness. It may be possible that this is due to the large angled side supporting the sheet, or suppressing scallop from tool stepover.

• Tools with very small contact surfaces can result in an increased amount of material spalled from the part.

• Analysis of fracture shapes suggested that the tool design may have an effect on the stress state during forming.

• A combination of desirable characteristics can be obtained by beginning a part with a flat-ended tool and switching to a parabolic tool to achieve improve surface finishing once the contact surface angle has been established.

The foregoing information is useful in defining the tool shape needed for a variety of profiles needed in single point incremental forming. These results show how careful
selection of tool shape can result in both optimal formability and surface condition.

References


Chapter 8

Using Single Point Incremental Forming to produce usable parts: A series of case studies

Prelude

The following paper has been submitted to the CIRP Journal of Manufacturing Science and Technology.

Abstract

Single Point Incremental Forming is a rapid dieless forming method for producing sheet metal parts at low cost. In this paper the production of several parts by Single Point Incremental Forming is summarized. A series of case studies are performed on several parts which were made for various research projects and design teams. Parts produced include several iterations of designs for engine air intakes, powertrain guards, centre bodies for an annular diffuser as well as custom fitting hats to demonstrate the process flexibility and accuracy. All of the parts made were subsequently
put into service in their various applications. The goal of this paper is to demonstrate the viability of SPIF as a market-ready process for forming bespoke usable parts rapidly and at low cost. Where applicable, comparisons are made between SPIF and conventional processes regarding the cost and time to produce parts.

8.1 Introduction

Single Point Incremental Forming (SPIF) is a highly flexible sheet metal forming process [1]. Unlike conventional sheet metal forming processes such as stamping, spinning and shear forming, SPIF does not require a die, instead relying on the motion of the tool to produce the desired shape. Due to the fact that SPIF does not require a die, it is capable of low per-part cost for small production runs [2,3], making it ideal for custom and prototyping work.

While SPIF shows considerable potential as a commercial prototyping process, only a handful of examples of SPIF in practice have been published including a solar cooker [1], a vehicle headlight [3], and a thermoforming mold for a shower [4]. To demonstrate the feasibility of SPIF as a commercial process for custom and small production of complex parts at low cost, an open invitation was extended by the authors to design groups and researchers within the department of Mechanical and Materials Engineering at Queens University to submit parts for production.

Requests were received for the following:

- A series of four-lobed air intake plenums for engine testing of a competition vehicle
- A linear plenum design for the same vehicle
- A series of powertrain guards for another competition vehicle
- A series of annular diffusers for CFD studies
- A plastic cap for wear and noise reduction in a biomedical testing machine
• Several custom fitted Aluminium hats

8.2 Method

Before creating a part, the first step was to consult the customer to determine the true design intent and guide them toward designing a part that is as easy as possible to make with SPIF. When designing parts, customers were given the following list of suggestions to help their design. Unless otherwise specified by the customer, parts were made from 3003-O aluminium, with an initial thickness of 1.59 mm.

• Where possible, keep draft angle ($\phi$) below 74°

• If possible, avoid transitions from concave to convex

• All parts must fit within the working volume of the machine (480 x 327 x 203 mm)

The maximum wall angle, $\phi_{max}$ is specified based on the maximum wall angles published by Jeswiet et al [1] and adapted for this material thickness. For each combination of material, tool size and thickness a different value of $\phi_{max}$ is used, and can be determined using a variable wall angle conical frustum (VWACF) test [5]. For parts with a specified wall angle greater than $\phi_{max}$, multi-pass forming methods are used. Multi-pass forming [6-8] is a technique used to extend the wall angles that can be formed with SPIF by re-forming the wall after an initial forming pass has been completed. Parts, and any intermediate steps that are required, are created using the method shown in Figure 8.1

After all CAD models and intermediate steps were complete, toolpaths are generated using MasterCAM™ commercial machining software. Toolpaths consist of a series of 2-dimensional contours, with a constant vertical stepdown between each pass. For additional passes, the tool is moved from the bottom of the part to the top to help redistribute material from the centre. While multi pass forming strategies can be
either down-down or down-up \( \phi \), down-up forming was favoured as it reduces axial stress on the part, reducing the risk of shape defects in exchange for a less uniform wall thickness distribution.

### 8.2.1 Model Preparation

To form parts with wall angles greater than \( \phi_{\text{max}} \), a series of intermediate models are created with gradually increasing wall angles. To create intermediate forming steps, a method was used, hereafter referred to as cut-and-loft.

The cut-and-loft method of creating intermediate parts is shown in Figure 8.2. In step 1, a draft analysis is performed on the part. Sections of the part above \( \phi_{\text{max}} \) are highlighted in yellow. In step 2, a section of the part is removed. In step 3, a lofted surface is created from the bottom profile and the upper section. The wall angle of this lofted section is now below \( \phi_{\text{max}} \) and can be formed in a first pass.

In step 4, the cut-and-loft method is repeated, replacing the lofted section from step 3 with another section of increasing wall angle. The bottom and top profiles are preserved, with maximum deviation from the step 3 profile at mid-span between the two ends. By preserving the end shapes throughout the process, radial stress on the part is minimized. If a part fails during an intermediate forming step (step 4), additional forming steps can be added and forming tried on a new part.
8.3 Engine air intake plenum

8.3.1 Customer requirements

The Queen’s University Formula SAE team is an undergraduate student design team that constructs a car to compete in the Formula SAE design competition. As part of a design project, the team wanted to study the effects of plenum volume on their engine. The air intake plenum is a large volume that sits in the intake system downstream of the intake restrictor and upstream of the individual intake runners for each of the four cylinders. The plenum acts as a buffer, allowing high flow rates to the intake port of each cylinder in the engine while smoothing out flow rates through the intake restrictor.

To study the effects of plenum volume on engine performance, the team commissioned three plenums to be made, shown in Figure 8.3, with volumes of 1.5, 2.0 and

Figure 8.3: Plenums of varying size made with SPIF
Figure 8.4: Draft analysis of the requested plenum shape. Yellow sections indicate regions above $\phi_{max}$

2.5 litres. During operation the inside of the plenum is subjected to negative pressure, and must not have any cracks or flaws that let air in. The plenum seals to the throttle body by fitting inside of a mating 6° tapered section with a 30 mm inlet. The interior surface at this inlet must be as smooth as possible to maximize airflow into the engine. Figure 8.4 shows a CAD model of the desired part, with regions above $\phi_{max}$ highlighted in yellow.

### 8.3.2 Toolpath Generation

The cut-and-loft method was used to create a series of intermediate steps to form the final shape. The CAD model was cut at the base of the region above $\phi_{max}$, and a profile was created allowing the draft angle to be less than $\phi_{max}$. This initial step is shown as step one in Figure 8.5. Steps 2 and 3 were then created using the same loft boundaries and varying tangency constraints. As the parts become closer to the final shape, the cutting plane (visible in steps 2 and 3 as the middle blue line) is raised to reduce cycle time. Step 4 is close enough to the final shape that instead of using the cut-and-loft method, a large radius is applied to the end. Table 1 shows the tools and settings used to successfully form the part.
## Table 8.1: Settings used to form the plenums

<table>
<thead>
<tr>
<th>Step</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Tool</strong></td>
<td>9.53 mm hemispherical</td>
<td>6.35 mm hemispherical</td>
<td>6.35 mm hemispherical</td>
<td>Flat end, 12.7 mm diameter, 3.18 mm corner radius</td>
<td>Flat end, 12.7 mm diameter, 3.18 mm corner radius</td>
</tr>
<tr>
<td><strong>Reason for tool choice</strong></td>
<td>From previous tests, this tool showed the best combination of surface quality and maximum wall angle</td>
<td>Selected to ensure clearance around tool at the end of the lofted shape</td>
<td>Selected to ensure clearance around tool at the end of the lofted shape</td>
<td>Useful for re-forming and forming flat bottomed shapes [10]. Tool end is the same diameter as the tool shank so it will not interfere with the part</td>
<td></td>
</tr>
<tr>
<td><strong>Stepdown</strong></td>
<td>0.38 mm</td>
<td>0.38 mm</td>
<td>0.38 mm</td>
<td>0.38 mm</td>
<td>0.38 mm</td>
</tr>
<tr>
<td><strong>Feedrate</strong></td>
<td>4064 mm/min</td>
<td></td>
<td></td>
<td>Set due to maximum acceleration capabilities of axis servos</td>
<td></td>
</tr>
</tbody>
</table>
8.3.3 Depth Requirements

A challenge when forming the plenum was the extreme depth, at 193 mm, nearly the full depth of the forming rig. Because sufficiently long tooling was not readily available, multiple backing plates were initially used, forming the part in stages in order to prevent the mill spindle from crashing into the part fixture. The central section was first formed, and the part was then removed from the blank holder and placed on a larger backing plate.

Parts formed using the multiple backing plate method were not usable because the part profiles from each setup did not line up. Figure 8.6 shows an attempt at making a plenum using the multi-setup method. Note the step-shaped defect in the final part in Figure 8.6B. Cracks were also visible around this defect. It is likely that this method did not work because the area around the small backing plate became strain hardened due to bending. When the second forming pass was performed, the non-strain hardened sections of the wall deformed before the formed portion, resulting in a buildup of material around the tool. The final desired shape was obtained by
using a single setup after purchasing a longer toolholder.

8.4 Linear plenum

In addition to the three plenums mentioned above, the Queen’s Formula SAE team commissioned a second plenum design that allowed for straight air intake runners to be used. The linear plenum, shown in Figure 8.7, consisted of two components that were made with SPIF.

8.4.1 Lower Section

The lower plenum section, shown in Figure 8.8, houses the intake bellmouths, and connects to the upper section via a mating flange. Placement of the holes for the runners as well as mounting holes on the flange was important, so these features were machined into the part after forming while still clamped in the mill.

A draft analysis revealed that no part of the lower section exceeded $\phi_{max}$, so no additional forming steps were taken beyond the initial one. Forming was performed with a hemispherical tool of 9.53 mm diameter, with a stepdown of 0.38 mm. Holes were machined in place with a conventional endmill.
Figure 8.7: Cutaway view of the linear plenum design. Air flows in the inlet in the top section and exits through the intake ports cut in the bottom section.

Figure 8.8: Linear plenum lower section with intake bellmouths (blue) in place.
8.4.2 Upper Section

A draft analysis of the upper section of the plenum, shown in Figure 8.9, revealed that multiple forming steps would be required only on the inlet portion. As the final draft angle was identical to the inlet used in the circular plenums, a forming strategy for the inlet was adopted that was identical to that of the plenums.

The area that was re-formed was small in comparison to the entire area of the part. The cycle time for additional forming passes was therefore small in comparison to the time to produce a new part should the part fail. A conservative forming strategy was therefore adopted that favoured many small changes in part shape as opposed to large changes with a greater risk of part failure. Cumulatively, the five additional forming steps, shown in Figure 8.10, had a combined cycle time of 8 minutes and 45
seconds, while the initial forming step had a cycle time of 23 minutes and 7 seconds.

### 8.5 Powertrain Guards

The Queen’s Baja SAE team is an undergraduate student design team similar in nature to the Formula SAE team, with a focus on designing an off-road race car. Powertrain guards are used to protect the car’s continuously variable transmission (CVT) from external debris. Two designs of the guard are presented, made for the team’s 2011 and 2012 seasons, respectively. The parts are shown in Figure 8.11. For each design, two identical replicates were made, allowing the team to have spare parts at competition.

Figure 8.11: Powertrain guards made for the Queen’s Baja SAE team for the 2011 season (left) and the 2012 season (right)
For the 2011 guard design, pictured on the left in Figure 8.11, two forming steps were used to form the inner, then outer formed sections, in that order. The multi-step strategy necessitated two setups, using two backing plates. Based on a draft analysis of the part, no intermediate forming steps were needed. The parts were formed using a 6.35 mm hemispherical tool. The small tool was favoured to obtain tighter radii around the lower corners where the other half of the guard, made from fibreglass, would mate. Once the second forming step was completed, finishing operations were performed using conventional machining tools. The part was completed by using an endmill to cut the outer periphery to the plane of the backing plate, removing the connection to the stock.

The 2012 design (right image in Figure 8.11) design took greater advantage of the abilities of SPIF by incorporating features such as clearance for tools around mounting bolts and creating asymmetrical lofted sections to clear around structural members. Only one forming step was required, as a different connection method was used to fasten to the opposite side of the guard needing only a flat flange. Using threaded fasteners instead of fitting into a lofted section allows the parts to fit using precisely located holes, reducing tolerance demands on the formed sections of the part.

As with the 2011 design, the guard did not require additional forming passes to be made. Holes for the gearbox (large hole) and engine crankshaft (small hole) were cut with an endmill of diameter 12.7 mm. The final profile of the part was made by cutting a v-shaped groove through the part to a depth of 75% of the sheet thickness. The part was then easily broken from the stock after removal from the clamping setup, leaving a clean edge.

8.6 Centre bodies for an annular diffuser

The performance of a range of conical centre bodies for gas turbine exhaust systems was studied by Cerantola and Birk [11][12]. To validate computational models used in
the study, several centre bodies were made using SPIF, following the design outlined in Figure 8.12. SPIF was selected as an ideal process for making centre bodies because the hollow shapes that are produced can readily have pressure taps fitted, and the flexibility of the process allowed several designs to be made rapidly and at low cost.

From the study of computational models that were evaluated, three shapes were selected to be tested. The three centre bodies that were made are shown in Figure 8.13. Drawings for the parts made can be found in Appendix G.
8.6.1 Toolpath Generation

Intermediate steps were generated using the cut-and-loft method for each of the parts. Parts were formed using a 9.56 mm hemispherical tool for initial forming, and a flat ended tool with a radius of 3.18 mm for the additional passes. The flat ended tool allows for vertical walls to be formed with relatively tight connecting radii to adjacent surfaces.

Some shape defects are visible in shapes A and B (see Figure 8.13) visible as a ridge between two sections. These defects occur at the point of overlap between multiple toolpaths. It should be noted that the most severe defects occur at points where different tools are used between passes, suggesting that there is some error in either tool setting or tool compensation. More careful tool length setting resulted in smaller flaws in designs B and C.

Upon completion of the formed shape, parts were cut from the stock using a 76 mm slitting saw. Early attempts to cut the parts free using an endmill resulted in damaged edges, as the negative radial component of the cutting force was large with such a small tool.

8.7 Custom fit hats

SPIF is uniquely suited to manufacturing biomedical parts due to the high degree of flexibility inherent to the process, enabling small design changes to be made with a short turnaround time for manufacture, at low cost. To demonstrate the ability to manufacture custom shapes for biomedical applications that are fitted exactly to an individual with SPIF, several hats were made to fit individual heads. While conventional fabric hats fit to an individual by stretching and flexing to shape when worn, a metal hat must be exact due to the inflexibility of the material. A series of hats were created based on scans of individual heads. The creation process for one such hat is shown in Figure 8.14, and the intermediate model generation is shown in
In order to produce a fitting shape, anthropometric data were collected through a non-contact measurement system. A point cloud was generated of the head, and used as the basis for the formed shape. Scans were originally taken using a FARO arm laser line scanner, however the large scanning time combined with constant small motions of the human body resulted in a very low quality model. The final scans were taken using an Xbox Kinect™ camera and ReconstructMe™ software to produce an accurate 3d model. Models were smoothed and trimmed using Geomagic™ software.

After creation of the models, intermediate models were then prepared in SolidWorks CAD software using the cut-and-loft method described above. The resulting hats showed a very close fit on each individual. Fit was determined by the feel of the individual wearing their own bespoke metal formed shape. Although not a technical measure, it is believed the “feel” for a customer is important. In all cases the hat was placed on the head and then it was observed whether or not the hat would fall off when the base was at ninety degrees to the ground.
Figure 8.14: Hat making process (clockwise from top left): initial scan, pro-processing model, toolpath generation, final shape
8.8 Fatigue testing machine plastic cap

The Niagara foot™ is a low cost prosthetic foot designed to be accessible at low cost in developing countries. To ensure that the foot lasts as long as possible, samples are fatigue tested at Queen’s University in a pneumatic testing rig.

To prevent undesired wear on the foot due to friction between the steel testing platen and the foot, the Niagara foot™ team wanted to fit wear resistant nylon 6/6 plastic covers to the testing platens. From test pieces of nylon 6/6 and other plastics, it was discovered that the main challenge of forming the material would be the large amounts of springback, along with the twisting encountered when attempting to form polymers [13,14]. With the platen making thousands of cycles, it was desired to have the plastic cap “snap” onto the platen if possible such that it would not fall off. Thus, having the wall angle be greater than 90° would be ideal. A cap was designed with a wall angle of 95 degrees (5 degrees past vertical) in attempt to make it snap onto the platen securely. The design was then produced using SPIF, and shown installed in Figure 8.15.

Parts were initially formed using a hemispherical tool with a diameter of 6.35 mm. Walls were then formed to the final angle using a T-shaped tool to over-form the walls past vertical. Due to large springback and an inability to remove highly overformed parts from the backing plate, it was only possible to achieve a vertical wall angle. The vertical wall was deemed sufficient for the Niagara foot™ team, and the final cap fit closely to the platen. At the time of writing the cap remains in service after approximately 3 months of continuous testing.
Figure 8.15: Wear reduction cap fitted to the testing platen in the Niagara foot™ testing machine.

8.9 Conclusion

The ability to make bespoke shapes with SPIF, with short leadtimes for manufacturing has been demonstrated. The custom shapes have been shown to be sufficiently accurate and provide the ability to make an operational rapid prototype using any commercially available 3-axis mill that has a blankholder mounted on the mill table. The use of a commercial CNC mill also allows for precision machined features to be added in situ, enabling high precision features to be easily added to complex shapes.

Many shapes have been used in the foregoing demonstration showing its flexibility for manufacturers and a wide variety of potential users, including biomedical, automotive and engineering test groups. Though the parts here were made from 3003-O
Aluminium and nylon 6/6, other materials can be formed with SPIF.

References


Chapter 9

General Discussion

The following chapter is a discussion of the limitations and reasoning behind the work and results in this thesis. In addition this chapter will present some results that may have not yet been published in a journal article.

The work presented in this thesis documents, among other research, the initial stages of experimentation with electrically assisted SPIF. A major point of focus was quantifying results as a function of current density, as opposed to current, in order to better determine the mechanism by which formability is altered by applied current. By demonstrating that current density correlates with maximum wall angle, and exhibits the current threshold effect seen in literature, it can be inferred that current density is a useful predictor of forming conditions.

In general, it was found that while formability can in some cases be increased through application of electric current, there is a penalty in the form of increased surface friction. As current is further increased, the additional surface friction appears to negate any formability gains to the point at which the part may fail prematurely, at lower wall angles than without applied current. The mechanism of this friction increase (high temperature causing lubricant failure versus increased material ductility causing spalling and adhesion) is important to being able to control forming conditions.
9.1 Lubrication and Formability

At the outset of the experimental campaign with electrically assisted SPIF, a set of tests were performed varying the lubricant. While the results were not published in the preceding papers, the lubricant that was used and the method of application was seen to have some effect on the wall angles that were achievable.

Using a 6.35 mm hemispherical tool, a set of tests were performed to evaluate wall angle varying only the lubricant used. Wall angle results were evaluated using the VWACF method outlined in chapter 5. The lubricants that were used were:

- 75 w 90 synthetic gear oil: kinematic viscosity 193.2 Pa s @ 40 °C, (ASTM D-445) viscosity index 180 (ASTM-2270); density 0.87 kg/m³ @ 15°C (ASTM D 1298).
- Solid graphite lubricant
- Kluber Isoflex NBU 15 spindle bearing grease: kinematic viscosity 4.5 mm²/s (ASTM D-445); Density 0.99g/cm³ @ 20°C.
- Cool Tool II, a cutting lubricant. Boiling point: 204.4°C.

The cutting lubricant (cool tool II) was selected due to its ability to boil at a relatively low temperature (204.4 °C [1]), thereby regulating the temperature of the tool and sheet by removing energy through boiling.

The gear oil was applied both through direct application and by atomization in a stream of compressed air. In the case of the directly applied gear oil and all other lubricants, chilled air was applied to the top side of the part (the same side as the lubricant) to help understand the effects of temperature on formability. The results are shown in Figure 9.1.

Because the Cool Tool boiled off, the part may have been better lubricated throughout the test than the other lubricants. Further, the applied cold air resulted in most of the lubricants being blown off the part, resulting in poor lubrication. The Cool
Tool was not used with applied cold air. This potentially explains the result, suggesting that electrically assisted formability issues may be at least in part a limitation of lubricant performance at high temperatures. Simply applying chilled air to the top of the sheet was found not to be a suitable method of cooling the workpiece, as the stream of chilled air blew the lubricant away from the tool.

A major limitation of the tests was that the lubricant misting system was only able to apply lubricant to one side of the tool. As a result of the partial coverage the tool became starved of lubrication for roughly 50% of each pass as the lubricator became eclipsed behind the walls of the part. With a better designed lubricant applicator, it may be possible to achieve better results. Misting lubricant is potentially an ideal method of lubricating EASPIF because it is able to constantly apply new lubricant to the tool during forming, ensuring good lubrication while removing heat from the tool and sheet.

In addition to the quality of lubrication possible through misting lubricant, there may be additional benefits to tool longevity due to the cooling effects of the lubricant/air combination. In the case of forming Aluminium alloys, the tool has a significantly higher electrical resistance than the material being formed, resulting in excessively high temperatures within the tool. By ensuring that the tool is constantly...
coated in a new layer of lubricant and cold air, the forming surfaces may potentially be prevented from degrading.

9.1.1 Temperature measurements

Throughout the tests performed in chapter 5, temperature measurements were taken during electrically assisted forming. Early temperature measurements were taken using a FLIR infrared camera looking at both the underside and top of the sheet. Due to the high infrared reflectivity of Aluminium, however, temperature measurements proved unreliable. Further, the camera that was used had a maximum temperature reading of 150°C, resulting in an unknown maximum temperature of the tool, and that temperature reflected in parts of the sheet. Thermal images of the tool and sheet can be seen in Figure 9.2.

To produce more reliable results, thermocouples were later attached to the underside of the samples during forming. Figure 9.3 shows the maximum wall angle as a function of maximum recorded sheet temperature for a set of tests. Tests were performed at 400 A, using a tool diameter of 6.35 mm. It should be noted that in Figure 9.3 the cooling was applied as cold air applied to the underside of the sheet. As the thermocouples were attached to the underside of the sheet, the temperatures
Figure 9.3: Wall angle as a function of maximum sheet temperature for parts formed at a current of 400 A. Cooling was applied as cold air applied to the underside of the sheet.

recorded may not necessarily be representative of the temperatures in the forming zone.

While the results in Figure 9.3 suggest a temperature dependence, a formability increase is observed at the same current density for multiple tool sizes, suggesting there is also some dependence on current density. This formability jump with some small temperature dependence is consistent with literature on electrically assisted forming [2].

Because the method of collecting sheet temperature data evolved throughout the series of tests, and temperature data was not the main objective of the study, consistent data were only collected for a few tests. Similarly, the methods of cooling the sheet that were employed did not necessarily cool the forming zone, or could disturb the lubricant. As a result of the methods used, no strong conclusions can be made regarding the role of temperature in electrically assisted SPIF, however the temperatures measured are considerably lower than those seen in [3], who observed for Titanium, temperatures of 300 - 500 °C for \( \phi_{\text{max}} = 72 \), at current levels of 400 A, and a feedrate of 15 mm/s. Their work did not attempt to discern if the formability change was due to the current or the temperature. A hypothesis that can be tested in future is that the formability change is identical at the same temperature using both applied current and direct heating to achieve the same temperature. This, however,
was deemed out of scope of the project as it would require the design of an external heating system.

9.2 Electrically assisted SPIF of 304 Stainless Steel

In addition to 6061-T6, a set of tests were performed to evaluate formability response of 304 Stainless steel to applied current. Due to length constraints, these results were not published in the paper presented in Chapter 5. These results were also not published because severe tool wear made it difficult to determine what if any formability gain is realized due to electric current.

Parts were formed using the same VWACF test shape used in Chapter 5 but an initial sheet thickness of 0.794 mm was used. The wall angle response from each test is shown in Figure 9.4.

Because 304 has a much higher resistivity than 6061, the bulk temperatures of the sheet were higher than those observed with 6061, even at much lower current densities. Typical maximum sheet temperatures when forming 6061-T6 ranged from 70-100 °C, while temperatures recorded in forming 304 ranged from 120-180 °C. Additionally, the high yield strength of the material being formed resulted in very drastic wear of the tool, resulting in some cases in complete failure of the tool (see Figure 9.10 for an example). Tool wear was reduced by oil quench hardening the tool to a hardness of 58 RHC, however tool wear was still present after each test.

The high sheet temperatures obtained when forming 304 SS also resulted in fouling of the lubricant to a greater extent than with 6061, with the lubricant running black and very thick after each test. Figure 9.5 shows the surface roughness of the inside surface for a set of parts with and without applied cooling to the underside of the sheet.

The wall angle results in these tests were inconclusive in determining a strong correlation between current density and wall angle. Partially this is due to the very
Figure 9.4: Wall angle results for a series of tests performed forming 304 Stainless Steel

Figure 9.5: Surface roughness of the inside surface for a set of parts formed from 304 Stainless Steel
high tool wear causing change in tool shape throughout the duration of the test, and partially this is due to the excessive temperatures from resistive heating in the sheet causing lubricant breakdown. Future studies with this material should use much harder tool materials, ideally with as low as possible of a formability response to applied current. Since performing this work more knowledge on tool design has been published in [4]. Additionally, misted lubricant applied directly to the tool through the duration of the test will allow the tool to be best lubricated while cooling the sheet.

9.3 Shape tolerance

Some of the literature on EAF mentions that applied current is capable of reducing springback [5, 6]. Generally the springback reduction is attributed to lowered flow stress and increased recrystallization, resulting in lowered residual stresses after forming.

A short study was performed to determine if there is a relationship between applied current and form deviation for parts formed with EASPIF.

9.3.1 Experimental Method

To evaluate form deviation, a series of parts were formed from 6061-T6 Aluminium at a range of applied current values. The shape that was formed was a four-sided pyramidal shape; a drawing can be found in Appendix F. Formed parts were measured using a FARO arm laser line scanner and CAM 2 Measure 10 software.

After unclamping from the forming rig, part edges were measured using a 3 mm ball probe, attached to the FARO arm. The edges were measured in order to establish the part coordinate system, which was then used to compare point cloud measurements to the CAD model. With the part aligned with the original CAD coordinate system, a point cloud was measured using the laser line scanner.
Form deviation was defined as the normal distance from a point to the nearest surface on the CAD model. Colour plots of form deviation for a set of parts are shown in Figure 9.6.

Three repetitions each of parts were formed at a constant current magnitude of 0, 50, 100, 150 and 300 A. Parts were formed with a hemispherical tool of diameter 9.53 mm, a stepdown of 0.254 mm and a feedrate of 1270 mm/min.

9.3.2 Results and Analysis

Point cloud data were exported from Cam 2 Measure 10 software in the form of a histogram representing the number of points in each tolerance range. Histogram data were then normalized by dividing the number of points in each bin by the total number of measurement points for the part. The result is a histogram of each part.
representing, with each bin containing the fraction of total points in that tolerance band. Histograms of deviation from nominal are shown for each of the current values used are shown in Figure 9.6.

To help determine if there is any significant difference between the form deviation as a result of varied applied current, a weighted average of the deviations was taken for each of the 15 runs. The weighted averages for each of the current set points are shown in Figure 9.7.

Using a single factor ANOVA to evaluate the effect of current magnitude on formability, no significant difference in form deviation was found as a result of varying current. The results of the ANOVA are shown in Table 9.1.

### 9.3.3 Limitations of the study

The study presented, attempted to determine if there is a measurable effect of applied current on shape retention. No significant difference was found in the shape retention of a variety of parts formed at a series of current densities.

One limitation of the method presented is that the accuracy to which deviations are reported from the CAD surface is a function of how accurately the measurements of the part are aligned to the CAD model coordinate system. The patterns of deviation in Figure 9.6 may be consistent with a rotation of the CAD model relative to the measurements, resulting in a net loss of accuracy due to rotation. Furthermore, vertical alignment was confounded due to warping of the part after releasing from the clamping rig.

A second limitation of the measurement method used was a non-uniform point-cloud density. Because there may be a higher prevalence of points in one region than

---

Table 9.1: ANOVA results for averaged form deviation from parts formed at varying DC current.

<table>
<thead>
<tr>
<th>Source of Variance</th>
<th>SS</th>
<th>df</th>
<th>MS</th>
<th>F</th>
<th>P-value</th>
<th>F crit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Between Groups</td>
<td>0.4315</td>
<td>4</td>
<td>0.1079</td>
<td>2.2314</td>
<td>0.1383</td>
<td>3.47805</td>
</tr>
<tr>
<td>Within Groups</td>
<td>0.4834</td>
<td>10</td>
<td>0.04834</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>0.9150</td>
<td>14</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Figure 9.6: Histograms for normal deviation from the CAD model for parts formed at varying current.

Figure 9.7: Averaged deviation from CAD for parts formed at varying current values.
another, the histogram data in Figure 9.6 may be skewed. Filtering the points to a uniform spatial density in future will help reduce this problem.

Finally, the current values that were selected were quite low in comparison to those used in Chapter 5. The low currents were selected to reduce possible wear on the machine, however the current densities that are produced may not be large enough to produce a large formability change. Additionally, the material was formed because it was readily available at the time, however this same study may yield more interesting results at higher current densities and harder to form materials such as Ti-6Al-4V.

9.4 Current density and formability

The current density values presented in chapter 5 are estimated based on a geometrical model presented in the same chapter and elaborated on in chapter 6. While a simplified empirical model was presented in chapter 6, these measurements had not been performed at the time of writing chapter 5. As a result of not incorporating the experimental measurements into the contact area prediction, the indentation height $h$ is slightly different, as it is predicted as a function of wall angle due to sheet thinning, whereas the results in chapter 6 found it to be a function of both wall angle and stepdown. The assumption that there is no contact area behind the tool, however, was shown to be valid by the experimental measurements.

Because the current was varied throughout the constant density tests according to the model, it is not possible to re-evaluate the findings without repeating the tests in Chapter 5. Nonetheless, the conclusion that the material exhibits a significant threshold current density remains true regardless of exact current density.

If the formability reduction due to increased surface friction is in fact due to lubricant and tool failure as opposed to excessive temperature within the sheet, further study using misted lubricants and better tool materials may yield favourable results.
9.5 Microstructural effects

In order to determine if applied cooling has any effect at the microstructural level during forming with EASPIF, samples were cut from the part after forming and etched to show the microstructure.

Figure 9.8 shows the microstructure of 6061-T6 formed at 400 A with and without applied cooling. Qualitatively, very little difference is observable in grain size between the two samples, suggesting that there is little effect of the temperature change on the microstructure.

A similar set of images were taken for parts formed from 304 stainless steel, shown in Figure 9.9. Once again little noticeable difference is observable in between the cooled and uncooled samples.
9.6 Tool wear

During electrically assisted SPIF, significant degradation of the tools was observed. As mentioned in Chapter 5, this can adversely affect the formability due to increased friction. Since this research began, similar tool degradation has been published in studies where electric current was used for resistive heating purposes [4]. When forming 304 Stainless Steel, extreme tool degradation was observed, an extreme example is shown in Figure 9.10, resulting in complete loss of the tool shape. Further research into tooling materials can help to reduce the wear experienced by the tool.

There appear to be two mechanisms of tool degradation: deposition of sample material on the tool, and shape deformation of the tool. Both of these result in a loss of formability due to increased friction, however shape deformation is much more detrimental to formability.

Just as the increased formability of the sample being formed can be explained by the electroplastic effect, the tool experiences a similar change in formability. One possible way of mitigating tool degradation is by selecting tool materials that experience
the lowest change of yield strength under applied electric current.

Additionally, as the current density is at least as high within the tool as within the sheet being formed, and the tool typically has a small cross section near the tip, resistive heating becomes significant within the tool. An ideal tool design would therefore maximize the cross section along the length of the tool, as opposed to having a long tapered section above the forming tip.

References


Chapter 10

Conclusions and Recommendations for Future Work

10.1 Conclusions

Through the research performed in this thesis, the following conclusions can be reached:

- Electric current can increase the formability of 6061-T6 Aluminium and to a lesser extent 304 Stainless Steel.

- Materials formed with EASPIF exhibit a current threshold density, similar to that seen in EAF literature.

- While electric current increases formability, it can also increase surface friction and spalling, offsetting formability gains.

- The type of lubricant and application method has an effect on achievable wall angle with electrically assisted forming.

- There is no contact between the tool and sheet behind the axis of the tool in the direction of tool motion.
• Indentation of the tool below the sheet is a function of primarily wall angle and stepdown, and not tool diameter.

• Non conventional tool shapes such as parabolic can be used to achieve a better combination of formability and interior surface roughness.

• SPIF can be successfully applied to a wide variety of custom, prototyping and bespoke production applications. This is further supported by case studies previously published in the literature.

10.2 Contributions to Research

Through the research presented in the preceding chapters, the following contributions have been made. These various research contributions have either been published or are currently in the process of peer review, as explained below.

• An unique electrified tool design has been presented that is more robust and fail-safe, as well as stiffer than designs presented in literature [1].

• Through studying the effects of formability response to current density, formability has been shown to rise following a pattern similar to previously published EAF literature, suggesting a direct electron interaction as opposed to a purely resistive heating based mechanism.

• A method of determining the contact area and shape has been presented, paving the way for previous contact models to be validated.

• Novel tooling designs have been tested, and shown to be better than conventional hemispherical tooling both in terms of formability and surface roughness.

• Several new applications of SPIF have been documented.

All of these results have been published or are in the process of peer-review for publication. The EASPIF results are published in [2], and the tool shape information
was presented at [3]. Results regarding the measurement of energy consumption were also published in [4].

10.3 Recommendations for future work

Based on the conclusions that have been reached in this work, the following are recommendations for future work that will continue to expand the understanding of this process and expand the performance limits.

10.3.1 Lubrication and cooling

Much of the formability loss observed appears to be a result of increased surface friction, as evidenced by the increased surface spalling and roughness with current density. The surface roughness occurs immediately with applied current (as shown in Figure 5.12, as opposed to gradually as would be expected with temperature increase. In Chapter 5 chilled air was applied in an attempt to separate resistive heating effects from electroplastic effects, however the results were inconclusive. As the chilled air was applied to the underside of the sheet, it is probable that the effects were small in comparison to the local heating at the tip of the tool. One method of ensuring effective lubrication while cooling the tool and forming area is through using misted lubricants atomized in a stream of compressed air.

Forming forces can be compared by measuring the power input to the axis servos. For a constant feedrate, a given change in power corresponds to a change in servo force. As strain gauges on the tool will not give accurate readings due to the high currents and temperatures, measuring servo power can be useful for comparing relative changes in process forces based on varying lubrication and applied current.
10.3.2 Contact model update

The contact model presented in Chapter 6 represents an initial version of a geometrical model for contact area based on measurable features. The empirical models presented at the end of the chapter for each feature were measured only for a single material and thickness, and do not account for any non-linearities. The linear experimental design was selected in order to reduce the number of tests needed, however as a result it is unknown how accurate the model is over the range to which it extends.

In order to verify the model over the levels presented, more measurements should be taken. Furthermore, tests with different materials would allow the model to be more widely applied.

10.3.3 Tool analysis

The tools that were studied in Chapter 7 represent an initial stage of experimentation with non-standard tooling in SPIF. A potential next stage to this research could be to measure process forces, allowing more in depth comparison of tools as well as an analysis of the tribological conditions between the tool and the sheet.

References


Appendix A

Slip Ring toolholder design drawings
All dimensions are in mm unless otherwise specified.

Queen's University
Manufacturing and Metal Processing Lab

TITLE:
Slip Ring Toolholder Exploded View

Material

Quantity

A
David Adams

SCALE: 1:5
<table>
<thead>
<tr>
<th>ITEM NO.</th>
<th>PART</th>
<th>DESCRIPTION</th>
<th>QTY.</th>
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<td>23018</td>
<td>Techniks toolholder</td>
<td>1</td>
</tr>
<tr>
<td>2</td>
<td>end cap</td>
<td>Techniks end cap</td>
<td>1</td>
</tr>
<tr>
<td>3</td>
<td>transfer block style 2</td>
<td>Transfer block</td>
<td>1</td>
</tr>
<tr>
<td>4</td>
<td>61811</td>
<td>SKF bearing</td>
<td>2</td>
</tr>
<tr>
<td>5</td>
<td>brush housing</td>
<td></td>
<td>2</td>
</tr>
<tr>
<td>6</td>
<td>conductor bar</td>
<td></td>
<td>1</td>
</tr>
<tr>
<td>7</td>
<td>solid brush</td>
<td></td>
<td>2</td>
</tr>
<tr>
<td>8</td>
<td>backing ring taper</td>
<td>Rear taper</td>
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</tr>
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<td>9</td>
<td>taper slip ring</td>
<td>Copper Ring</td>
<td>1</td>
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<td>10</td>
<td>front taper</td>
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<td>11</td>
<td>TG100 1 inch collet</td>
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<td>15</td>
<td>transfer wire</td>
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**Queen's University**  
Manufacturing and Metal Processing Lab

**TITLE:** Slip Ring Toolholder Assembled View

**Material:**

**Quantity:**

<table>
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<th>Quantity</th>
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<td>1</td>
</tr>
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**SCALE:** 1:5
3: Transfer Block

All dimensions are in mm unless otherwise specified.

Drilled and tapped

8-32

SolidWorks Student Edition.
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SECTION A-A
SCALE 1 : 1

Queen's University
Manufacturing and
Metal Processing Lab

5: Brush Housing

Material
Ultem

Quantity
2

All dimensions are in mm
Unless otherwise specified

SolidWorks Student Edition.
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SolidWorks Student Edition. For Academic Use Only.
Drilled and tapped
1/4 20

All dimensions are in mm
Unless otherwise specified

Material
Copper

Quantity
2

SolidWorks Student Edition.
For Academic Use Only.

Queen's University
Manufacturing and Metal Processing Lab

TITLE:
7: Solid Brush

SIZE
A
Name
David Adams

SCALE: 1:5

REV

SHEET 6 OF 11
TITLE: 8: Backing Ring Taper

Material: Ultem

All dimensions are in mm Unless otherwise specified

Quantity: 2

SCHOOL: Queen's University Manufacturing and Metal Processing Lab

Name: David Adams

REV: A

SCALE: 1:5

SHEET 7 OF 11
Title: 9: Taper Slip Ring

Material: Copper
Quantity: 1

All dimensions are in mm unless otherwise specified.

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Queen's University
Manufacturing and Metal Processing Lab

Name: David Adams

Scale: 1:1

REV A

Sheet 8 of 11
Threaded 1.875-12 ACME thread

SECTION C-C
SCALE 1 : 1

All dimensions are in mm
Unless otherwise specified

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Drilled and tapped to accept 1/4-20 heli-coil

SECTION J-J
SCALE 1:1

SECTION L-L
SCALE 1:1

All dimensions are in mm
Unless otherwise specified

Material
Copper

Quantity
1

TITLE:
14: Tool conductor ring

SIZE
A

Name
David Adams

REV

SCALE: 1:5
SHEET 11 OF 1

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Appendix B

Specifications for high current wires
PART NUMBER: 10418
DESCRIPTION: 4/0 WELDING CABLE
CONSTRUCTION: This cable consists of one bare copper conductor with integral insulation and jacket.
APPLICATION: Welding Cable Applications

Construction Parameters:
- Conductor: 4/0 AWG Bare Copper
- Stranding: 2052 Strands
- Insulation Material: EPR
- Separator/Wrap Tape: Tape Separator
- Insulation Thickness: 0.083" Nom.
- Insulated Conductor Diameter: 0.695" Nom.
- Number of Conductors: 1
- Approximate Cable Weight: 762.7 Lbs/1M' Nom.

Electrical Properties:
- Temperature Rating: -50°C to 105°C
- Operating Voltage: 600V Max
- DC Resistance per Conductor @ 20°C: 0.052 Ohms/1M'

Insulation Color: Black (Other colors available for minimum order)
Legend (White Surface Ink Print): CCI ROYAL/EXCELENE®  4/0 (103mm²) WELDING CABLE 600V - 50C TO +105C MADE IN USA

This product complies with European Directive 2002/95/EC (RoHS)
On special orders, the customer will accept all mil lengths and +/- 10 percent of total order requested.
The jacket is sequentially footprinted.

The information presented here is, to the best of our knowledge, true and accurate. Since conditions of use are beyond Coleman Cable's control all product data presented is for informational purposes only and does not create a binding obligation or liability on Coleman Cable or confer any rights on any customer. The sale of product(s) is conditioned upon acceptance of a purchase order subject to Coleman Cable's standard terms and conditions contained therein, including without limitation Coleman Cable's standard warranty. Coleman cable disclaims all liability in connection with the use of information contained herein or otherwise.

This specification is proprietary intellectual property of Coleman Cable. Any information contained herein shall not be disclosed to any party without written consent of Coleman Cable.

Customer Name__________________________________ Date Signed____________________________
Customer Approval ______________________________________

Specification Issue Date: September 2, 2011
X-FLEX®

POWER SUPPLY SYSTEMS CABLE

DESCRIPTION
This product is designed to meet or exceed test requirements called for by Underwriters Laboratories and the National Electric Code. It is recommended for use in accordance with UL and CSA for internal wiring of uninterruptible power supply equipment, UL Standard 1778. Cobra’s X-FLEX® is also suitable for use in transformers, switchboard panels, controls, electronic circuits and meters. It can be used as battery cable, battery charger cable, motor lead, and power hookup cable. Approved for both the internal and external wiring of appliances.

STRANDING
Class K 30 gauge bare copper. (Also available in tinned copper)

STANDARD
NEC Types: MTW & THW, UL AWM Styles: 1232-1283- 1284-1337-1338-1339-10070-10269, BC-5W2 on 6 AWG to 4/0 AWG, TEW A/B FT-1 on 6 AWG to 4/0 AWG, AWM A/B FT-1 on 250 MCM to 750 MCM

INSULATION
This product offers a unique flame retardant polyvinyl chloride compound (VW-1), and is moisture, abrasion, acid, diesel fuel and oil resistant.

VOLTAGE
600/1000 Volts

TEMPERATURE
105°C Dry, 75°C Wet

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<th>Cobra Part Number</th>
<th>Size</th>
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<th>UL Style</th>
<th>AMPS</th>
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<tr>
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<td></td>
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Appendix C

Specifications for insulating materials
Quadrant EPP Duratron® T4203 PAI, Polyamide-imide, extruded (electrical grade)

**Material Notes:**
Duratron® T4203 extruded PAI offers excellent compressive strength and the highest elongation of the Duratron® PAI grades. It also provides electrical insulation and exceptional impact strength. This grade is commonly used for electrical connectors and insulators due to its high dielectric strength.

Duratron® PAI is the highest performing melt processable plastic. It has superior resistance to elevated temperatures. It is capable of performing under severe stress conditions at continuous temperatures to 500°F (260°C). Parts machined from Duratron® PAI stock shapes provide greater compressive strength and higher impact resistance than most advanced engineering plastics. Its extremely low coefficient of linear thermal expansion and high creep resistance deliver excellent dimensional stability over its entire use range. Duratron® PAI is an amorphous material with a Tg (glass transition temperature) of 53°F (280°C).

Quadrant EPP’s extruded Duratron® stock shapes are post-cured using the latest technology and procedures developed by Quadrant eliminating the need for additional curing by the end user in most situations. A post-curing cycle is recommended for components fabricated from extruded shapes where optimization of chemical resistance and/or wear performance is required.

Data provided by Quadrant Engineering Plastic Products from tests on stock shapes and parts produced by Quadrant EPP.

### Physical Properties

<table>
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<tr>
<th>Property</th>
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<th>English</th>
<th>Comments</th>
</tr>
</thead>
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<tr>
<td>Specific Gravity</td>
<td>1.41 g/cc</td>
<td>0.0509 lb/in³</td>
<td>ASTM D792</td>
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<tr>
<td>Water Absorption</td>
<td>0.4 %</td>
<td>0.4 %</td>
<td>Immersion, 24hr; ASTM D570(2)</td>
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<tr>
<td>Water Absorption at Saturation</td>
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<td>1.7 %</td>
<td>Immersion; ASTM D570(2)</td>
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### Mechanical Properties

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<td>ASTM D638</td>
</tr>
<tr>
<td>Flexural Modulus</td>
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<td>600 ksi</td>
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</tr>
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<td>24000 psi</td>
<td>ASTM D790</td>
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<td>Dry vs. Steel; QTMS5007</td>
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<td>Limiting Pressure Velocity</td>
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<td>4000 psi-ft/min</td>
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<td>1.07 J/cm</td>
<td>2 ft-lb/in</td>
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### Electrical Properties

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<td>Min 1e+016 ohm</td>
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<td>1MHz; ASTM D150</td>
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</table>

quadrant.matweb.com/SpecificMaterialPrint?bassnum=p1sm38
ULTEM®
(Polyetherimide)

ULTEM is an amorphous thermoplastic polyetherimide (PEI) material that combines exceptional mechanical, thermal, and electrical properties. Natural ULTEM® 1000 (unreinforced) is a translucent amber material. The addition of glass fiber reinforcement to the basic ULTEM® provides it with both greater tensile strength and rigidity while at the same time improving dimensional stability.

- Excellent mechanical strength
  ULTEM® exhibits high tensile strength at room temperature and retains a significant portion of this strength at elevated temperatures. Glass fibers further increase high-temperature strength.
- Outstanding heat resistance
  ULTEM® retains its physical properties at elevated temperatures.
- Exceptional resistance to environmental forces
  Environmental characteristics of ULTEM® include it’s stress resistance
- Inherent flame resistance with low smoke evolution
- High mechanical strength
- High dielectric strength and stability
The high dielectric strength and constant values of ULTEM® make it an excellent electrical insulator UL94 VO
- Low dissipation factor over a wide range of frequencies
- Excellent machinability and finishing characteristics
ULTEM® can be easily machined with conventional metalworking tools, painted, hot stamped, printed, or metallized.
- Natural Grade is FDS, NSF, and USP Class VI compliant
ULTEM® has many applications in medical, electronic/electrical, microwave, automotive, and aircraft industries.

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<td>@ 66 psi, 1/4&quot;</td>
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<td>In Air</td>
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</tr>
<tr>
<td>Dissipation Factor, 1kHz, 50% RH, 73°F (23°C)</td>
<td>D150</td>
<td>-</td>
<td>0.0013</td>
<td>0.0014</td>
<td>0.0015</td>
<td>0.0015</td>
</tr>
<tr>
<td>Volume Resistivity, 1/16&quot;</td>
<td>D257</td>
<td>ohm-cm</td>
<td>1.0 x 10^17</td>
<td>1.0 x 10^17</td>
<td>7.0 x 10^16</td>
<td>3.0 x 10^16</td>
</tr>
</tbody>
</table>

7600 Anagram Drive – Eden Prairie, MN 55344 | Phone: 952-934-2303 | Toll Free: 800-776-7769 | Fax: 952-934-2314
Appendix D

VWACF test shape
Queen's University
Manufacturing and Metal Processing Lab

VWACF test shape

All dimensions are in mm Unless otherwise specified

Material

Quantity

Name: David Adams

REV

SCALE: 1:5

SHEET 1 OF 1
Appendix E

Friction Test shape
SolidWorks Student Edition
For Academic Use Only

Queen's University
Manufacturing and Metal Processing Lab

All dimensions are in mm
Unless otherwise specified

TITLE:
Friction Test Part

Material
6061-T6

Quantity

Name
David Adams

SIZE
A

REV

SCALE: 1:5

SHEET 1 OF 1
Appendix F

Square Pyramid Test
SolidWorks Student Edition. For Academic Use Only.

Queen's University
Manufacturing and Metal Processing Lab

Title: Square Pyramid
Material: 6061-T6
Quantity: 6

All dimensions are in mm unless otherwise specified.

Name: David Adams

REV
Scale: 1:2
Scale: 4:1
Scale: 1:5
Sheet 1 of 1
Appendix G

Center Body Drawings

The following drawings are included as a reference to the parts made for David Cerantolla. These drawings are the property of David Cerantolla and are used with permission.
Notes:
1) Outer wall dimensions given
2) Schedule 16 aluminum sheet metal
   (assumed final thickness is 0.022in)
3) Essential to achieve OD=4.5in at base
4) Tip profile may be determined by bit diameter

Drill size 18 for TD6 8-32
machine screw
head diameter = 0.309in
Notes:
1) Outer wall dimensions given
2) Schedule 16 aluminum sheet metal (assumed final thickness is 0.022 in)
3) Essential to achieve OD=4.5in at base
4) Tip profile may be determined by bit diameter

Drill size 18 for TD6 8-32 machine screwhead diameter = 0.309 in
Notes:
1) Outer wall dimensions given
2) Schedule 16 aluminum sheet metal (assumed final thickness is 0.022in)
3) Essential to achieve OD=4.5in at base
4) Tip profile may be determined by bit diameter

Drill size 1/8 for TD6 8-32 machine screw
head diameter = 0.309in