

University of Strathclyde

Department of Mechanical & Aerospace Engineering

An experimental and numerical investigation of
refill friction stir spot welding of aerospace
aluminium alloys

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This thesis is submitted in fulfilment of the requirements for the degree
of Doctor of Philosophy

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Abstract

Refill friction stir spot welding is a solid-state joining process that offers exceptional joint strength and quality in high strength aluminium alloys. However, industrial adoption has not been forthcoming, in part due to the impractically long welding times required by the process that make it uncompetitive with conventional techniques. The research presented in this thesis addresses the issue of long welding times by investigating the microstructural characteristics and mechanical properties of AA2024-T3 lap joints produced at welding times significantly below those previously reported in the literature. Various microscopy techniques were used to assess weld microstructure and defect evolution, while mechanical properties were determined through hardness measurements and tensile lap shear testing.

A series of novel welding tool shoulders were developed to improve material flow and counteract the formation of defects as welding time was reduced. The influence of shoulder design on weld microstructure, defect evolution, material flow, and mechanical properties was assessed. It was found that implementing a chamfer on the inner rim of the shoulder tip improved joint quality at short welding times by promoting material flow towards critical joining interfaces during the refill stage of the process. The formation of voids in the region of the weld periphery was eliminated and tensile lap shear strength of the welded joints was increased by 27%. However, large tunnel defects remained, and fractography of the failed joints revealed that strengthening of the weld periphery came at the cost of weakening across the lap interface.

To better understand material flow behaviour during welding, and the mechanism by which defects form, a fully coupled thermomechanical model was developed using commercial finite element analysis software. The model was validated against experimental temperature measurements and observations of microstructure, material flow, and defect characteristics. By observing the simulated flow of material, the formation process of the commonly observed tunnel defect was identified; the

model was then used to predict the optimal tool volume ratio to eliminate the defects.

With the knowledge gained through the above outlined experimental and numerical research, a second generation of chamfered shoulders was developed and explored experimentally. Improvements to the path of the welding tools were investigated by implementing the optimal volume ratio predicted numerically, as well as modifying the end position of the shoulder and probe. Microstructural and mechanical testing of the resulting welds revealed that defect free joints can be produced in approximately half the welding time, and with a higher lap shear strength, than previously reported in the literature for a comparable alloy.

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Nomenclature

Abbreviation	Definition
CEL	Coupled Eulerian-Lagrangian
FEA	Finite element analysis
FSSW	Friction stir spot welding
FSW	Friction stir welding
HAZ	Heat affected zone
PPO	Plug pull-out
RFSSW	Refill friction stir spot welding
SEM	Scanning electron microscope
SZ	Stir zone
TLI	Through the lap-interface
TMAZ	Thermal mechanical affected zone
TWI	The Welding Institute
TWS	Through the weld shear
ULSF	Ultimate lap shear force
UTS	Ultimate tensile strength
VR	Volume ratio

Chapter 1. Introduction

1.1 Background

As the developed world pivots towards a net zero emissions agenda, the transport sector is evolving to meet the tightening emissions policies adopted by responsible governments [1.1]. Within the aerospace and automotive industries, this has led a drive towards lightweight design as a means of reducing the environmental impact of cars and aircrafts [1.2,1.3]. To this end, aluminium alloys are extensively utilised for their excellent specific mechanical properties [1.2,1.3]. Simultaneously, manufacturers within these industries seek to increase competitiveness by improving their own productivity [1.4], meaning efficient processing methods suitable for aluminium alloys are crucial.

A common assembly task in manufacturing of vehicle structures (air frames, car bodies etc.) is the joining of metal components. Conventional joining methods include fusion welding processes (laser beam, arc etc.) and mechanical fastening (bolts, rivets), although each of these has its limitations. In the aerospace industry, 2xxx and 7xxx series aluminium alloys are used extensively for their high strength to weight ratio [1.2,1.5,1.6], yet both are challenging to join through fusion processes due to their propensity for solidification cracking [1.7,1.8]. Common approaches to joining these alloys therefore are bolts and rivets, however each fastener adds mass to the vehicle and is counterproductive to the objective of lightweighting.

Refill friction stir spot welding (RFSSW) is a novel alternative to fusion based spot welding methods, such as resistance spot weld, and mechanical fastening that has seen a rapid uptick in research attention over the last decade [1.8–1.11]. Derived from the widely known friction stir welding (FSW) process, in RFSSW a non-consumable, rotating tool heats and plasticises overlapping metal sheets to create a permanent, high strength joint. Often referred to as a solid-state process, meaning that the joined materials are not melted, only softened, RFSSW offers a number of advantages over

conventional spot joining methods [1.12,1.13]. Unlike with bolts and rivets, no material is added so the process does not increase the mass of the assembled structure, and no shielding gas is required as with fusion welding [1.14]. As the process occurs below the solidus temperature, negative thermal effects, such as heat distortion and high residual stresses in the joined components is minimised [1.12,1.15]. The solid-state nature of the process allows for the joining of 2xxx and 7xxx series aluminium alloys, without the risk of solidification cracking [1.12]. Dissimilar alloys and metals can also be joined without the formation of brittle intermetallic compounds [1.9,1.16–1.20].

However, one of the challenges that remain to be addressed before RFSSW can be adopted in an industrial setting is the amount of time required to create a weld, commonly referred to as the welding time. The vast majority of the published literature thus far has focused on maximising joint strength without considering other factors of importance to industrial users, such as production rate [1.21,1.22].

A typical RFSSW joint of aluminium components, of approximately 2 mm thickness, requires a welding time in the region of 2 - 6 s for maximum strength to be achieved, with higher strength alloys such as 2xxx and 7xxx series often requiring even longer [1.21–1.26]. The few studies that have addressed short welding times for higher strength alloys have found that defects emerge within the welds and strength is severely impacted [1.21,1.27–1.29]. As depicted in Figure 1-1, this creates a trade-off between material strength, weld quality, and production rate that has yet to be addressed in the literature.

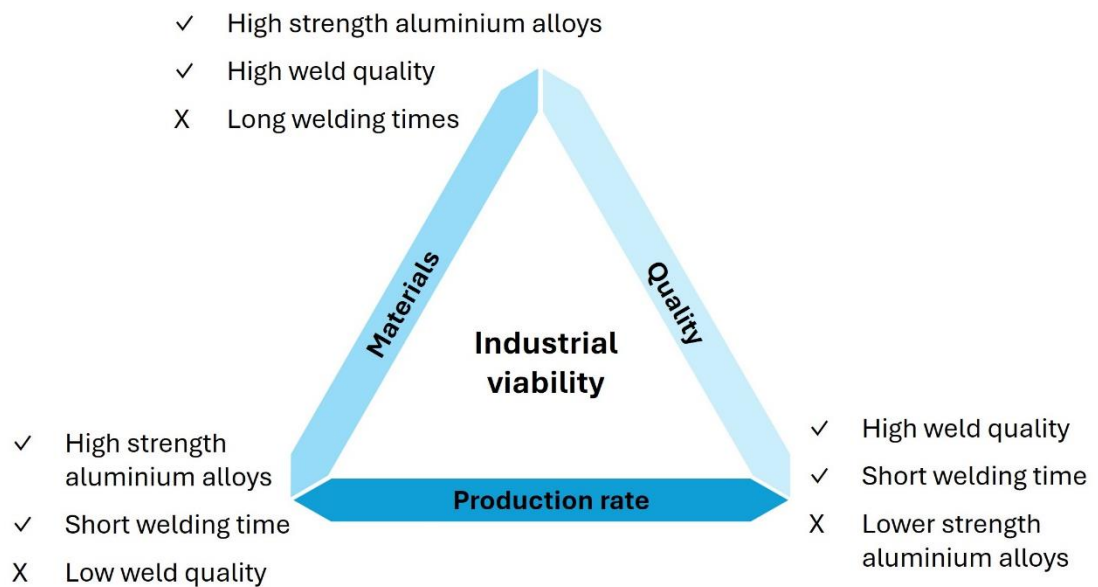


Figure 1-1 Diagram of trade-offs for the industrial viability of RFSSW

1.2 Aim and objectives

The current thesis consists of a series of investigations structured around aim of solving the trilemma described in Figure 1-1. This work seeks to minimise the welding time for joints of the high strength aluminium alloy AA2024-T3, while optimising the RFSSW process to counteract any degradation in joint quality. AA2024-T3 was selected due to its prevalence in the aerospace industry [1.11,1.30], and for the absence of any prior studies addressing short welding times.

To achieve this aim, the key research areas of process optimisation, process simulation, and tool design are explored using a combination of experimental and numerical methods. These research areas are summarised as follows:

- **Process optimisation** refers to the investigation of interacting welding parameters, including welding time, tool rotational speed, tool plunge depth etc., and how these parameters influence the quality of the resulting weld.
- **Process simulation** refers to the use of numerical modelling to simulate the RFSSW process. The purpose of this is to reduce experimental effort of process optimisation, and to improve the general understanding of the joining process.

- **Tool design** concerns the geometry of the welding tool itself, and how alterations of this geometry can be used to influence the properties of the weld.

There are four primary objectives of this work:

Objective 1 - Explore the influence of short welding times on defect formation and mechanical performance using conventional RFSSW tools.

Objective 2 - Design and manufacture a selection of novel welding tool geometries and experimentally explore their ability to improve the quality of short duration welds.

Objective 3 - Develop and validate a thermo-mechanical numerical model capable of simulating all stages of the welding process.

Objective 4 - Build on knowledge gained in objectives 1-3 to identify the most promising tool geometry and optimise this design for short duration welds.

1.3 Thesis structure

1.3.1 Novelty statement

The novelty of the work reported herein lies principally in 3 areas. Firstly, the topic of short cycle times as a primary objective is investigated, which to date has been only sparsely studied by a single research group [1.21,1.22,1.31], and never for the AA2024-T3 grade aluminium used extensively in the aerospace industry. Secondly, in the pursuit of short welding times, several geometrical variants to the conventional welding tool design are experimentally investigated. Thirdly, a fully coupled thermo-mechanical numerical model is developed, and utilised in the optimisation of tool geometry and process parameters. The details of peer reviewed journal publications resulting from this work can be found in Appendix A.

1.3.2 Chapter summaries

This thesis encompasses a series of investigations organised around the objectives outlined in section 1.2. The thesis is comprised of 7 chapters, including 2 introductory

chapters to provide context for the work and a broad literature review of the subject matter, 4 novel investigation chapters, consisting of experimental and numerical modelling work, and a final chapter detailing conclusions and recommendations for future work. Although a literature review is provided in chapter 2, results chapters also include a short presentation of the technical literature most relevant to their contents, and each chapter includes its own reference list to allow it to be read as a standalone study. A short summary of each chapter is as follows:

Chapter 1 presents an introduction to the topic of RFSSW and the research gap that provides the context for the current work. The aim and objectives are also clearly laid out.

Chapter 2 comprises a broad literature review of the relevant friction stir welding processes and the key processing parameters of RFSSW to provide the reader with the terminology and understanding required for subsequent results chapters. A short theoretical background to the relevant numerical modelling techniques used within this work is also provided.

Chapter 3 presents an experimental study with the objective of understanding the effect of minimising welding duration. The influence on the weld microstructure and mechanical properties are explored and the conclusions of this chapter directly influence the development process detailed in subsequent chapters.

Chapter 4 follows on from chapter 3 by investigating novel welding tools designed to counteract the ill effects of short welding times. The microstructural and mechanical effects of novel tool geometries are explored.

Chapter 5 details the development and validation of a fully coupled thermo-mechanical numerical model of the RFSSW process. This chapter briefly details how the model was developed and what assumptions and simplifications were made in the process. Analysis of the model's ability to predict temperature, strain, material flow, and defect formation is presented and validated using welds examined in chapter 3. The model is then used to improve weld quality by optimising the tool path, which is explored experimentally in chapter 6.

Chapter 6 is the final experimental study that builds upon the learnings of all previous chapters to deliver short duration, defect free welds with increased mechanical properties. The most promising tool geometry identified in chapter 4 is explored in greater depth and combined with optimised tool path predicted by the numerical model in chapter 5. Microstructural analysis and mechanical testing of the resulting joints is presented.

Chapter 7 concludes the thesis by summarising the key findings of each chapter and presenting recommendations for scientifically impactful work to be addressed by future researchers.

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Chapter 2. Review of the State of the Art

This chapter presents a detailed review of the relevant topical background information to familiarise the reader with the subject matter. A brief introduction into the core concepts of friction stir welding is given, as well as a thorough description of the RFSSW process, weld structure, typical defects, and the key operating parameters of the process. A theoretical background to relevant numerical modelling techniques and their application to friction stir processes is also presented.

2.1 Friction stir welding

Friction stir welding (FSW) was pioneered by The Welding Institute (TWI) in the United Kingdom in the early 1990s [2.1]. Commonly referred to as a solid-state joining process, meaning the workpiece never reaches its melting temperature, FSW carries with it a number of benefits over conventional fusion welding processes. The lower process temperatures of FSW lead to reduced thermal effects, such as geometric distortion and softening in the finished joint [2.2]. By avoiding the solid to liquid phase transition from, solid-state processes allow for the joining of metals and alloys that would otherwise be susceptible to solidification cracking, such as 2xxx and 7xxx series aluminium alloys [2.3,2.4]. The FSW process involves a non-consumable rotating tool, typically made of a hardwearing steel or ceramic material, that is forced against the surface of abutting workpieces (Figure 2-1) [2.3].

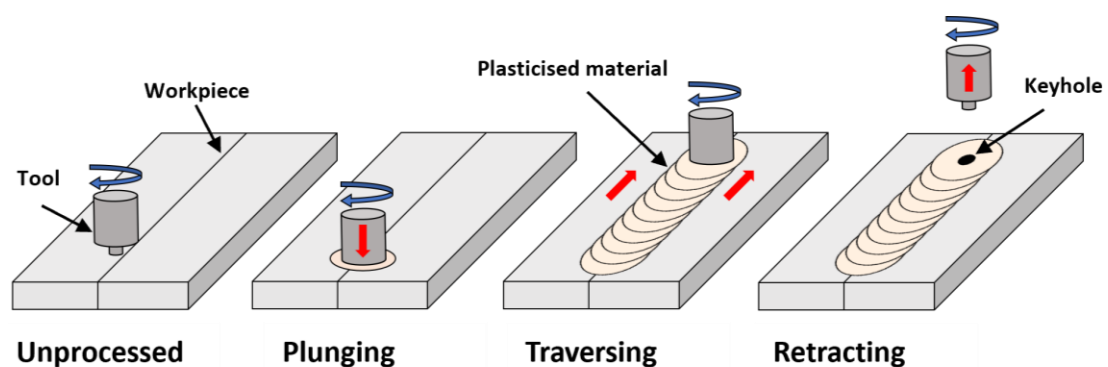


Figure 2-1: Diagram depicting the different stages of the FSW process

As the frictional heat generated at the interface increases, the workpiece softens and

the tool can penetrate to a desired depth. The tool then traverses along the length of the abutted workpieces, stirring and joining the material as it moves. As the tool is retracted, a small hole is left by its final position [2.3].

2.2 Friction stir spot welding

Friction stir spot welding (FSSW) is a joining process derived from FSW, allowing for single point spot welds of overlapping sheets [2.4]. The process is similar to FSW and consists of plunging, stirring, and retracting stages (see Figure 2-2). As in FSW, a non-consumable rotating tool is forced against the workpiece surface, generating frictional heat. As the material heats and softens, the tool is able to plunge downward, stirring and plasticising the material to form a weld between the overlapped sheets. During plunging, a small flash of displaced material forms around the periphery of the tool, and a keyhole defect remains once the tool is retracted. As well as being unsightly, the keyhole acts as a potential stress concentrator and is a major disadvantage of the FSSW process [2.5].

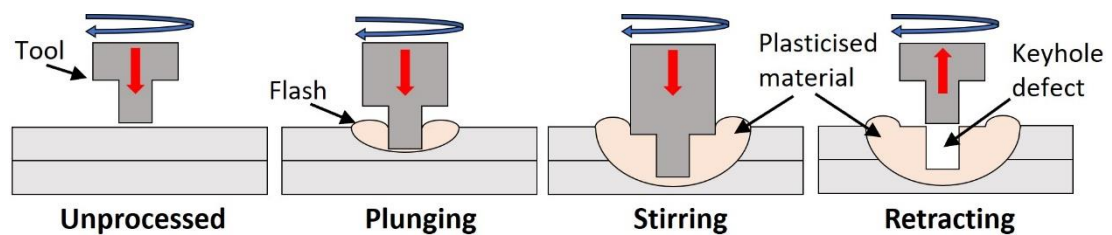


Figure 2-2: Diagram depicting the different stages of the FSSW process

2.3 Refill friction stir spot welding

Refill friction stir spot welding (RFSSW), referred to synonymously herein as 'the process', addresses the keyhole defect associated with FSSW [2.5]. As with FSSW, RFSSW is a spot-welding process suitable for joining overlapping sheets of low melting temperature metals (aluminium, magnesium) and plastics [2.6], whilst limited research has attempted to join higher melting temperature metals [2.7,2.8]. The keyhole defect is addressed by the use of a multi-part tool system, consisting of a clamping ring, a shoulder, and a probe, as shown in Figure 2-3.

The components are assembled concentrically, with the probe nesting inside the

shoulder, which is in turn nested inside the clamping ring (Figure 2-3d). The shoulder and probe rotate within the clamping ring and can be actuated independently along the axis of rotation. This allows the shoulder to move downwards while the probe moves upwards and vice versa.

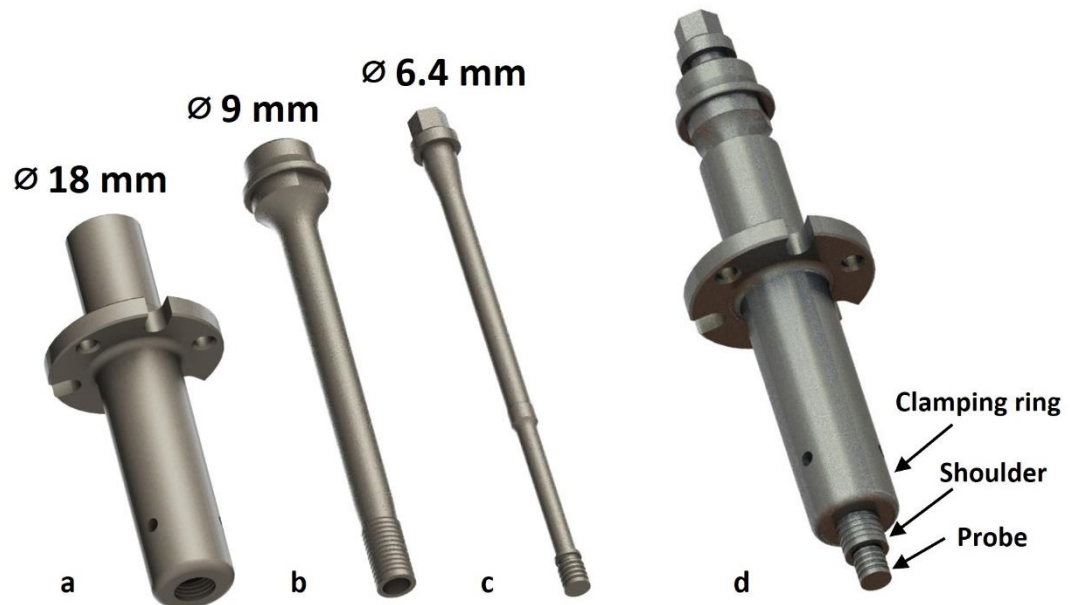


Figure 2-3: Refill friction stir spot welding tool for RPS100 machine a) clamping ring, b) shoulder, c) probe, d) assembled tool

The process consists of three stages as described below and illustrated in Figure 2-4:

Stage 1 – Clamping

The clamping ring exerts a downward force on the overlapping workpieces, securing them against a backing anvil. The clamping ring does not rotate and once the downward force has been applied it remains stationary throughout the process. The shoulder and probe both rotate during the clamping stage and generate some frictional heating within the tool, although they are not yet in contact with the workpiece.

Stage 2 – Plunging

During the plunging stage, the rotating shoulder moves downwards, penetrating into the workpiece. The frictional heat generated through this contact softens the workpiece, allowing it to flow like a thick paste rather than a rigid solid. To

accommodate the displacement of material, the probe simultaneously retracts upwards to provide a cavity into which the softened material can flow.

Stage 3 – Refilling

Once the shoulder has reached the desired depth, both the shoulder and probe reverse direction and begin returning to their original positions. As the shoulder retracts, the probe moves downwards, forcing the displaced material back into the space previously occupied by the shoulder. If appropriate welding parameters are used, the material will flow into this space and fully consolidate, leaving a high-quality weld and a nearly flat workpiece surface [2.4,2.9].

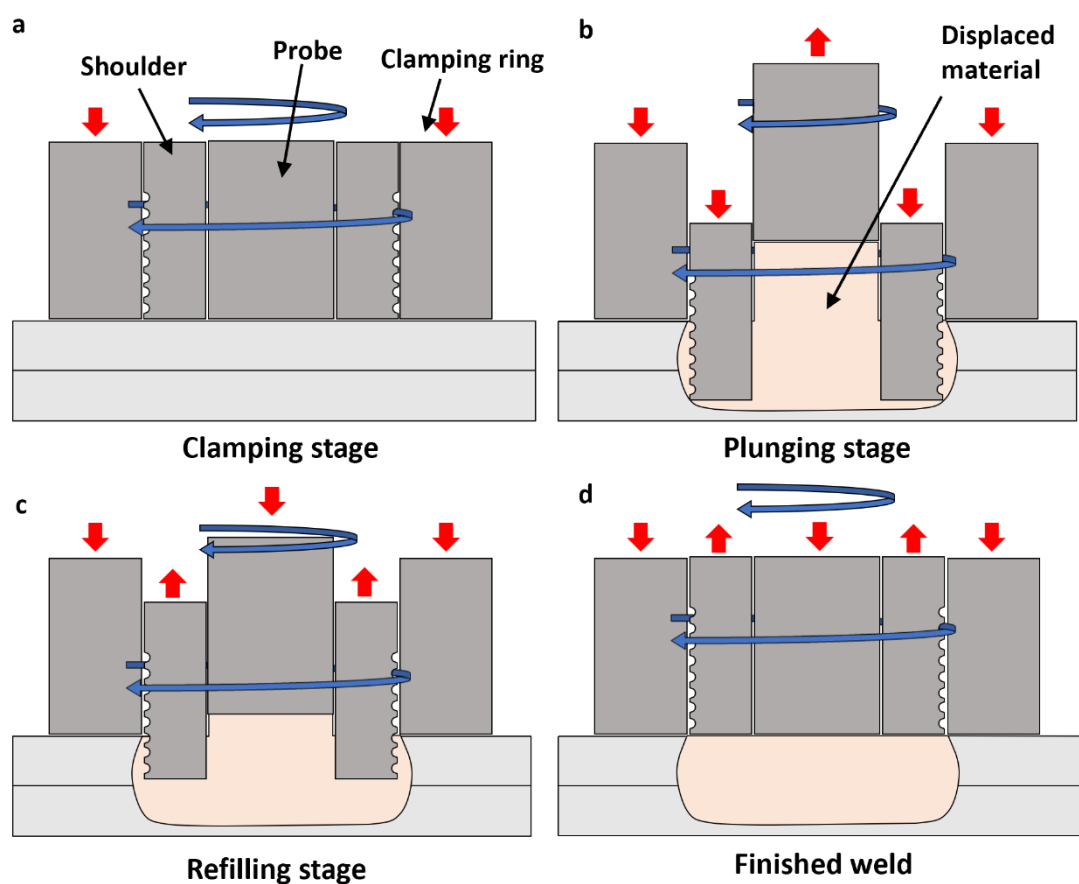


Figure 2-4: Diagram depicting the different stages of the RFSSW process

The extreme deformation during the plunge and refill stages results in complex material flow of both the top and bottom sheets [2.9–2.11]. Although material flow can change depending on process parameters, Figure 2-5 illustrates the general movement of the sheet interface throughout the process. During the plunging stage,

the interface of the sheets bows upwards as material flows from beneath the shoulder into the space vacated by the probe (Figure 2-5a-b) [2.12]. As the material is forced back downwards during refilling, the interface folds over itself into a mushroom pattern [2.13] (Figure 2-5c-d).

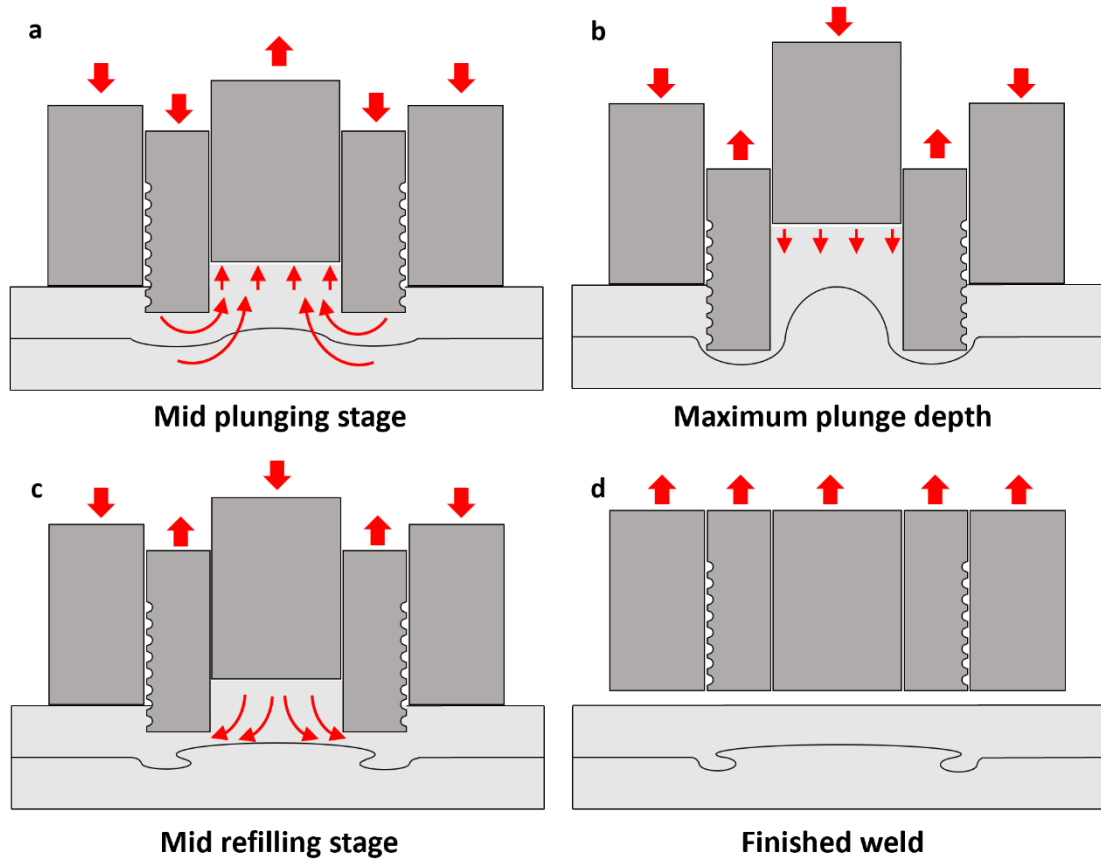


Figure 2-5: Diagram demonstrating the material flow during the plunging and refilling stages of RFSSW

An experimental technique commonly referred to as ‘stop-action’ analysis is useful for understanding macro-scale material flow [2.10,2.12,2.14]. This involves halting the welding equipment at various stages throughout the weld cycle to lock the material in a deformed position, extracting the tools, and sectioning the partially welded samples vertically through the weld centre. An example of stop-action analysis performed by Shen et al. [2.12], on AlMgSc alloy KO8542 is given in Figure 2-6. The completed weld can be seen in the topmost image, with each subsequent image showing the material movement at different shoulder positions throughout the plunge and refill stages.

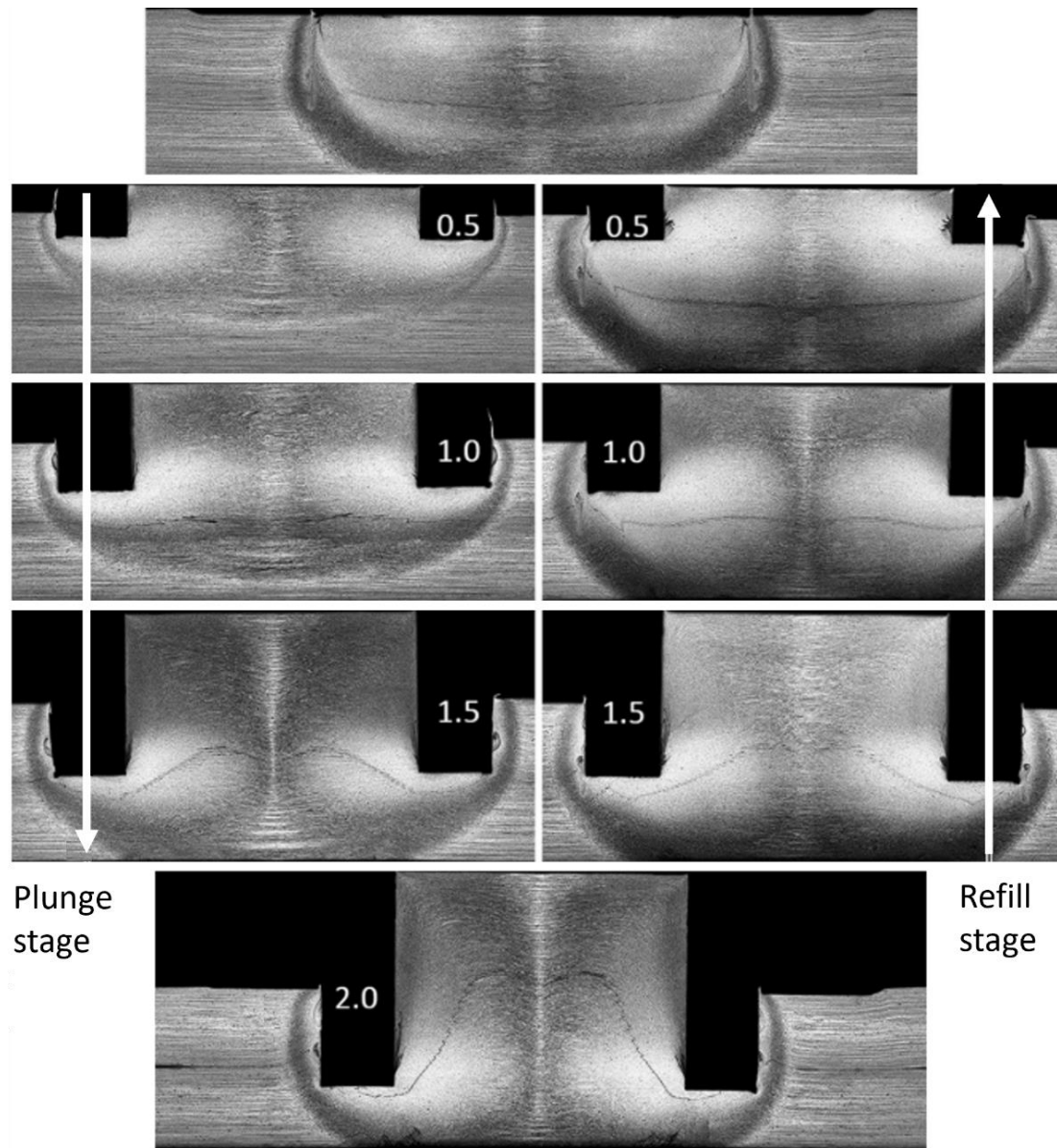


Figure 2-6: Macrographs of a RFSSW weld at different stages throughout the welding process, adapted from [2.12]. Numbers in the figure refer to the depth of the shoulder in mm.

Figure 2-7 shows the top surface of a typical RFSSW joint. A slight marring is visible where the high clamping force was applied to secure the workpiece. Grooves are also common around the probe/shoulder and shoulder/clamping ring interfaces, where a small amount of material is extruded into the gap between components [2.15]. Small grooves generally do not influence the joint performance, although excessive extrusion can be a sign of tool wear and contribute to incomplete refilling.

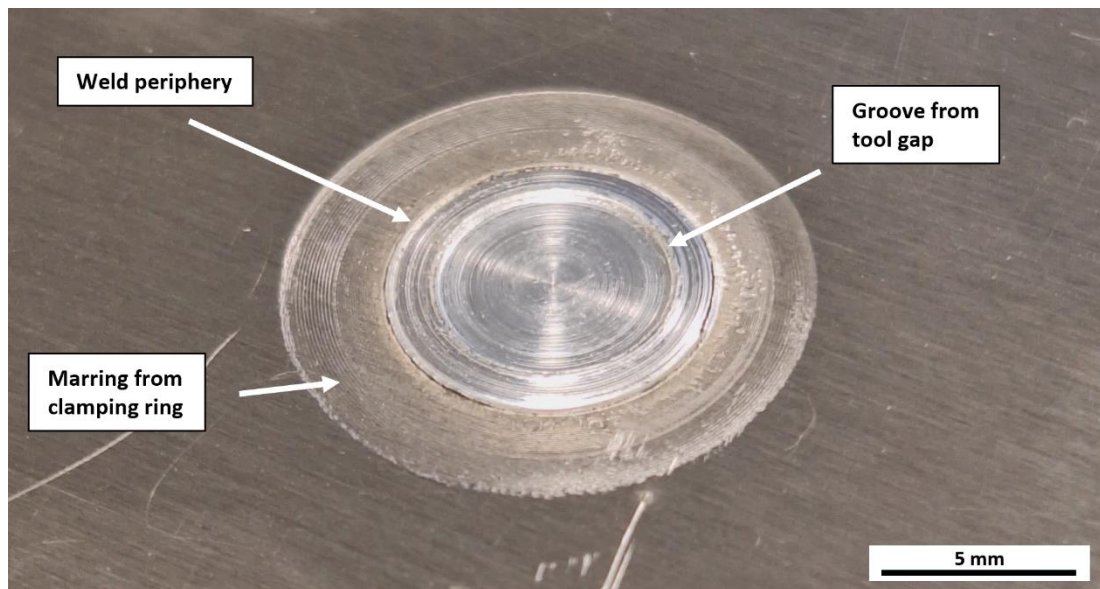


Figure 2-7: The upper surface of a typical aluminium refill friction stir spot weld

2.3.1 Weld microstructure

Regardless of external appearance, it is the weld's internal structure at both the micro and macro scale that ultimately dictates its mechanical integrity. During the welding process, intense heat and plastic deformation drastically alter the microstructure of the base material, giving rise to several distinct regions. Figure 2-8a presents a macrograph of a typical RFSSW joint, sectioned vertically through its centre, with the key microstructural regions outlined.

2.3.1.1 Stir zone (SZ)

The SZ (Figure 2-8a) constitutes the majority of the weld and is comprised of material that has been severely plasticised by the welding tool. A fine and equiaxed grain structure can usually be found in the SZ as a result of dynamic recrystallisation brought about by high strain and temperature [2.16]. Depending on processing parameters, the grain structure throughout the SZ may not be homogeneous as material beneath the probe often experiences less strain than material within and around the shoulder's plunge path [2.17]. The SZ can therefore be differentiated into the sub-regions SZ_P (probe affected SZ) and SZ_S (shoulder affected SZ) [2.18–2.20]. Figure 2-8b,c indicate the difference in grain size between these two regions.

2.3.1.2 Thermal mechanical affected zone (TMAZ)

The TMAZ is a transitional region outside of the shoulder plunge path, in which material has been partially affected by the shoulder movement. The microstructure within the TMAZ varies from fine and equiaxed in the region directly adjacent to the shoulder path, to heavily elongated in the direction of rotation, and finally to very minor grain distortion further from the shoulder path [2.21,2.22]. The transition between SZ and TMAZ is shown in Figure 2-8d. Crack propagation commonly occurs through this area, which can be exacerbated if insufficient mixing is achieved [2.20,2.23].

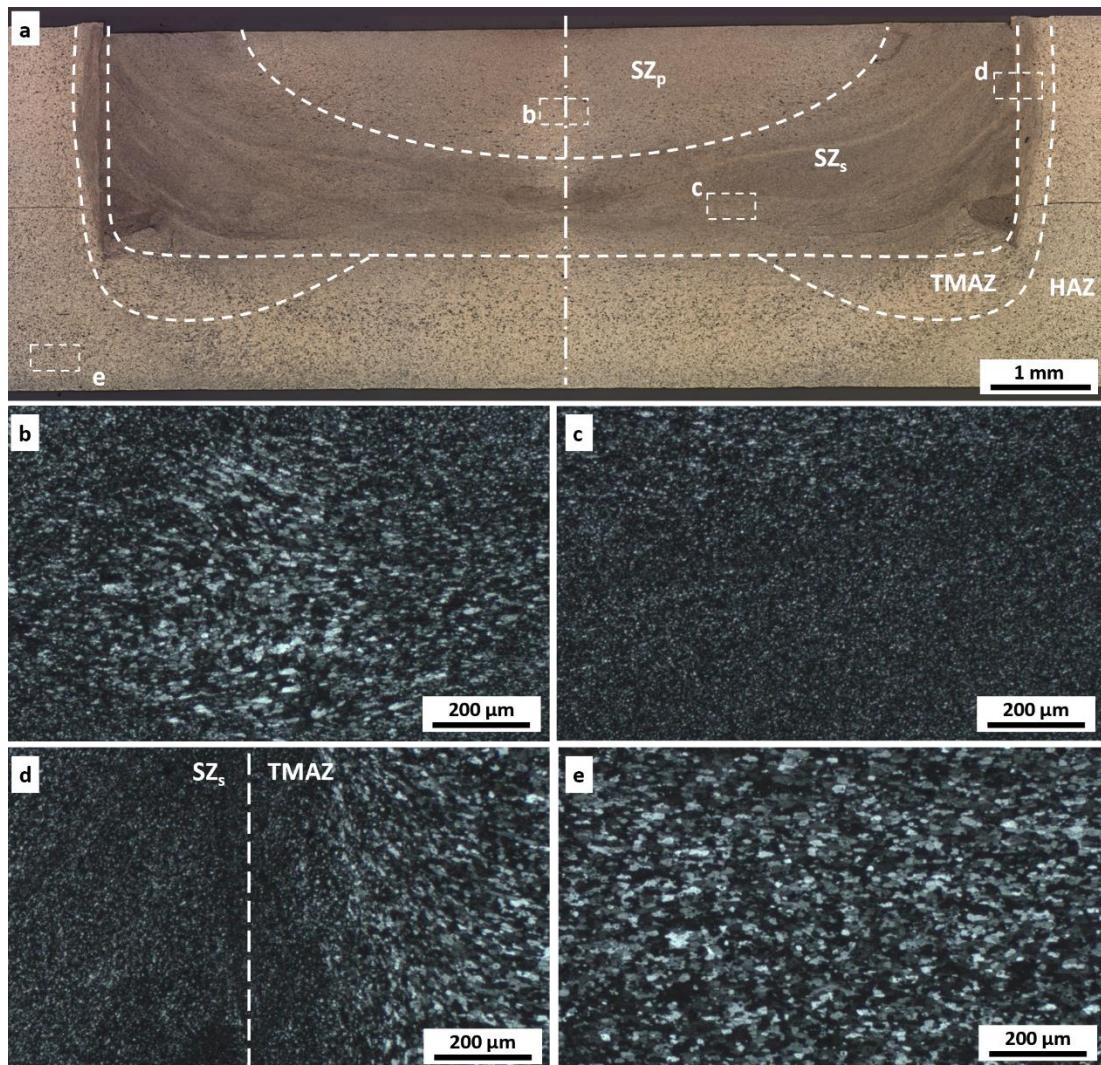


Figure 2-8: Microstructural regions of a typical RFSSW cross-section a) macrograph of weld cross-section, b) SZ_p, c) SZ_s, d) transition between SZ and TMAZ, e) HAZ. Adapted from [2.25]

2.3.1.3 Heat affected zone (HAZ)

The HAZ (Figure 2-8e) lies outside of the TMAZ and only experiences thermal effects during the welding process. Hardness in this region is typically lower than the base material, particularly when welding rolled sheets, as the thermal cycle allows for some grain recovery [2.24]. If too much frictional heat is generated during welding, excessive softening can occur and cause premature failure of the joint [2.22].

2.3.2 Weld defects

A significant advantage of RFSSW is the elimination of the keyhole defect discussed in section 2.2. However, this section discusses a number of other defects not visible from the weld surface, which can result from incorrect process parameters and degrade the structural integrity of the joint. Figure 2-9a presents a weld cross-section containing several common defects.

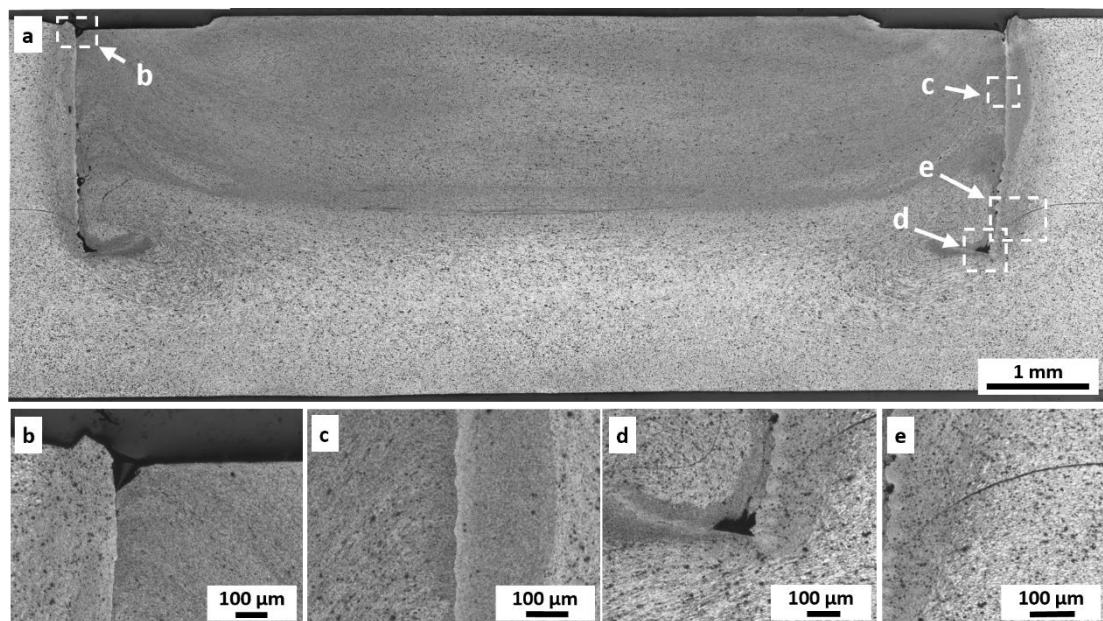


Figure 2-9: Common defects within RFSSW joints a) macrograph of defective weld cross-section b) incomplete refill, c) SZ/TMAZ transition, d) tunnel void, e) hook defect. Adapted from [2.26]

2.3.2.1 Incomplete refill

Incomplete refill (Figure 2-9b) results in a circumferential groove left on the top surface of the weld, where the shoulder and clamping ring meet. Several causes have been cited for this defect, most notably loss of workpiece material. Adamus & Adamus [2.15] explain that plasticised material is permanently lost due to extrusion

into gaps between the tool components. This is exacerbated as tools wear and the gap size increases. As well as material loss, Yue et al. [2.27] attributed incomplete refill to excessive shoulder plunge depth, while Shen et al. [2.28] ascribed the effect entirely to poor material flow associated with insufficient heat input.

Although incomplete refill tends not to be cited as a mechanical disadvantage, this is largely due to the fact that most RFSSW joints are assessed using tensile lap-shear tests, where the weld surface is not significantly loaded. In practice however, such a surface defect could present a significant weakness under bending loads, where the weld surface is in tension, as well as create a site for contamination and corrosion.

2.3.2.2 SZ/TMAZ interface

The SZ/TMAZ interface (Figure 2-9c) marks the point at which displaced material makes contact with the periphery of the weld during the refill stage. Insufficient mixing in this region provides a favourable path for crack propagation and often leads to premature failure of the weld [2.20,2.23]. Some researchers have characterised this as a kissing bond, while other simply refer to a lack of mixing, yet generally it is associated with insufficient material temperatures during the refill stage of the process [2.15,2.29].

2.3.2.3 Tunnel defect

The terminology of the tunnel defect varies substantially throughout the literature, with some researchers referring to voids, volume defects, or lack-of-refill. In this work, however, the term tunnel defect will be used to describe the circumferential void that can form at the bottom, outside corner of the SZ, indicated in Figure 2-9d. During refilling, material must be forced into this region, replicating an extrusion process into a sharp corner. When heat input is insufficient, poor material flowability often leads to the formation of such tunnels [2.15]. While not often associated with poor static strength, the influence of tunnel defects on fatigue performance has yet to be explored.

2.3.2.4 Hook defect

The hook defect forms at the interface of the top and bottom sheet, at the transition

from welded to un-welded material. The plunging motion of the shoulder plastically deforms the sheets into a downwards sloping hook, as shown in Figure 2-9e. Under certain welding conditions, the retraction of the shoulder during refilling can also result in an upwards sloping hook. Hooks act as a stress concentrator and point of crack initiation, with larger hook heights amplifying this effect and reducing the tensile strength of the joint [2.30–2.33].

2.3.3 Process parameters

The quality of a RFSSW joint depends heavily on the process parameters selected. The key difficulty in optimising the process lies in the complex interplay between these numerous parameters. Different material types, material thickness, the required production rate, etc., all play a role in the end quality of the weld [2.34–2.37]. This section describes each welding parameter and discusses the state of the art in the relevant research literature.

2.3.3.1 *Welding time (s)*

Welding time, also referred to as joining time or welding duration, is the combined time taken to complete the plunging and refilling stages. In some cases, a dwell period between plunging and refilling also contributes to welding time. This parameter plays a critical role in weld formation as it dictates the amount of time the rotating tool is in contact with the workpiece, and therefore the amount of heat generated. If too short a welding time is used, insufficient heat results in poor flowability of the welded material, leading to tunnel defects and poor mixing at the SZ/TMAZ interface [2.15]. Too long a welding time can introduce other deleterious effects, such as excessive softening of the HAZ due to grain recovery [2.24], and excessive wear of the welding tools [2.38]. A core objective of the current work is to increase the production rate of RFSSW, hence welding time is highly relevant and should be well understood.

Due to the relative immaturity of RFSSW compared with competitor techniques such as resistance spot welding, the predominant goal of past research [2.4,2.5] has been to optimise welding parameters for maximum joint strength, rather than aspects such as production rate. In the RFSSW literature, it is common to evaluate joint strength

using the single lap-shear test [2.39] to establish its ultimate lap-shear force (ULSF). Figure 2-10 compares ULSF values against welding times, as reported in the literature for different aluminium alloy series and reveals the trend of ULSF increasing with welding time up to ≈ 6 s. This presents a challenge when considering the widespread adoption of the process in mass manufacturing industries [2.16,2.40], as the durations for resistance spot welding range from only 1-2 s [2.41]. RFSSW should match or improve on this to be a competitive joining technique for high production rate applications. Several studies have shown that welding times below 2 s are achievable for aluminium alloys in the 7xxx and 6xxx series [2.16,2.20,2.33,2.42], yet little is known about applying such short welding times to 2xxx series alloys.

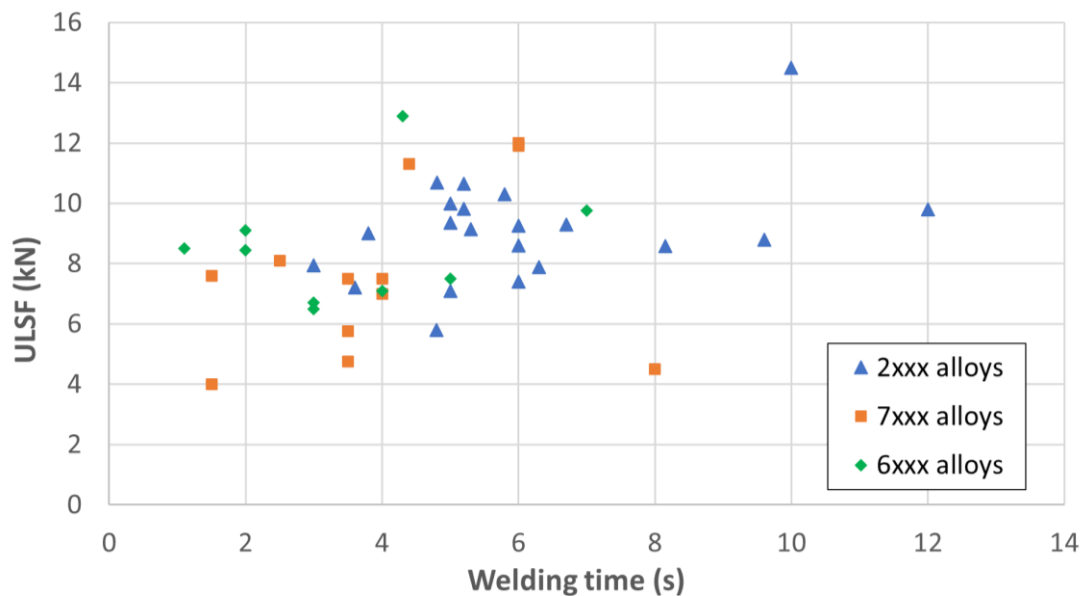


Figure 2-10: Summary of RFSSW literature for different aluminium alloys comparing welding time vs ULSF [2.9,2.16,2.17,2.19,2.21,2.22,2.24,2.27,2.28,2.32–2.35,2.43–2.69]

2.3.3.2 Tool rotational speed (rpm)

Rotational speed refers to the speed at which the shoulder and probe components rotate during the plunge and refill stages of the process. As with welding time, rotational speed influences the amount of heat generated at the tool-workpiece interface and can be highly impactful on weld quality [2.19,2.52].

Where rotational speed is too low, insufficient heat generation may lead to poor material flow and the formation of volumetric defects (lack-of-refill, tunnel, etc.). Higher rotational speeds have been shown to reduce defects by increasing process

temperatures, further softening the material, and allowing it to flow more easily during the refill stage [2.19,2.52,2.67]. In some instances, however, it has been reported that excessively softened material can be lost through gaps between the tool components, leading to increased volumetric defects [2.19,2.52,2.68]. Additionally, slippage at the tool-workpiece interface can occur when rotational speed is too high, hindering material mixing [2.22,2.28]. Li et al. [2.19] studied the influence of rotational speeds on the mechanical performance of AA2024 joints between 1000 rpm and 2500 rpm. It was found that higher speeds caused the tools to slip over the workpiece, rather than sticking and stirring, leading to a coarser grain structure in the SZ. Ji et al. [2.29] confirmed this but found that a higher rotational speed was beneficial as it improved mixing at the SZ/TMAZ interface.

Throughout the literature (see Figure 2-11), maximum ULSF is often found at moderate rotational speeds, as higher process temperatures can lead to a loss of hardness throughout the finished joint, providing a favourable path for crack propagation [2.45,2.46]. Figure 2-11 summarises the ULSF values achieved at different rotational speeds as reported in the literature.

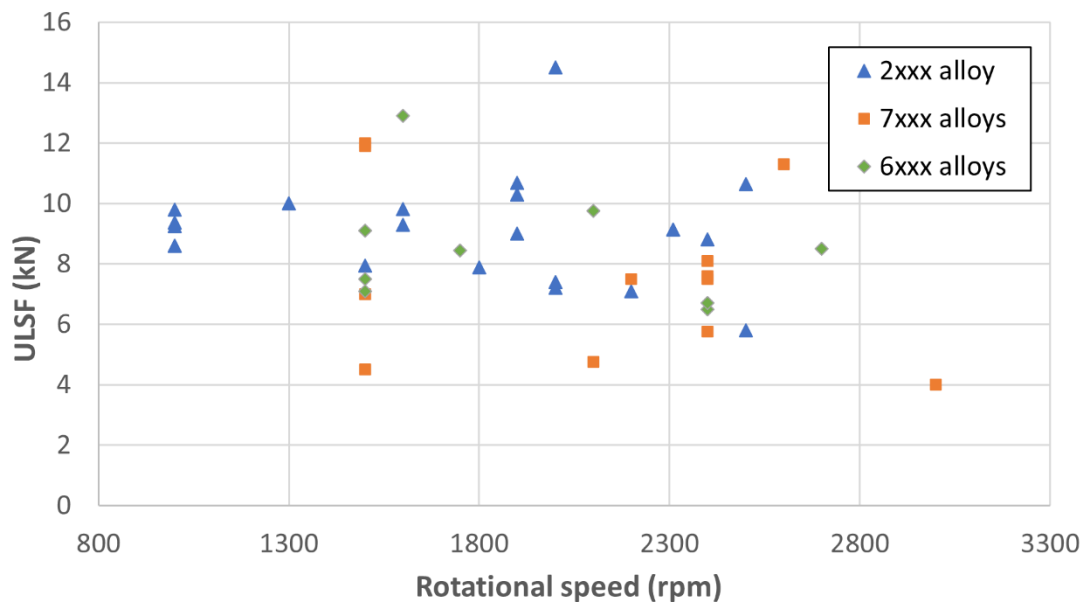


Figure 2-11: Summary of RFSSW literature for different aluminium alloys comparing rotational speed vs ULSF [2.9,2.16,2.17,2.19,2.21,2.22,2.24,2.27,2.28,2.32–2.35,2.43–2.69]

The spread of rotational speeds is much broader than welding times, particularly for 2xxx series alloys where researchers have reported similar ULSF with rotational speeds ranging from 1000 rpm to 2500 rpm [2.19,2.69].

2.3.3.3 Plunge depth (mm)

Plunge depth dictates how deep the shoulder penetrates the workpiece during the plunging stage. This plays a key role in determining joint performance as it influences the size of the SZ, the formation of defects, and the hook geometry. Naturally, different sheet thicknesses require different plunge depths, however the consensus within the literature for 2xxx series alloys (Figure 2-12) is that 120% to 130% of the upper sheet thickness tends to yield optimal joint strength. When welding AA2198, Yue et al. [2.27] found that plunging beyond 130% the thickness of the top sheet resulted in incomplete refill and tunnel defects. It was found that greater plunge depths led to higher welding temperatures, softening the workpiece to the point that material was extruded between the tool gaps.

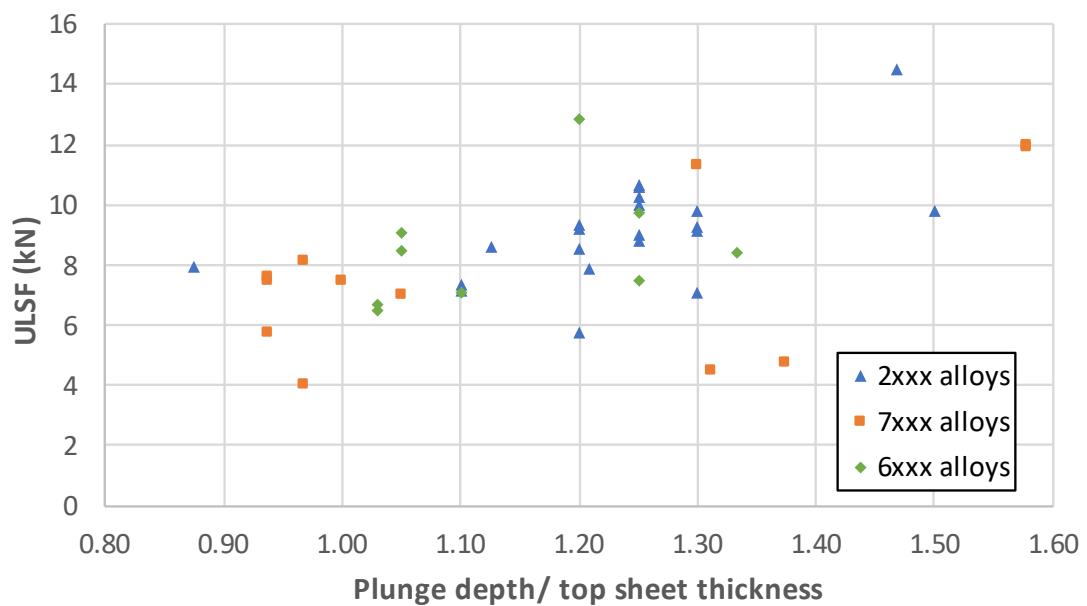


Figure 2-12: Summary of RFSSW literature comparing ULSF with plunge depth divided by upper sheet thickness [2.9,2.16,2.17,2.19,2.21,2.22,2.24,2.27,2.28,2.32–2.35,2.43–2.69]

2.3.3.4 Volume ratio

Volume ratio is a sparsely referenced parameter in the literature. It refers to the ratio between the volume of material displaced as the shoulder plunges into the workpiece

(V_S), to the volume of space created by the retracting probe (V_P). This can be defined according to Equation 2-1:

Equation 2-1

$$\text{Volume ratio} = \frac{V_S}{V_P} = \frac{A_S * y_S}{A_P * y_P}$$

Where $A_{\{S,P\}}$ are the cross-sectional areas of the shoulder and probe, and $y_{\{S,P\}}$ are the vertical distances travelled by the shoulder and probe during the process. For example, a ratio of 1 indicates that the probe retracts exactly enough to accommodate the displaced material, leaving no space between the probe and material. A ratio of less than 1, however, would mean the probe retracts further than required, leaving a space between probe and material. Figure 2-13 provides a simple depiction of this principle.

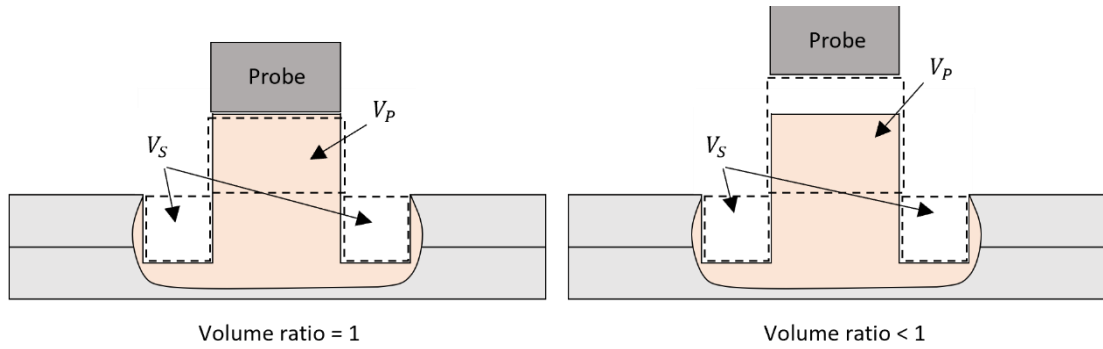


Figure 2-13: Diagram of weld cross-sections with different volume ratios

No published studies have thus far explored the influence of different volume ratio on joint performance, however Berger et al. [2.70] refer to “non-volumetric kinematics” to describe welding with a volume ratio greater than 1. The authors found that such conditions increase the hydrostatic pressure within the SZ, improving forging between the sheets and reducing volumetric defects.

2.4 Numerical modelling of refill friction stir spot welding

Coupled thermomechanical Finite element analysis (FEA) has been utilised to simulate metal forming processes such as hot/cold forging, rolling, and extrusion [2.71–2.73]. However, comparatively few researchers have applied these techniques to solid-state joining processes, with the vast majority of published literature

addressing FSW, rather than FSSW or RFSSW. The principal challenges encountered when using FEA to model these processes are the mesh distortion due to extreme plastic deformation of the workpiece material, and the highly complex contact conditions between the tool and the workpiece.

In the present work, the Abaqus Explicit FEA package is used for all modelling activities, and the terminology has been adopted based on the Abaqus technical documentation. Terminology for different software packages may vary, yet analogous functions and settings to those discussed herein are generally available. This section introduces the key model parameters and summarises the state of the art in the current modelling literature.

2.4.1 Coupled Eulerian-Lagrangian modelling

Finite element modelling relies on the discretisation of a model geometry into many small parts, or elements, through a process called meshing. In traditional mechanical FEA, the nodes of a mesh are fixed to the material being modelled, meaning that as the material is deformed, so too is the mesh; this is referred to as a Lagrangian model. Lagrangian models benefit from their simplicity and stability and are highly effective at simulating most thermal-mechanical problems. However, a notable limitation of this modelling approach is its ability to handle large deformation problems such as in friction stir processes, where the corresponding mesh distortion can cause model instabilities [2.74].

A modern FEA technique well suited to addressing large deformation problems is the Coupled-Eulerian Lagrangian (CEL) method. Unlike a Lagrangian mesh, a Eulerian mesh does not discretise the material itself, but rather the space in which the material is contained. The material moves through the mesh as it is deformed, but the mesh itself remains fixed. This is achieved using the volume-of-fluid method of material tracking [2.75], which bypasses the issues associated with mesh distortion in conventional Lagrangian modelling, although at the cost of high computational effort [2.76]. Figure 2-14 illustrates the difference between the two mesh types.

As the name suggests, CEL models combine the use of both element types, using

Eulerian elements to capture the heavily deformed material, and Lagrangian elements to capture those that are comparatively rigid and experience little or no deformation. This is particularly suited to simulating the FSW, FSSW, and RFSSW processes, where the soft, heavily plasticised workpiece can be modelled using Eulerian elements, while Lagrangian elements model the rigid tool [2.77].

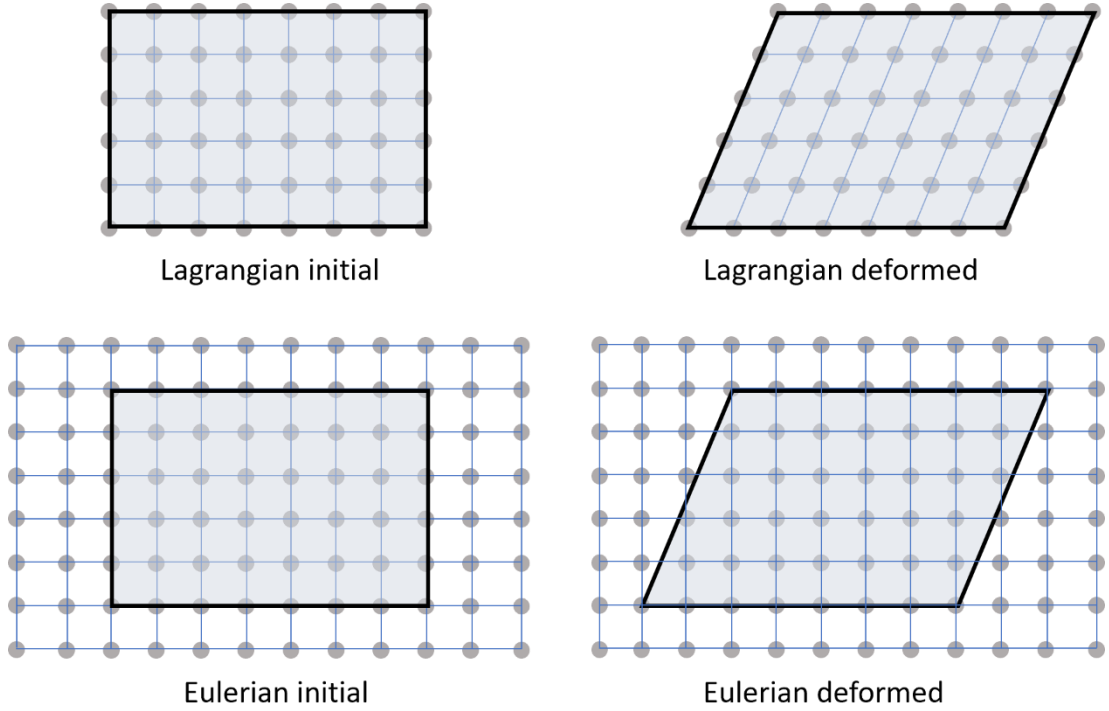


Figure 2-14: Illustration of material deformation in FEA model using Lagrangian and Eulerian meshes

2.4.2 Constitutive material model

A constitutive model is used to describe the relationship between a material's flow stress (σ), temperature (T), strain (ϵ), and strain rate ($\dot{\epsilon}$) [2.78,2.79]. Given the variation in temperatures, tool rotational speeds, strains, etc. during the RFSSW process, a suitable constitutive model is critical for achieving accurate results. The Johnson-Cook formula is given in Equation 2-2 and the variable descriptions are given in Table 2-1 [2.79].

Equation 2-2

$$\bar{\sigma} = [A + B\bar{\epsilon}^n] \left[1 + C \ln \frac{\dot{\bar{\epsilon}}}{\dot{\bar{\epsilon}}_0} \right] \left[1 - \left(\frac{T - T_{ref}}{T_{melt} - T_{ref}} \right)^m \right]$$

This plasticity model is well established in the literature and is by far the most

common approach used for simulating friction stir processes (references provided in Table 2-2) and as such is utilised in the current work.

Table 2-1: Johnson-Cook variables

Variable	Description
$\bar{\sigma}$	Equivalent material flow stress
A	Reference tensile strength
B	Strain hardening constant
$\bar{\epsilon}$	Equivalent strain
$\dot{\bar{\epsilon}}$	Equivalent strain rate
$\dot{\epsilon}_0$	Reference test strain rate
T, T_{melt}, T_{ref}	Temperature, materials melting temperature, reference test temperature
C, n, m	Experimentally derived constants

2.4.3 Thermal parameters

2.4.3.1 Heat generation

Heat is generated through two mechanisms: plastic deformation, and frictional heating. Equation 2-3 [2.80] defines the plastic deformation component, where Q_p is the total heat energy from plastic deformation, σ is the equivalent shear stress, $\dot{\epsilon}$ is the strain rate, and η is the inelastic heat fraction. Various values of η are proposed in the modelling literature, typically ranging from 90% to 100% (Table 2-2), although a dedicated study of inelastic heating found that 95% was correct for AA2024-T3 [2.81]. An inelastic heat value of 95% is adopted in the current work.

Equation 2-3

$$Q_p = \sigma \dot{\epsilon} \eta$$

However, the majority of thermal energy is contributed through frictional heating [2.82] as defined in Equation 2-4, where Q_f is the total frictional heat, τ is the frictional shear stress, and $\dot{\gamma}$ is the slip rate.

Equation 2-4

$$Q_f = \tau \dot{\gamma}$$

In the vast majority of relevant literature, τ is determined using the Coulomb's friction model as given in Equation 2-5 [2.83], where μ is the friction coefficient, and σ_n is the normal contact pressure. This simplified model assumes frictional stress to be entirely independent of sliding velocity.

Equation 2-5

$$\tau = \mu\sigma_n$$

Abaqus expands upon this by incorporating a user defined maximum shear stress (τ_{max}); when $\tau = \tau_{max}$, a total slip condition is assumed and τ remains constant regardless of σ_n [2.84].

While Coulomb's model is accepted as a suitable approximation for friction conditions within the literature, the friction coefficient selected by researchers varies significantly. Choudary et al. [2.85], Zhu et al. [2.86], and Al-Badour et al. [2.87] all simulated FSW of AA6061 but adopted different friction coefficients of 0.2, 0.58, and 0.8 respectively. Each of these researchers reported good agreement between modelled and experimental temperatures and material flow, suggesting that factors other than friction coefficient drive a model's accuracy.

In practice, friction coefficient fluctuates with temperature and sliding velocity [2.88]; a number of researchers have considered this in their simulations [2.59,2.77,2.79,2.80,2.82,2.89,2.90]. Experiments by Xiong et al. [2.82] showed that μ between an H13 steel tool and an AA2524 sheet decreases as tool rotational speed and temperature increase, varying between 0.25 and 0.07. Based on this data, the authors calculated that an average μ of 0.13 was suitable for modelling RFSSW and achieved strong correlation with experiments in terms of temperature and material flow predictions. Elsewhere [2.79,2.89], constant friction coefficients have been assumed below a defined workpiece temperature, above which μ declines towards 0. Janga et al. [2.59] modelled RFSSW of AA7075-T6 and adopted a friction coefficient that degraded linearly from 0.35 at room temperature to 0.01 at the material melting temperature. The authors demonstrated robust validation of the model and found

good agreement with experimental temperature data and material flow behaviour. This friction model [2.59] is utilised in chapter 5 of the current work.

2.4.3.2 Heat distribution

Frictional heat generated between the tool and workpiece is dissipated into both bodies. However, due to differences in the thermal conductivity and heat capacity of each material, the ratio of heat distribution is uneven. Although rarely discussed in the literature (see Table 2-2), researchers have achieved accurate results by assuming that 50% of the heat enters the workpiece [2.90], while others have assumed 90% [2.91,2.92] with satisfactory results. A combined experimental and numerical study by Chao et al. [2.93] found that for FSW of aluminium using a steel tool, 95% of all heat generated will be dissipated into the aluminum workpiece due to its higher thermal conductivity and specific heat capacity. A value of 95% heat dissipation into the workpiece is assumed in the current work.

2.4.3.3 Thermal boundary conditions

As with heat generation, heat exiting the system through conductive, convective, and radiative losses must also be carefully considered. Figure 2-15 illustrates these losses within a typical RFSSW joining set-up. Q_1 and Q_4 are well documented in the literature, with the majority of researchers agreeing on values of 1000 W/m^2 , and 30 W/m^2 respectively [2.76–2.80,2.82,2.84,2.87,2.91]. Q_2 , Q_3 , and Q_5 are rarely discussed in the literature, although Guerdoux and Fourment [2.94] reported that $20,000 \text{ W/m}^2$ was appropriate for conduction into the tool body (Q_3), and Wen et al. [2.78] included radiative losses in their model. Values of 1000 W/m^2 , and 30 W/m^2 have been assumed in the current work for Q_1 and Q_4 respectively.

2.4.3.4 Computational efficiency

The primary approaches for improving computational efficiency in CEL simulations are known as time scaling and mass scaling. The time scaling approach involves artificially increasing the loading rates (rotational speed and plunge rate) within the model to reduce the overall length of the simulation. In this case, thermal conductivity and all thermal boundaries must also be scaled appropriately [2.95].

Although very little published literature discusses time scaling, Li [2.96] used this technique when simulating FSW of AA2024-T3. Loading rates were increased by a factor of 100 and through trial and error it was found that accurate results could be obtained by scaling thermal conductivity, convective heat losses, and conductive heat losses by a factor of 200, 100 and 20 respectively. These values have also been applied in chapter 5 of the present work.

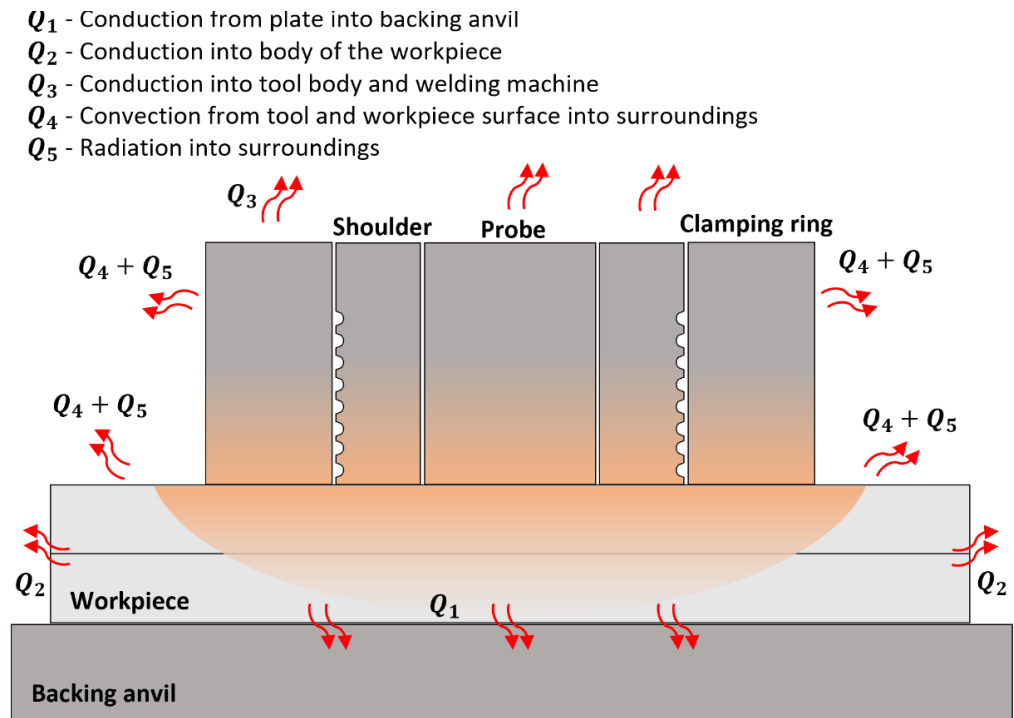


Figure 2-15: Diagram of thermal boundaries in a typical RFSSW simulation

The mass scaling technique operates by artificially increasing the density of materials within the model [2.95]. In explicit dynamics, simulations are divided into a large number of time increments that are solved sequentially. As such, a larger time increment means fewer total increments to be solved and a lower overall computational cost [2.97]. The practical upper limit for this time increment, referred to as the stable time increment (Δt_{stable}), is dictated by the dilatational wave speed (c_d) of the materials within the model, as well as the characteristic length of the smallest element (L_e). This relationship is given in Equation 2-6 and essentially describes the time taken for a stress wave to pass through the model's smallest element [2.97].

$$\Delta t_{stable} = \frac{L_e}{c_d}$$

A Δt_{stable} greater than this would imply that stress could propagate through certain elements of the model without being recorded; this would lead to major numerical instabilities.

Equation 2-7 shows that c_d is determined by the Young's modulus (E) and the density (ρ) of the modelled material, meaning that increasing ρ acts to slow the dilatational wave speed and increase Δt_{stable} .

$$c_d = \sqrt{\frac{E}{\rho}}$$

While there is no fixed rule for the level of mass scaling that can be applied to a model, too high a scaling factor will cause inertial forces to dominate the model and reduce solution accuracy [2.97]. Constantin et al. [2.95] simulated FSW of copper using mass scaling factors between 1 – 10,000. As shown in Figure 2-16a, computation time was reduced from 4000 hours to 162 hours by applying a mass scaling factor of 1000 but introduced a 10% error in peak temperature (Figure 2-16b). Xiong et al. [2.82] on the other hand reported accurate results for simulations of RFSSW of AA2524 with a mass scaling factor of 10,000.

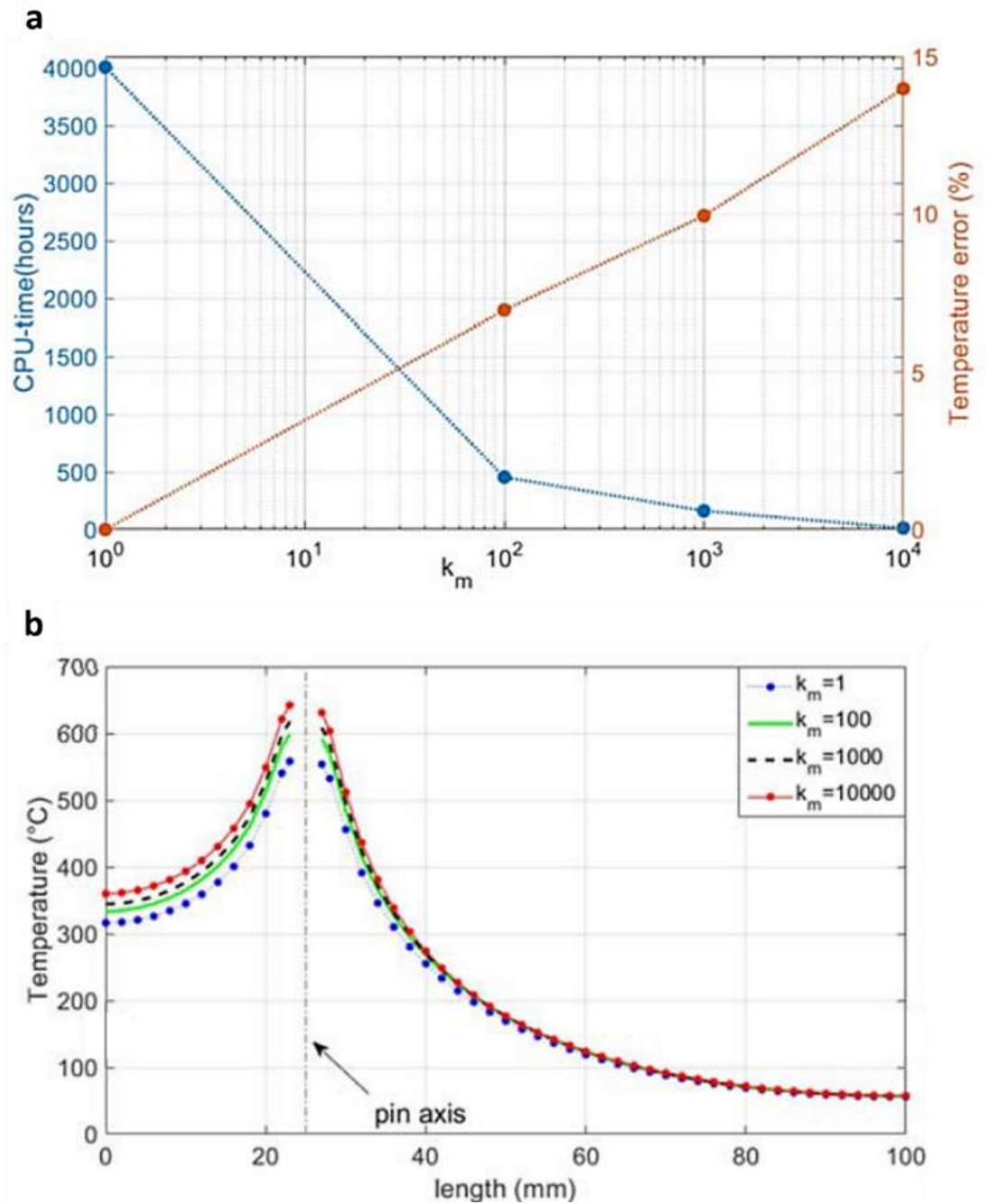


Figure 2-16: Results from Constantin et al. [2.95] showing the influence of mass scaling factor on simulations of FSW, a) computation time vs temperature error compared with no mass scaling, b) temperature profile along the weld path mid-way through process

Table 2-2: Summary of model details reported in literature for coupled Eulerian-Lagrangian modelling of friction stir welding processes

Reference	Process	Tool definition	Constitutive model	Plastic heat generation	Frictional heat generation	Heat fraction to workpiece	Validation method
Zhang et al. [2.79]	RFSSW	Rigid	Johnson-Cook	$\eta = 90\%$	Coulomb's law - $\mu = 0$ to 0.25	N/A	Thermocouple
Xiong et al. [2.82]	RFSSW	Rigid	Johnson-Cook	$\eta = 90\%$	N/A	N/A	Thermocouple
Chu et al. [2.98]	FSSW	N/A	N/A	N/A	N/A	N/A	N/A
Akbari et al. [2.99]	FSW	N/A	Johnson-Cook	N/A	Coulomb's law		Thermocouple
Das et al. [2.100]	FSW	N/A	N/A	N/A	N/A	N/A	Thermal camera
Ragab et al. [2.101]	FSW	Rigid	Johnson-Cook	$\eta = 90\%$	Coulomb's law - $\mu=0.35$	N/A	Flow pattern
Wen et al. [2.78]	FSW	Rigid Isothermal	Johnson-Cook	$\eta = 90\%$	Coulomb's law - $\mu=0.577$	N/A	N/A
Ansari et al. [2.84]	FSW	Deformable	Johnson-Cook	$\eta = 100\%$	Modified Coulomb's law	N/A	Thermocouple
Li et al. [2.91]	FSW	Rigid Isothermal	Johnson-Cook	N/A	N/A	0.9	Thermocouple
Chauhan et al. [2.102]	FSW	Rigid Isothermal	Johnson-Cook	$\eta = 100\%$	N/A	0.7	Tool forces
Zhu et al. [2.86]	FSW	Rigid Isothermal	Johnson-Cook	$\eta = 90\%$	Coulomb's law - $\mu=0.58$	N/A	Thermocouple
Vairis et al. [2.90]	FSW	Deformable	Johnson-Cook	$\eta= 90\%$	Coulomb's law	0.5	None
lordache et al. [2.103]	FSW	Rigid	Johnson-Cook	N/A	Coulomb's law - $\mu=0.3$	N/A	None
Malik et al. [2.104]	FSW	Rigid Isothermal	Johnson-Cook	N/A	Coulomb's law - $\mu=0.1$	N/A	None
Al-Badour et al. [2.87]	FSW	N/A	Johnson-Cook	N/A	Coulomb's law - $\mu = 0.8$	N/A	Thermocouple
Raut et al. [2.105]	FSW	N/A	Johnson-Cook	N/A	Coulomb's law - $\mu = 0.4$	N/A	Thermocouple
Salloomi et al. [2.76]	FSW	Deformable	Johnson-Cook	$\eta = 100\%$	Coulomb's law - $\mu = 0.01$ to 0.21	N/A	Thermocouple
Das et al. [2.106]	FSW	N/A	Johnson-Cook	N/A	N/A	N/A	Thermocouple
Choudhary & Jain [2.85]	FSW	N/A	Johnson-Cook	$\eta = 100\%$	Coulomb's law - $\mu=0.2$	0.7	Tool forces
Salloomi [2.77]	FSW	Rigid	Johnson-Cook	$\eta= 90\%$	Coulomb's law - $\mu = 0.01$ to 0.21	N/A	Microstructure
Al-Badour et al. [2.107]	FSW	Rigid Isothermal	Johnson-Cook	N/A	Coulomb's law $\mu = 0.3, 0.58, 0.8$	N/A	Tool forces, microstructure
Salloomi [2.80]	FSW	Rigid	Johnson-Cook	$\eta = 90\%$	Coulomb's law - $\mu = 0.01$ to 0.21	N/A	Thermocouple

2.5 Applications of RFSSW

Although RFSSW remains in its research and development phase due to the key issues of variability of results, tool wear and long welding times, demonstrator components have been produced using the process. Derlatka et al. [2.108] joined C-channels and flat sheets of AA6061-T6 to produce the thin-walled I-beam structure shown in Figure 2-17. The beam was evaluated using three-point bending tests and the authors claim that these results prove the viability of this manufacturing approach.

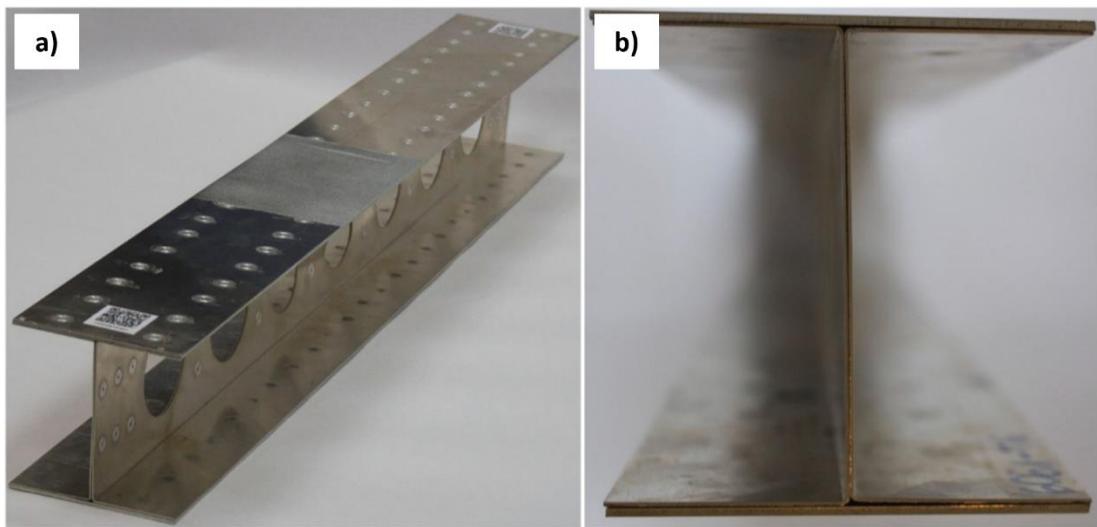


Figure 2-17: Thin-walled AA6061-T6 I-beam constructed using C-channels, flat sheets, and RFSSW joints a) overview of beam b) end elevation of beam [2.108]

Patnaik et al. [2.109] joined an AA2024 sheet and T-section to create a stiffened skin panel. When subject to compression testing, the panel buckled in the predicted manner, but with a 9% lower load than its riveted equivalent. Kubit et al. [2.110] produced a stringer-stiffened panel of AA7075-T6 to replicate an aircraft skin structure and achieved similar buckling loads to a conventional riveted panel. Okada et al. [2.111] used a robotic RFSSW system to manufacture a structural mock-up of a helicopter door (Figure 2-18). The door was assembled of AA6061 and AA2024 sections and the authors identified minimal thermal and residual stress distortions from the welding process.

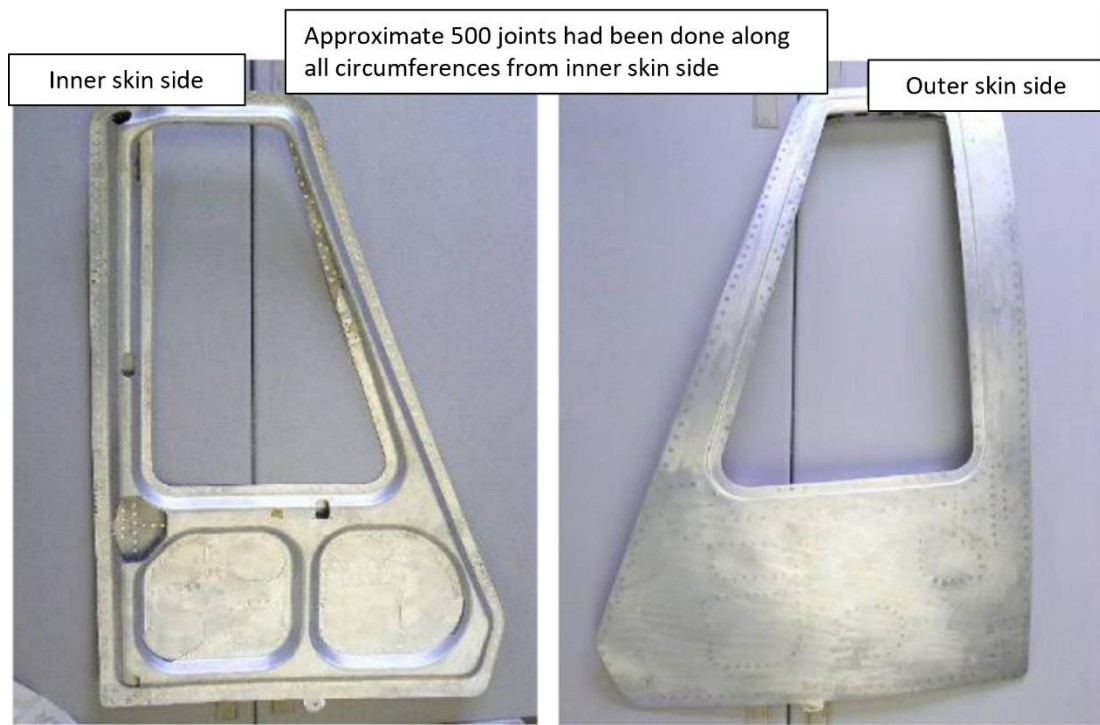


Figure 2-18: Mock-up of helicopter door produced using RFSSW joints of AA6061 and AA2024 sections. Adapted from [2.111]

2.6 Concluding remarks

Despite the advantages of RFSSW compared to alternative processes, such as high joint strength, reduced thermal effects, the ability to join dissimilar alloys, and insensitivity to operating environment (i.e. no need for shielding gas), there remain several issues hindering its industrial adoption.

The collective research focus on maximising weld strength has produced a body of literature that demonstrates well the mechanical performance of the process, but nevertheless does not make it attractive to industrial users due to the long welding times typically adopted; this is particularly true for 2xxx series aluminium alloys. To better prepare the process for industrialisation, it is important to understand its performance using welding times comparable to competitor technologies (1-2 s).

A substantial number of numerical studies exist for solid-state welding processes, namely FSW, although in most cases key model details are omitted from the methodology. Studies of RFSSW are limited, which can be attributed to its novelty compared with FSW, as well as the added complexity of modelling the multi-part tool.

This represents a clear gap in the current state-of-the-art which will be addressed in the current work.

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Chapter 3. Exploring the influence of short welding times on joint performance

Long welding time is an ongoing challenge that hinders industrial adoption of the process. To address this, the present study explores much shorter welding times than those previously reported in the literature and assesses the impact on joint quality. Joints of 1.8 mm thick AA2024-T3 sheet were produced with welding times from 3 s, down to 0.75 s and rotational speeds of 1000 rpm to 2500 rpm. These findings provide a benchmark against which weld quality improvement can be assessed in subsequent chapters and have formed the basis of a published journal article [3.1].

3.1 Introduction

As discussed in section 2.3.3, much research to date has addressed the influence of rotational speed, plunge depth and welding time on refill friction stir spot welding (RFSSW) joint properties. However, the predominant focus of this research [3.2,3.3] has been optimising welding parameters to maximise ultimate lap shear force (ULSF), without consideration for the requirements of industrial users, such as production rate. Welding time, the time taken to complete the plunge and refill stages of the RFSSW cycle, presents a challenge when considering the future adoption of the process in mass manufacturing industries [3.4–3.6]. A key competitor for RFSSW, resistance spot welding, provides a useful comparison in terms of welding time, requiring approximately 1-2 s per weld [3.7]; RFSSW should match or improve on this to be a competitive joining technology for high production rate applications. A limited number of studies have shown that welding times of around 2 s are achievable for common automotive aluminium alloys such as 5xxx and 6xxx series [3.5,3.8–3.10]. However, little is known about applying such short welding times to aerospace alloys, such as the 2xxx series, with the vast majority of literature addressing welding times greater than 5 s [3.11–3.20].

The adverse relationship between welding time and the formation of weld defects has been previously reported [3.21,3.22]. Shang et al. [3.22] investigated welds of 2

mm thick AA2060-T8 with weld times of 4.8 - 6 s and rotational speed of 2500 rpm. A maximum ULSF of 5.71 kN was achieved and cavity defects were observed within the welds, characteristic of poor material flow into the shoulder region of the weld. The authors [3.22] concluded that voids do not meaningfully influence the ULSF, which is instead influenced by the hook morphology. A study by da Silva et al. [3.15] investigated welds of AA2024-T3 with a welding time of 3.8 s. The highest ULSF was achieved with a rotational speed of 1900 rpm and 1700 rpm for the plunge and refill stages respectively; however, voids were still found to be present within the weld. Tier et al. [3.20] investigated joints of Alclad coated AA2024-T3 and concluded that higher ULSF is achieved at longer welding times (10.68 kN at 4.81 s). Amancio-Filho et al. [3.11] studied the influence of welding time on AA2024-T3 joints. Varying the welding time from 4.8 - 6.8 s and rotational speed from 1900 - 2900 rpm, the highest ULSF of 10.3 kN was achieved with the longest time and lowest rpm. Parra et al. [3.23] studied joints of AA6181-T4 with welding time ranging from 2 - 3.4 s. The maximum ULSF was achieved with a welding time of 3 s, below which the ULSF decreased and the results became more scattered. It was concluded that welding times shorter than 3 s caused excessively high strain rates and the formation of defects [3.23].

Hovanski et al. [3.24] presented the only prior study of welding time reduction for aerospace series aluminium joints. The investigation assessed AA7075-T6 joints and showed that, using a 7 mm diameter shoulder, ULSF of 4.28 kN could be achieved with a welding time of 1 s and a rotational speed of 3000 rpm. However, no micrographs of the resulting weld cross-sections were presented, and the presence of any weld defects was not discussed. To the best of the author's knowledge, de Castro et al. [3.25] achieved the shortest welding time for a 2xxx series joint, reporting a ULSF of 7.95 kN at a welding time of 3 s. It was found that ULSF decreased as welding time was reduced, but that this could be counteracted by raising the rotational speed to increase heat generation. However, an important caveat to these findings [3.25] is the narrow parameter window that was investigated (rotational speeds of 1100 – 1500 rpm, welding times of 3 -5 s).

The objective of the current study is to address the knowledge gap that exists for short

duration RFSSW of 2xxx series aluminium alloys. Through detailed analysis of the weld defects, microstructures, mechanical properties and failure modes, an understanding of the influence of short welding times on these characteristics is developed that will support future industrial uptake of the RFSSW process.

3.2 Experimental methods

3.2.1 Materials

The base material used in this work is 1.8 mm thick AA2024-T3 sheet. This high strength, heat treatable aluminium alloy is commonly used as a fuselage skin material in aerospace applications. AA2024 is less corrosion resistant than many aluminium alloys due to its high copper content [3.26] and is often supplied with a thin protective coating of pure aluminium (Alclad); however, an uncoated variant is used in this work. Table 3-1 lists the chemical composition and mechanical properties of AA2024-T3. The chemical composition was provided by the supplier inspection certificate [3.27], while the mechanical properties were established in-house using an Instron 5969 tensile testing machine, in accordance with BS EN ISO 6892-1:2019 [3.28].

Table 3-1: Chemical composition and mechanical properties of AA2024-T3 [3.27]

Chemical composition (wt%)										Mechanical properties		
Al	Si	Fe	Cu	Mn	Mg	Cr	Ni	Zn	Ti	Tensile strength (MPa)	Yield strength (MPa)	Elongation (%)
Bal.	0.5	0.5	4.9	0.9	1.8	0.1	0.05	0.25	0.15	509	375.8	13.9

3.2.2 Welding equipment

3.2.2.1 Welding machine

All welding was performed using a Harms & Wende RPS 100 welding head mounted on a C-frame structure, shown in Figure 3-1. The RPS 100 actuates each of the tool components independently, allowing for a constant clamping force to be applied through a pneumatic ram, while the shoulder and probe are controlled by electric motors. However, this system does not have the ability to monitor tool reaction forces. The shoulder and probe movement is controlled by specifying displacements and durations for each stage (plunging, dwelling, refilling).

3.2.2.2 Tooling

The shoulders and probes used in this study were manufactured at the University of Strathclyde, using the design specifications of the welding machine manufacturer (see Figure 3-2). The clamping ring was supplied directly from the manufacturer. H13 hot work tool steel was used for all tool components. A detailed discussion on tool geometry can be found in the work of Li et al. [3.29].

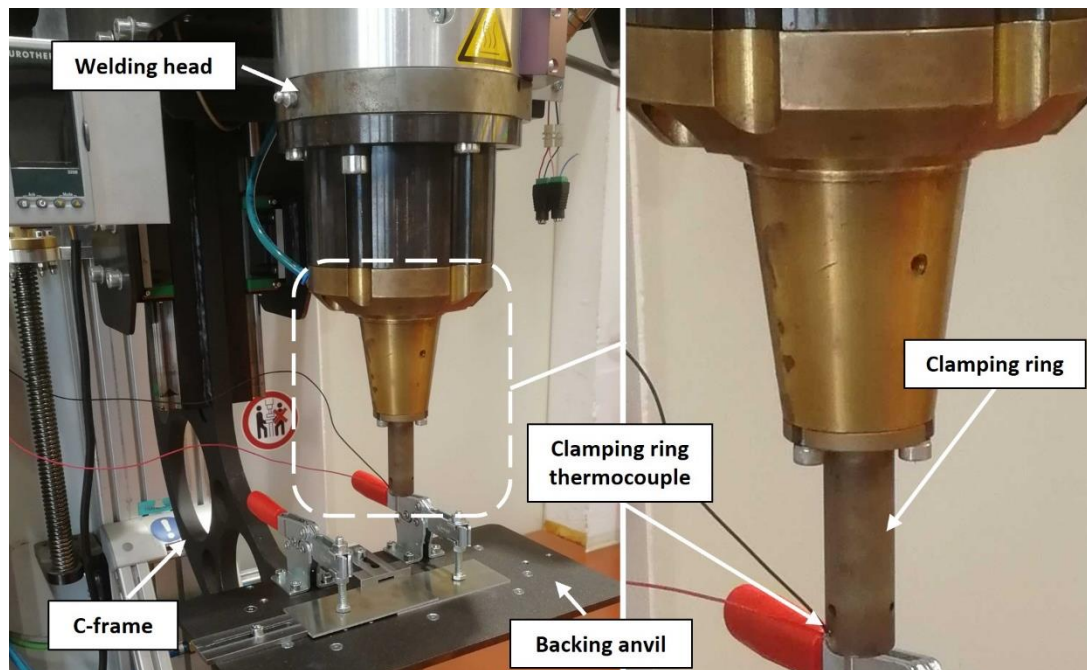


Figure 3-1: Harms & Wende RPS 100 refill friction stir spot welding machine

3.2.3 Weld specimens

3.2.3.1 Parameter selection

As detailed in Table 3-2, three welding times were selected to assess the influence on ULSF and defect formation. The limitations of the welding machine dictated a minimum welding time as 0.75 s. The maximum time of 3 s was selected to match the previous shortest welding time of a defect free joint reported in the literature for a 2xxx series aluminium [3.25]. The rotational speed of the tools was varied from 1000 - 2500 rpm to assess the influence of different heat inputs within the optimal range discussed in section 2.3.3. The shoulder plunge depth was kept constant at 2.25 mm, equal to 1.25 times the top sheet thickness. This is broadly in line with the optimal plunge depth most often identified in the literature and discussed in section 2.3.3.

The tool volume ratio was kept constant at 0.8. Table 3-2 details the full list of welding parameter sets investigated. Three replicant tensile test specimens and two microscopy specimens were produced for each parameter set, with the exception of sets 09 and 10. In these cases, the maximum spindle torque of the welding machine was exceeded due to the combination of low rotational speed and short welding time, thus the welds could not be completed.

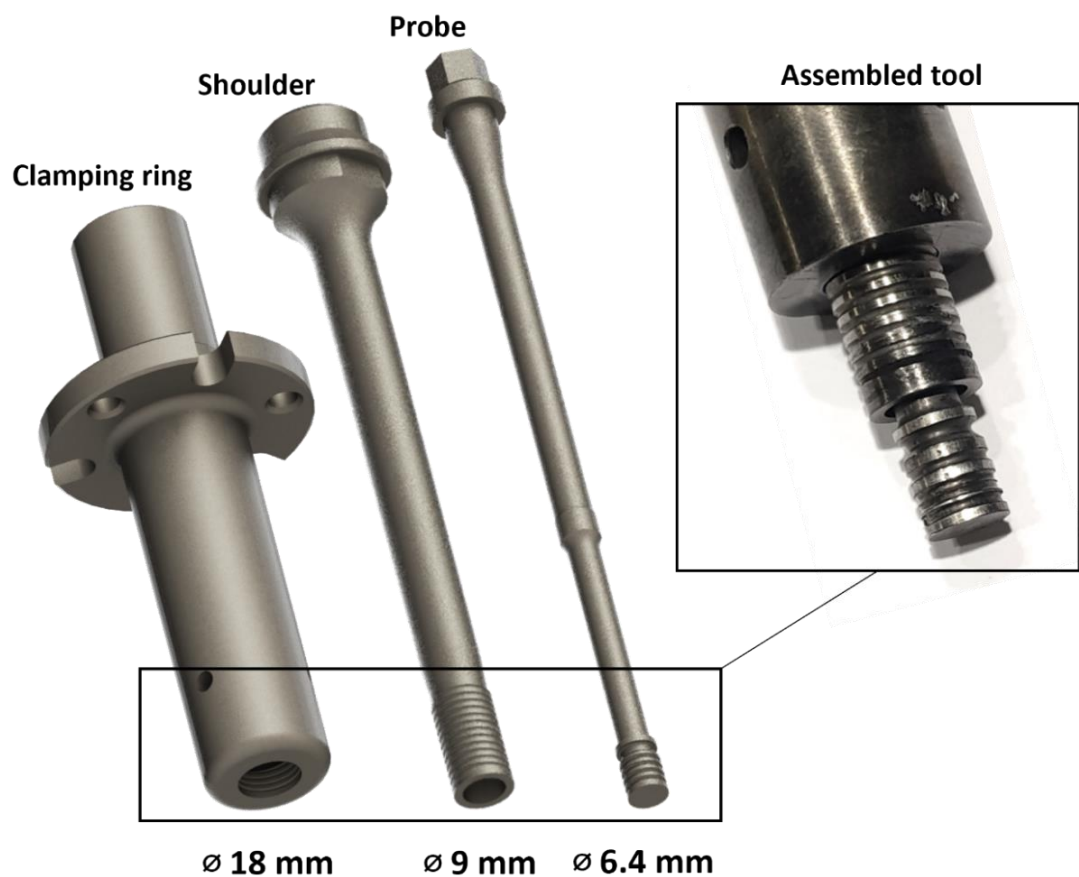


Figure 3-2: Exploded view of clamping ring, shoulder, probe, and assembled welding tool

Table 3-2 Welding parameters for sample production (volume ratio = 0.8)

Parameter ID	Rotational speed (rpm)	Welding time (s)	Plunge depth (mm)
01	1000	3	2.25
02	1500		
03	2000		
04	2500		
05	1000	1.5	
06	1500		
07	2000		
08	2500		
09	1000	0.75	
10	1500		
11	2000		
12	2500		

3.2.3.2 Welding procedure

At the beginning of each welding session, several preparatory steps were taken to ensure consistent tool conditions. Firstly, all tool components were soaked in a 5% solution of sodium hydroxide to remove residual aluminium left over from previous welding sessions; the tools were left in the solution until all aluminium had visibly dissolved. The first several welds attempted with clean tools are typically of low quality as fresh material fills the gaps between the tool components, leaving substantial lack-of-refill defects. For this reason, 3-5 warm-up welds were performed until the appearance of the weld surface was consistent. This entire cleaning procedure was repeated after approximately every 50 welds to prevent tool seizures. To ensure a consistent weld position for all specimens, the holding jig shown in Figure 3-3 was used to secure the aluminium coupons during welding.

All weld specimens were produced in the following sequence:

1. Clean the coupon surfaces with isopropanol to remove potential contaminants.
2. Secure the coupons into the holding jig (Figure 3-3).

3. Perform a dummy weld to raise the temperature of the welding tools to between 200°C to 300°C.
4. Begin the welding cycle when the clamping ring temperature cools to 125°C (monitored through the attached thermocouple).
5. Immediately after the welding cycle, remove the welded specimen from the holding jig and place on a steel block to cool.
6. Record rotational speed and tool position data output from the welding machine to ensure correct welding parameters were used.

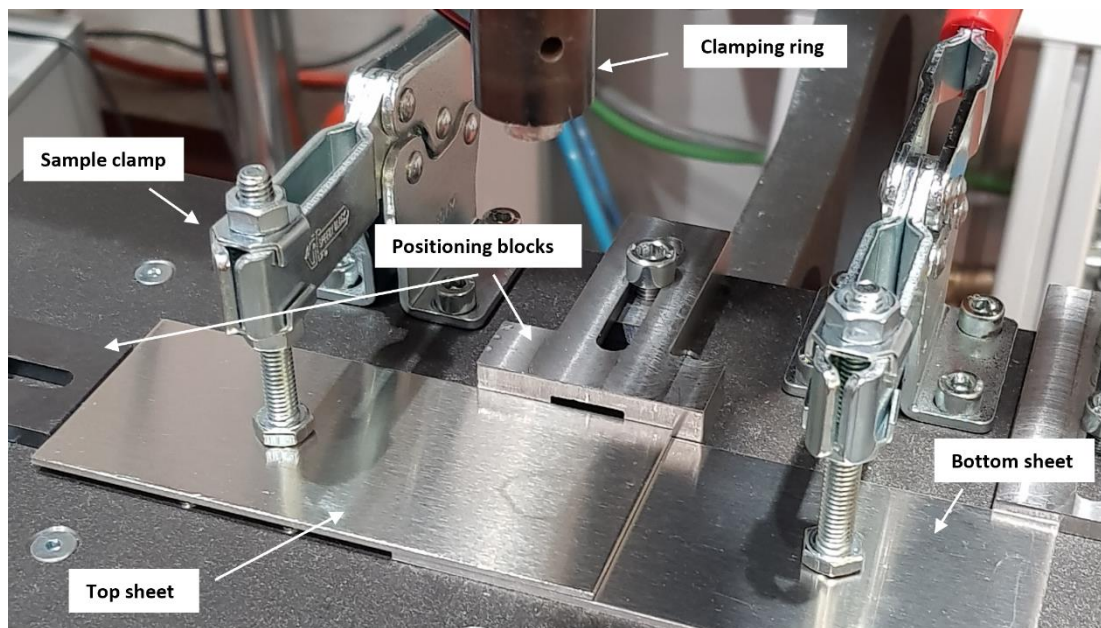


Figure 3-3 Holding jig used to position aluminium sheets during the joining process

3.2.4 Microstructural analysis

Welds were prepared for metallographic inspection using a Struers Rotopol-21 polishing wheel with a Rotoforce-4 semi-automatic head unit. Specimens were sectioned vertically through the weld centre and mounted in cold-setting epoxy resin, followed by grinding and polishing with silicon carbide paper and diamond suspension. The stages of the preparation process (load, time, etc.) are detailed in Table 3-3. Electro-etching was performed in Barker's solution (20 ml HBF₄, 80 ml H₂O) for 120 s. The microstructure and defect formation of the welds were optically examined using an Olympus GX51 metallurgical microscope. Fractography of the weld

failure surfaces was performed using a Hitachi S-3700N scanning electron microscope (SEM), following mechanical testing.

Table 3-3 Details of metallographic preparation process

Stage	Grit/particle size	Time (s)	Wheel speed (rpm)	Load (N)
Grinding	1 st	P500	30 – 60	300
	2 nd	P800	120	
	3 rd	P1200	120	
	4 th	P2000	120	
Polishing	5 th	6 μm	120	15
	6 th	3 μm	120	
	7 th	0.05 μm	120	

3.2.5 Mechanical testing

The ULSF of the weld samples can be compared with the acceptance criteria for competitor joining technologies. Elsewhere [3.30], it has been established that the resistance spot welding standard BS ISO 16338:2017 [3.31] provides an acceptable reference for the ULSF requirements of RFSSW joints. This standard states that, for 1.8 mm thick sheets of aluminium with a tensile strength greater than 386 MPa, spot welds should carry an average minimum ULSF of 4.605 kN [3.31]. A further comparison can be made with riveting; according to MMPDS-11 [3.32], the ULSF of a single countersunk rivet joint for AA2024-T3 sheets of 1.8 mm thickness should reach a minimum of 7.27 kN. The tensile lap-shear specimen configuration was in line with BS ISO 16338:2017 [3.31] and is shown in Figure 3-4. Tests were performed using an Instron 5969 universal testing machine. Samples were loaded with a crosshead speed of 2 mm/min until failure, in accordance with EN ISO 14273.

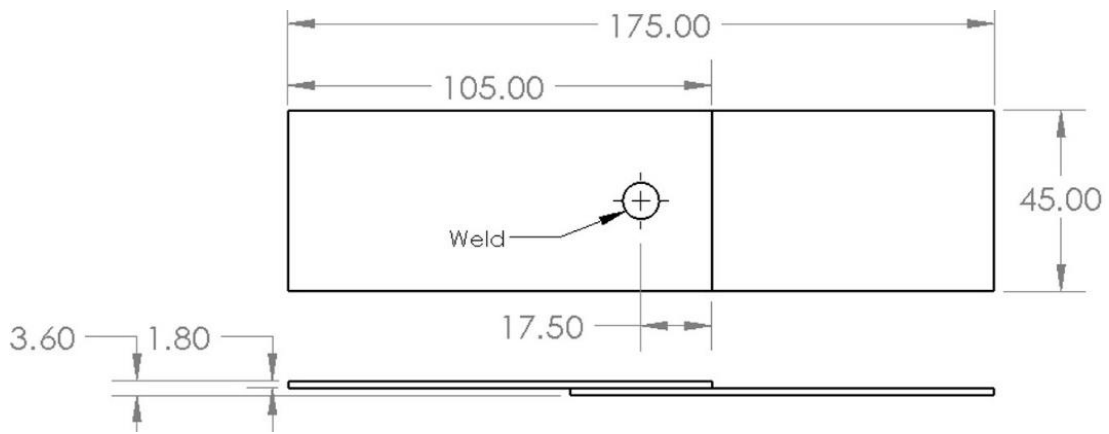


Figure 3-4: Configuration of tensile lap-shear specimen as defined in BS ISO 16338:2017 [3.31]

To assess the influence of the welding process parameters on the hardness of the material throughout the welding region, Vickers microhardness maps were produced using a Qness 60 A+ Evo hardness tester (QATM), with 0.5 N load and 10 s dwell time, in accordance with BS EN ISO 6507-4:2018 [3.33]. Measurements were taken in a square grid pattern of 0.1 mm increments.

3.3 Results & discussion

3.3.1 Defect formation

The defects commonly found in RFSSW joints, namely the tunnel defect, poor interface mixing, and hooking, were introduced in section 2.3.2. Figure 3-5 indicates the positions of each defect within a typical weld cross-section and can be referred to for clarity when viewing the following figures in this section.

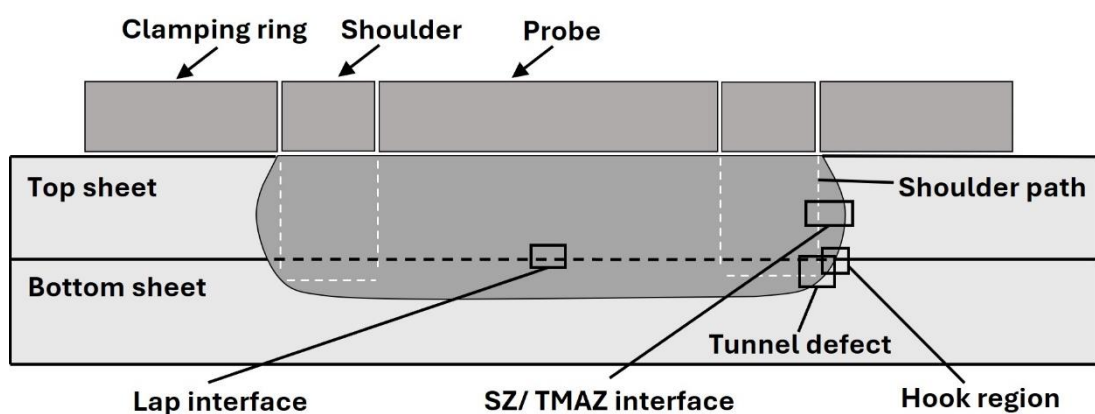


Figure 3-5 Schematic of weld cross-section showing the location of common defects

Figure 3-6a presents a complete cross-section of a defective weld, highlighting the

shoulder plunge region where the majority of defects are found. While RFSSW is generally considered an axisymmetric process, slight deviations in tool alignment, the presence of external threading on the shoulder, elasticity in the welding machine's C-frame etc. mean that microstructure through the weld cross sections inevitably varies depending on the orientation of the cut. Figure 3-6a, demonstrates this by revealing slight differences in defect shape at each side of the weld. However, these variations are minor in the context of RFSSW and do not risk the quality of a weld being mischaracterised. As such, Figure 3-6b presents micrographs of a single shoulder plunge region for each of the tested parameter sets. Due to insufficient heat input and material softening, welds could not be produced with welding times of 1.5 s or 0.75 s at 1000 rpm as the torque required exceeded the welding machine limit.

3.3.1.1 Tunnel defects

The presence of tunnel defects is clearly visible irrespective of welding parameters (Figure 3-6), although their size and position are strongly influenced by welding time and rotational speed. These defects are minimised at high welding time and low rotational speed. At all welding times it was found that increasing rotational speed led to a larger tunnel defect area. While this appears counterintuitive, as a higher rotational speed naturally results in greater heat input, which may be expected to improve material flow, the additional heat generation at the tool/material interface has been shown in other studies to promote slipping [3.34], resulting in localised mixing at the interface and reduced overall mixing in the SZ. This is in agreement with the work of Shen et al. [3.21] and Rosendo et al. [3.35] who observed comparable results attributable to tool slippage.

At shorter welding times and high rotational speed (P08 and P12), the tunnel defect region is poorly consolidated (Figure 3-6) and provides little structural integrity to the weld, contributing to low ULSF measured for these parameters (see 3.3.2). This can be attributed to a lack of heat input as the welding time is reduced, resulting in material that cannot flow back into the shoulder region during the refill stage. Similar results have been reported by other groups [3.21,3.36].

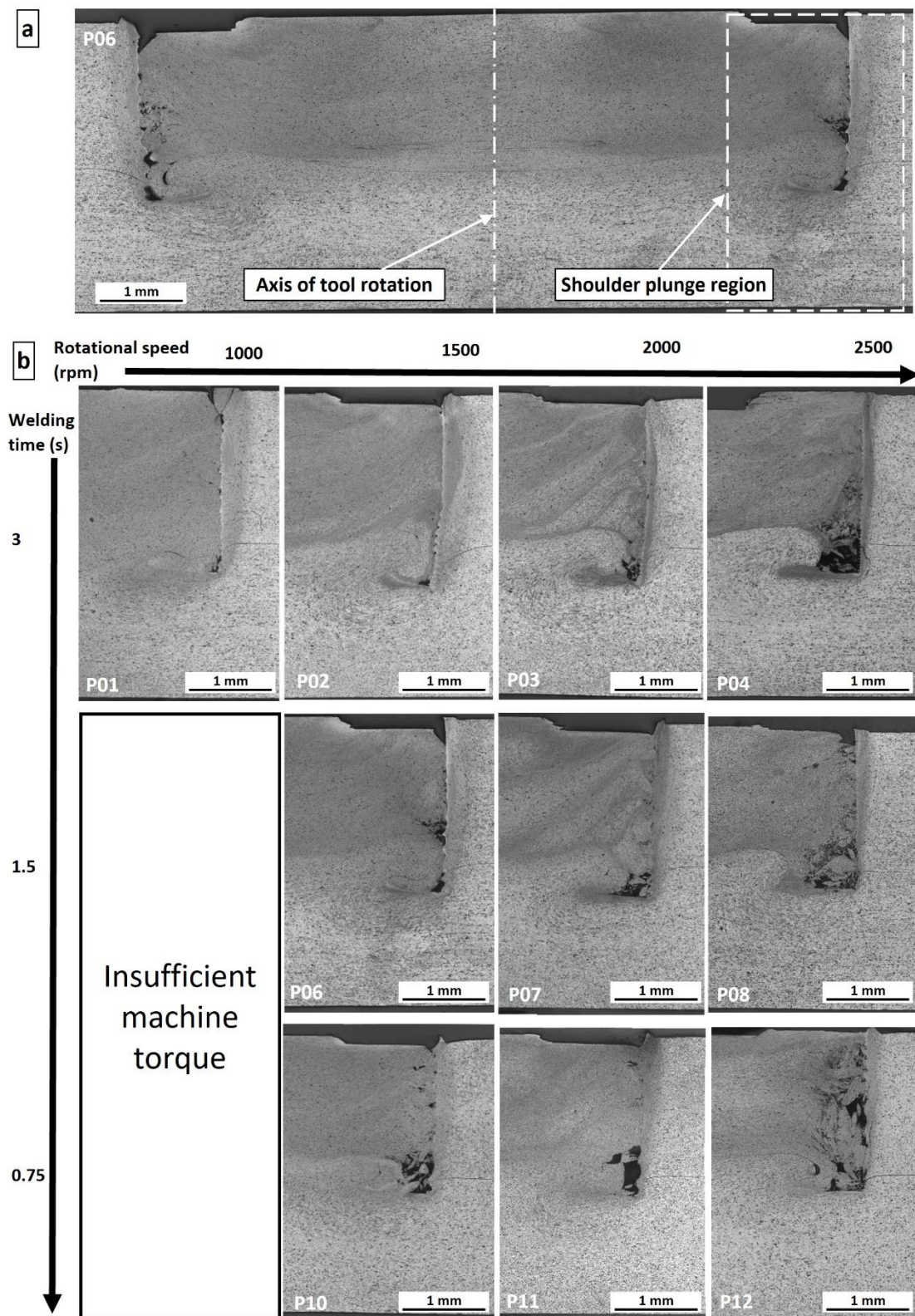
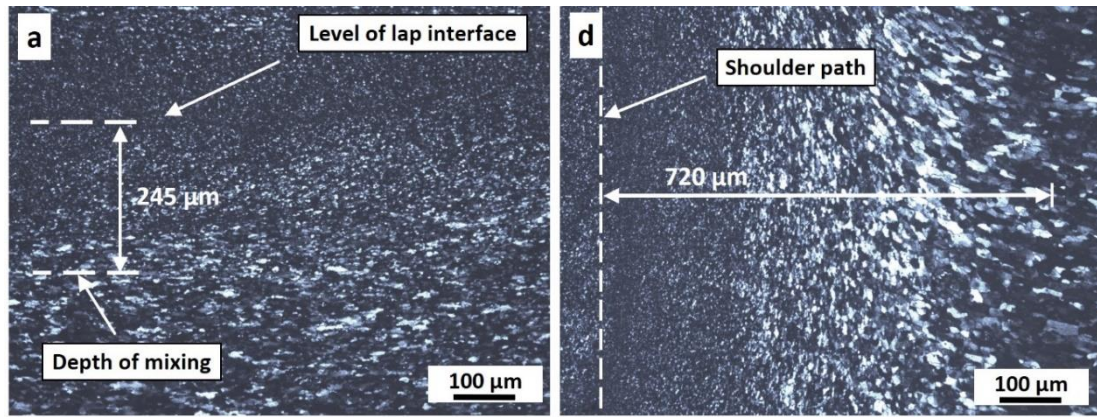


Figure 3-6 Optical micrographs of a) full cross-section of representative weld sample demonstrating relative symmetry around tool rotational axis (parameter set P06) b) comparison of shoulder plunge region of all welding parameter sets

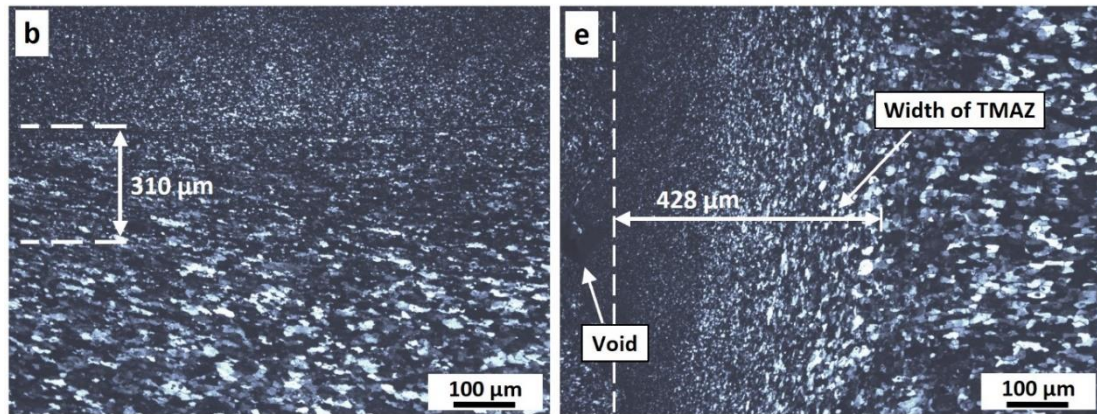
3.3.1.2 *Mixing defects*

Figure 3-7 presents micrographs of the lap interfaces (a-c), and SZ/TMAZ interfaces (d-f) of welds produced at 2000 rpm rotational speed and varying welding times. Overall, it can be seen that reducing welding time led to poorer quality mixing at both interfaces. For 3 s welds, full mixing up to $\approx 250\ \mu\text{m}$ below the lap interface is evident from the gradual transition from fine grains in the SZ, to the coarser structure of the bottom sheet, with no visible joining seam (Figure 3-7a). Similarly, at the SZ/TMAZ interface (Figure 3-7d), thorough mixing can be identified from the consistent structure of fine equiaxed grains across the interface (indicated by the white dashed line). At 1.5 s welding time, the transition in grain structure at the lap interface is more abrupt (Figure 3-7b) and a narrower TMAZ is evident, indicating less plasticisation and mixing in these regions. This trend continues at 0.75 s welding time (Figure 3-7c), where the lap interface is characterised by an immediate transition from fine, equiaxed grains in the top sheet, to coarse grains in the bottom sheet, with only minor evidence of plastic deformation.

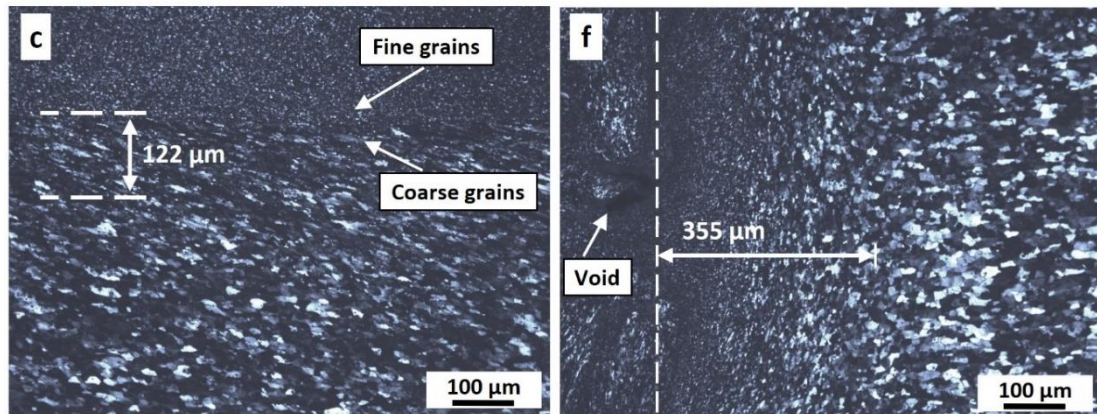
An inhomogeneous grain structure of fine and coarse bands is visible at the SZ/TMAZ interface of 1.5 s and 0.75 s welds, and voids can be identified along the shoulder plunge path (Figure 3-7e,f). Although no existing literature has characterised defect formation at such short welding times, the present results are consistent with trends observed at conventional welding times [3.21,3.36]. As with the previously discussed tunnel defects, it is proposed herein that poor material flowability brought about by insufficient heat generation is the primary driver of mixing defects at short welding times. Attempting to increase heat generation by raising rotational speed did not improve weld quality, and resulted in poorer mixing and greater defect formation, as shown in Figure 3-6 (P08, P12). Gerlich et al. [3.34] discussed this phenomenon in depth in the context of friction stir spot welding; they concluded that increasing rotational speed inhibits mixing by promoting slippage at the tool/workpiece interface. Others have reported similar observations with the RFSSW process [3.21,3.35].



Welding time = 3 s



Welding time = 1.5 s



Welding time = 0.75 s

Figure 3-7 Micrographs of key mixing regions of welds produced with 2000 rpm rotational speed at different welding times a-c) lap interface, d-f) SZ/TMAZ interface

3.3.1.3 Hook defect

Figure 3-8 presents an indicative comparison of welds produced at 2000 rpm to demonstrate the influence of short welding times on hook geometry.

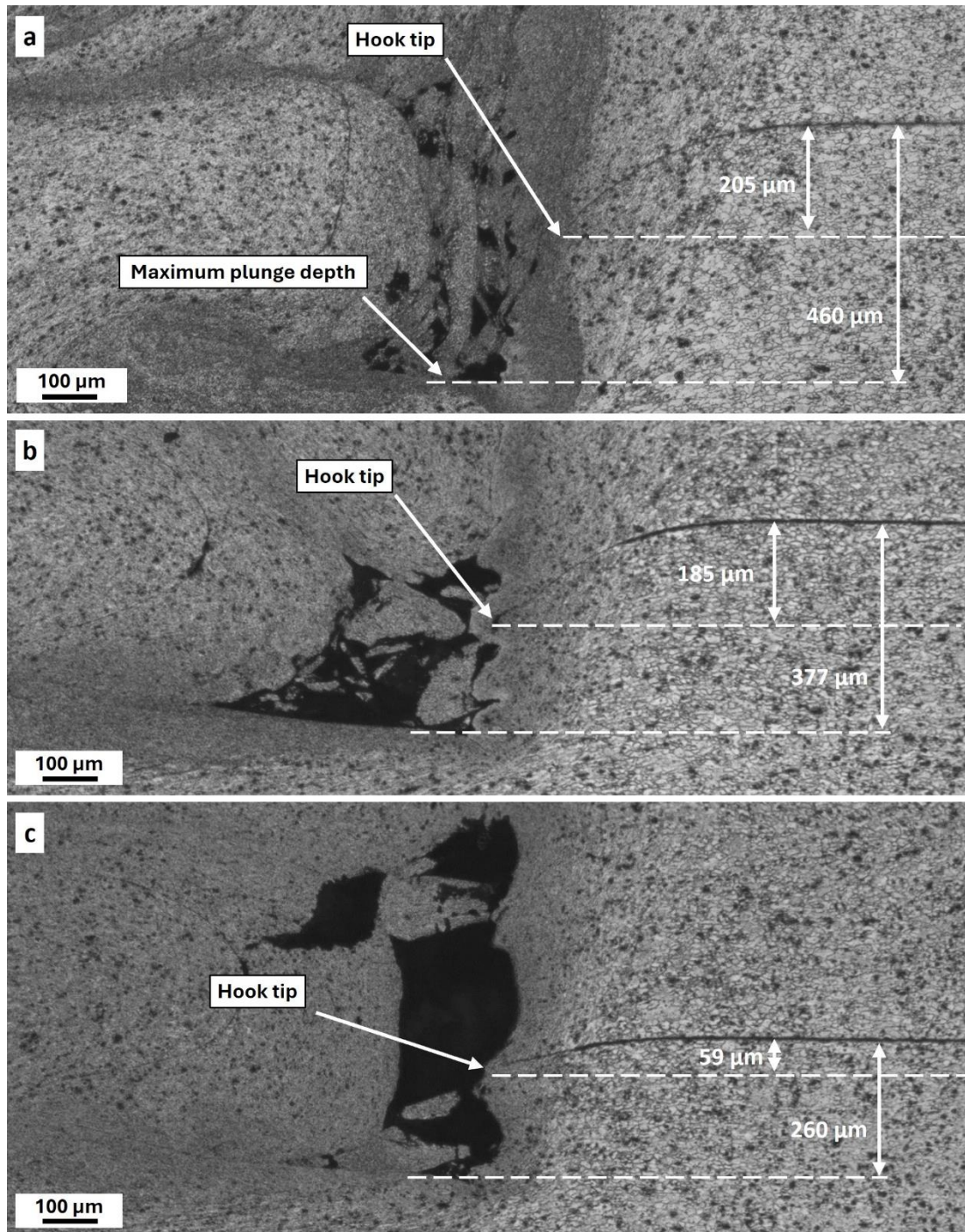


Figure 3-8 Micrographs of hook regions for welds produced with 2000 rpm rotational speed and different welding times, a) 3 s, b) 1.5 s, c) 0.75 s

All welds displayed downward sloping hooks; however, the slopes became flatter as the welding time was reduced. Li et al. [3.12] reported similar findings and correlated hook flattening with a reduction in plunge depth. While a nominal plunge depth of 2.25 mm was specified for all welds in the current work ($\approx 450 \mu\text{m}$ penetration into the bottom sheet), Figure 3-8a-c show that this was actually achieved only for 3 s

welds (Figure 3-8a), and plunge depth decreased as welding time was reduced. In the case of 1.5 s (Figure 3-8b) and 0.75 s (Figure 3-8c) welds, penetration into the bottom sheet only reached 377 μm and 260 μm , respectively. This discrepancy is attributed to elasticity in components of the welding apparatus (backing anvil, frame, etc.), coupled with the increased tool forces exerted when welding time is reduced [3.37]. Due to resource limitations, it was not possible to modify the apparatus to increase its rigidity, and therefore the disparity in plunge depth could not be addressed within the work of this chapter. However, this issue is mitigated in chapter 6 by the adoption of an alternate, more rigid, welding machine.

3.3.2 Mechanical testing

As shown in Figure 3-9 and Table 3-4, the ULSF achieved with all parameter sets exceeded the minimum value set by resistance spot welding standards [3.31]. This standard defines the minimum lap shear load a spot must carry before failure based on the sheet thickness and ultimate tensile strength (UTS) of the base material; in this case, a sheet thickness of 1.8 mm and base material UTS of >386 MPa mandates a minimum load capacity of 4.605 kN per spot.

For welds produced with a welding time of 3 s, the ULSF also exceeded the minimum requirements of 7.27 kN for comparable rivet joints of equivalent sheet thickness AA2024-T3 [3.32]. The highest ULSF of 9.21 kN was achieved for parameter set P03 (2000 rpm, 3 s). Consistent with the findings of Shang et al. [3.22], these results demonstrate that lap-shear strength is not strongly correlated with void size, as both parameter sets P01 and P02 produced welds with smaller tunnel defects yet reached lower ULSF than P03. ULSF was influenced more by the bonding quality of the SZ/TMAZ interface and the lap-interface, as discussed in section 3.3.1.2. Overall, a positive relationship was found between ULSF and welding time, wherein higher welding times led to higher ULSF at a given rotational speed. An exception to this trend was observed at 2500 rpm, where ULSF was slightly lower at a welding time of 1.5 s than 0.75 s.

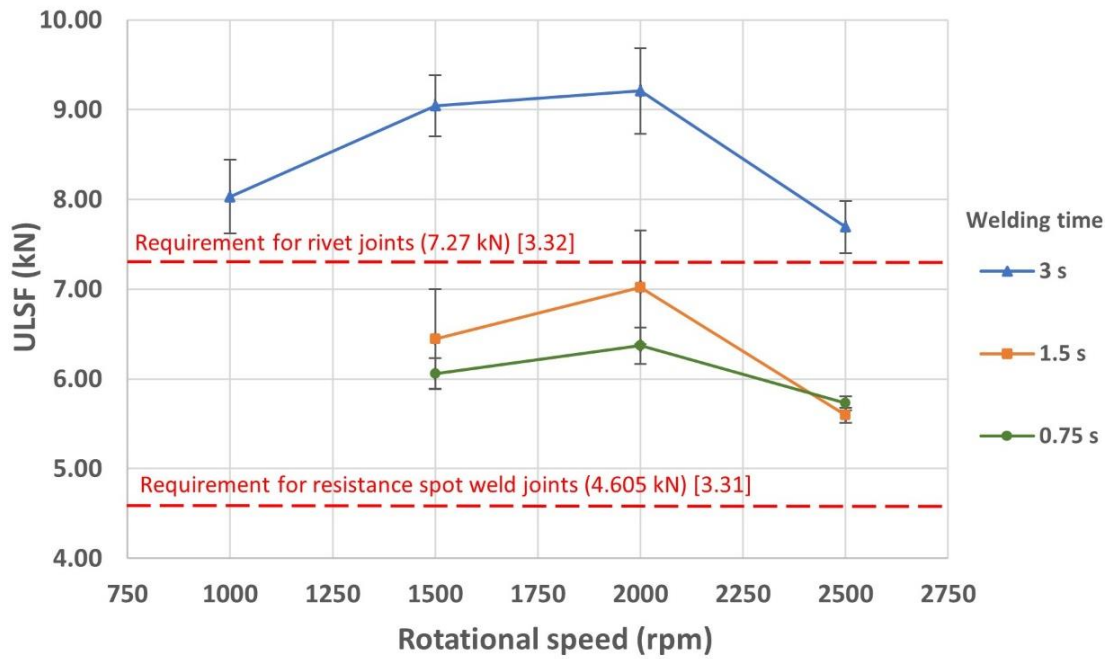


Figure 3-9 ULSF for welds produced at various rotational speeds and welding times, with a constant plunge depth of 2.25 mm

At all welding times, ULSF increased with rotational speed up to 2000 rpm, beyond which a sharp decrease was observed. As discussed in section 3.3.1.2, a higher rotational speed has been shown to hinder the mixing process by promoting slippage at the tool interface. Li et al. [3.12] and Kluz et al. [3.38] presented similar findings for AA2024-T4 and AA7075-T6, respectively, and attributed the effect to overheating of the workpiece material. The results presented in Figure 3-9 are consistent with the findings of previous studies [3.11,3.20,3.23] that also achieved maximum ULSF at a similar rotational speed of 1900 rpm.

Despite the very short welding times used in this study, ULSF values are comparable with those reported in the literature for similar 2xxx series aluminium alloys. Figure 3-10 compares the results of the 2000 rpm joints from the current work with existing literature in terms of welding time vs ULSF. This demonstrates that welding time has been reduced significantly, even when accounting for variations in sheet thickness across the literature, with only modest reductions in weld strength.

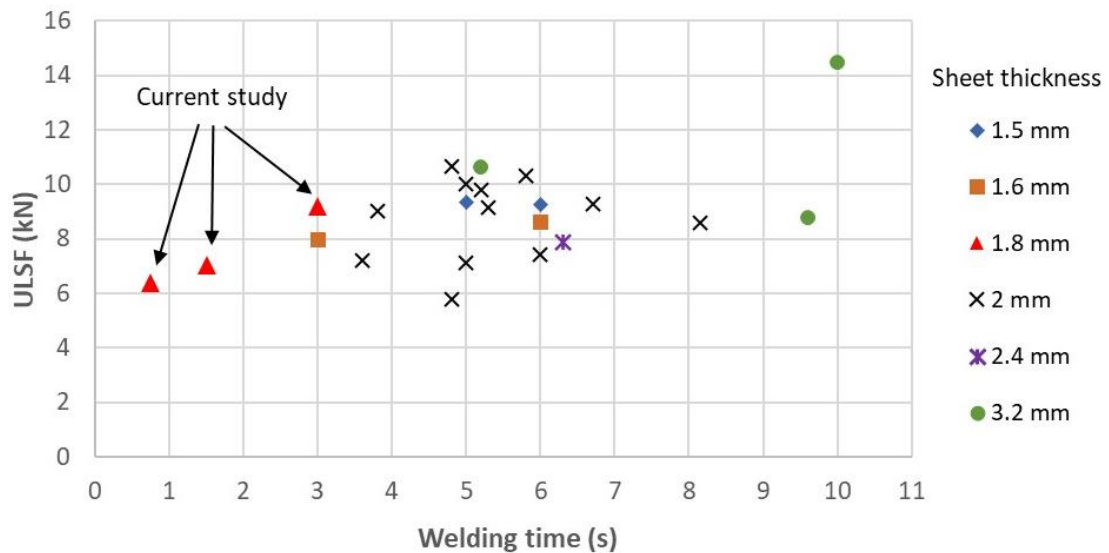


Figure 3-10 Summary of published research for RFSSW joints of 2xxx aluminium, comparing lap shear strength vs welding time with different top sheet thicknesses: 1.5 mm [3.12,3.13], 1.6 mm [3.25,3.40], 1.8 mm (current study), 2 mm [3.11,3.14-3.20,3.22,3.39,3.41,3.42,3.46], 2.4 mm [3.45], 3.2 mm [3.43,3.44,3.47]

3.3.2.1 Failure modes

The complex loading condition that arises during lap shear testing results in regions of tension and compression throughout the weld (indicated in Figure 3-11a) and leads to three distinct failure modes: plug pull-out (PPO), through the lap interface (TLI), and 45° through the weld shear (TWS). Each failure mode begins in a similar manner, with a crack initiating at the hook tip in the tensile region of the top sheet.

Figure 3-11b shows a PPO failure, with point 1 indicating the site of crack initiation. The crack then propagates upwards through the SZ/TMAZ region towards 2, and around the circumference of the SZ to 3. As the crack propagates around the weld periphery, a moment acts about point 1, allowing a second crack to form at the hook tip on the opposing face (point 4). At this stage, the three failure modes diverge depending on the weakest propagation path for the second crack, either through the bottom sheet (PPO), through the lap interface (TLI), or through the SZ (TWS). Similar failure modes have been reported extensively in previous studies [3.5,3.11–3.14,3.48,3.49].

In the case of PPO failure (Figure 3-11a-b), the initial crack continues around the weld periphery in the top sheet to point 3. Simultaneously, the secondary crack advances

through the tunnel defect to point 5, downwards towards the bottom weld face (point 6), and around the weld circumference to point 7. In this study, the presence of large tunnel defects promoted PPO failure in most cases by offering a weak crack propagation path towards the bottom face. Figure 3-12a shows the fracture surface of the weld nugget detached from the upper sheet. Ductile dimple rupture, indicative of tensile pull-out [3.12], can be identified around the weld periphery in both the bottom (Figure 3-12b) and top (Figure 3-12c) sheets.

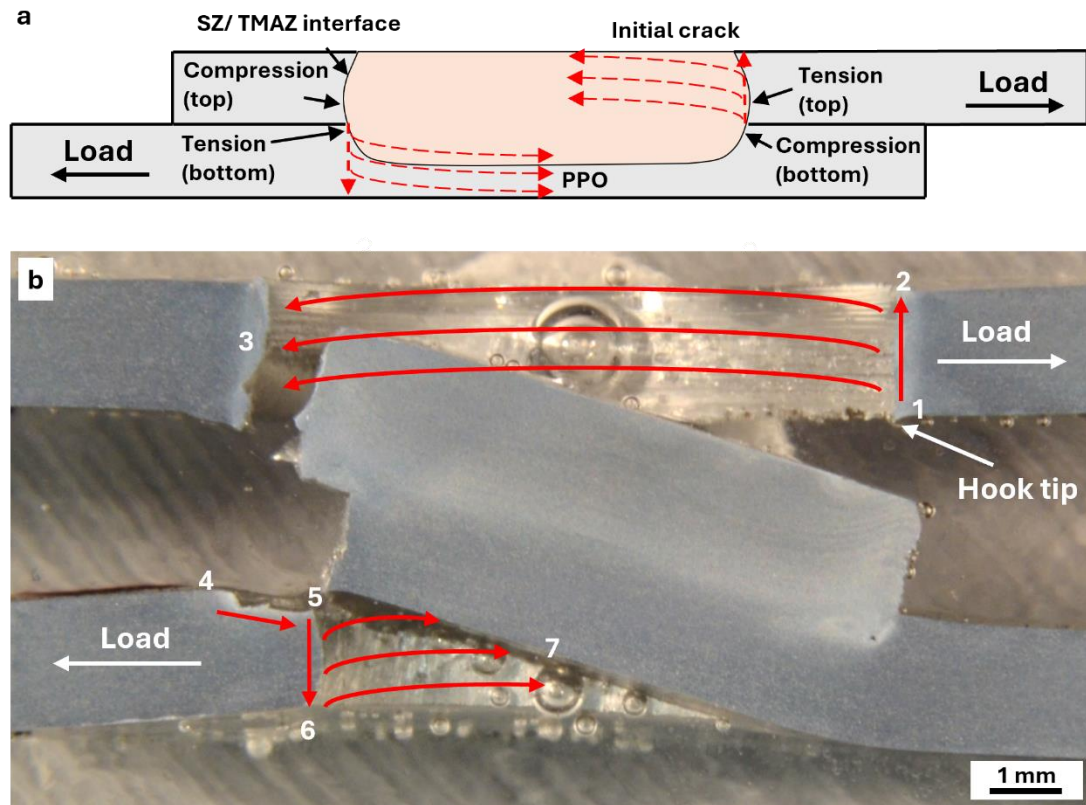


Figure 3-11 Plug pull-out (PPO) failure mode, a) schematic of crack path, b) cross-section of PPO failure from parameter set 11

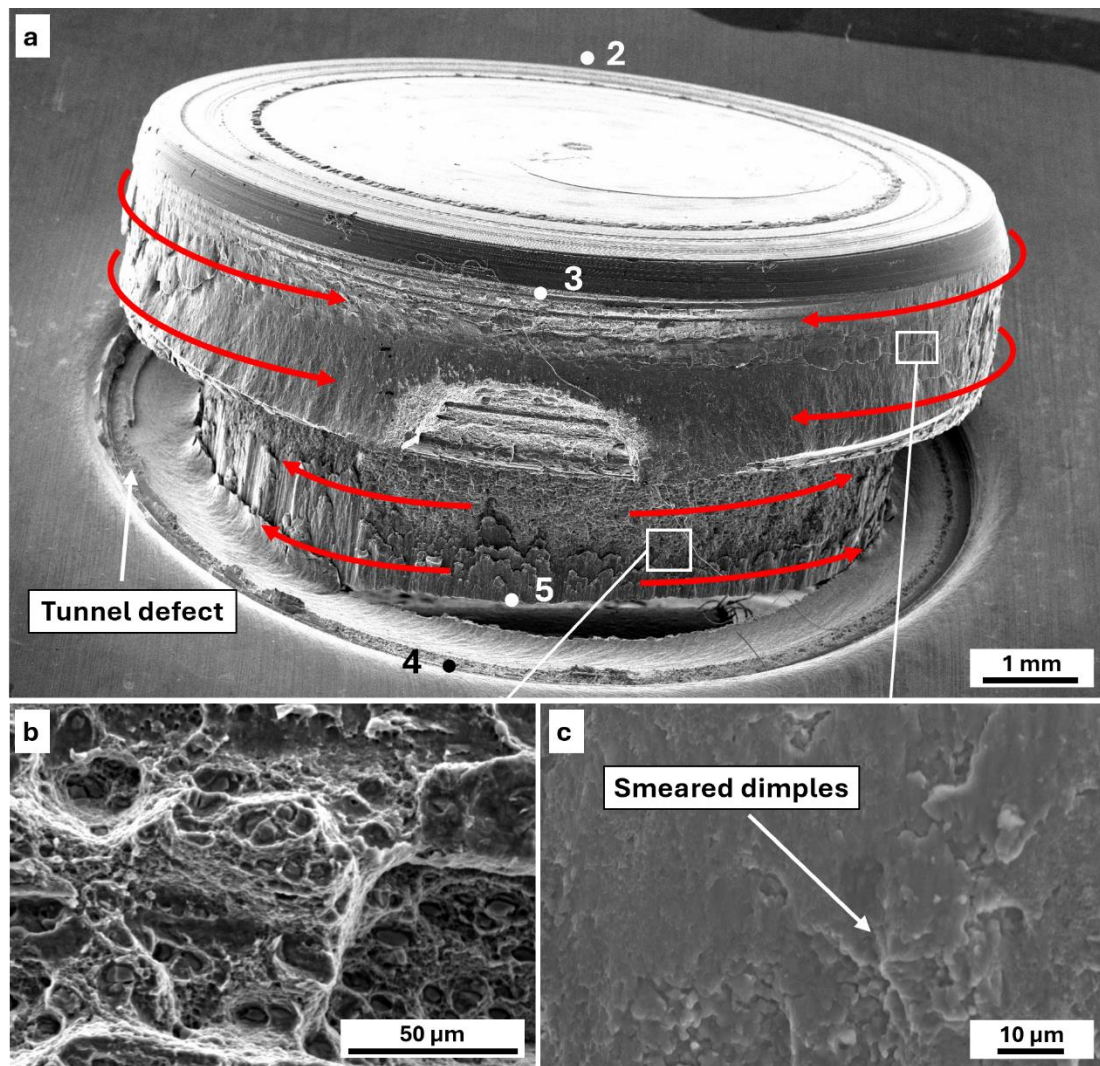


Figure 3-12 Fracture surface of PPO failure mode a) overview of the weld nugget detached from the top sheet and partially attached to the bottom sheet, b) dimple rupture in the top sheet, c) smeared dimples in the bottom sheet

TLI failure occurs when a poorly bonded lap interface presents a weaker crack propagation path than through the bottom sheet (Figure 3-13a). From Figure 3-13b, it can be seen that the primary crack advances only partially around the weld periphery, while the secondary crack path can be traced from initiation at point 4, through the large tunnel defect (point 5-6-7) and directly through the lap interface to point 8. This is the result of partial bonding at the lap interface [3.5] (discussed in 3.3.1.2), as evidenced in Figure 3-14a-b, where small islands of ductile dimple rupture can be identified within an otherwise unbonded region.

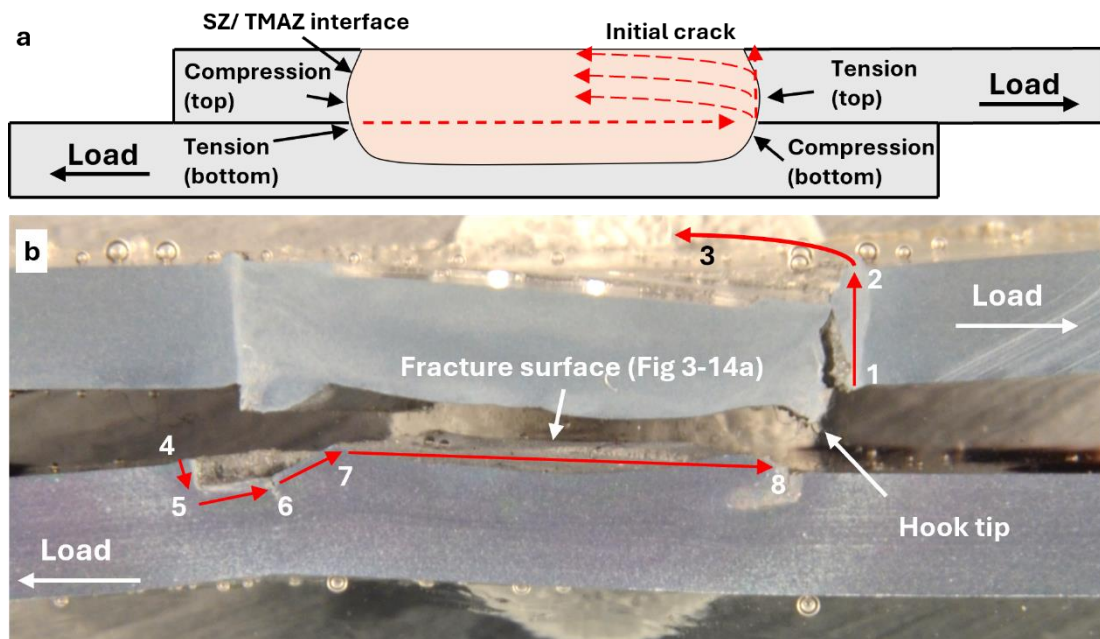


Figure 3-13 Through lap interface (TLI) failure mode, a) schematic of crack path, b) cross-section of TLI failure from parameter set 10

TWS failure (Figure 3-15) occurs when the SZ presents a weaker crack path than the bottom sheet or the lap interface. As the initial crack advances around the weld periphery, the area carrying the test load reduces and the stress at the compressive top face increases. Where no weaker path through the bottom sheet (leading to PPO) or the lap interface (leading to TLI) is available, a critical stress is eventually reached and a abrupt shear fracture initiates at the compressive top face, propagating up through the SZ at approximately 45° [3.50]. The shear failure surface (Figure 3-16a) is distinguished by directional smearing and lack of ductile dimples (Figure 3-16b). Smeared dimple rupture can be seen around the weld periphery (Figure 3-16c), similar to the PPO failure shown in Figure 3-12c. The magnified image presented in Figure 3-15c reveals the progression of an additional crack in the direction of the bottom sheet, indicating that this sample was nearing a PPO failure when the final shear fracture occurred.

A comparison of the lap shear results and failure modes for each parameter set (Table 3-4) shows that there is no strong correlation between failure mode and weld strength, as PPO and TWS failures both occurred in samples with $ULSF > 9$ kN, and < 6.5 kN.

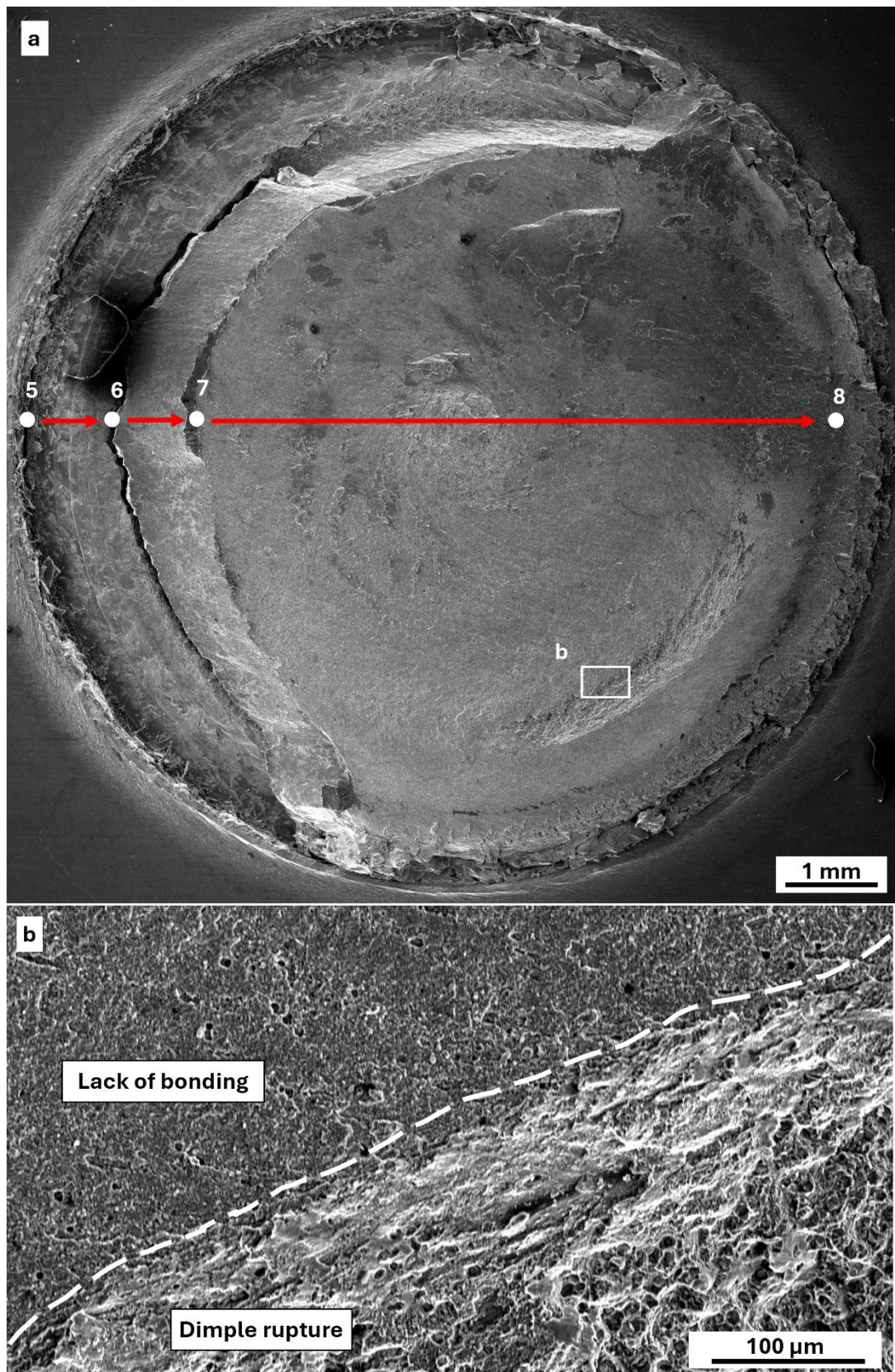


Figure 3-14 Fracture surface of TLI failure mode a) top-down view of weld zone of the bottom sheet showing regions of ductile and shear failure, b) transition between bonded and unbonded failure region

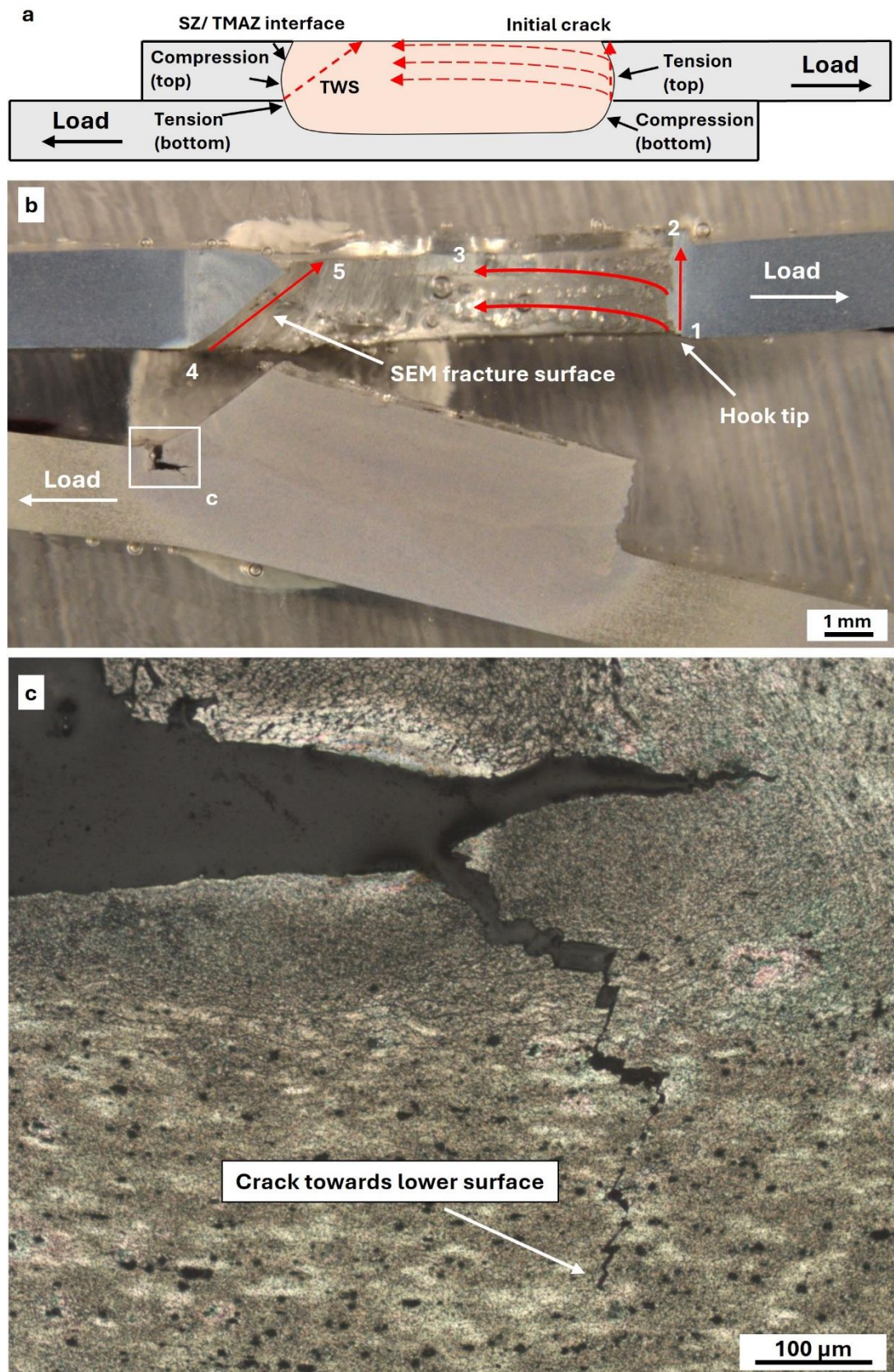


Figure 3-15 Through the weld shear (TWS) failure mode, a) schematic of TWS crack path b) cross-section of TWS failure from parameter set 03, c) high magnification image of secondary crack

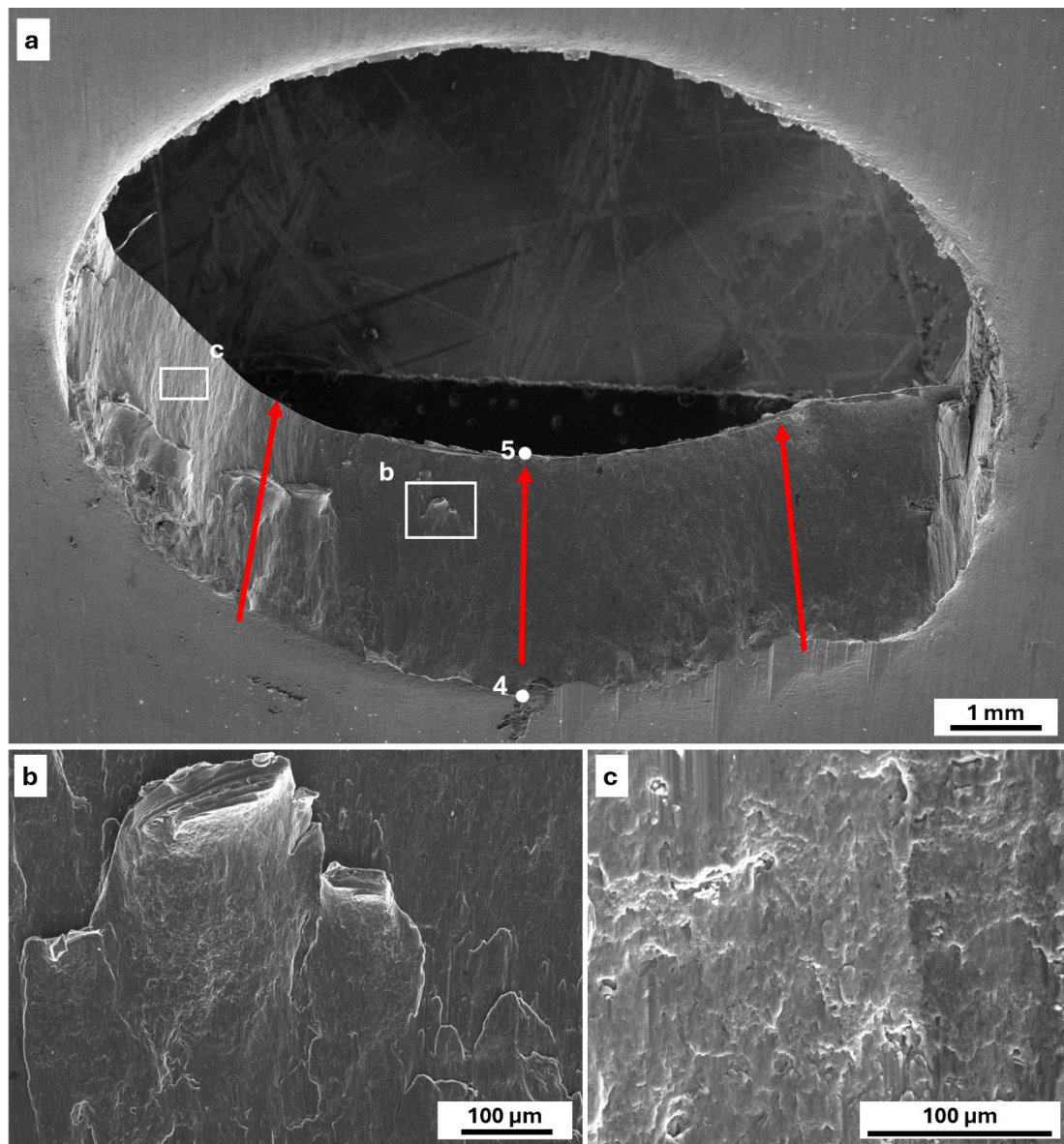


Figure 3-16 Fracture surfaces of TWS failure mode, a) face-on view of the shear fracture surface indicated in Figure 3-15b, b) high magnification image of smearing in the direction of shear, c) smeared dimples around the weld periphery

Table 3-4 Results of lap shear testing and failure modes of each replicant

Parameter ID	Welding time (s)	Rotational speed (rpm)	Lap shear force (kN)	Standard deviation (kN)	Sample failure mode		
					1	2	3
P01	3	1000	8.03	0.41	PPO	PPO	PPO
P02		1500	9.05	0.34	PPO	PPO	PPO
P03		2000	9.21	0.48	PPO	TWS	PPO
P04		2500	7.69	0.29	PPO	TLI	PPO
P05	1.5	1000	N/A	N/A	N/A	N/A	N/A
P06		1500	6.45	0.55	PPO	PPO	PPO
P07		2000	7.02	0.63	PPO	PPO	PPO
P08		2500	5.60	0.08	PPO	PPO	PPO
P09	0.75	1000	N/A	N/A	N/A	N/A	N/A
P10		1500	6.06	0.17	PPO	PPO	PPO
P11		2000	6.37	0.20	TWS	PPO	PPO
P12		2500	5.73	0.08	PPO	PPO	PPO

3.3.2.2 Microhardness mapping

Microhardness maps for welds produced at 2000 rpm and varying welding times are shown in Figure 3-17. In comparison to the bulk material hardness (measured to be 138 HV), distinct regions of hardening and softening can be identified. Hardening within the SZ of approximately 15 HV is clear at all welding times (Figure 3-17a-c), which can be attributed to the intense plastic deformation experienced within the SZ, and resultant fine grain structure and high dislocation density [3.51].

Figure 3-17a shows that hardness begins to decrease abruptly at the SZ/TMAZ interface, and into the TMAZ where it is comparable to the bulk material. In the case of the 3 s welds (Figure 3-17a), a HAZ can be seen surrounding the SZ where hardness drops to a minimum of around 125 HV. Prior research [3.21,3.51] has addressed this phenomenon in more depth and attributed the softening to slight grain recovery and overaging of the bulk material. In contrast, neither the 1.5 s nor 0.75 s welds present any measurable softening characteristic of a HAZ (Figure 3-17b-c). This can be attributed to very short exposure time of the workpiece to high temperatures, and

therefore less opportunity for heat effects to occur [3.52]. The similar hardness values in the TMAZ and the bulk material, coupled with the lack of an obvious HAZ, makes it challenging to distinguish these different regions of the short duration welds using hardness mapping (Figure 3-17b-c).

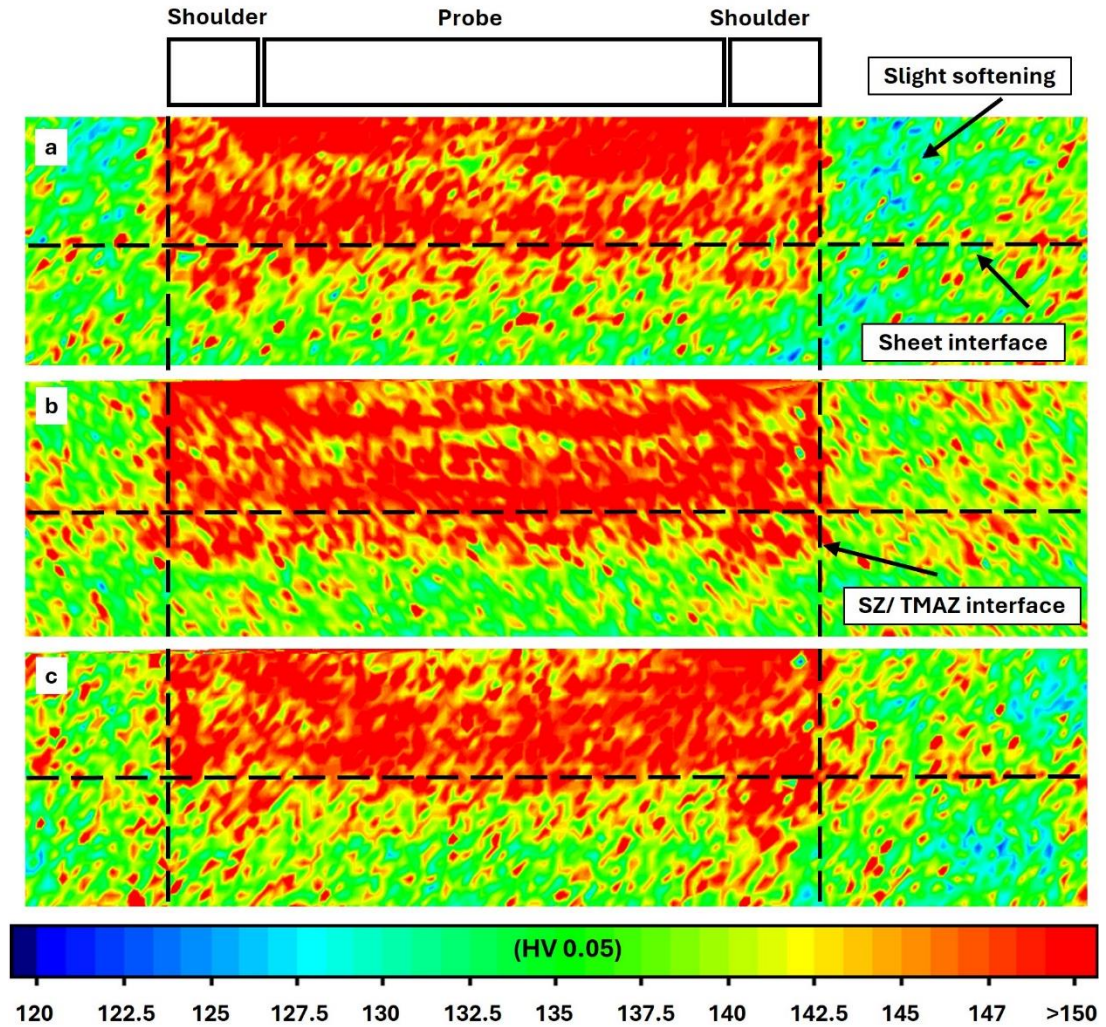


Figure 3-17 Microhardness maps through weld cross-section for welds produced at 2000 rpm rotational speed and welding times of a) 3 s, b) 1.5 s, and c) 0.75 s

3.4 Conclusions

This work has addressed a knowledge gap regarding the influence of short welding time on the defect formation, microstructure, mechanical properties and failure modes of RFSSW AA2024-T3 joints. Welds were produced at welding times ranging from 0.75 s - 3 s, and rotational speeds from 1000 rpm - 2500 rpm. Defects were observed at all process parameter combinations, with increasing severity at all

rotational speeds as welding time decreased. Based on the results presented in this study, the following conclusions can be drawn:

1. Shorter welding times amplified defect formation, particularly tunnel defects, due to lack of heat generation and poor material flow, resulting in lower ULSF.
2. Increasing rotational speed is ineffective at increasing heat generation at short welding times, and actually exacerbates defect formation. Maximum ULSF was achieved with a rotational speed of 2000 rpm at all welding times.
3. The highest ULSF was achieved when welding parameters resulted in a balance of quality mixing at the SZ lap interface and at the SZ/TMAZ interface.
4. Joints with static ULSF of 6.37 kN and 7.02 kN can be achieved at very fast welding times of 0.75 s and 1.5 s, respectively, far faster than previously reported in the literature for 2xxx series aluminium RFSSW joints. These exceeds the static strength requirements of comparable resistance spot welding joints. At a welding time of 3 s, joints with static ULSF of 9.21 kN were achieved, exceeding the requirements of comparable rivet joints.

3.5 References

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Chapter 4. Investigation of novel shoulder geometries for short duration welds

As a relatively immature technology compared to FSW and FSSW, the geometry of RFSSW tools has yet to be widely explored. In this chapter, two novel variations on the shoulder tool component are tested to assess their ability to improve joint quality at short welding times. The influence of each shoulder variant on material flow is investigated using stop-action analysis, while weld microstructural and mechanical properties are assessed as in chapter 3. The same joint material and configuration is used as in the previous chapter, as well as comparable process parameters. Some of the findings reported herein have formed the basis of a published journal article [4.1].

4.1 Introduction

Modifying the geometry of FSW and FSSW tools has been well established in the literature as a method of manipulating material flow, improving mixing, and preventing defect formation during the welding process [4.2–4.11]. In FSW, this ranges from simple variations on the pin shape (cylindrical, conical, pyramidal, prismatic etc.), to TWI's complex multi-helix MX-Triflute™ [4.12] design shown in Figure 4-1.

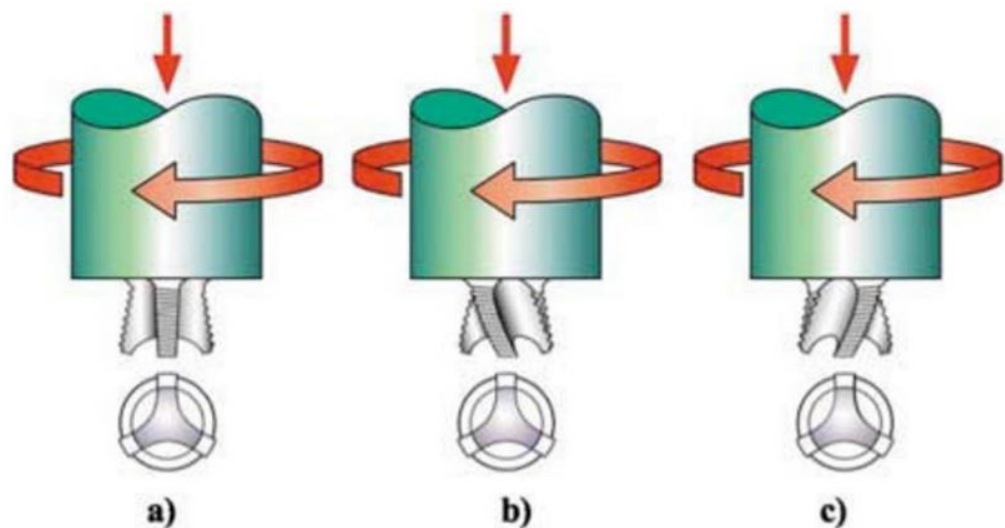


Figure 4-1 Variations of the MX-Triflute™ FSW tool developed by TWI a) neutral flutes, b) left hand flutes, c) right hand flutes [4.2]

FSSW tools developed by Bakavos et al. [4.7] incorporated various spiral features on the contact surfaces, as shown in Figure 4-2. When joining 5xxx series aluminium, the modifications were found to enhance mixing between the top and bottom sheets, improving ULSF and significantly reducing welding time. Chu et al. [4.6] explored spiral grooved tool geometries for joining sheets of AA7075-T651. Material flow was tracked by embedding tungsten particles in the upper sheet. It was found that tools with a greater number of grooves distributed the particles more evenly due to enhanced mixing [4.6].

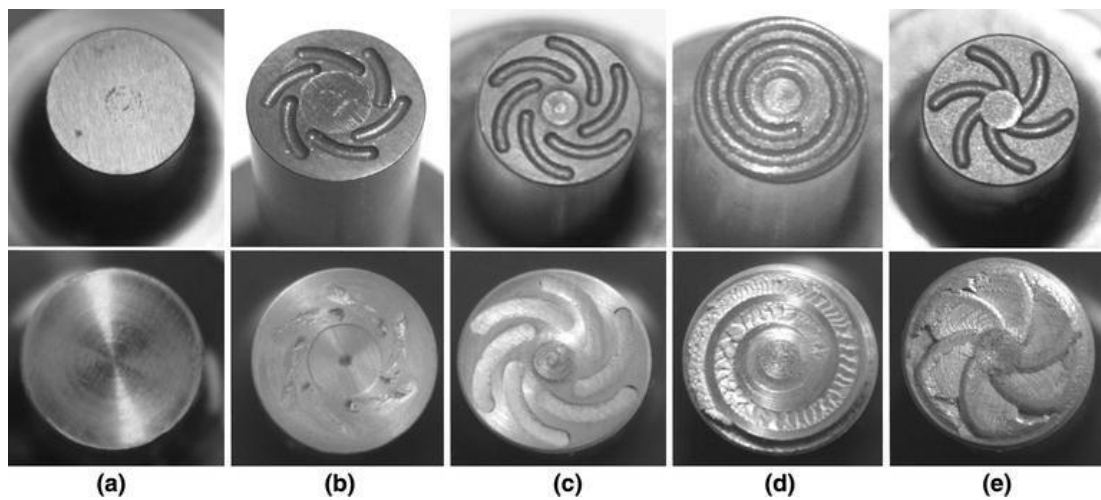


Figure 4-2 Variations of FSSW tools developed by Bakavos et al. [4.7] a) featureless flat, b) short flute, c) long flute, d) fluted scroll, e) protruding long flute (top row = before welding, bottom row = after welding)

Despite the benefits established in FSW and FSSW, only a small number of studies have explored tool geometry as a process variable in the field of RFSSW [4.13–4.19], and the majority of these have come from only two research groups. Łogin et al. [4.13] reported on the influence of various clockwise and counterclockwise grooves on shoulder end-faces (Figure 4-3) to improve the distribution of an Alclad coating when joining AA2024-T3 sheets. The full spiral shoulder offered the best performance, increasing ULSF by 15%, up to 6.18 kN, at a welding time of 4 s. The authors [4.13] attributed this to a more even distribution of the alclad layer. The other designs had a negligible or negative influence compared with the conventional shoulder. A later study from the same group [4.19] iterated on the designs shown in Figure 4-3 with more elaborate spiral patterns and was able to slightly increase ULSF to 6.52 kN.

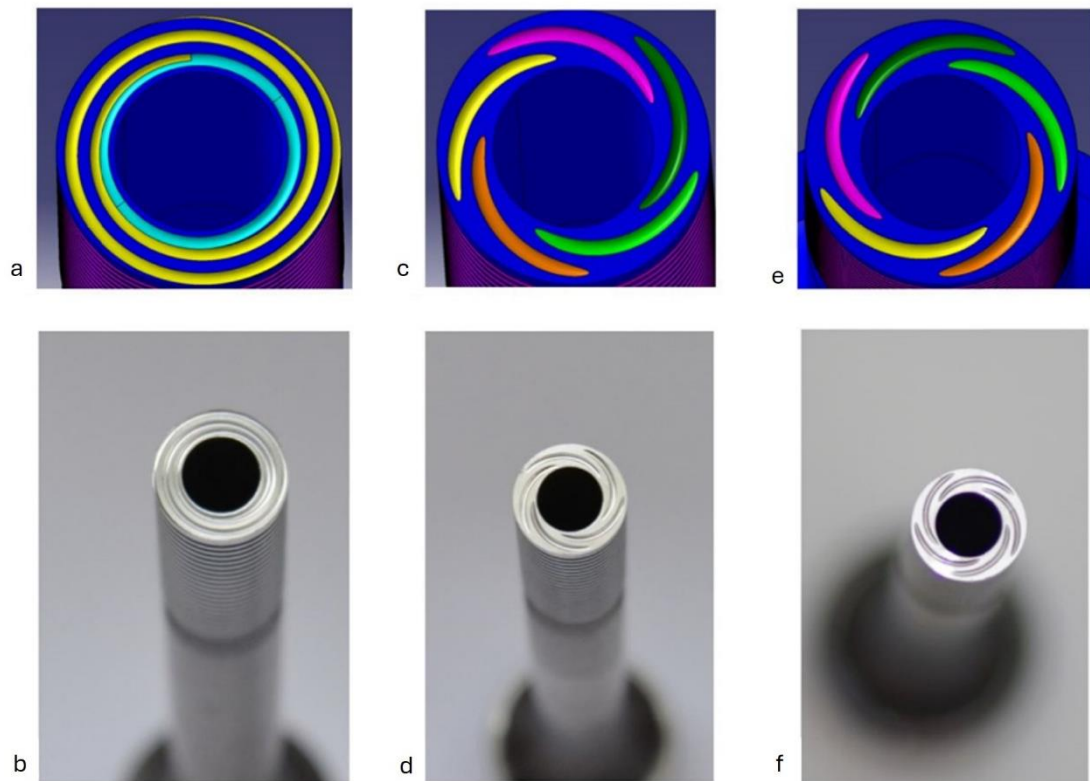


Figure 4-3 Modified RFSSW shoulders, adapted from Łogin et al. [4.13], a-b) full spiral, c-d) inward grooves, e-f) outward grooves

Shen et al. [4.14–4.16] explored the V-notched shoulder design shown in Figure 4-4 in a number of studies focused on joining dissimilar Al and Mg alloys.

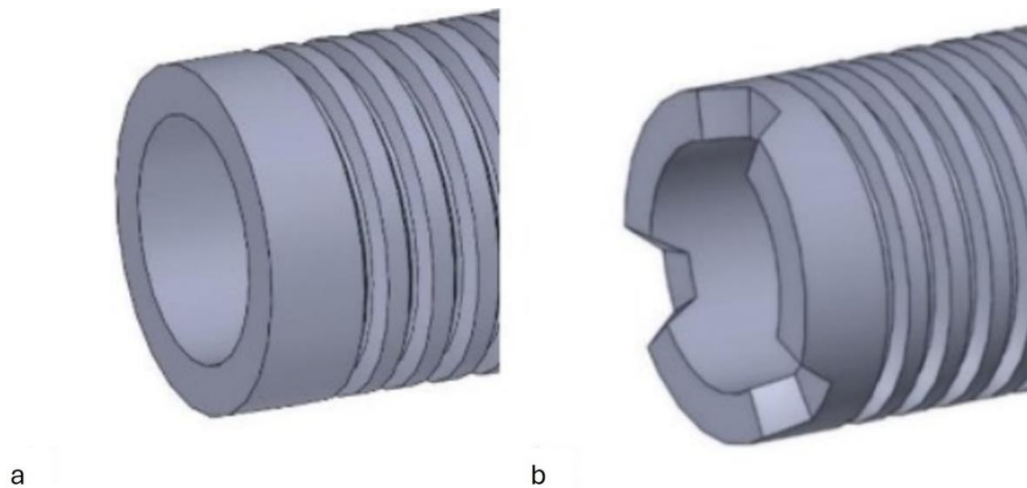


Figure 4-4 Modified RFSSW shoulder design by Shen et al. [14] a) conventional shoulder, b) v-notched shoulder

The authors [4.14] demonstrated that the notched shoulder reduced defects and increased mixing in the weld centre. ULSF increased using the notched shoulder and

was found to be less sensitive to changes in rotational speed. A later study [4.15] of the same shoulder design used stop-action analysis to establish that the notches promote material flow from the bottom sheet towards the top of the weld. Mechanical interlocking occurred and enhanced ULSF, although only long welding times of 5 s were considered.

Li et al. [4.17] investigated three shoulder variants to join 2A12-T42 aluminium sheets (Figure 4-5). Both threaded¹ and notched shoulders increased material mixing, with the notched variant providing a maximum ULSF of ≈ 10 kN, however only long welds of 12 s were studied.

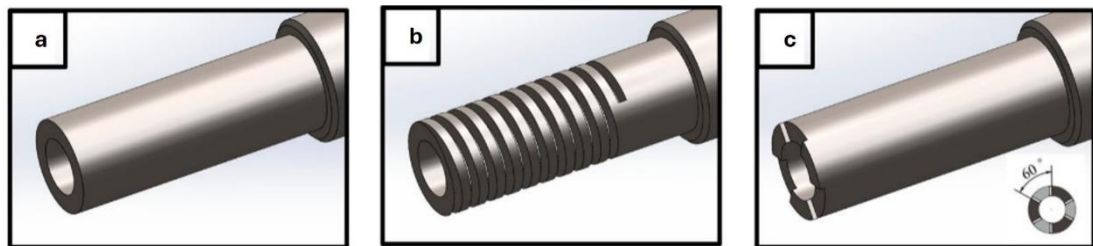


Figure 4-5 Modified RFSSW shoulder designs by Li et al. [4.17] a) bare shoulder, b) threaded shoulder, c) notched shoulder

The current research builds on the existing state-of-the-art by investigating two novel shoulders, the designs of which are informed by the existing literature and the findings presented in chapter 3. Continuing the central theme of increasing the production rate of high strength aluminium RFSSW joints, this chapter again considers welding times far below those previously reported in the literature, ranging from 3 s down to 0.75 s.

4.2 Experimental methods

4.2.1 Materials and welding procedures

The base material used in this investigation is aluminium alloy AA2024-T3, for which the chemical composition and mechanical properties are reported in chapter 3. Similarly, details of the welding machine and welding procedures used can be found in chapter 3.

¹ Although Li et al. [4.17] present external threading as a modification, the current work considers this as a standard feature of the conventional RFSSW shoulder

4.2.2 Development of modified shoulder geometries

Figure 4-6 illustrates three shoulder designs; the conventional design presented previously in chapter 3 (Figure 4-6a), a design introducing slanted grooves to the end-face (Figure 4-6b), and a design incorporating a 45° chamfer on the inner rim (Figure 4-6c). The latter two are novel designs first developed herein.

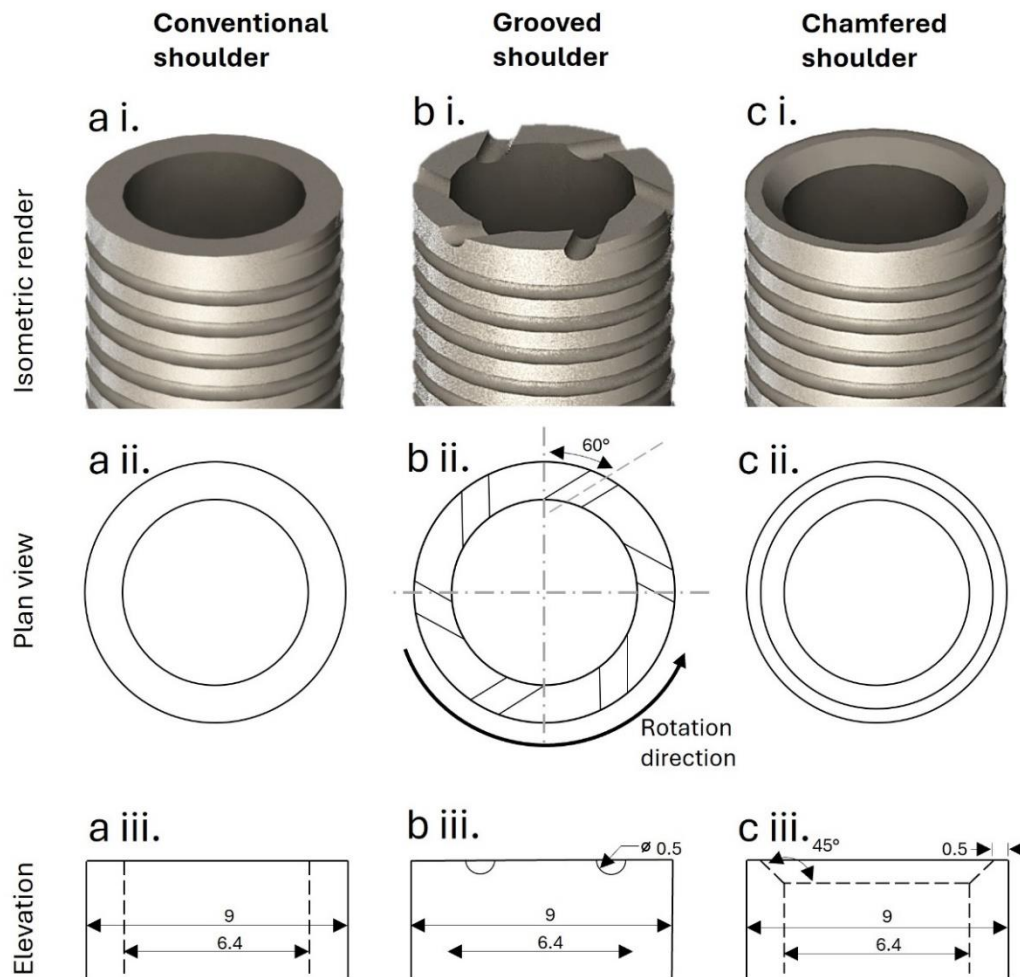


Figure 4-6 Shoulder geometries a) conventional shoulder, b) grooved shoulder, c) chamfered shoulder

From the findings of chapter 3, it is known that most defects form at the periphery of the weld, and that mixing in the SZ/TMAZ interface region is impactful on weld strength [4.20]. The two novel shoulder geometries (Figure 4-6b-c) detailed in this chapter were designed specifically to reduce defects and enhance mixing at the periphery by promoting material flow towards the outside of the weld during the refill stage, as illustrated in Figure 4-7. With a conventional shoulder (Figure 4-7a), the 90°

angle of the inner rim restricts outward material flow, particularly at short welding times where material flowability is poor [4.20]. The design of the grooved shoulder (Figure 4-7b) was projected to address this issue by actively drawing material to the weld periphery through the slanted grooves. A different approach was used for the chamfered design depicted in Figure 4-7c. Where the grooved shoulder is intended to actively drag, or stir material towards the outside of the weld, the introduction of a chamfer to the shoulder's inner rim simply provides a more direct path for material to flow toward the periphery during the refill stage.

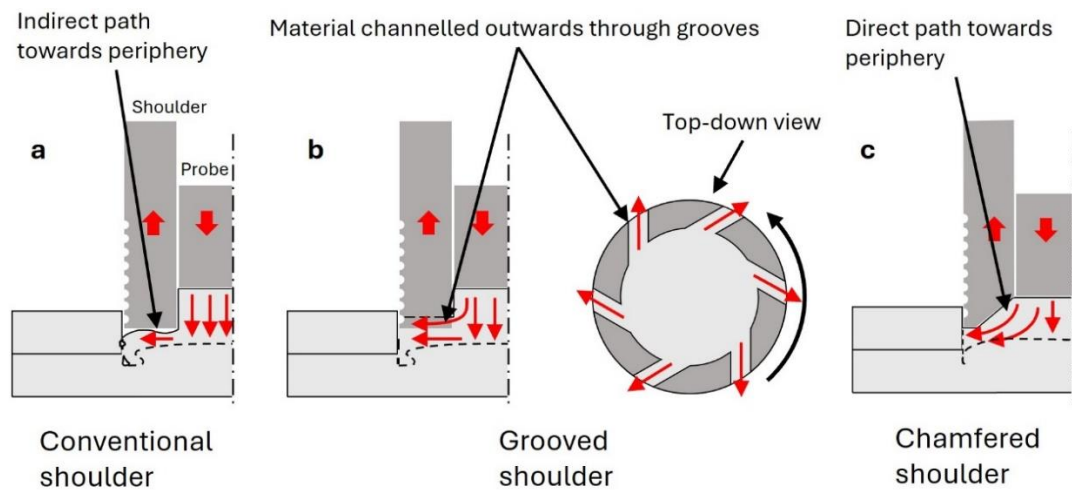


Figure 4-7 Diagram of material flow during refill stage for different shoulder geometries a) conventional shoulder, b) grooved shoulder, c) chamfered shoulder

4.2.3 Stop-action analysis

Stop-action analysis (detailed in section 2.3) was used to visualise the flow of material throughout the welding process. For each shoulder, partial welds were produced at 6 different increments, with the 6th increment being a standard weld with no interruption. This was achieved by programming the welding machine to interrupt the weld cycle at a specified plunge depth, as stated in Table 4-1. A nominal welding time of 4 s (i.e. welding time if the cycle was not interrupted) was found to be the practical minimum that the machine could repeatably halt cycles at specified plunge depths. A rotational speed of 1500 rpm and volume ratio of 0.8 were selected for consistency with the other parameter sets explored in this chapter.

Table 4-1 Plunge depth increments used for stop-action analysis

Stage	Stop position	Nominal plunge depth (mm)
Plunge	1	0.75
	2	1.5
	3	2.25 (max plunge depth)
Refill	4	1.5
	5	0.75
	6	0 (complete weld)

4.2.4 Welding parameters

Joints were produced using the welding parameters detailed in Table 4-2.

Table 4-2 Welding parameters for all tested specimens (volume ratio = 0.8)

Parameter ID	Shoulder	Welding time (s)	Rotational speed (rpm)	Plunge depth (mm)	Volume ratio
01	Conventional	3	1500	2.25	0.8
02		1.5			
03		0.75			
04	Chamfered	3	1500	2.25	0.8
05		1.5			
06		0.75			
07	Grooved	3	1500	2.25	0.8
08		1.5			
09		0.75			

4.2.5 Microstructural and mechanical analysis

Optical microscopy and SEM were used to analyse the microstructure and failure surfaces of the investigated welds. All equipment and procedures, including sample preparation and etching technique were identical to those detailed in chapter 3. Likewise, tensile lap-shear testing and hardness testing were performed for all welds as detailed in chapter 3.

4.3 Results and discussion

4.3.1 Stop-action analysis

The stop-action analysis provides a valuable insight into the flow of material throughout the welding process, which is particularly useful for evaluating the influence of the modified shoulder designs. Figure 4-8 presents a series of cross-section macrographs at different points of the plunge and refill stages, produced using a conventional shoulder. During the first 1.5 mm of the plunge stage (positions 1 & 2), it is evident that the upwards flow of material comes entirely from the top sheet, as no mixing or deformation is visible in the bottom sheet.

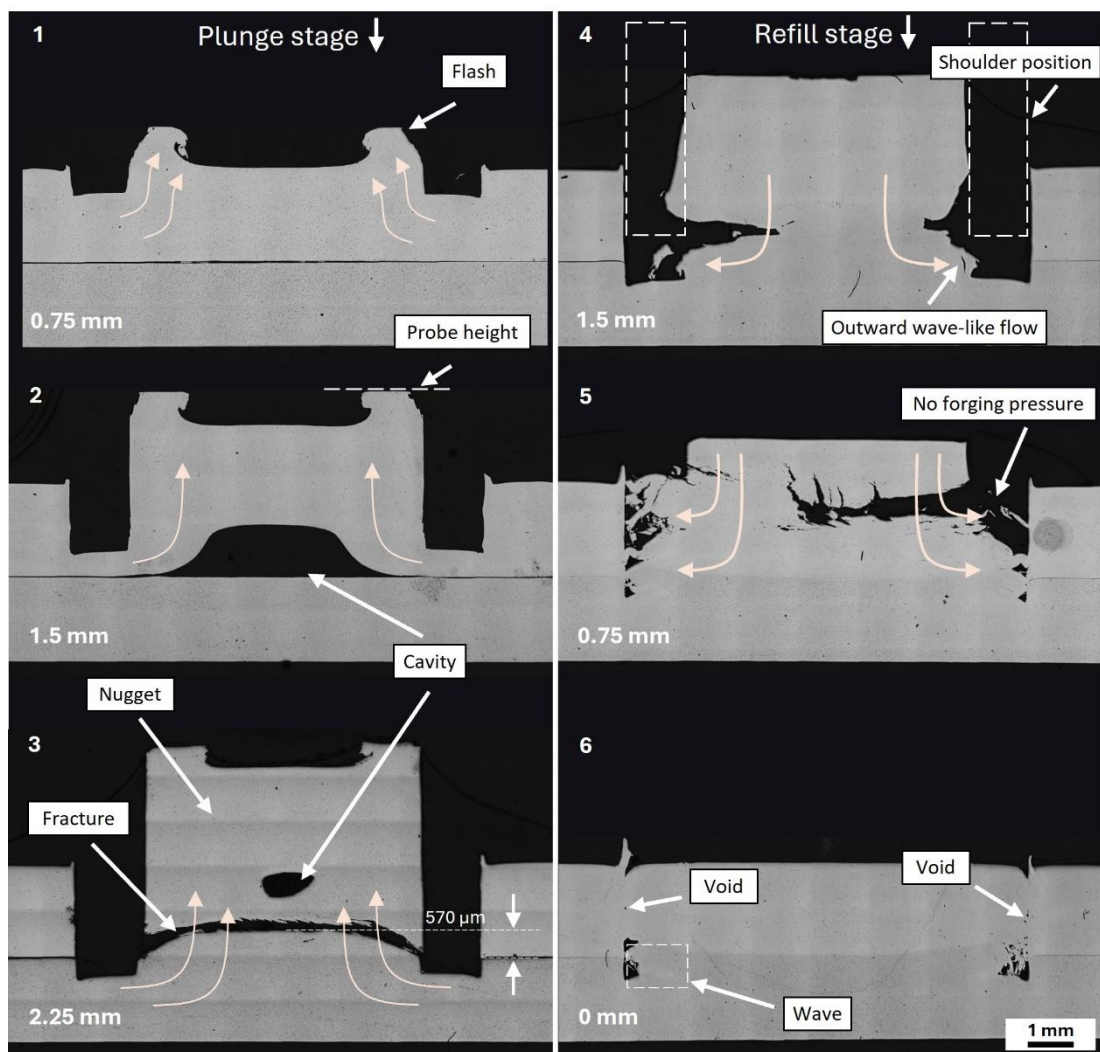


Figure 4-8 Stop-action sequence using a conventional shoulder, showing weld cross-sections at various points throughout the plunging and refilling stages (welding time 4 s, rotational speed 1500 rpm)

Due to the specified volume ratio of 0.8, material movement in the upwards direction is not restricted by the probe, allowing deflection of the top sheet and the formation of a central cavity. The exact position of the probe is evident from the flattened flash, where its growth is restricted by contact with the probe.

At the maximum plunge depth (position 3), material from the lower sheet is drawn upwards, raising the height of the lap-interface. The fracture between the column of displaced material, herein referred to as the nugget, and the lower sheet often occurs while removing tooling from the interrupted weld, rather than during the process itself [4.17,4.21].

As the shoulder retracts to position 4, the downward pressure applied by the probe begins to consolidate the lap-interface, yet only the beginnings of a wave-like flow indicate material movement towards the weld periphery. By position 5, the wave-like flow has reached the weld periphery and folded upon itself to form the tunnel defect. The lack of downwards pressure from the shoulder allows the tunnel to remain open, as well as other voids to form along the SZ/TMAZ interface. Figure 4-9 presents an etched micrograph of the tunnel defect region in the finished weld (position 6), where the wave-like flow can be identified by the distortion in grain direction.

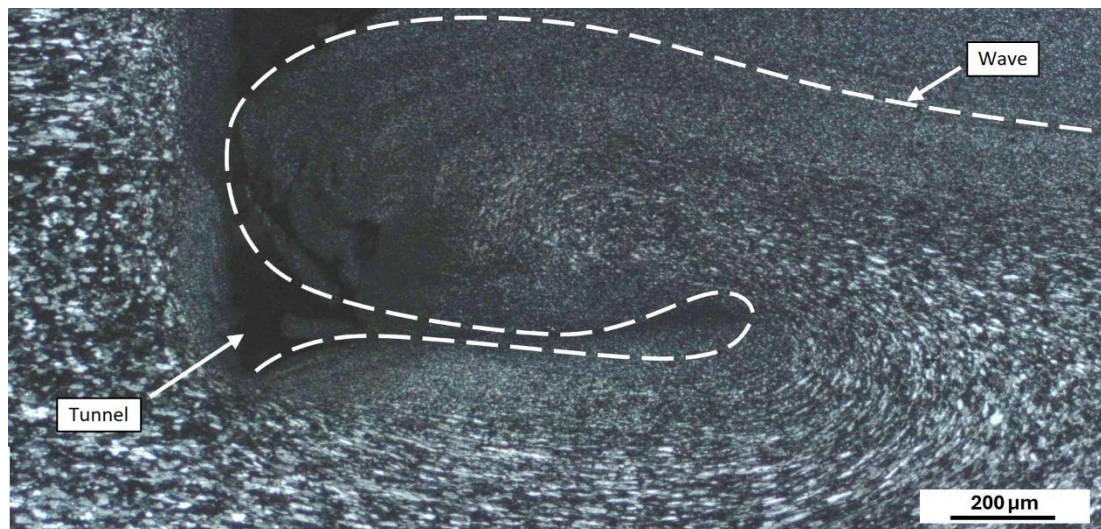


Figure 4-9 Micrograph of the tunnel defect region for a conventional shoulder weld, revealing a wave-like flow of material towards the weld periphery (sample etched with Barker's solution)

As both tool components reach their original level, pressure beneath the shoulder consolidates the SZ/TMAZ interface but is insufficient to close the tunnel defect, leaving the final weld seen at position 6 (Figure 4-8).

Figure 4-10 presents the stop-action sequence produced using the grooved shoulder, where substantial changes in material flow are evident throughout both stages of the process.

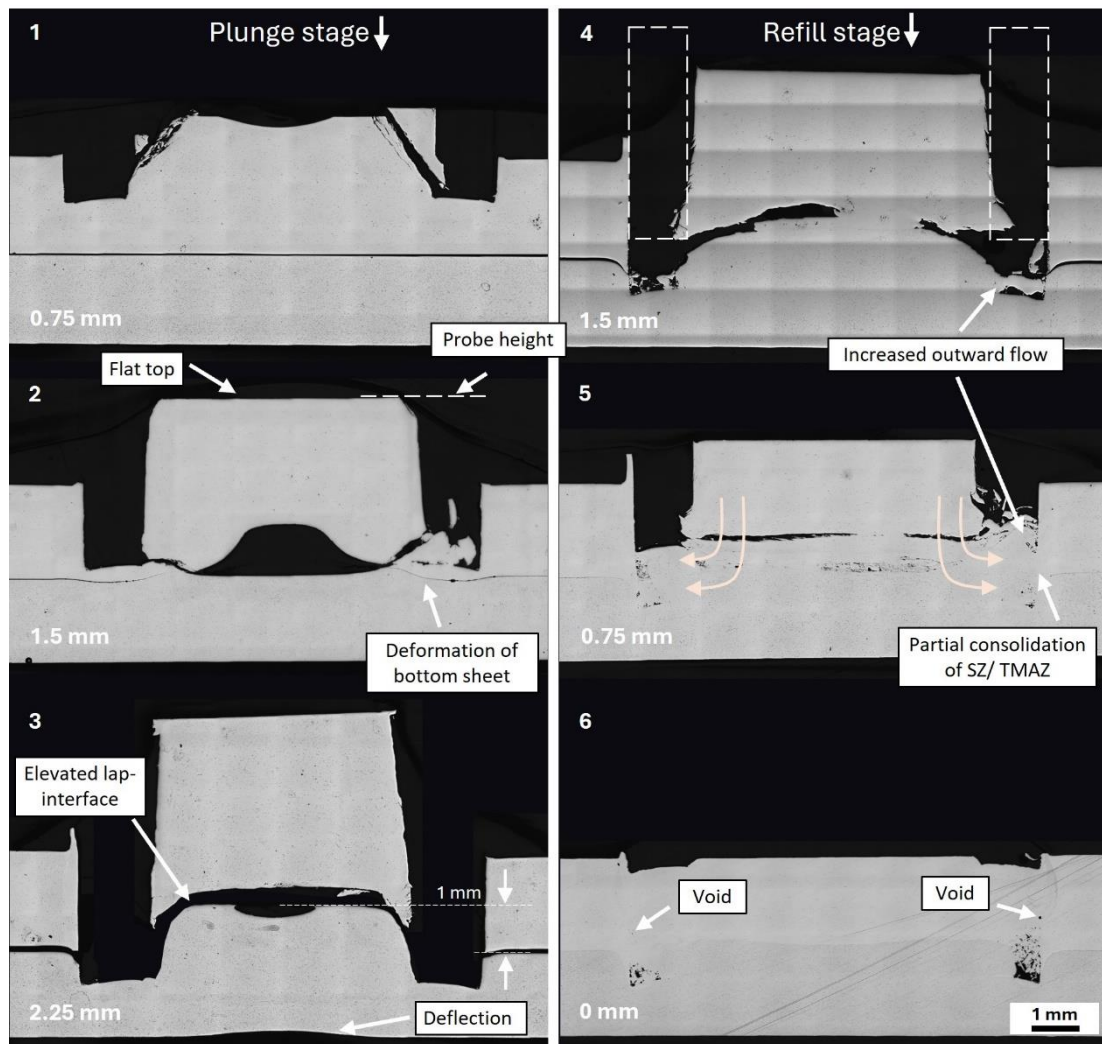


Figure 4-10 Stop-action sequence using a grooved shoulder, showing weld cross-sections at various points throughout the plunging and refilling stages (welding time 4 s, rotational speed 1500 rpm)

Unlike with the conventional shoulder, no flash forms at any point of the plunge stage and the flat top of the nugget indicates it has made contact with the probe. This is attributed to the enhanced stirring effect of the grooved shoulder drawing more material into the weld nugget. The deformation of the bottom sheet at position 2

further indicates greater stirring, as this is not present with the conventional shoulder. Li et al. [4.17] observed a similar behaviour, concluding that the grooves agitate material and provide a flow path towards the weld centre. A substantially higher lap interface at the maximum plunge depth (position 3) further demonstrates this effect.

Increased outwards material flow is observed throughout the refill stage, and earlier consolidation of the SZ/TMAZ can be seen at position 5, compared to the conventional shoulder. However, the condition of the final weld does not appear substantially different, and significant tunnel defects are still present.

The stop-action sequence for the chamfered shoulder is given in Figure 4-11 and reveals several differences in material flow compared to the previous designs.

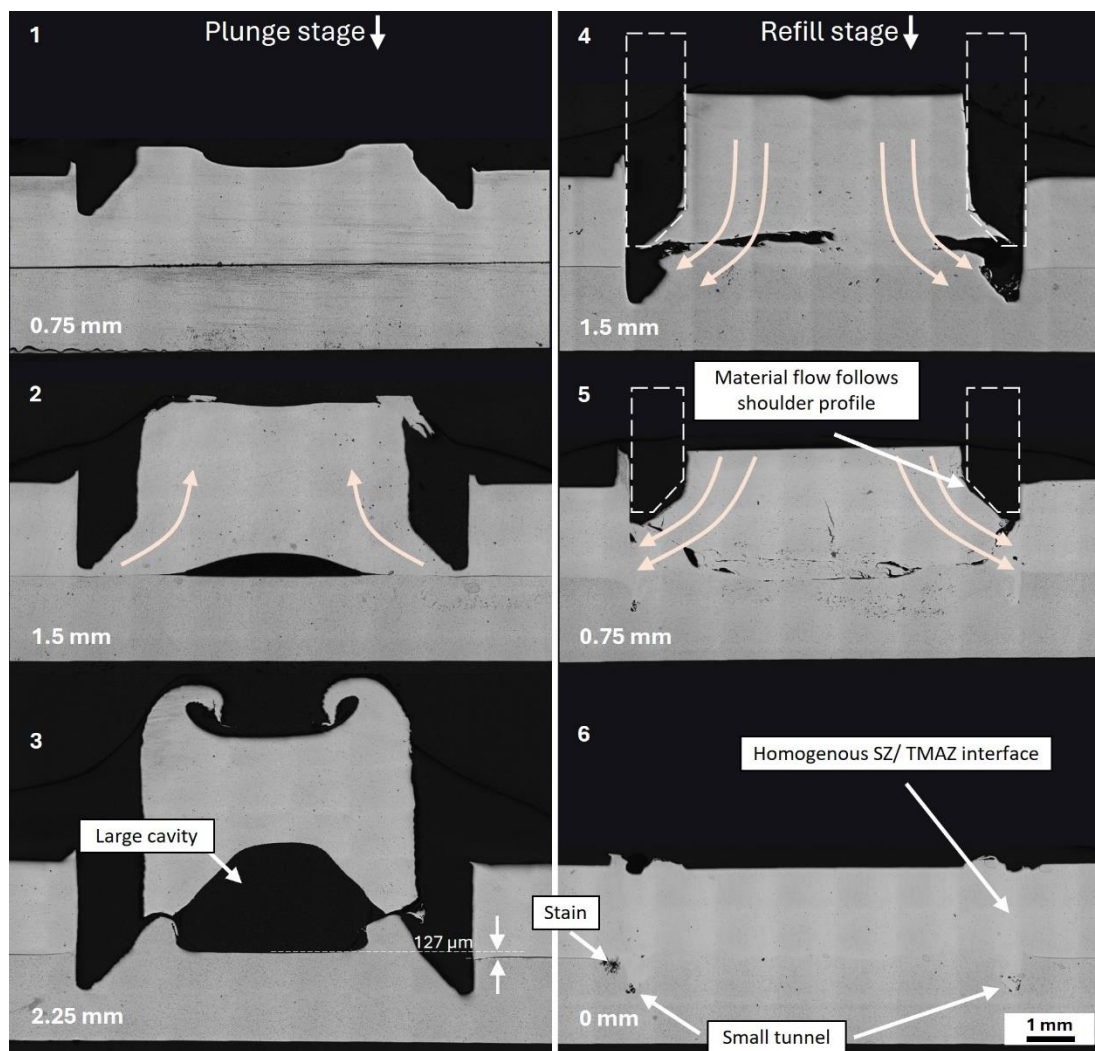


Figure 4-11 Stop-action sequence using a chamfered shoulder: weld cross-sections at various points throughout the plunging and refilling stages (welding time 4 s, rotational speed 1500 rpm)

The introduction of the chamfer naturally requires some of the steel shoulder material to be removed, which in turn results in less displacement of the workpiece throughout the plunge stage. This contributes to the formation of a large central cavity, while the height of the lap-interface remains almost unchanged (position 3).

As the refill stage progresses and the shoulder retracts to 1.5 mm (position 4), no clear improvement in material flow towards the weld periphery is evident. By position 5, however, the flow of material appears to be guided towards the periphery, following the chamfer profile. This leads to a considerable reduction in tunnel size compared with both other shoulder designs.

A marked improvement in the quality of the finished weld is evident from the reduction in tunnel size, and no visible voids along the SZ/TMAZ interface (Figure 4-11, position 6). This is a critical outcome as the positive influence of high-quality bonding at the SZ/TMAZ interface on joint strength has been established in chapter 3 and was the primary objective motivating the design of the modified shoulders.

4.3.2 Defect analysis using novel shoulder geometries

Figure 4-12 presents macrographs of welds produced using each shoulder variant and reveals several significant differences in tunnel defect and void formation, extent of mixing at the lap-interface and SZ/TMAZ interface, and hook geometry.

4.3.2.1 Tunnel defect

Figure 4-12 demonstrates that tunnel defects were present in all welds and were influenced by shoulder geometry and welding time. For the conventional (Figure 4-12a) and grooved (Figure 4-12b) shoulders, small tunnels were found at longer welding times, but were enlarged significantly as welding time was reduced. The wave-like refill behaviour discussed in section 4.3.1 provides an explanation for this phenomenon. As indicated in Figure 4-12a,b, wave-like flows are identifiable in all of the conventional and grooved shoulder welds. As welding time is reduced, so too is the wave's progression towards the weld periphery, resulting in the enlarged tunnels. This is consistent with the findings of Shen et al. [4.22] and Adamus & Adamus [4.23] and can be attributed to poor material flowability due to reduced heat generation.

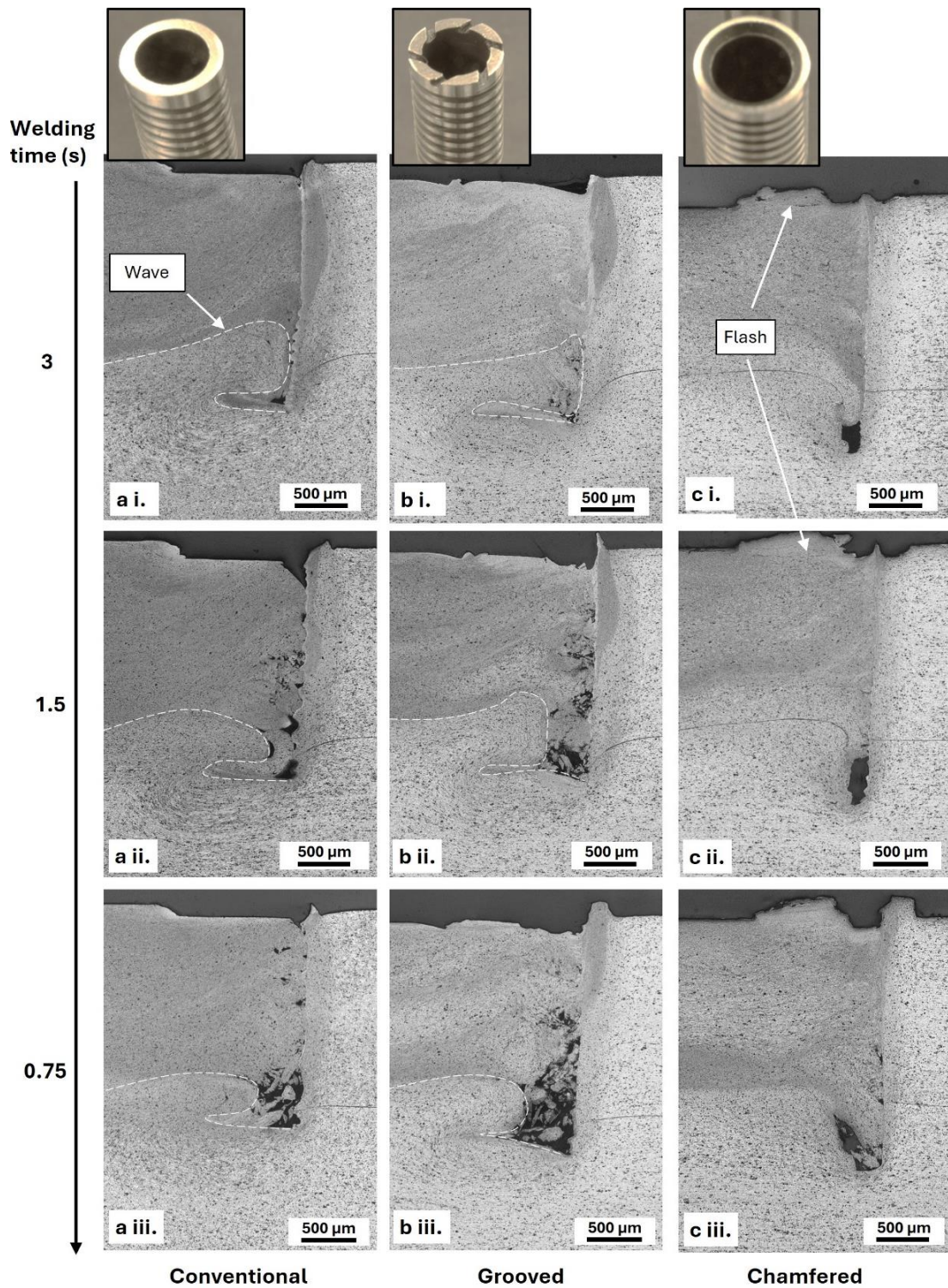


Figure 4-12 Shoulder plunge regions for welds produced using different welding times, a constant rotational speed of 1500 rpm, and various shoulder geometries a) conventional shoulder, b) grooved shoulder, c) chamfered shoulder

Conversely, welds produced with the chamfered shoulder featured moderately sized tunnels that remained comparatively consistent at all welding times (Figure 4-12c). The small volume of shoulder material removed to create the chamfer profile left a corresponding flash on the surface of each weld, as indicated in Figure 4-12c-iii. This represents workpiece material that is not refilled back into the weld, and as such, contributes to the larger tunnel defects seen in the chamfered shoulder welds at 3 s welding time (Figure 4-12c). Hovanski et al. [4.24] implemented a so-called “tamping” step to alleviate similar defects, where the welding tools were programmed to apply additional downward forging pressure at the end of the weld cycle; this will be explored further in chapter 6.

4.3.2.2 *Mixing defects*

In chapter 3, it was established that the quality of mixing at the SZ/TMAZ interface region was strongly influenced by welding time when using a conventional shoulder, with shorter times leading to voids and poor material consolidation (Figure 4-12a). Poor bonding in this region offers a weak path for crack propagation and leads to reduced joint strength [4.25–4.27]. The grooved shoulder performed in a similar manner to the conventional, with no visible voids or bonding line along the SZ/TMAZ interface at a welding time of 3 s (Figure 4-12b-i). At shorter durations, however, numerous large voids are present along the interface, similar to the conventional shoulder. Due to the poor material flowability associated with short welding times, it is likely that the grooves simply became clogged with aluminium, rather than providing additional stirring.

In contrast, the SZ/TMAZ interfaces of welds produced with the chamfered shoulder were not affected by welding time as thorough mixing is evident in all cross-sections (Figure 4-12c). These improvements can be attributed to the chamfer guiding material flow toward the weld periphery during the refill stage, as demonstrated in section 4.3.1.

When the probe begins to refill, it applies pressure to the top of the weld nugget, forcing it back downward into the weld region to forge bonds at the lap and SZ/TMAZ

interfaces [4.28,4.29]. Taking into consideration the distribution of the refill pressure is critical to understanding the influence of the chamfered shoulder; Figure 4-13 illustrates this for each of the shoulder variants.

In the case of the conventional (Figure 4-13a) and grooved shoulders (Figure 4-13b), material must flow around the 90° inner rim in order to progress towards the SZ/TMAZ interface, creating an indirect flow path. In longer duration welds, this does not present a problem as higher temperatures and lower deformation rates mean the material can flow more easily [4.22]. At short welding times, however, where flowability is reduced [4.29], the indirect path around the shoulder rim restricts material movement and causes higher refill pressure on the lap-interface compared with the SZ/TMAZ. This is evidenced in Figure 4-13a and Figure 4-13b by the poor mixing and presence of voids at the SZ/TMAZ interfaces of both conventional and grooved shoulder welds. Conversely, high-quality, homogenous mixing is found at the lap-interfaces. By introducing the chamfer (Figure 4-13c), a more direct flow path towards the outside of the weld allows more refill pressure to reach the SZ/TMAZ interface, leading to improved mixing and elimination of voids in this region (Figure 4-13c-i). However, a consequence of this is a reduction of pressure at the lap-interface and a subsequent lack of mixing between top and bottom sheets, as shown in Figure 4-13c-ii. These results suggest that the chamfer angle could be tailored to balance pressure between SZ/TMAZ and lap-interfaces for optimal bonding in both regions; this is a concept that will be explored further in chapter 6.

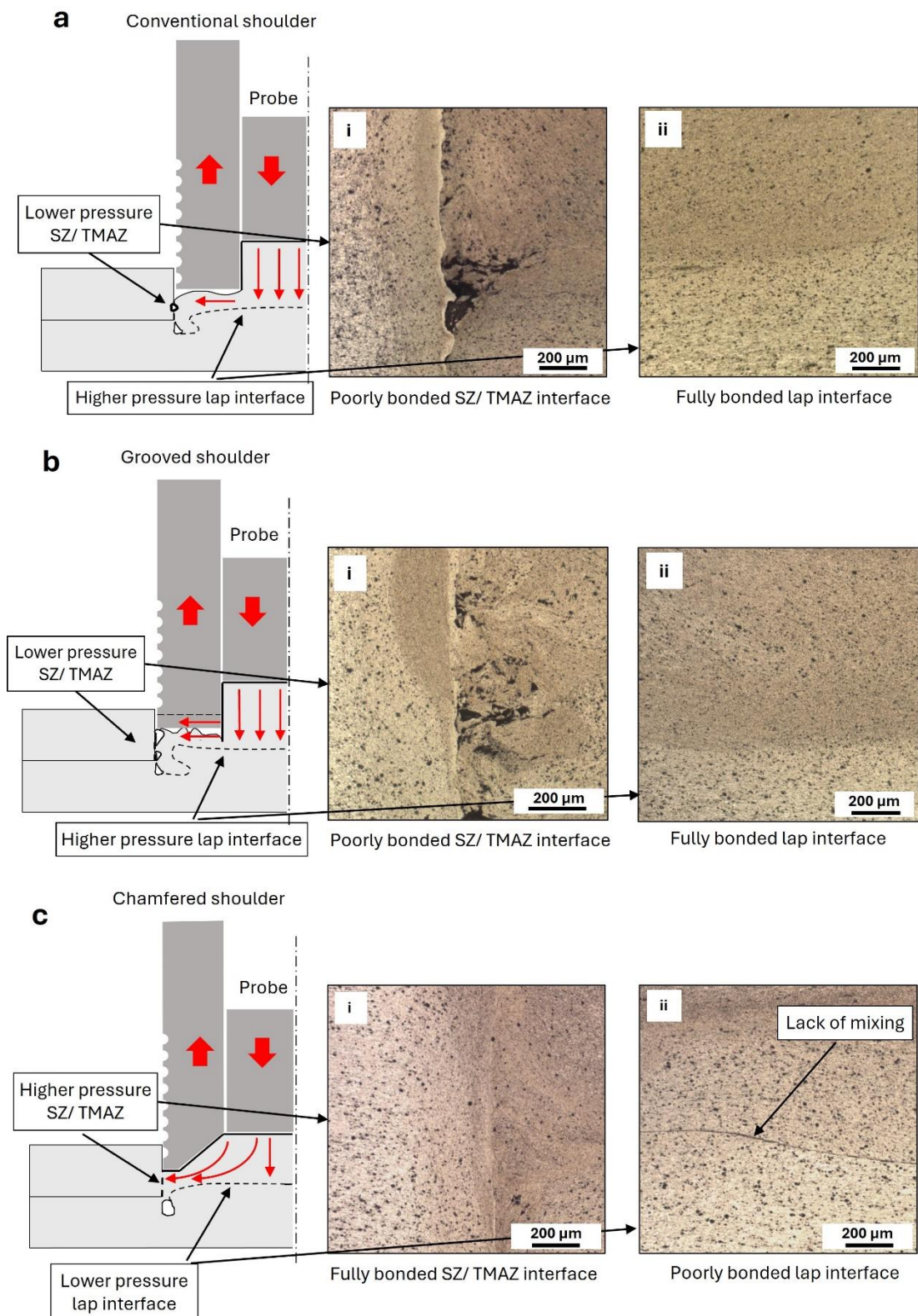


Figure 4-13 Material refill behaviour for 1.5 s duration welds using a) conventional shoulder, b) grooved shoulder, c) chamfered shoulder

4.3.2.3 Hook defect

The hook defect forms at the transition from welded to un-welded material, where the plunging motion of the shoulder plastically deforms the seam of the upper and lower sheet into a downwards sloping hook. The hook acts as a stress concentrator and point of crack initiation under tensile shear loading; larger hook heights have been shown to amplify this effect and reduce ULSF [4.30–4.33]. Figure 4-14a shows that the conventional shoulder produced downwards sloping hooks at all welding times, with hook height decreasing with welding times.

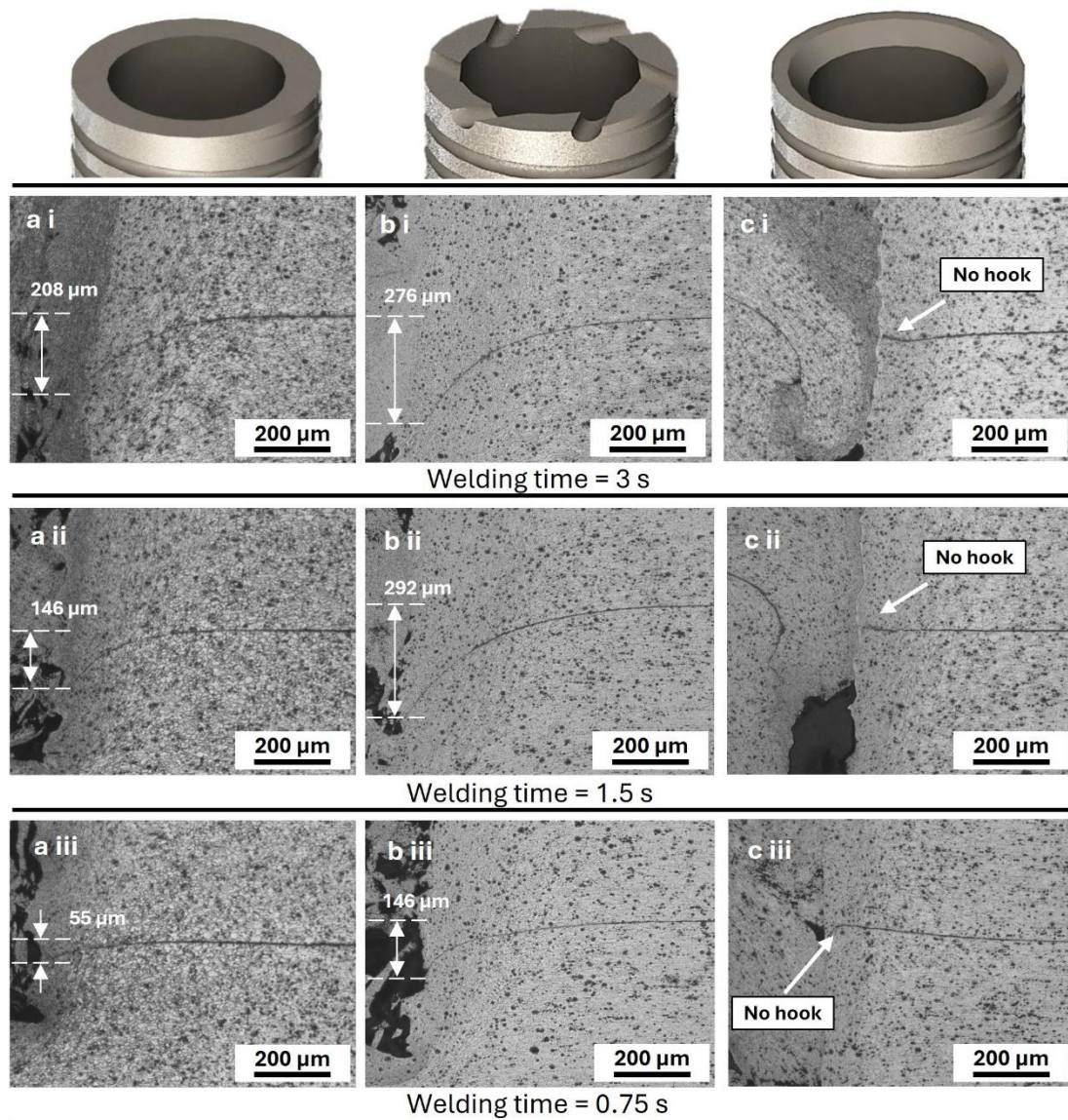


Figure 4-14 Micrographs of welds produced at varying weld times, 1500 rpm rotational speed, and 2.25 mm plunge depth using a conventional shoulder (a), grooved shoulder (b), and chamfered shoulder (c)

Cao et al. [4.30] reported a similar connection between hook height and welding time, concluding that lower heat generation limits stirring during the plunging stage. The grooved shoulder (Figure 4-14b) led to an increased hook height at all welding times, indicating improved stirring as it plunges through the material.

A marked difference for welds produced with the chamfered shoulder (Figure 4-14c) is observed, no hook formed at any welding time and the join between the top and bottom sheets meets the SZ at a normal angle. While no other research has explored a similar shoulder design, this departure from the normal hook geometry can be attributed to the narrower end-face of the chamfered shoulder, allowing it to plunge through the sheets with less plastic deformation.

4.3.3 Microstructural evolution

This section explores the influence of each shoulder variant on the weld microstructure. The following figures present etched micrographs of the SZ and SZ/TMAZ interface regions of welds produced with conventional (Figure 4-16), grooved (Figure 4-17), and chamfered (Figure 4-18) shoulders. Samples were sectioned in the transverse direction (top-down) through the widest point of the TMAZ, approximately 1 mm below the weld surface, as illustrated in Figure 4-15.

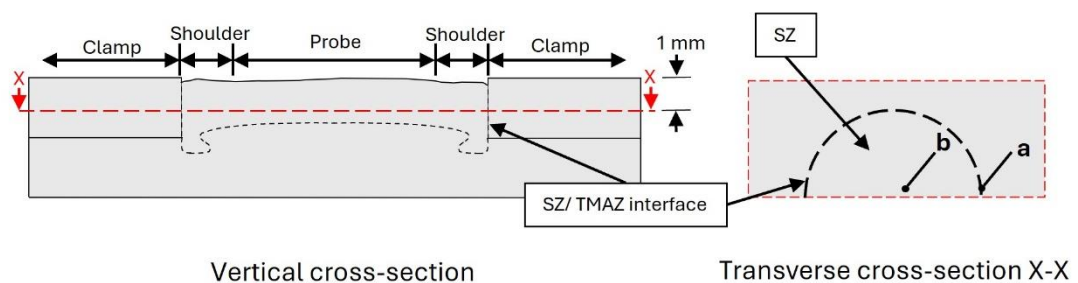


Figure 4-15 Diagram of weld cross-sections showing position of microstructure images

A similar microstructure can be observed within the SZ of welds produced using the conventional (Figure 4-16a) and grooved (Figure 4-17a) shoulders, with fine equiaxed grains characteristic of intense heat and plastic deformation [4.21].

With regard to the weld periphery, the conventional shoulder produced a fine, homogenous microstructure in the outer SZ, with a distinct band of extremely fine grains indicating the transition to the TMAZ (Figure 4-16b). The grooved shoulder, on

the other hand (Figure 4-17b), led to a coarser, inhomogeneous grain structure, as well as evidence of large voids that are consistent with those seen in vertical cross-section shown in Figure 4-12b-ii.

Progressing into the TMAZ, both shoulder variants display similar results, with larger grains elongated in the direction of the tool rotation, before transitioning into the coarse structure of the HAZ after approximately 450 μm . Figure 4-19 presents a comparison of the microstructure in the HAZ with that of the bulk material. No obvious grain growth is evident, which can be attributed to low heat exposure at such short welding times of 1.5 s [4.34]. This was characteristic of all welds produced with each shoulder variant.

In section 4.3.2, the chamfered shoulder was found to redistribute pressure from the centre of the weld towards the periphery. The influence of this can be identified in the grain structure of the respective regions. A coarser grain structure is present in the SZ (Figure 4-18a) compared with the other shoulder variants, which is consistent with lower refill pressure in this region. In contrast, a more refined, homogenous grain structure is visible in the SZ/TMAZ region. The smooth transition from ultra fine grains in the SZ to coarser, directional grains in the TMAZ, without an identifiable bonding line, is further evidence of improved mixing in this region (Figure 4-18b). The TMAZ of the chamfered shoulder weld is approximately 20% narrower than for the conventional and grooved shoulders. This can be attributed to the narrower shoulder end-face generating a smaller region of plastic deformation during the plunging stage.

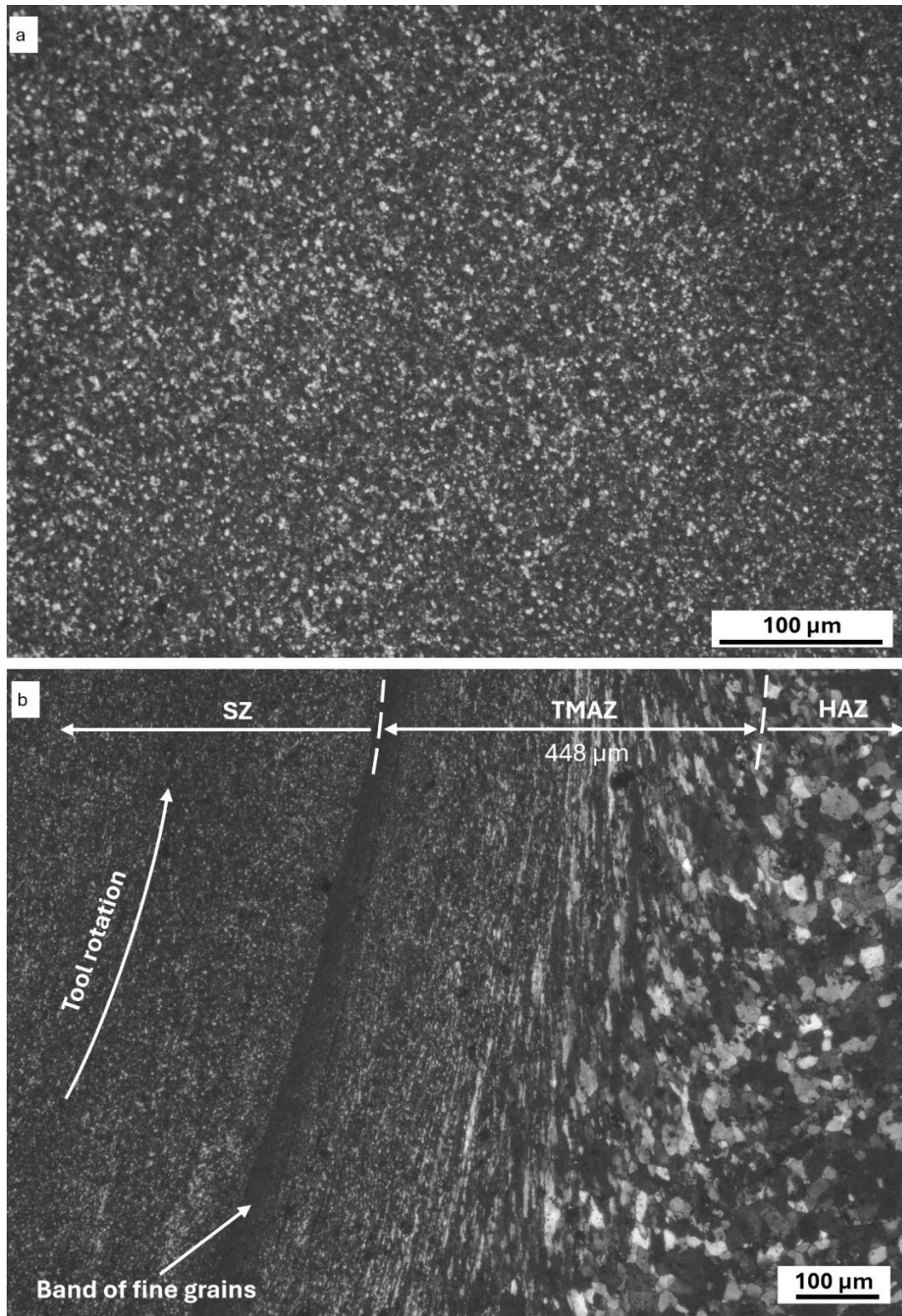


Figure 4-16 Transverse micrographs of conventional shoulder welds etched with Barker's solution (welding time = 1.5 s) a) SZ, b) SZ/TMAZ interface

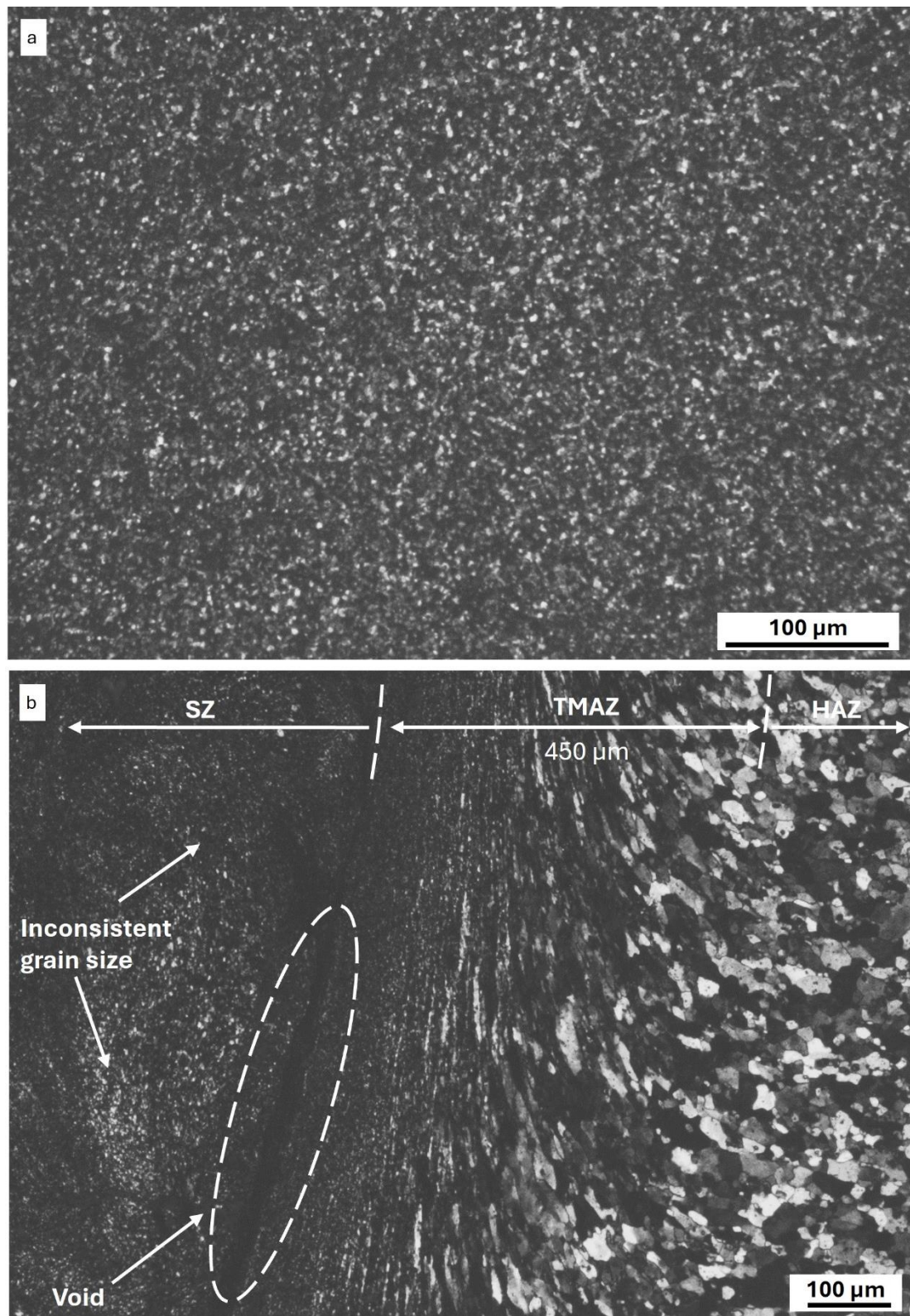


Figure 4-17 Transverse micrographs of grooved shoulder welds etched with Barker's solution (welding time = 1.5 s) a) SZ, b) SZ/TMAZ interface

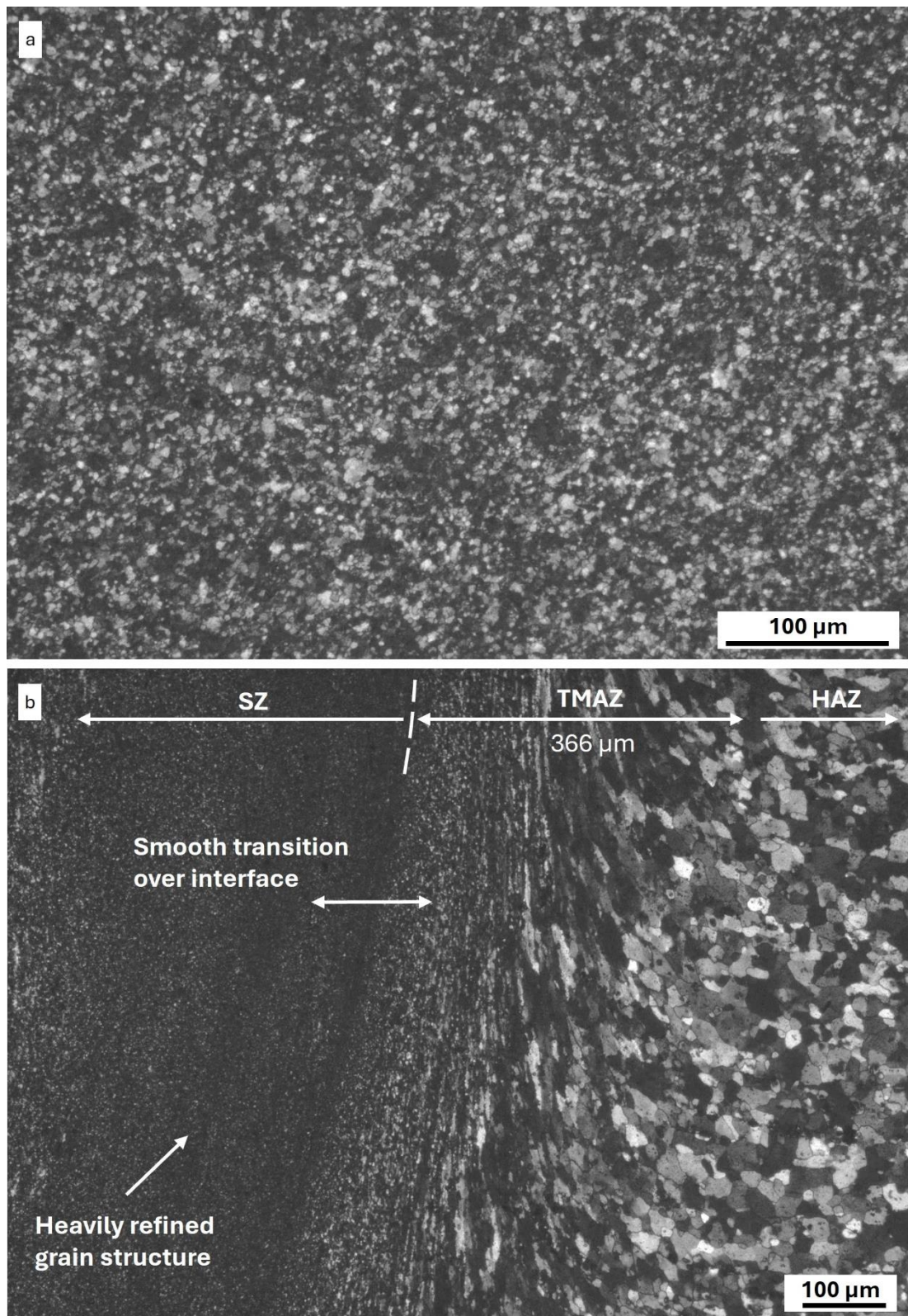


Figure 4-18 Transverse micrographs of chamfered shoulder welds etched with Barker's solution (welding time = 1.5 s) a) SZ, b) SZ/TMAZ interface

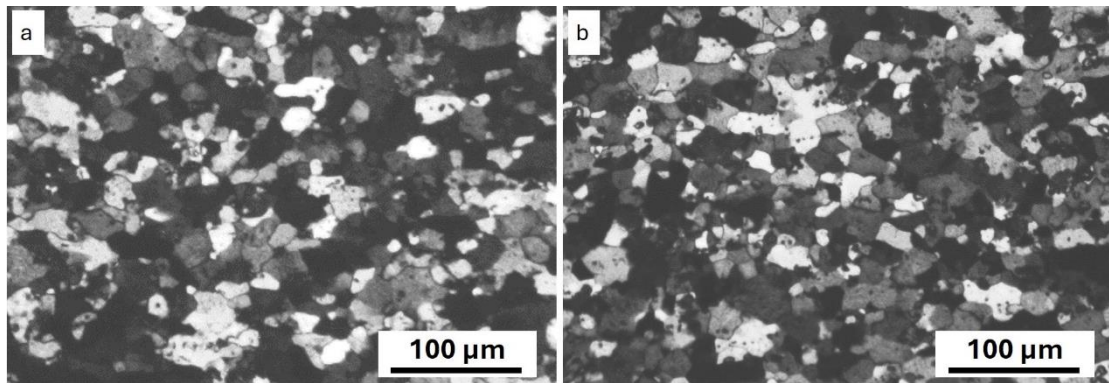


Figure 4-19 Micrographs etched with Barker’s solution comparing microstructure in the HAZ with the bulk material, a) HAZ of conventional shoulder weld with 1.5 s welding time b) bulk material

4.3.4 Microhardness

Indicative microhardness maps for 1.5 s duration welds produced with each shoulder design are presented in Figure 4-20. All welds displayed clear hardening in the SZ region, consistent with intense plastic deformation and the grain refinement identified in Figure 4-16, 18, 19. As discussed in section 4.3.2.2, the conventional and grooved shoulders displayed strong mixing at the lap-interface but poor mixing at the SZ/TMAZ interface at shorter welding times, whereas the opposite was the case with the chamfered shoulder. As a result, a deeper SZ that extends into the bottom sheet is present in conventional and grooved shoulder welds (Figure 4-20a,b). In contrast, chamfered shoulder SZ does not extend into the bottom sheet, further confirming the lack of mixing at the lap-interface observed in Figure 4-13c-ii.

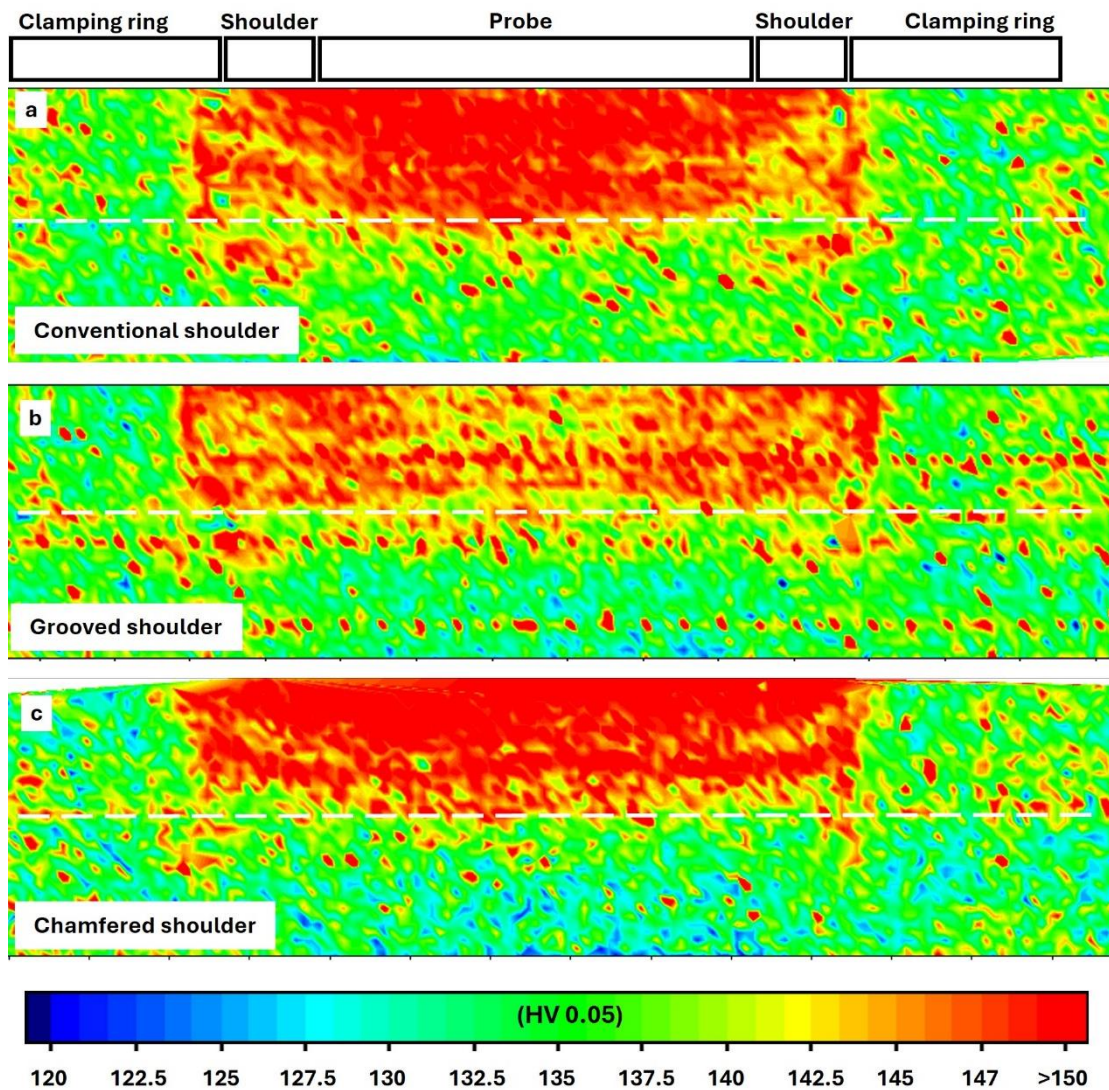


Figure 4-20 Hardness map for welds produced at 1.5 s welding time and 1500 rpm rotational speed with modified shoulder geometries

4.3.5 Tensile lap-shear testing and failure modes

The results of the tensile lap-shear testing are presented in Figure 4-21 and Table 4-3. The conventional and grooved shoulder welds performed similarly at all welding times. Both shoulders produced welds with an average ULSF of ≈ 9 kN at 3 s welding time, compared with 8.01 kN for the chamfered shoulder. However, as welding time was reduced to 1.5 s, both the conventional and grooved shoulder welds suffered a sharp loss in strength to 6.45 kN and 6.06 kN respectively. In stark contrast, the chamfered shoulder exhibited a slight increase in ULSF to 8.16 kN. This finding is significant as it represents the first instance of dissociating the reduction of welding time and the loss of ULSF.

As welding time was reduced to 0.75 s, welds from all shoulder variants demonstrated a loss of ULSF. The chamfered shoulder, however, produced the strongest welds, with an average ULSF of 7.2 kN; this is an increase of 19% compared with its conventional counterpart. This improvement in performance can be attributed to the chamfered shoulder consistently providing superior mixing at the SZ/TMAZ interface at all welding times, as discussed in section 4.3.2.2.

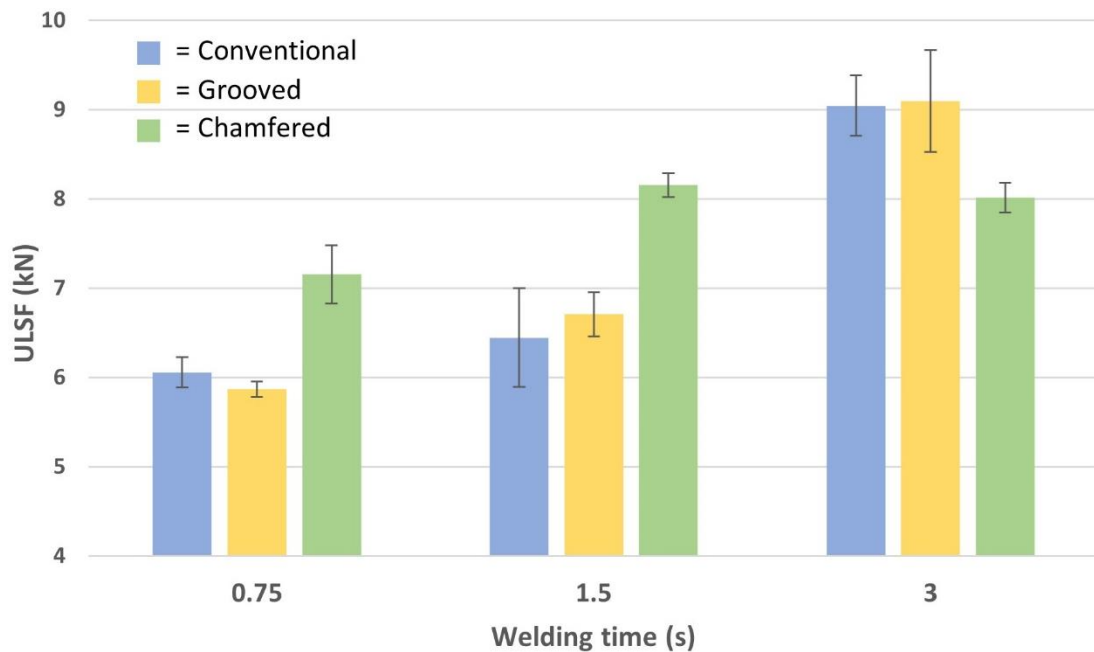


Figure 4-21 Results of tensile lap-shear testing for welds produced with each shoulder variant (n=3)

Table 4-3 Tabulated results of tensile lap-shear testing and failure modes

Parameter ID	Shoulder type	Welding time (s)	Lap-shear force (kN)	Standard deviation (kN)	Sample failure mode		
					1	2	3
P01	Conventional	3	9.05	0.17	PPO	PPO	PPO
P02		1.5	6.45	0.55	PPO	PPO	PPO
P03		0.75	6.06	0.34	PPO	TWS	PPO
P04	Chamfered	3	8.01	0.33	TWS	TWS	TWS
P05		1.5	8.16	0.13	TWS	TWS	TWS
P06		0.75	7.2	0.18	PPO	PPO	PPO
P07	Grooved	3	9.1	0.57	PPO	PPO	PPO
P08		1.5	6.7	0.25	PPO	PPO	PPO
P09		0.75	5.87	0.09	PPO	PPO	PPO

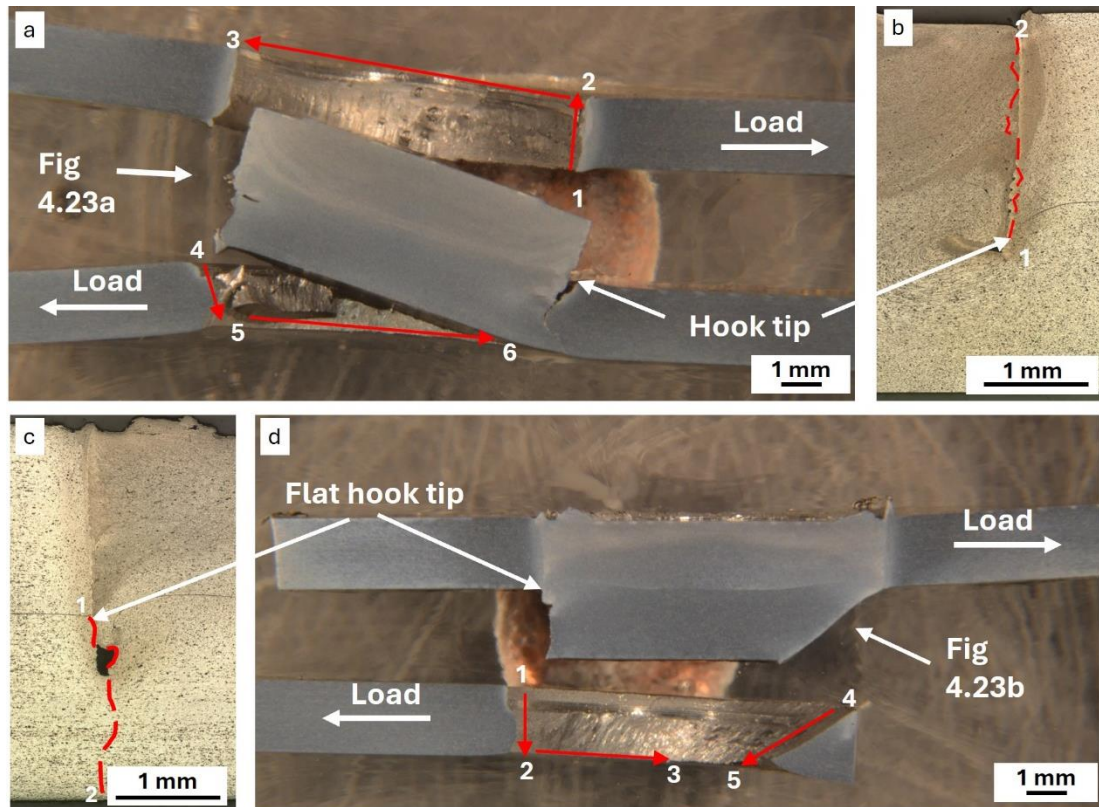


Figure 4-22 Fracture modes of RFSSW joints made at 3 s welding time a-b) PPO fracture, c-d) 45° shear

Tensile testing revealed two modes of failure that have been reported extensively in the literature [4.28,4.35–4.39]: plug pull-out (PPO) (Figure 4-22a-b) and 45° shear through the weld (TWS)(Figure 4-22c-d). PPO failure occurred in all welds produced with the conventional and grooved shoulders, as well as 0.75 s welds produced with the chamfered shoulder. Welds produced at 1.5 s and 3 s welding time with the chamfered shoulder all failed via TWS.

During PPO failures, a crack initiates at the hook tip on the tensile face and propagates upwards through the SZ/TMAZ interface and around the weld periphery, moving through points 1, 2 and 3 as indicated in Figure 4-22a [4.39]. The offset loading of the lap configuration also causes a rotation about the centre of the joint, allowing a secondary crack to open at the compressive face hook (point 4). This secondary crack advances downwards towards the bottom of the weld (point 5) and around the periphery towards point 6, until the weld nugget is pulled completely from one or

both sheets [4.22]. Figure 4-23a shows the compressive face of the weld nugget after PPO failure has occurred from the direction indicated in Figure 4-22a.

In the case of TWS failures through the bottom sheet, the initial fracture begins instead at the compressive hook tip (Figure 4-22c-d). Referring to Figure 4-22d, the crack travels downwards from the hook at point 1 and into the tunnel defect. The tunnel defect then acts as a stress concentrator, allowing the crack to propagate through the remainder of the bottom sheet to point 2. The crack then moves around the weld periphery towards point 3 until the remaining intact material can no longer support the test load. A secondary failure then begins at the tensile hook tip, shearing rapidly through the path from point 4 to point 5. The SEM image presented in Figure 4-23b shows the weld nugget of a TWS failure from the direction indicated in Figure 4-22d. The point at which tensile failure gives way to TWS can be identified by the disappearance of the tunnel defect, as the shear fracture does not pass through the tunnel.

These failure modes show that, in this study, the primary weld characteristic influencing ULSF was the quality of joining at the SZ/TMAZ interface. With a 3 s welding time, both shoulder variants produced good quality mixing in this region, however the chamfered shoulder welds failed prematurely due to large tunnel defects which led to crack propagation through the lower sheet. At a lower welding time of 1.5 s, ULSF dropped significantly for the conventional shoulder welds because of poor mixing at the SZ/TMAZ interface. Mixing remained sufficient for the chamfered shoulder welds because of the redistribution of refill pressure, and as such ULSF remained constant.

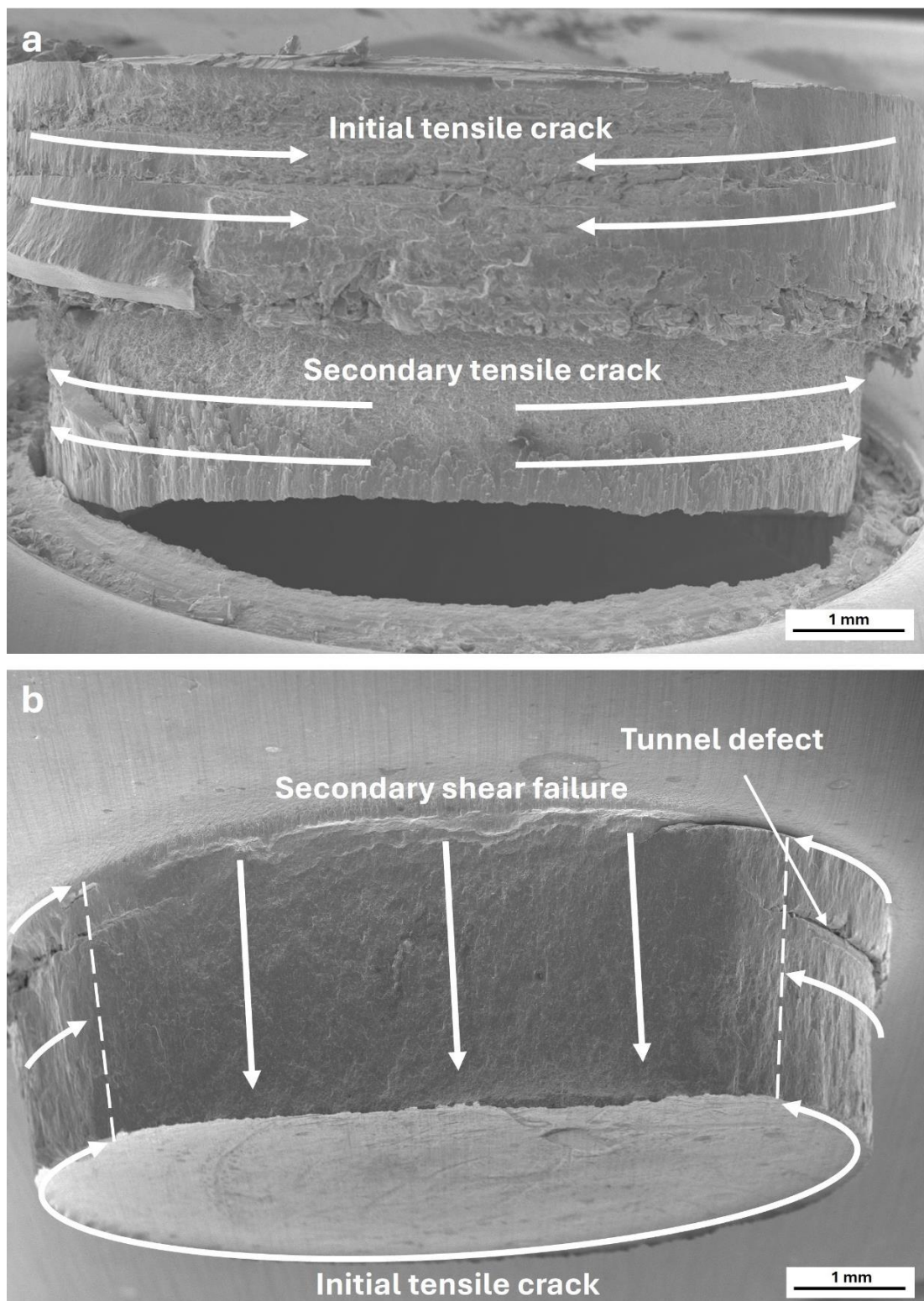


Figure 4-23 SEM images of a) weld nugget after PPO failure b) weld nugget after TWS failure through the bottom sheet

4.4 Conclusions

This chapter has presented an investigation of two novel geometrical modifications to the shoulder component of RFSSW tools and compared their performance with that of a conventional shoulder. Angled grooves were machined into the contact face of one shoulder, and a 45° chamfer applied to the inner rim of another. At all of the investigated welding times, weld strength either remained the same, or was reduced by the use of the grooved shoulder, and defect size was increased. Conversely, the chamfered shoulder was successfully shown to influence the refill path of the weld material, leading to higher strength joints at very low welding times of 0.75 s and 1.5 s, compared with the conventional and grooved shoulders.

However, the presence of the chamfer also caused large tunnels and lack of bonding at the lap interface. Further refinement of welding process parameters is required to eliminate these weld defects. The following key conclusions can be drawn from this work:

1. The inclusion of a chamfer to the shoulder tip improved bonding and eliminated defects at the SZ/TMAZ interface by partially redirecting the refill pressure of the probe from the lap-interface to the weld periphery. However, this also resulted in a lack of bonding at the lap-interface.
2. Quality of bonding at the SZ/TMAZ interface was more influential to ULSF than bonding at the lap-interface.
3. Welds produced with the chamfer modification were less influenced by welding time. Average ULSF of welds produced with the chamfered shoulder varied by only 0.8 kN over the tested welding times (3 s to 0.75 s) compared to 3 kN with the conventional shoulder.
4. The chamfered shoulder yielded increases in ULSF of 17% and 27% for 0.75 s and 1.5 s duration welds respectively, compared with the conventional shoulder.

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Chapter 5. Numerical modelling to predict defect formation in RFSSW joints

Numerical modelling is a powerful tool for accelerating the development of novel joining processes. In this study, a fully coupled thermomechanical model of the RFSSW process is created, utilising the coupled Eulerian-Lagrangian technique. Welding parameters explored experimentally in chapter 3 are simulated and validated against experimental data. The model is then used to explore how and when defects form during the process. This knowledge is applied to tailor the volume ratio parameter to minimise defects at short welding times, supporting the experimental investigation that follows in chapter 6.

5.1 Introduction

In recent years, various finite element modelling techniques have been adopted to simulate the RFSSW process [5.1–5.3]. The CEL method is particularly promising owing to its suitability for simulating high deformation processes, such as friction stir processes [5.4]. As detailed in section 2.4, the CEL method makes use of two separate mesh types to represent the workpiece material and the welding tool. The welding tool, which experiences negligible deformation during the process, is modelled with a traditional Lagrangian mesh (node position and material are fixed together), whereas the workpiece is modelled using a Eulerian mesh (node position is fixed and material flows through the mesh) [5.5]. This allows CEL models to simulate severe deformation processes without encountering mesh distortion issues. Xiong et al. [5.4] used the CEL method in Abaqus/Explicit to model void content and temperature profiles in RFSSW of AA2524. Non-destructive ultrasonic inspection confirmed similarities between the predicted and experimental tunnel defects, and close agreement was demonstrated between simulated thermal history and thermocouple measurements. However, thermocouples were positioned far from the weld centre and, as such, peak process temperatures were not validated experimentally [5.4]. Zhang et al. [5.6] used the same approach to simulate RFSSW of magnesium and

accurately predicted welding temperatures throughout the cycle, although once again, peak temperatures were not measured due to thermocouple position.

Researchers have also demonstrated alternative modelling approaches to CEL. Berger et al. [5.3] created a 2D axisymmetric model in the software package FORGE® NxT and were able to accurately predict the temperature at several locations within the weld, including the weld centre. The authors [5.3] also demonstrated the model's ability to predict defect formation during 1 s duration welds. Kubit and Trzepiecinski [5.1] also adopted the 2D axisymmetric approach using the Simufact Forming software, and visualised material flow using an extremely fine mesh. Incomplete refill and tunnel defects were predicted, however the model tended to overestimate temperature throughout the process, exceeding the material melting temperature within the SZ.

The current study applies CEL modelling using the Abaqus/Explicit FEA package. With this approach, the mechanisms of defect formation during very short duration welds are identified and mitigated through simulated optimisation of the volume ratio parameter.

5.2 Model description

This section details the specific input parameters used to develop a numerical model of the RFSSW process in the ABAQUS/Explicit finite element software. For full details of CEL modelling and the associated boundary conditions, the reader can refer to chapter 2, section 2.4.

Figure 5-1 provides an overview of the model configuration, showing a sectioned view mid-way through a weld cycle. Full geometrical details of external grooving on the shoulder and probe were included, as this has been shown experimentally to influence material flow [5.7]. Eulerian EC3D8RT elements were used to represent the workpiece material, while Lagrangian C3D8RT and C3D4T elements were used for the tool components. Only the overlap region of the sheets and the tips of the tool components are modelled to increase computational efficiency.

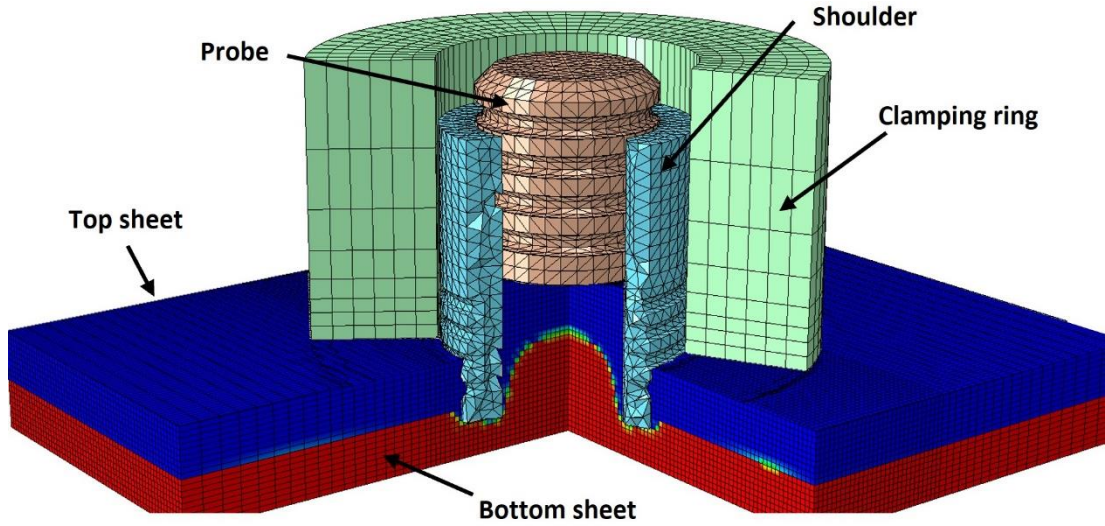


Figure 5-1: Section view of finite element model lay-out with the shoulder at maximum plunge depth

5.2.1 Material properties

The flow stress of the workpiece material was defined using the Johnson-Cook constitutive model, specified in Equation 5-1 and discussed section 2.4.2 . This model defines material flow stress as a function of temperature, strain and strain rate [5.4].

Equation 5-1

$$\bar{\sigma} = [A + B\bar{\epsilon}^n] \left[1 + C \ln \frac{\dot{\bar{\epsilon}}}{\dot{\bar{\epsilon}}_0} \right] \left[1 - \left(\frac{T - T_{ref}}{T_{melt} - T_{ref}} \right)^m \right]$$

In the current work, the Johnson-Cook model was modified to improve computational stability by omitting the strain hardening component ($B, n = 0$). Although this would not be a valid simplification if predicting residual stresses of finished welds, the primary purpose of the current work is to visualise material flow and welding defects. Schmicker et al. [5.8] concluded that strain hardening effects are negligible on the actual material flow characteristics during friction stir processes. The remaining Johnson-Cook parameters (Table 5-1) were derived numerically from the experimental findings of Bhimavarapu et al. [5.9].

Table 5-1 Johnson-Cook parameters derived from [5.9]

Material	T_{melt} (°C)	A (MPa)	B (MPa)	C	n	m
AA2024-T3	520	270	0	0.095	0	1.779

The temperature dependent material properties used in the model are given in Table

5-2. As solid-state processes by definition occur below the workpiece melting temperature, 520°C in the case of AA2024-T3 [5.9], the temperature range concerned is quite narrow (25°C to 520°C). Changes in density over this range are negligible [5.1,5.6], therefore a constant density was specified.

Table 5-2: Temperature dependent material properties of AA2024-T3 [5.10]

Temperature (°C)	Density (kg/m ³)	Youngs modulus (GPa)	Coefficient of thermal expansion (10 ⁻⁶ /°C)	Thermal conductivity (W/m°C)	Specific heat (J/kg°C)
25	2770	72	2.2	121	875
37.8		71.9	2.3	124	895
93.3		66.5	2.35	128	906
148.9		63.5	2.45	139	925
204.4		63	2.48	153	950
260		57.1	2.53	172	990
315		51.4	2.55	178	1040
371.1		47.3	2.6	182	1080
426.7		44	2.66	192	1110

5.2.2 RFSSW process parameters

Simulation results using two different welding times are presented to demonstrate the model’s predictive capabilities under different heat input conditions. Other than welding time, no changes were made to the model’s set-up between the different conditions. Table 5-3 details the welding parameters used, both of which match experimental parameters explored in chapter 3. For brevity, the two heat input conditions are referred to as “fast” and “slow” for the 1.5 s and 3 s welds, respectively.

Table 5-3 Process parameters for the low and high heat input conditions

	Welding time (s)	Rotational speed (rpm)	Plunge depth (mm)	Volume ratio
Condition 1 – “fast”	1.5	2000	2.25	0.8
Condition 2 – “slow”	3			

5.2.3 Thermal boundary conditions

Thermal boundary conditions (see section 2.4) were applied to the workpiece and

tool surfaces to account for heat losses during the process. A coefficient of 1000 W/m² was specified for the underside of the workpiece to account for conduction between workpiece and backing anvil, and a coefficient of 30 W/m² on top and side workpiece surfaces, and outer tool surfaces, represented convective and radiative heat losses to the atmosphere.

5.2.4 Heat generation and contact

Heat generation in the model occurs through two mechanisms: frictional heating and inelastic heating. A temperature dependent coefficient of friction was specified (Table 5-4), based on the Coulomb friction model, and the inelastic heat fraction of the aluminium workpiece was set as 0.9 [5.4,5.6,5.11,5.12]. Of the heat generated at the tool – workpiece interface, 90% was dissipated into the workpiece. Interactions between the tool – workpiece, and workpiece – workpiece were defined using Abaqus’s in-built “General contact” function.

Table 5-4: Coefficient of friction between aluminium and steel as a function of temperature [5.13]

Temperature (°C)	20	160	200	400	500	>580
Coefficient of Friction	0.35	0.3	0.26	0.08	0.03	0.01

5.2.5 Computational efficiency measures

Several measures were taken to increase the computational efficiency of the model:

1. Rigid body constraints were applied to all tool elements as tool deformation is negligible over the course of a single weld [5.4,5.6,5.14,5.15].
2. Time scaling was applied by increasing loading rates and decreasing process time by a factor of 100. When the loading rate is increased, the thermal conductivity of the workpiece and tool, and the rate of heat loss across boundaries must also be scaled. Factors of 200, 100, and 20 were adopted to scale thermal conductivity, convective losses, and conductive losses, respectively, as reported elsewhere [5.16].
3. Only the overlapping region of the workpiece and tips of the welding tools were simulated to reduce the total number of elements.
4. All surfaces of the welding tools not likely to come into contact with the

workpiece material were excluded from the general contact domain.

5.2.6 Experimental validation

A common practice for model validation within the RFSSW and FSW literature is through comparison of simulated and experimental temperatures [5.4,5.6,5.17–5.19]. In this work, process temperatures were measured experimentally using K-type thermocouples, spot welded to the top sheet, 6 mm from the weld centre (Figure 5-2a). To accommodate the thermocouple wires beneath the clamping ring, small channels were cut into the top sheet, as shown in Figure 5-2b. An average was taken of two replicant measurements for each welding condition. Microstructural analysis was used to further support model validation, and the reader can reference chapter 3 for details of welding procedures, metallographic preparation, and analysis techniques. The chemical composition and mechanical properties of the base material are likewise provided in chapter 3.

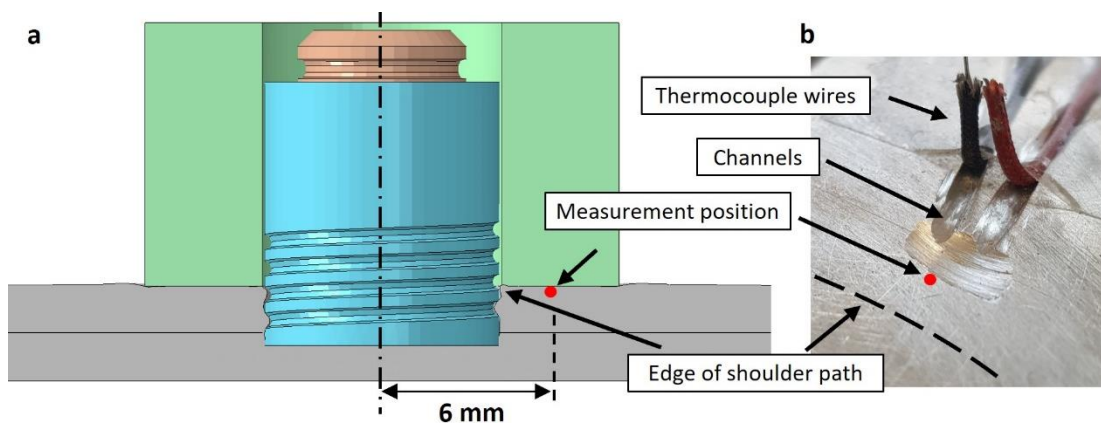


Figure 5-2 Diagram of thermocouple position within the welding set-up

5.3 Results and Discussion

5.3.1 Thermal history

Temperature distributions through the weld cross-section for the fast condition are presented in Figure 5-3a. The temperature trend shows a strong agreement between the simulated and experimental results throughout the plunge stage (Figure 5-3b). Both curves plateau and begin to decline early in the refill stage, however the simulated results show a second peak at 1.4 s. As evidenced in Figure 5-3a, this results from heat generation between the probe and workpiece, causing the SZ temperature

to rise above 450°C. Xiong et al. [5.4] and Zhang et al. [5.6] observed a similar double peak behaviour in their simulations and experiments. This was attributed to the slight pause in heat generation that occurs between the shoulder reaching its maximum depth, where it no longer exerts a significant downwards force on the workpiece, and the probe beginning to generate heat as it refills material.

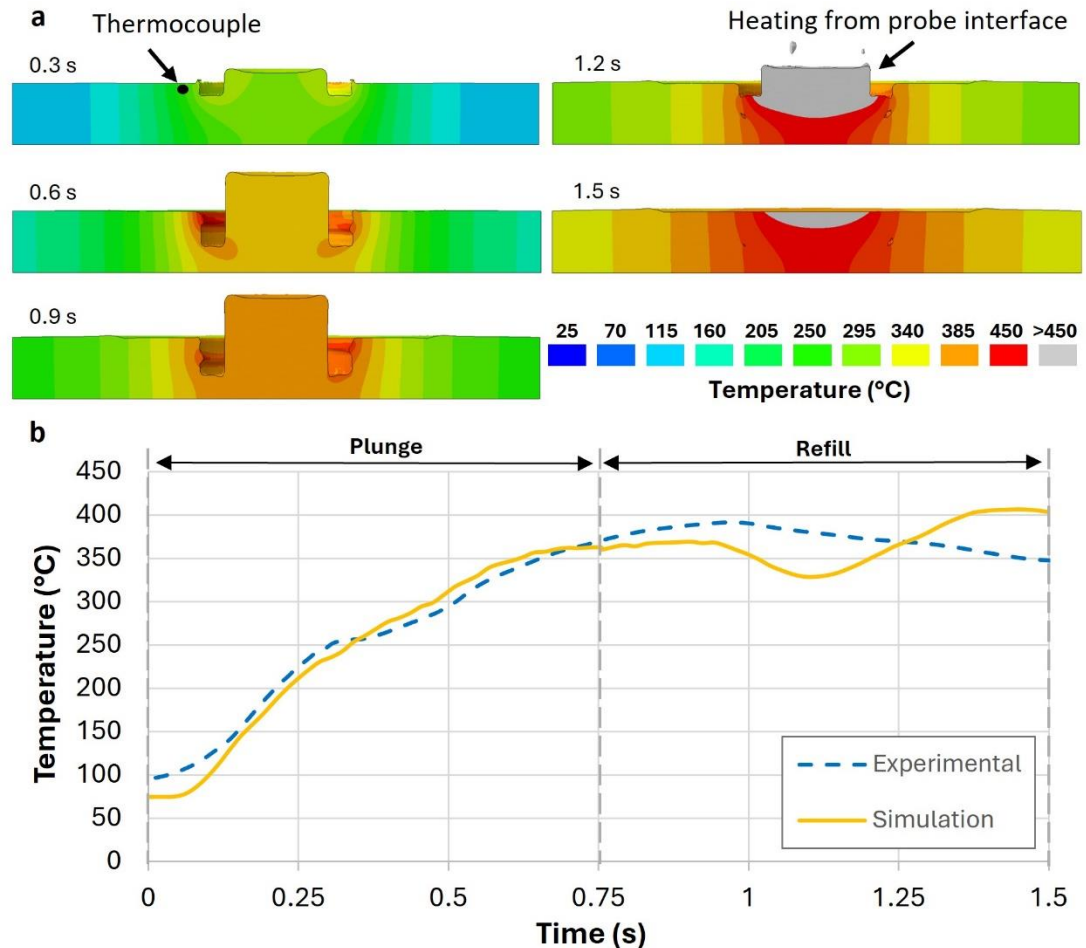


Figure 5-3 Simulated temperature distribution for the fast weld condition (1.5 s welding time, 2000 rpm rotational speed) a) contour plots of temperature distribution at different time steps, b) comparison of thermocouple data with simulated temperature

However, the model showed strong agreement with the peak temperature, predicting 406°C compared with the thermocouple measurement of 391°C. This represents only a 3.8% error with the experimental results, well below the 10% error observed in the models of Berger et al. [5.3] and Kubit & Trzepieciniski [5.1]. In the case of the slow weld conditions (Figure 5-4), the predicted temperature showed strong agreement with experimental data through the first two thirds of the cycle. However, the model again predicted a second peak that was not recorded by the thermocouple. This

disparity is most likely attributable to an imperfect connection between the thermocouple with the workpiece.

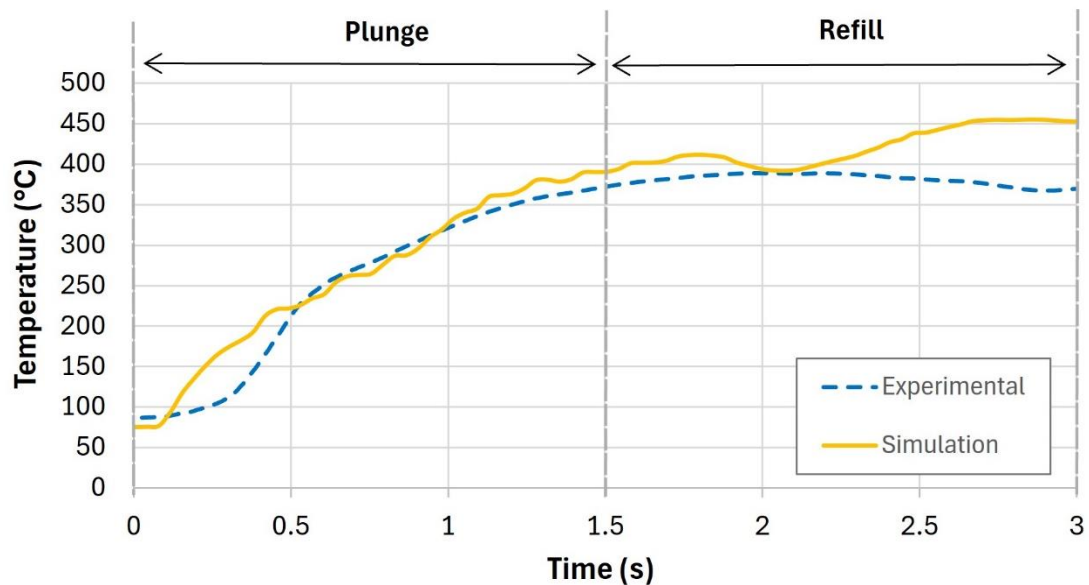


Figure 5-4 Comparison of thermocouple data with simulated temperature for the high heat input condition (3 s welding time, 2000 rpm rotational speed)

5.3.2 Plastic strain

The CEL approach can be used to provide a qualitative prediction of plastic strain in high deformation processes such as RFSSW [5.6]. The shoulder plunge region experiences the highest level of strain due to the mixing effect of the shoulder [5.6], as indicated in Figure 5-5a. Figure 5-5b & c compare contour plots of equivalent plastic strain in the shoulder plunge region with etched micrographs of comparable experimental welds. Several consistencies between the simulation and experimental results can be observed. Firstly, the model predicts a bow-shaped TMAZ for both fast (Figure 5-5b i) and slow (Figure 5-5c i) weld conditions. This behaviour is reported in the RFSSW literature [5.20–5.22] and is comparable to the regions of refined grains visible in Figure 5-5b ii, and Figure 5-5c ii. The model also predicts that a longer welding time leads to a wider TMAZ with higher strain, which is consistent with the experimental microstructure, as well as the findings presented in chapter 3 (Figure 3-7).

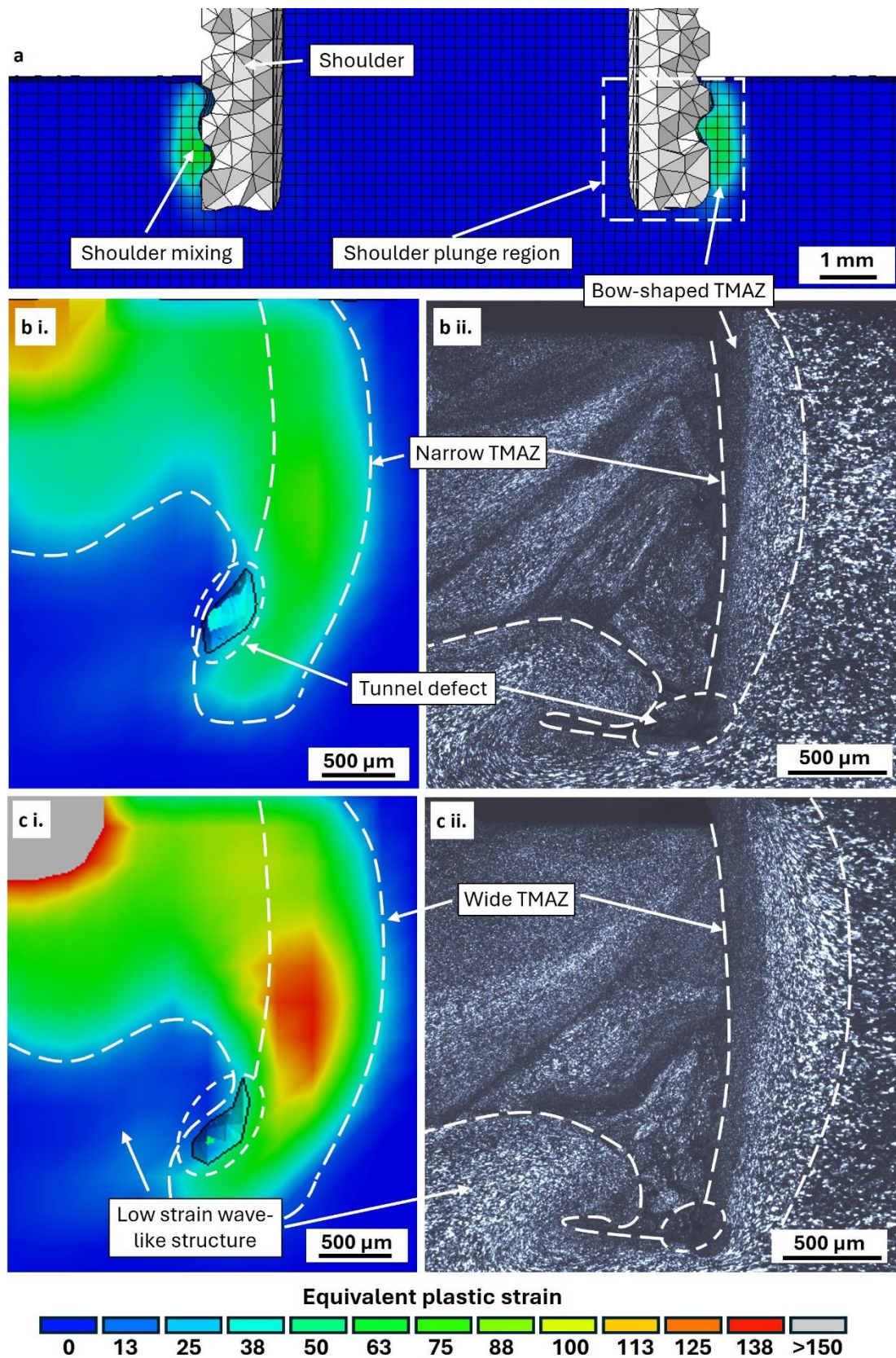


Figure 5-5 Comparison of simulated equivalent plastic strain with weld microstructure in the shoulder plunge region a) plastic strain distribution for low heat input conditions b) shoulder plunge region for low heat input conditions c) shoulder plunge region for high heat input conditions

A lower strain region is also predicted below and adjacent to the TMAZ which shares a similar wave-like shape to the results discussed in section 4.3.1. This occurs as material is drawn upwards from the bottom sheet during the plunge stage and is folded back upon itself during the refill stage [5.23,5.24]; section 5.3.4 discusses this phenomenon in more detail. Figure 5-5b ii and Figure 5-5c ii reveal similar wave patterns of coarser grain structure in both experimental welds that are indicative of lower plastic deformation in this region. These findings are further supported by the work of Kubit & Trzepieciniski [5.1], who observed very similar behaviour in a combined numerical and experimental study.

5.3.3 Defect formation

The CEL model predicted the formation of geometric defects, which were also found in the experimental welds. Figure 5-6 depicts transparent views of the simulated welds, revealing large tunnel defects around the circumference of the shoulder plunge region in both fast (Figure 5-6a) and slow conditions (Figure 5-6b). Berger et al. [5.3] presented similar findings as evidence of their models' validity.

Although the size and shape of the tunnels vary around their length, larger maximum and minimum heights are observed for the fast condition. This is consistent with the findings of chapter 3, where tunnel size was inversely related to welding time. Micrographs of the tunnel region for the fast (Figure 5-6a ii) and slow (Figure 5-6b ii) conditions further confirm this, as well as demonstrating that tunnel size is in the same approximate scale as in the simulated welds.

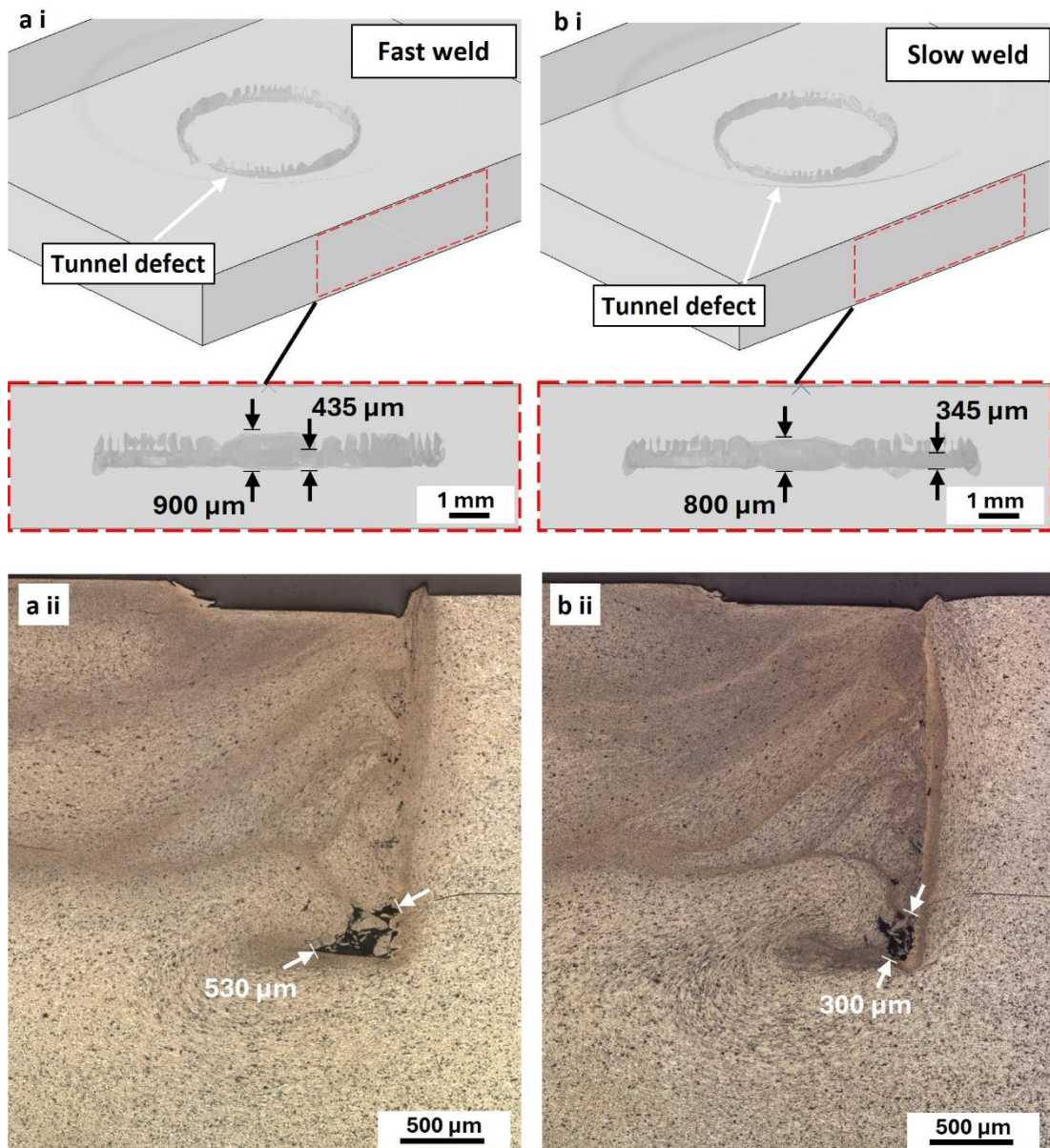


Figure 5-6 Comparison of tunnel defects found in simulated and experimental welds a) fast weld, b) slow weld - i. transparent view of simulated weld, ii. micrograph of experimental cross-section

5.3.4 Defect elimination by modifying volume ratio

The results presented in chapter 3 showed that increasing rotational speed was not effective at reducing tunnel defects; other welding parameters must therefore be optimised to address this problem. In this section, simulations are utilised to observe the manner in which the tunnel forms, and how it can be avoided by adjusting the volume ratio parameter.

Figure 5-7 presents a series of simulated weld cross-sections from different points of the refill stage, illustrating the influence of different volume ratios. Figure 5-7a shows

the fast weld conditions discussed throughout this chapter, where a volume ratio of 0.8 was specified. Near identical parameters were used for the welds of Figure 5-7b, with the only difference being an increase in volume ratio to 1.0.

For the 0.8 volume ratio condition, a clear gap is visible between the probe and the workpiece material at the end of the plunge stage (0.75 s). As the refill stage commences, the probe does not make contact with the material until ≈ 1 s, by which point the shoulder has retracted by nearly 1 mm, leaving a substantial volume of empty space. At 1.1 s, the movement of material from beneath the probe toward the weld periphery reveals how the tunnel defect forms, as a wave-like flow (highlighted in section 5.3.2) begins to fold upon itself [5.23,5.24]. By 1.2 s, the tunnel has fully formed due to a lack of forging pressure from the retracted shoulder. The full extent of the tunnel can be clearly seen in the transparent view of the final weld at 1.5 s.

The tunnel defect is greatly reduced in the simulation by increasing the volume ratio to 1.0, as seen in Figure 5-7b. In this case, the probe retracts just enough to accommodate the material displaced by the shoulder, leaving no gap between probe and material at the end of the plunge stage (0.75 s). As the probe is already in contact with the material, it immediately begins refilling the weld, preventing any space from forming below the shoulder. The effects of this are evident at 1.1 s and 1.2 s, where no tunnel defect is visible. A number of small voids can be identified in the transparent view of the final weld; it is clear that the increase of volume ratio from 0.8 to 1.0 almost entirely eliminated the tunnel defect. Chapter 6 explores the experimental verification of this predicted outcome.

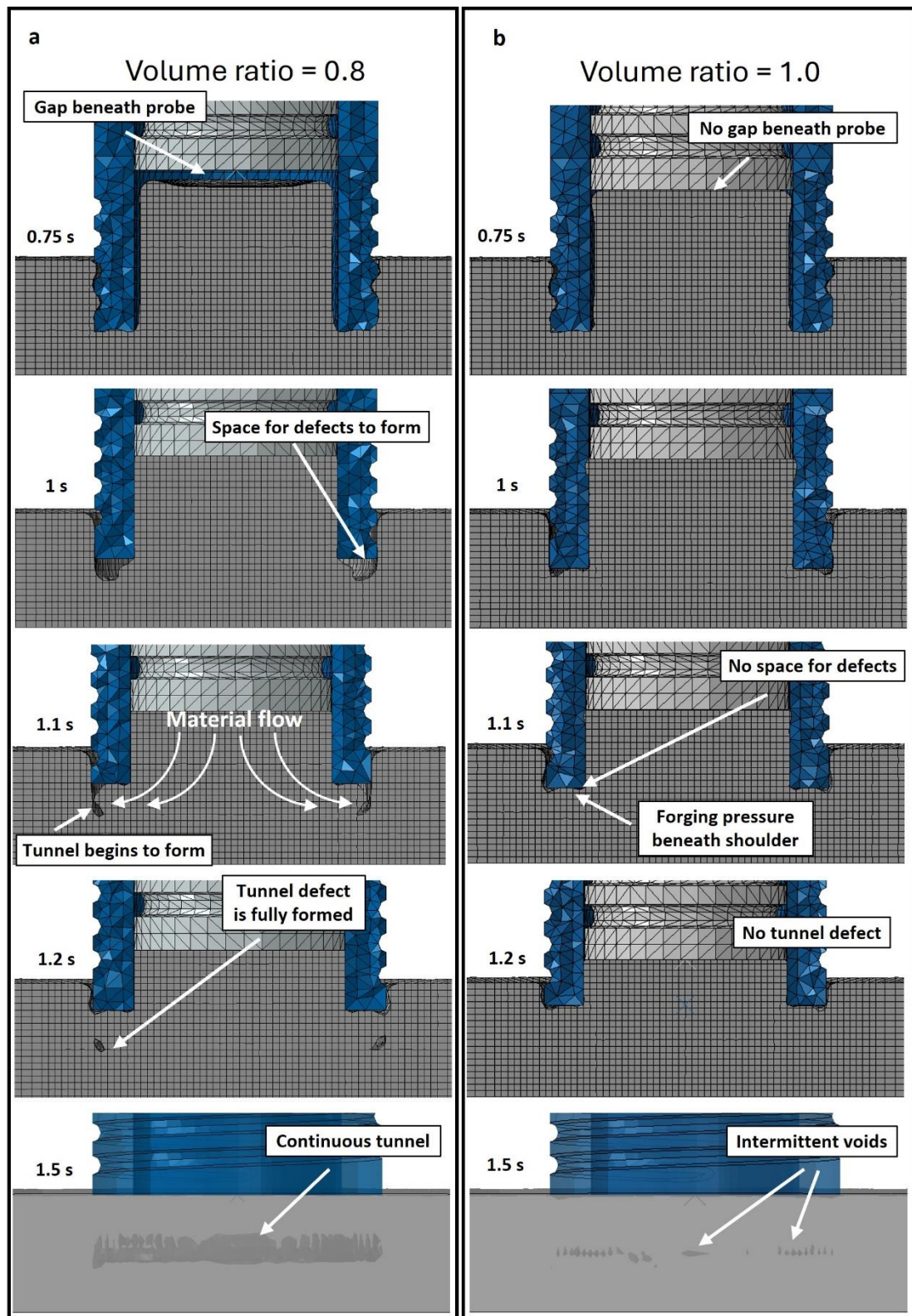


Figure 5-7 Simulated cross-sections of welds produced with moderate heat input conditions (1.5 s, 2000 rpm, 2.25 mm plunge depth), showing defect formation using different volume ratios, left column) 0.8 volume ratio, right column) 1.0 volume ratio

5.4 Conclusions

This chapter has detailed the development and application of a fully coupled thermomechanical model to simulate RFSSW of AA2024-T3. The CEL modelling technique (using Abaqus/Explicit) was successfully employed to overcome mesh distortion issues often experienced when simulating friction stir welding processes. The model was shown to accurately predict process temperatures during the plunging stage, and simulated plastic strain in the SZ and TMAZ could be qualitatively correlated with experimental weld microstructure. Finally, the tunnel defect that is typically associated with short welding times was accurately captured by the model and validated against experimental welds.

Two different welding times, 1.5 s and 3 s, were simulated to test the model under different heat input conditions. Under both conditions, the model was able to accurately predict the temperature at the edge of the TMAZ throughout the first two thirds of the process. This was validated against thermocouple data gathered under the same welding conditions. In both simulations, however, a second temperature rise was predicted during the refill stage that was inconsistent with experimental data. Although similar second peaks are reported in the literature, further investigation is required to determine whether the error lies in the model, or in the experimental data.

In the shoulder plunge region where the highest plastic deformation occurs, the model output of equivalent plastic strain was used to accurately predict the shape and size of the TMAZ under both welding conditions. This was validated qualitatively by correlating regions of high and low predicted strain with the level of grain refinement observed in the microstructures of experimental welds.

The simulation was also able to accurately capture the well-known tunnel defect. Larger tunnels were predicted at a shorter welding time, which was consistent with experimental findings. Observing the flow of material throughout the simulated process provided a novel insight into the role of volume ratio on the severity of defects. Using this knowledge, the volume ratio parameter was modified to

counteract tunnel formation, leading to a near elimination of the defects. Experimental validation of this will follow in chapter 6.

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Chapter 6. Investigation of chamfered shoulder designs and optimised tool paths

The previous chapters have taken two significant steps towards eliminating defects in short duration RFSSW joints. Firstly, it has been established that mixing defects around the weld periphery can be eliminated by the use of a novel chamfered shoulder design. Secondly, the formation mechanism of the tunnel defect has been established using stop-action analysis and numerical modelling. Weld simulations have demonstrated that the tunnel defect can be eliminated by optimising the volume ratio. This chapter will explore the interaction of these findings by further exploring the chamfered shoulder concept, as well as optimising the tool path, i.e. the volume ratio and end position of the shoulder and probe.

6.1 Introduction

Of the shoulder designs explored in chapter 4, the chamfered shoulder led to improved weld quality and an increase in lap shear strength of 17% and 27% for 0.75 s and 1.5 s duration welds respectively, whereas the grooved shoulder proved detrimental to both strength and quality. As such, no further investigation was carried out into the grooved shoulder, and this chapter focuses on variations of the chamfered design. Improvements derived from the chamfered shoulder were attributed to enhanced mixing at the weld periphery, as the outward flow of material was eased during the refill stage and refill pressure was redistributed away from the lap-interface. However, despite the improved mechanical performance, two separate defects were present in all chamfered shoulder welds: lack of bonding at the lap interface and large tunnel defects.

As bonding at the lap interface was excellent when using a conventional shoulder, but poor with the 45° chamfer design, it was postulated that an intermediate chamfer angle could provide a compromise that would balance bonding quality at the lap interface and the weld periphery. In chapter 5, numerical modelling was used to visualise material flow during the process and understand the formation mechanism

of the tunnel defect. It was found that the defect forms when the probe retracts too far during the plunge stage, leading to delayed refilling. The model predicted that if probe retraction is reduced (i.e. volume ratio increased), the delayed refill can be avoided and tunnel defects eliminated.

This chapter explores each of these concepts experimentally by comparing welds produced with three shoulder variants (conventional, 45° chamfer, 30° chamfer) and three different volume ratios (0.8, 0.9, 1.0).

In the welds explored in chapter 3, slight inconsistencies were observed in shoulder plunge depth across different welding times. In all cases, the shoulder was programmed to plunge to a depth of 2.25 mm, although in practice the plunge depth became shallower as welding time was reduced (Figure 3-6). As higher tool forces are required at lower welding times, this phenomenon can be attributed to increased flexure in the structure of the RPS100 welding machine. To address this issue, an alternative, more rigid machine was used for the investigation herein. A V-RSW30 welding machine from Stirtec was selected due to its rigid frame construction and the high 30 kN maximum plunging force of the welding head [6.1].

An unavoidable trade-off from changing welding machines was a variation in the basic design of the shoulder. The external threading incorporated in the RPS100 shoulders (see chapter 4) is not present in those used by the V-RSW30, meaning all shoulders explored in this chapter have a featureless outer surface. It has been reported elsewhere that the absence of external threading can hinder mixing at the weld periphery [6.2]. Additionally, the V-RSW30 shoulders have an internal diameter of 6 mm, rather than 6.4 mm, requiring the probe to retract further to accommodate displaced material. To account for these changes and provide an accurate performance baseline with which to compare the chamfered shoulders, the conventional shoulder design has also been explored.

6.2 Experimental methods

6.2.1 Materials and welding procedures

This investigation employs the same AA2024-T3 base material as previous chapters

and the reader is directed to chapter 3 for details of the chemical composition and mechanical properties. Likewise, sample geometry and joint configuration remained unchanged. The same welding procedure detailed in section 3.2.3.2 was used, although the new sample holding jig shown in Figure 6-1 was required to position the workpieces within the new welding machine.

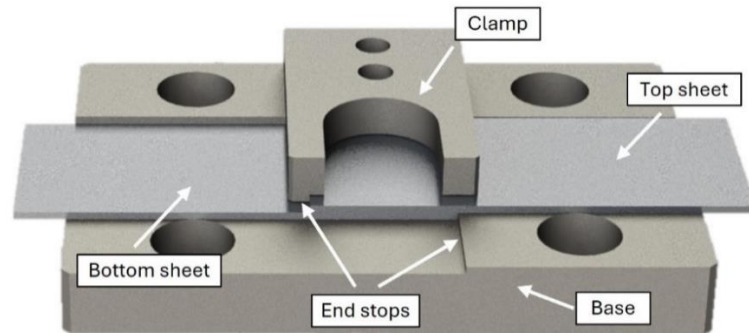


Figure 6-1: Sample holding jig used within the V-RSW30 RFSSW machine

6.2.2 Shoulder design

Three shoulder designs were explored: a conventional shoulder with no chamfer, a 45° chamfered variant based on the design explored in chapter 4, and a 30° chamfered variant to help reveal performance and microstructural trends as a consequence of chamfer angle. For ease of discussion, these will be referred to as the conventional, 45°, and 30° shoulders, and their respective welds will be referred to as conventional welds, 45° welds, and 30° welds. Figure 6-2 provides schematics of each shoulder design.

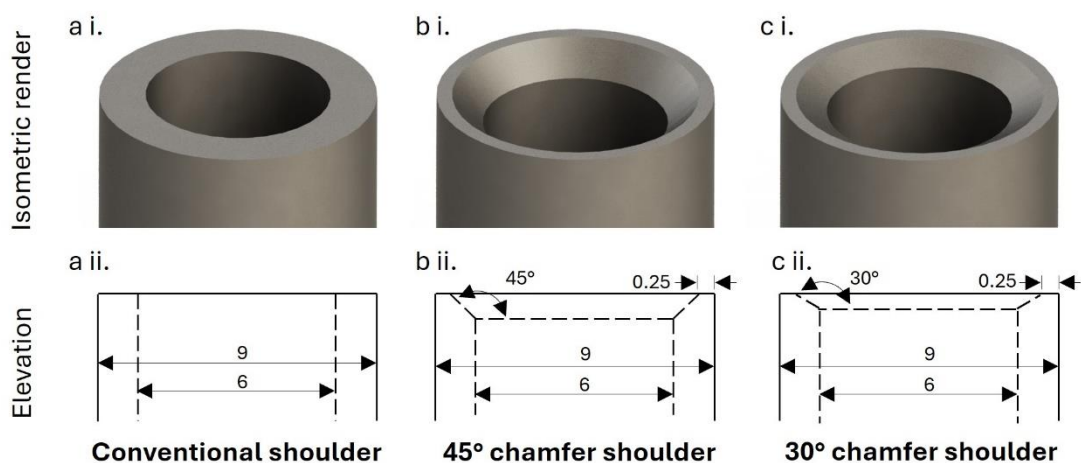


Figure 6-2 V-RSP30 shoulder designs a) conventional shoulder, b) 45° chamfered shoulder, c) 30° chamfered shoulder

6.2.3 Welding parameters

The welding parameters explored in this chapter are listed in Table 6-1. As the objective of this investigation was to understand the influence of shoulder geometry and the volume ratio parameter modelled in chapter 5, welding time, rotational speed, and plunge depth were kept constant. A welding time of 1.5 s was selected for consistency with the intermediate value discussed in chapters 3 and 4; this is 46% less than the next shortest welding time of 2.8 s reported in the literature for a comparable alloy [6.3].

A rotational speed of 2000 rpm was used as this provided the best weld performance in chapter 3. As in all previous chapters, a plunge depth of 2.25 mm was used. Volume ratios of 0.8 and 1.0 were selected to match the simulations presented in chapter 5, and an additional level of 0.9 was included to help identify trends in weld performance and microstructure.

Table 6-1 Welding parameters

Shoulder design	Volume ratio			Welding time (s)	Rotational speed (rpm)	Plunge depth (mm)
	0.8	0.9	1.0			
Conventional	P1	P2	P3	1.5	2000	2.25
30° chamfer	P4	P5	P6			
45° chamfer	P7	P8	P9			

6.2.4 Microstructural and mechanical analysis

Light optical microscopy and SEM were used to assess the presence of defects, microstructural evolution and post-testing failure surfaces of the welds. The metallographic preparation procedures and equipment used were identical to those detailed in chapter 3. Tensile testing and hardness mapping procedures were likewise unchanged and the reader is directed to chapter 3 for details.

6.3 Results and discussion

6.3.1 Baseline investigation with Stirtec welding machine

Prior to the main investigation of shoulder chamfer angles and differing volume ratios, a limited study was carried out to establish a performance baseline for the Stirtec V-

RSW30 and ensure consistency in the overall investigation. This was carried out using only the conventional shoulder. Figure 6-3 compares the shoulder plunge region of two welds created using identical process parameters, but different welding machines. The Harms and Wende RPS100 employed in previous chapters was used for Figure 6-3a, while the Stirtec V-RSW30 was used for Figure 6-3b.

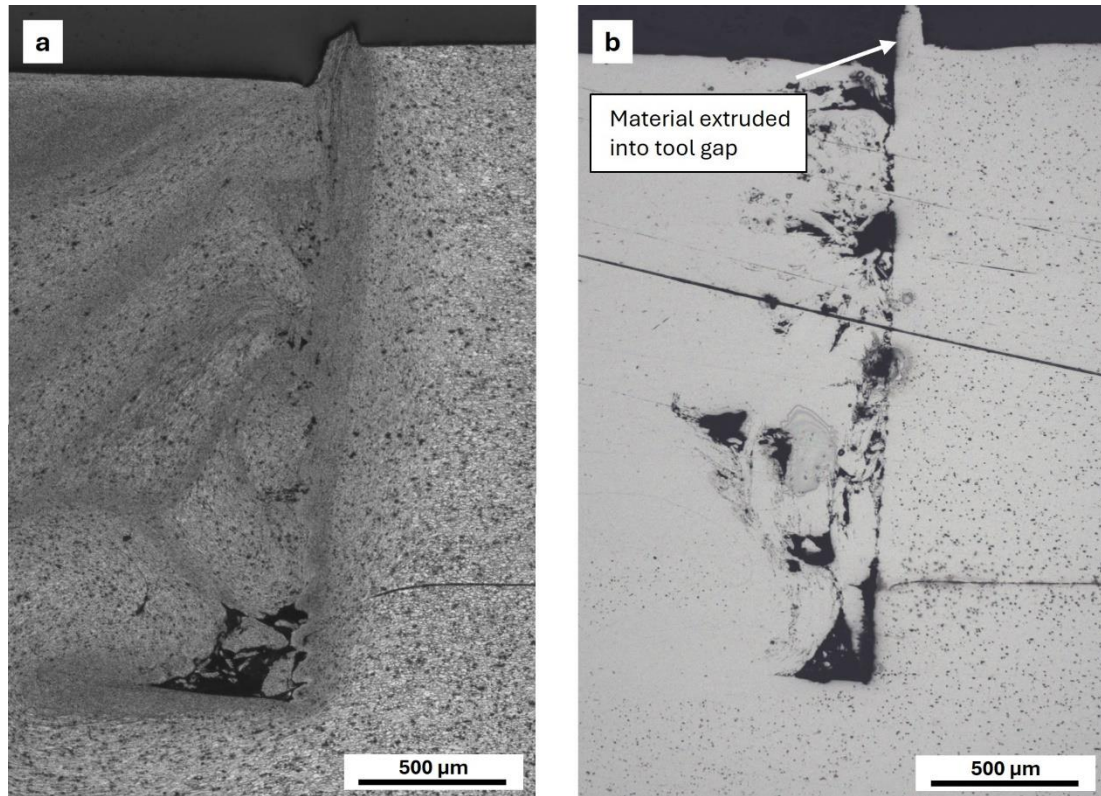


Figure 6-3 Pre-trial comparison of identical welding parameters (P1) using different welding machines a) RPS100, b) V-RSW30

Although a large tunnel is present in Figure 6-3a, the disparity in quality between the two welds is evident, with Figure 6-3b displaying not only a large tunnel defect, but significant lack of consolidation along the entire SZ/TMAZ interface. The extent of defects was so severe that proper metallographic preparation was challenging due to the quantity of contaminants escaping from the numerous voids. The drop in quality of the V-RSW30 weld was attributed to a greater loss of material through tool gaps than was experienced with the RPS100, resulting in a lack of forging pressure during the refill stage [6.4,6.5].

6.3.2 Weld quality improvement using surface indentations

Rather than progress to more resource intensive investigations with the poor-quality welds described in the previous section, an additional process parameter not yet explored within this work was implemented. Zhao et al. [6.4] reported similar excessive material loss and proposed the solution of altering the end position of the shoulder and probe to below the level of the top sheet surface. This compensates for the lost material and applies additional forging pressure, but at the cost of a permanent indent on the weld surface. The authors [6.4] implemented a 0.2 mm indent when joining a 7xxx series aluminium alloy and were able to eliminate all voids, although at significantly longer welding times (>4 s) than those considered in the current work.

Figure 6-4 reveals the shoulder plunge region of a weld produced with the V-RSW30 and the same basic parameters shown in Figure 6-3 (P1); in this case however, an indent of 0.2 mm has been included. A dramatic improvement in weld quality is evident as all voids have been eliminated from the SZ/TMAZ interface. The tunnel has similarly been reduced; however, a very small void is still identifiable in the tunnel region and will be explored further in later sections.

Based on these positive preliminary findings, the 0.2 mm indent was adopted as a parameter for all welds discussed in the remainder of this chapter.

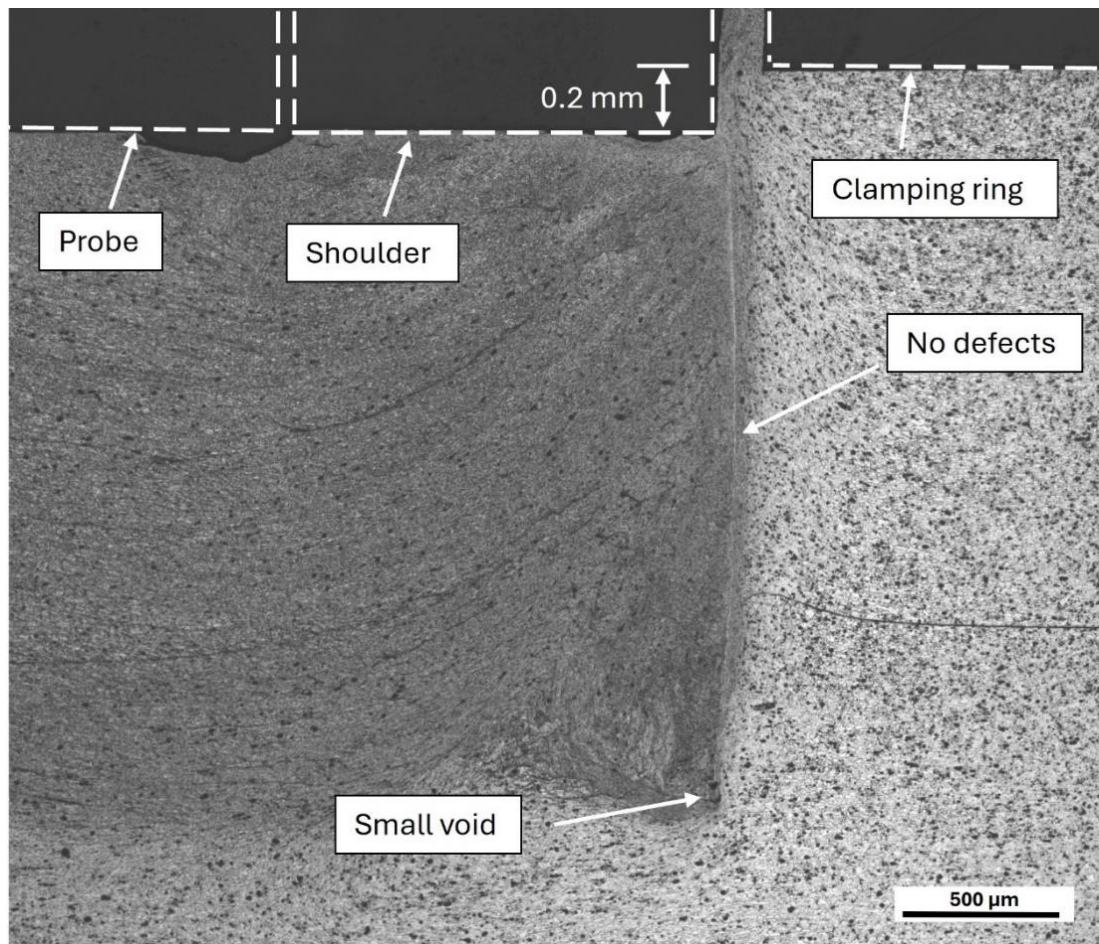


Figure 6-4 Shoulder plunge region of a weld produced using a V-RSW30 welding machine and process parameters set P1 and a 0.2 mm surface indentation

6.3.3 Defect analysis

6.3.3.1 Tunnel defect

Figure 6-5 presents micrographs of the shoulder plunge region of conventional welds produced using different volume ratios. At the lowest volume ratio of 0.8 (Figure 6-5a), small voids can be seen in the region where the tunnel defect is commonly found. Although these voids are minor compared with those discussed in chapters 3 and 4, the surrounding material also appears poorly consolidated, which indicates a lack of forging pressure (Figure 6-5a-ii). Welds presented by Shen et al. [6.6] had a similar appearance as a result of insufficient material flow. At higher volume ratios of 0.9 (Figure 6-5b) and 1.0 (Figure 6-5c), no voids are present and the material appears fully consolidated. This is consistent with modelling predictions made in chapter 5, thus supporting the validity of the numerical model.

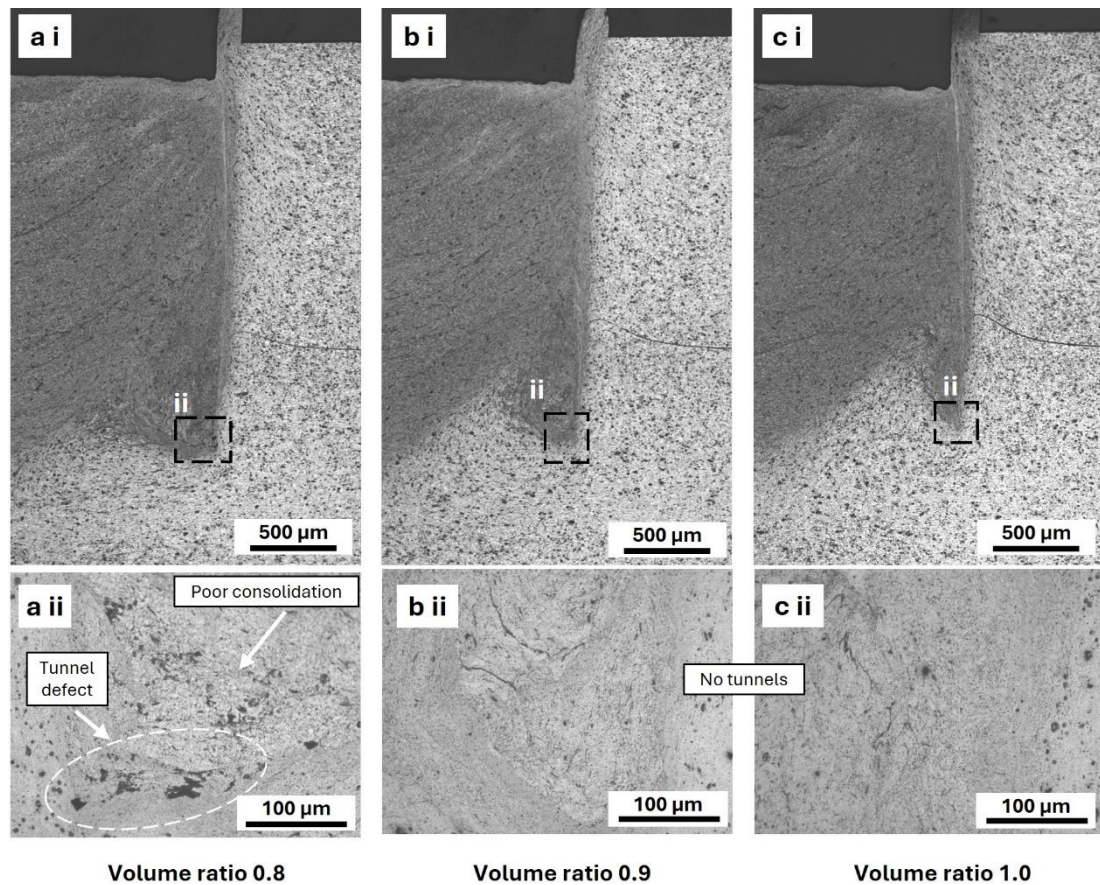


Figure 6-5 Shoulder plunge regions of welds produced using a conventional shoulder and volume ratios of a) 0.8, b) 0.9, c) 1.0

Small tunnel defects were also found in the 45° welds at a volume ratio of 0.8 (Figure 6-6a-ii). In this case, however, the surrounding material appears fully consolidated. As with the conventional shoulder, increasing volume ratio to 0.9 and 1.0 eliminated the tunnel defects entirely, as evidenced in Figure 6-6b-ii and Figure 6-6c-ii, respectively.

Unlike the previous two shoulder variants, the 30° shoulder produced welds with significant tunnel defects at volume ratios of 0.8 and 0.9 (Figure 6-7a-b). The reason for this disparity can be attributed to a minor error in the programming of the tool path that was only identified during analysis of the weld cross section. The error can be observed in Figure 6-8, where two cross section micrographs are used to compare the final tool positions in representative 45° and 30° welds. In the 45° weld (Figure 6-8a), the end positions of both tools are approximately correct and surpass the target indentation depth of 200 μm. In contrast, the indentation depth of the probe in the 30° weld (Figure 6-8b) only reaches 120 μm. This resulted in lower forging pressure

than welds produced with both other shoulder variants [6.7], leading to the larger defects observed in Figure 6-7.

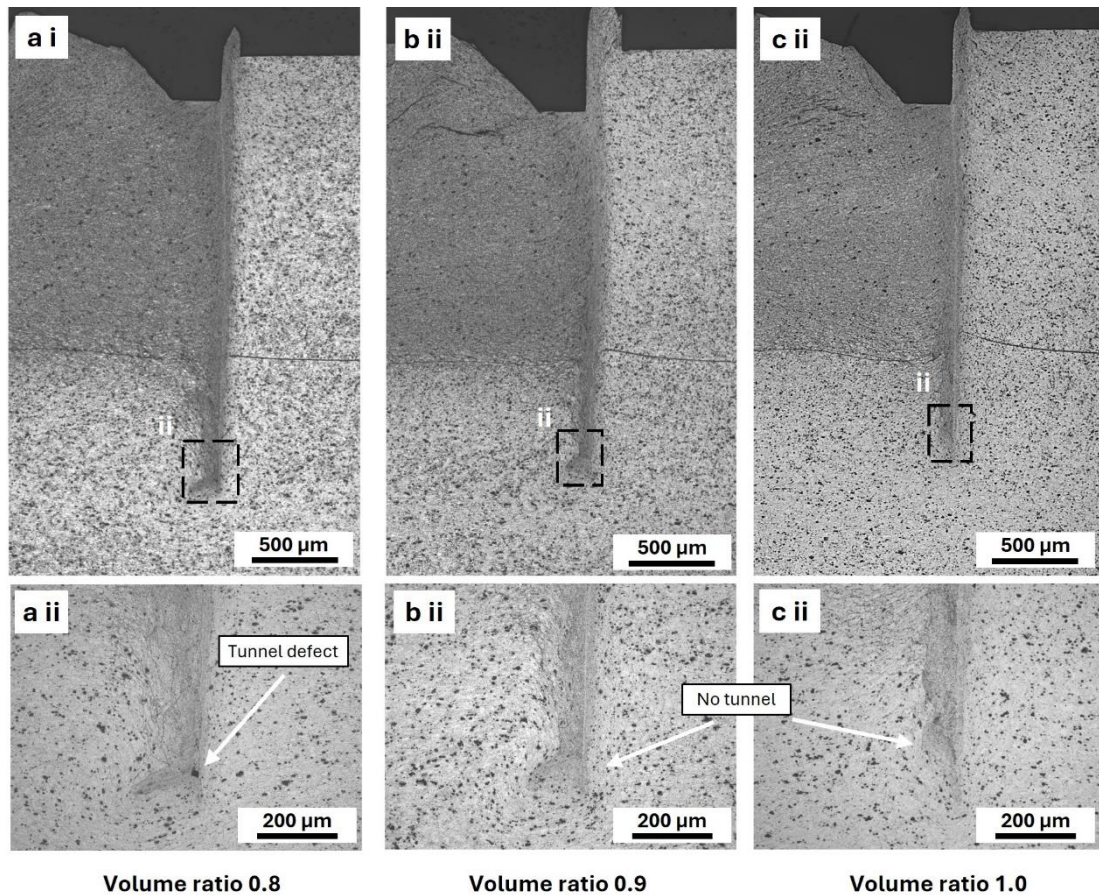


Figure 6-6: Shoulder plunge regions of welds produced using a 45° shoulder and volume ratios of a) 0.8, b) 0.9, c) 1.0

Although this error was unintentional, it affords an opportunity to better assess the influence of volume ratio on welds with larger tunnel defects. An inverse relationship between volume ratio and tunnel size is evident in Figure 6-7. The tunnel size is significantly reduced as volume ratio is increased to 0.9 (Figure 6-7b-ii) and eliminated entirely when further increased to 1.0 (Figure 6-7c-ii). This is consistent with the results of Berger et al. [6.7], who prevented tunnel defects in 1.1 s duration AA7075-T6 joints using a similar approach and ascribed the outcome to increased forging pressure below the shoulder. The validity of the numerical model presented in chapter 5 is further supported by these findings.

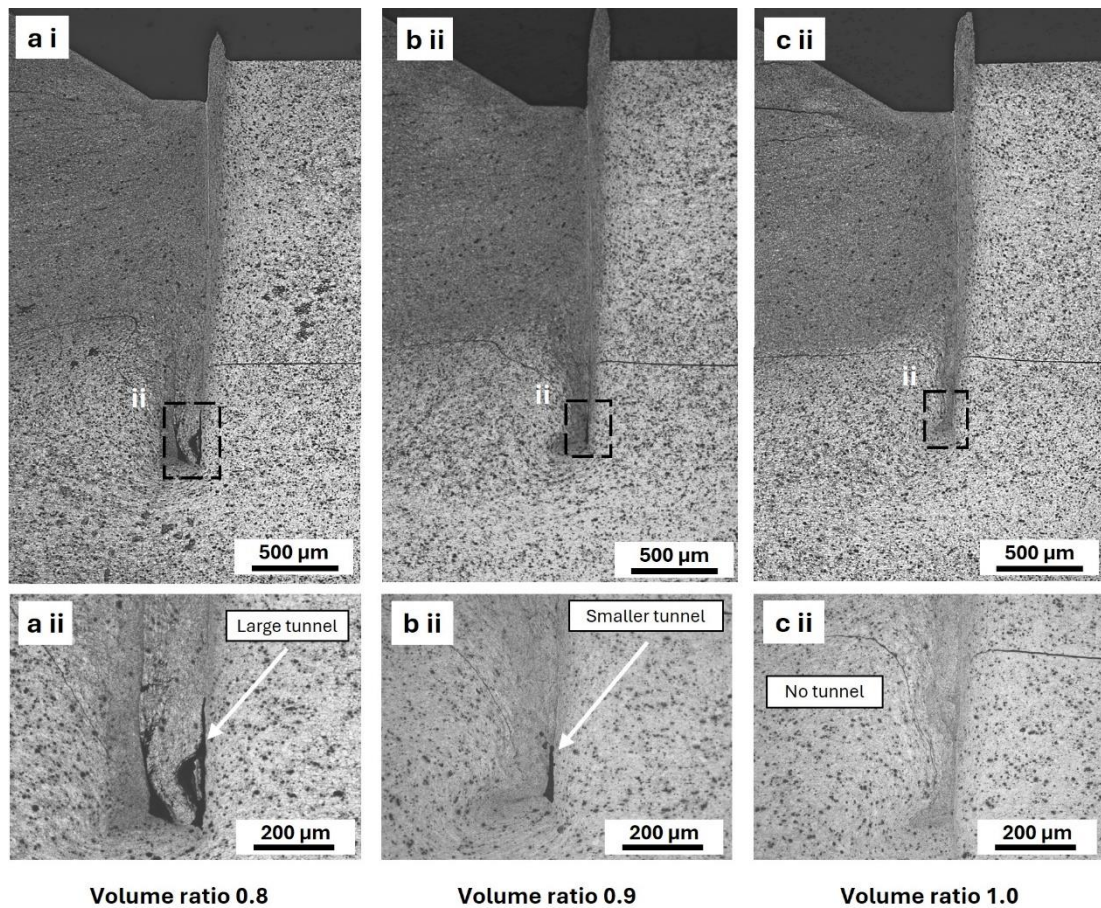


Figure 6-7 Shoulder plunge region of welds produced using a 30° shoulder and volume ratios of a) 0.8, b) 0.9, c) 1.0

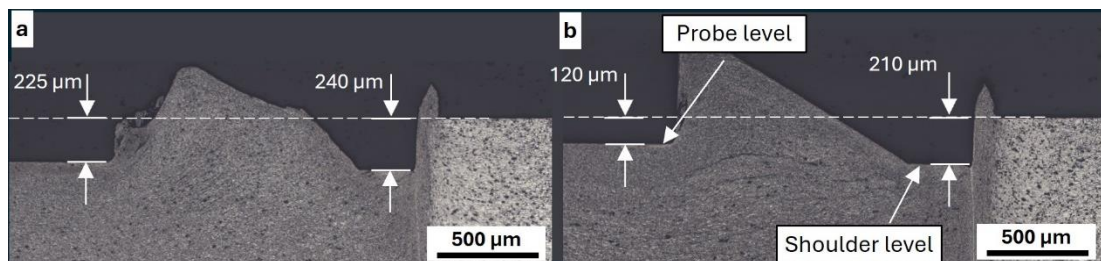


Figure 6-8: Weld cross sections showing the end position of shoulder and probe a) correct probe position for 45° chamfered shoulder welds b) incorrect probe position for 30° chamfered shoulder welds

6.3.3.2 Mixing defects

Figure 6-9 presents a series of images examining the bonding interfaces of a conventional weld. The SZ/TMAZ interface can be identified by the thin light band indicated in Figure 6-9b. Viewed under polarised light (Figure 6-9c), this band is revealed as a narrow region of highly refined grains. No voids are present along the interface, marking a significant improvement over the comparable 1.5 s duration

welds examined in chapters 3 and 4. This is attributed to the surface indentation discussed in section 6.3.2.

A bonding line shown in Figure 6-9d clearly marks the interface between top and bottom sheets. Li et al. [6.8] and Wang et al. [6.9] identified this line as a diffusion bond and found that greater forging pressure beneath the probe improved the strength of the interface.

In chapter 4, it was established that the 45° shoulder reduced bonding at the lap interface due to insufficient forging pressure. Despite the introduction of a surface indentation, the same lack of bonding can be identified in the current 45° welds, with Figure 6-10d highlighting a clear separation between the top and bottom sheets in the centre of the lap interface. As in chapter 4, improved mixing is apparent at the SZ/TMAZ interface (Figure 6-10 b-c). Unlike in the conventional welds, the interface is not marked by a distinct vertical band, and the grain structure across the interface region is more homogenous (Figure 6-10c).

Figure 6-11 presents a 30° weld produced with a volume ratio of 0.8. The SZ/TMAZ interface is comparable to the conventional weld, with a similar narrow band of heavily refined grains at the transition between the zones (Figure 6-11c). Unlike in the 45° weld (Figure 6-10d), a diffusion bond is present at the lap interface (Figure 6-11d) as a result of increased forging pressure. This supports the hypothesis first proposed in chapter 4 that chamfer angle can influence pressure distribution during refill.

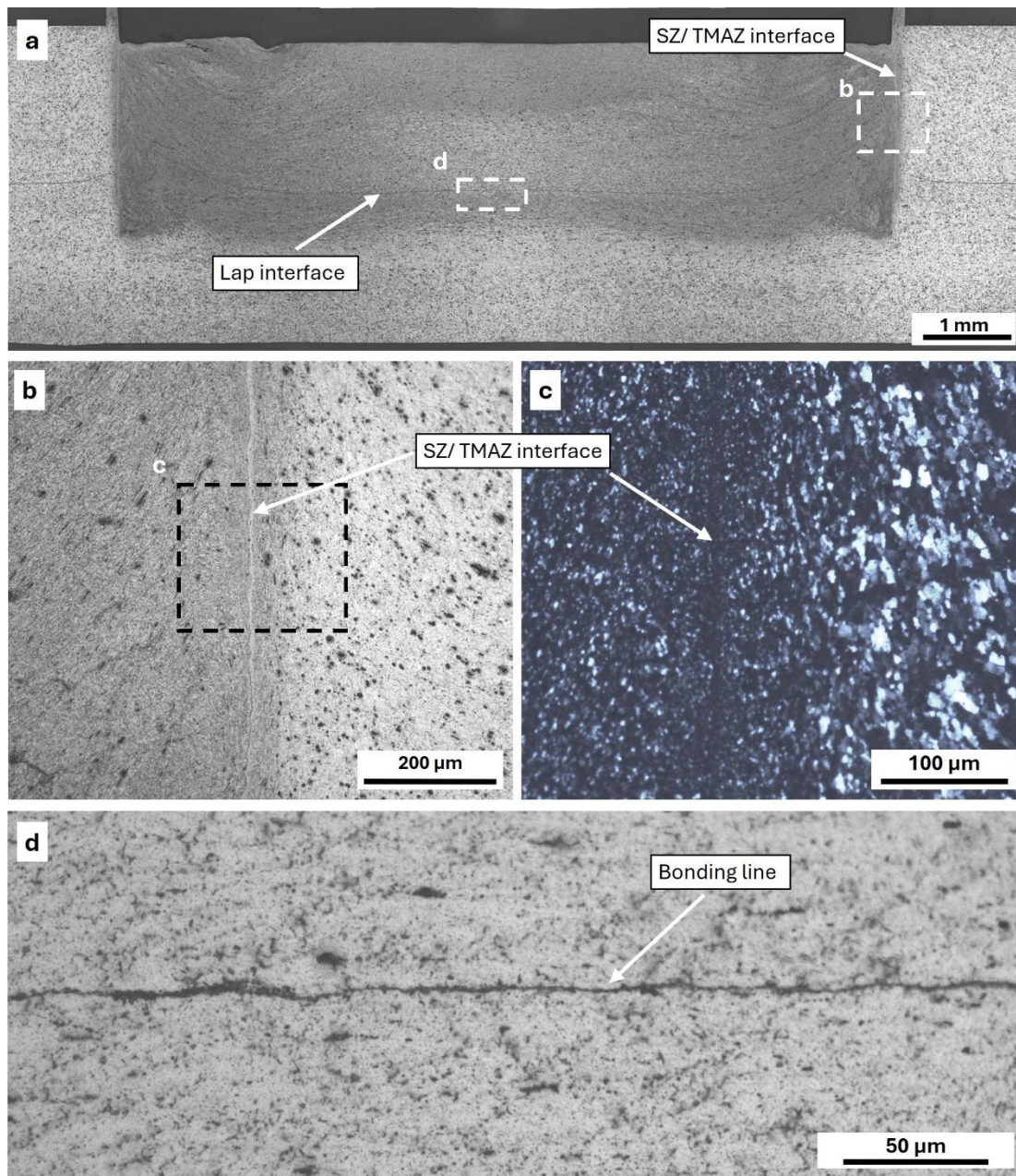


Figure 6-9 Micrographs of weld cross section produced with a conventional shoulder and volume ratio of 0.8 a) macro view of complete cross section, b) SZ/TMAZ interface under brightfield light, c) SZ/TMAZ interface under polarised light, d) bonding line at the lap interface

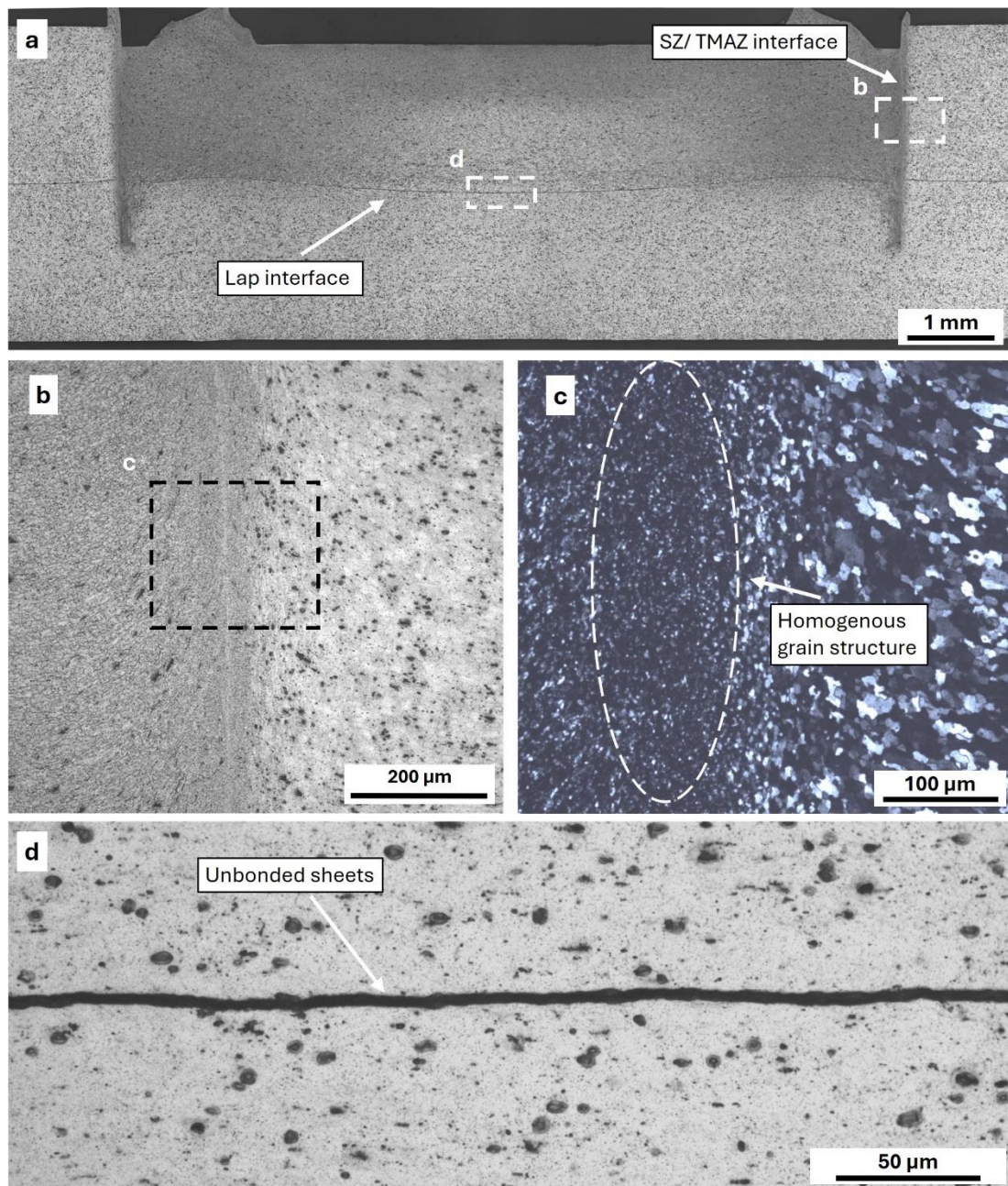


Figure 6-10 Micrographs of weld cross section produced with a 45° chamfered shoulder and volume ratio of 0.8 a) macro view of complete cross section, b) SZ/TMAZ interface under brightfield light, c) SZ/TMAZ interface under polarised light, d) bonding line at the lap interface

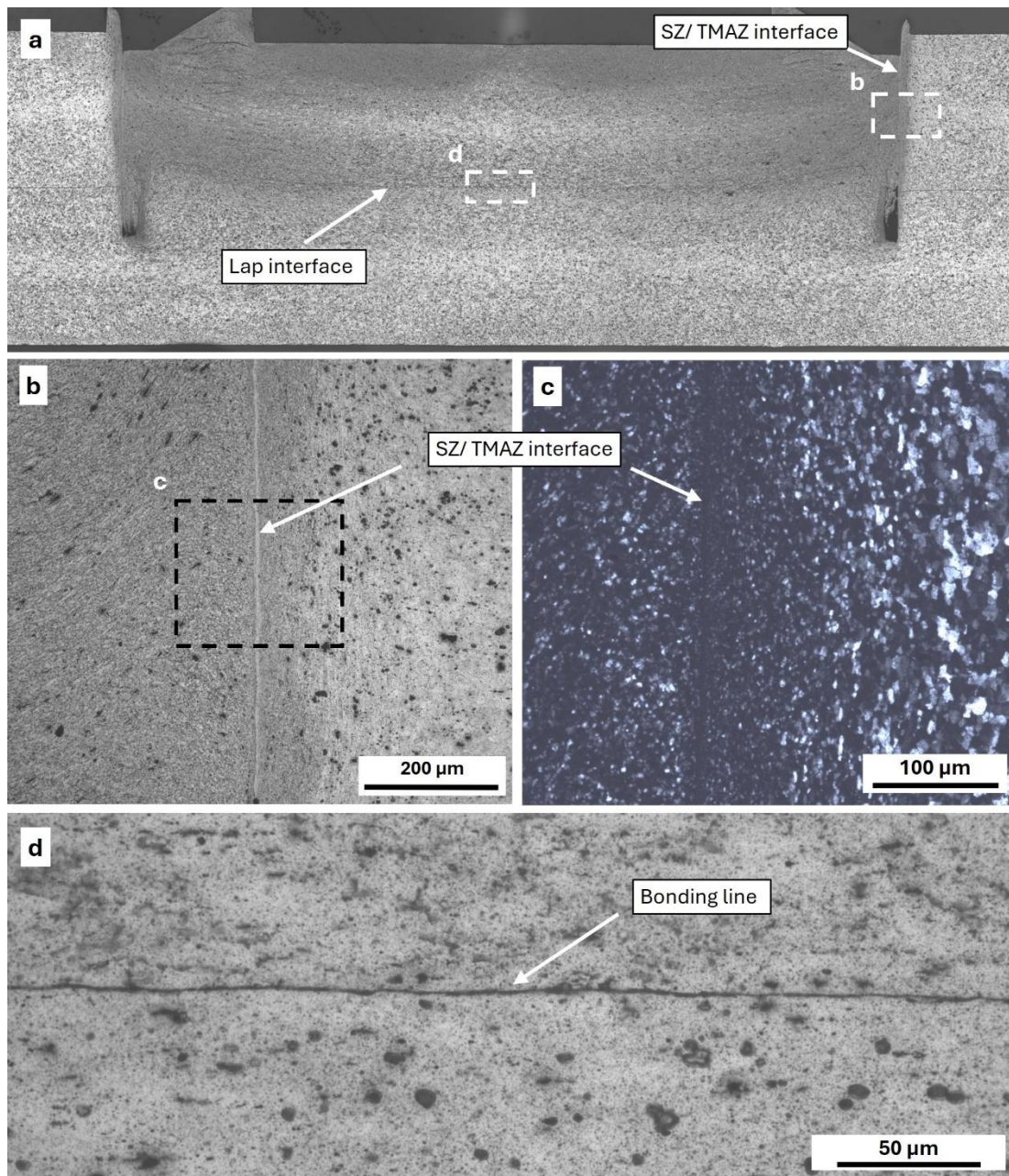


Figure 6-11 Micrographs of weld cross section produced with a 30° chamfered shoulder and volume ratio of 0.8 a) macro view of complete cross section, b) SZ/TMAZ interface under brightfield light, c) SZ/TMAZ interface under polarised light, d) bonding line at the lap interface

6.3.4 Hook defect

From Figure 6-12, it can be seen that both shoulder geometry and volume ratio had an influence on hook height and direction. In the conventional welds, all hooks were deformed in the upwards direction, and hook height increased with volume ratio (Figure 6-12a). The same trend applies to the 45° welds (Figure 6-12b), however in this case a flat hook is observed at a volume ratio of 0.8, which is consistent with the

findings of chapter 4. In contrast, the 30° shoulder produced only flat hooks, regardless of volume ratio (Figure 6-12c).

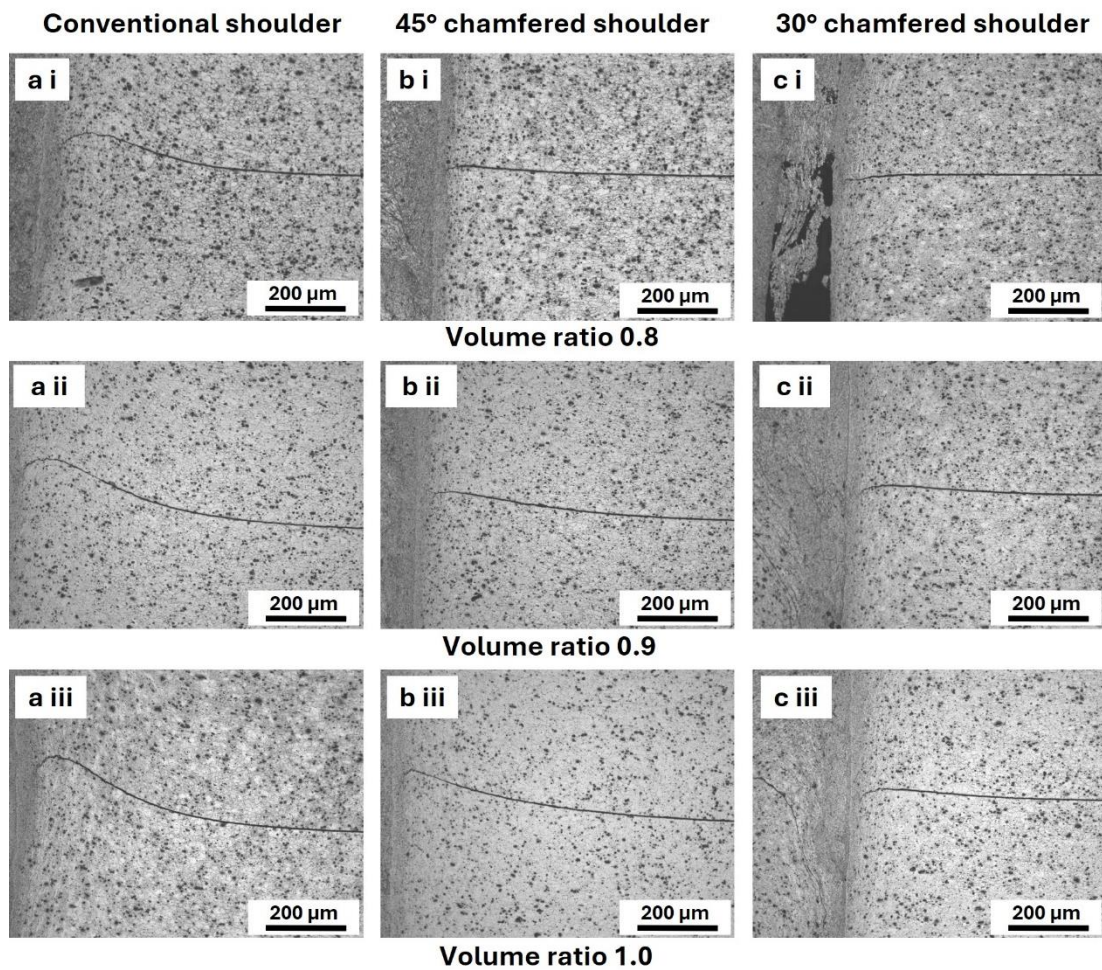


Figure 6-12 Comparison of hook regions in welds produced with each shoulder variant at different volume ratios

For all welds reported in chapters 3 and 4, hooks were either flat or deformed in the downwards direction. The reversal observed in these latest results can be explained by the increase in forging pressure resulting from the additional surface indentation [6.4]. As indicated in Figure 6-13a, material flows outwards from beneath the probe towards the weld periphery. As the flow continues, material is forced upwards, pressing against the periphery and deforming the hook in the direction of flow. This flow pattern is widely corroborated in the literature [6.2,6.10–6.13], yet is not observed in the 30° welds due to the incorrect probe end position and insufficient forging pressure (Figure 6-8).

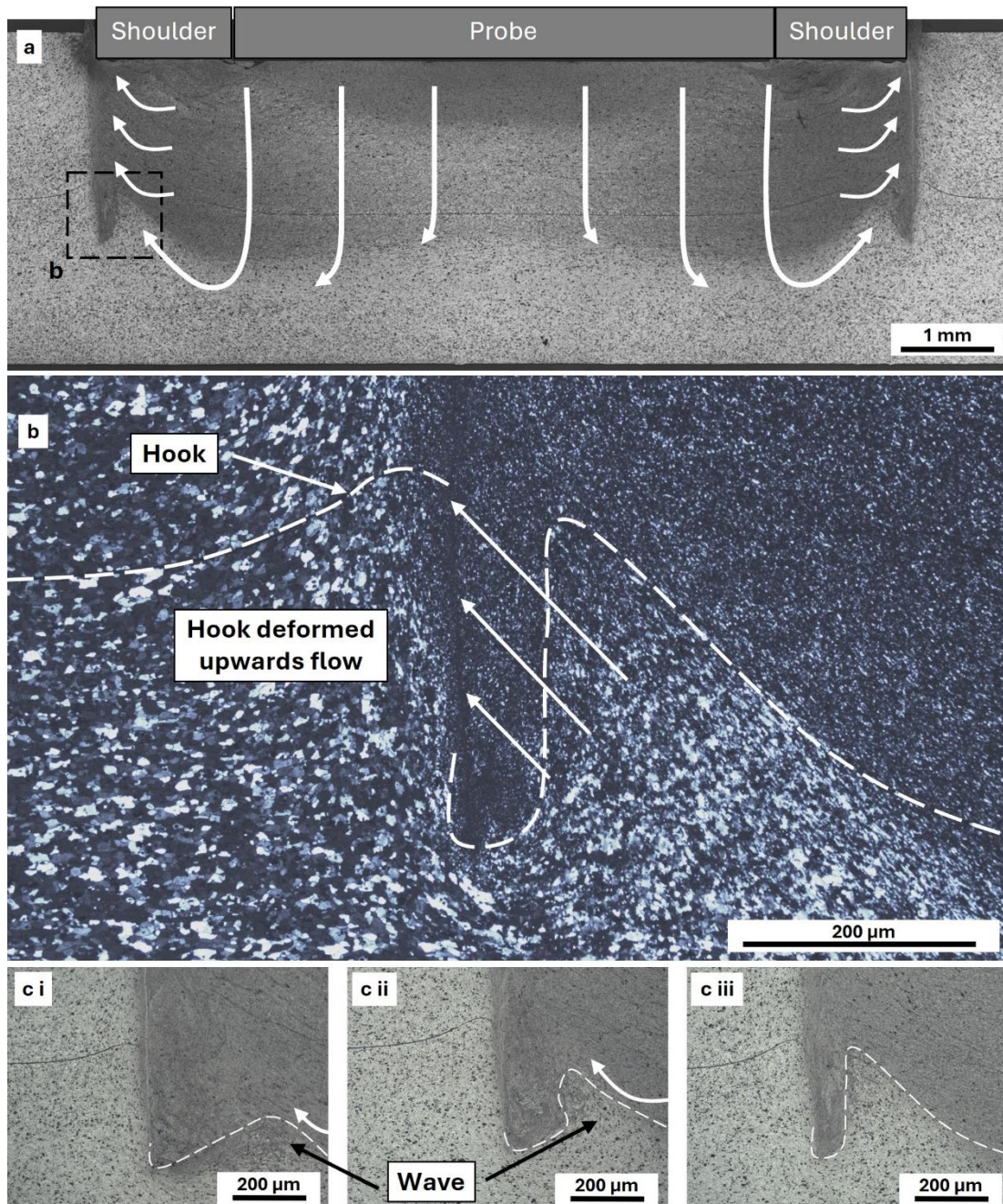


Figure 6-13 Hook formation at different volume ratios using a conventional shoulder a) flow pattern of refilled material at volume ratio 1.0, b) hook region at volume ratio 1.0 under polarised light, etched with Barker's solution, c) hook regions at volume ratio 0.8 (i), 0.9 (ii), 1.0 (iii)

Figure 6-13b presents an etched micrograph of the hook region, revealing a wave-like pattern of coarse grains; this is useful for understanding the influence of volume ratio on hook height. Under brightfield light, the waves can be identified as lighter regions within the SZ, indicated in Figure 6-13c i-iii. At a volume ratio of 0.8 (Figure 6-13c-i), the wave is small and has not progressed significantly towards the periphery. This is

consistent with weaker outward flow as a result of lower forging pressure, and corresponds to the low hook height seen in Figure 6-12a-i. As volume ratio is increased, so too is forging pressure, resulting in a more forceful outward flow of material that causes greater upwards deformation of the hooks. Evidence for this can be seen by the visibly larger wave patterns (Figure 6-13b ii-iii), and corresponding hook heights shown in Figure 6-12a ii-ii. While higher volume ratios were found to be effective at eliminating tunnel defects (section 6.3.3.1), it is understood that the hook acts as a stress concentrator and the correlation between increased hook height and reduced lap-shear strength is widely reported in the literature [6.2,6.12–6.15].

6.3.5 Microhardness

Weld cross sections produced with each shoulder variant and overlaid with microhardness heat maps are presented in Figure 6-14. Changes in volume ratio produced no measurable difference in hardness distribution, so only welds with a volume ratio of 0.8 are discussed.

The chamfered shoulder geometries did not significantly influence hardness distribution compared to the conventional shoulder. For all shoulder variants, hardness reached ≈ 150 HV throughout the SZ and decreased to ≈ 138 HV just outside of the SZ; this is comparable to the bulk material. The hardened region of the 45° weld is more well defined than in the conventional weld and extends evenly up to the SZ/TMAZ interface (Figure 6-14b). These results are typical of AA2024 joints [6.8,6.16,6.17] and are broadly consistent with the findings already discussed in sections 3.3.2 and 4.3.4.

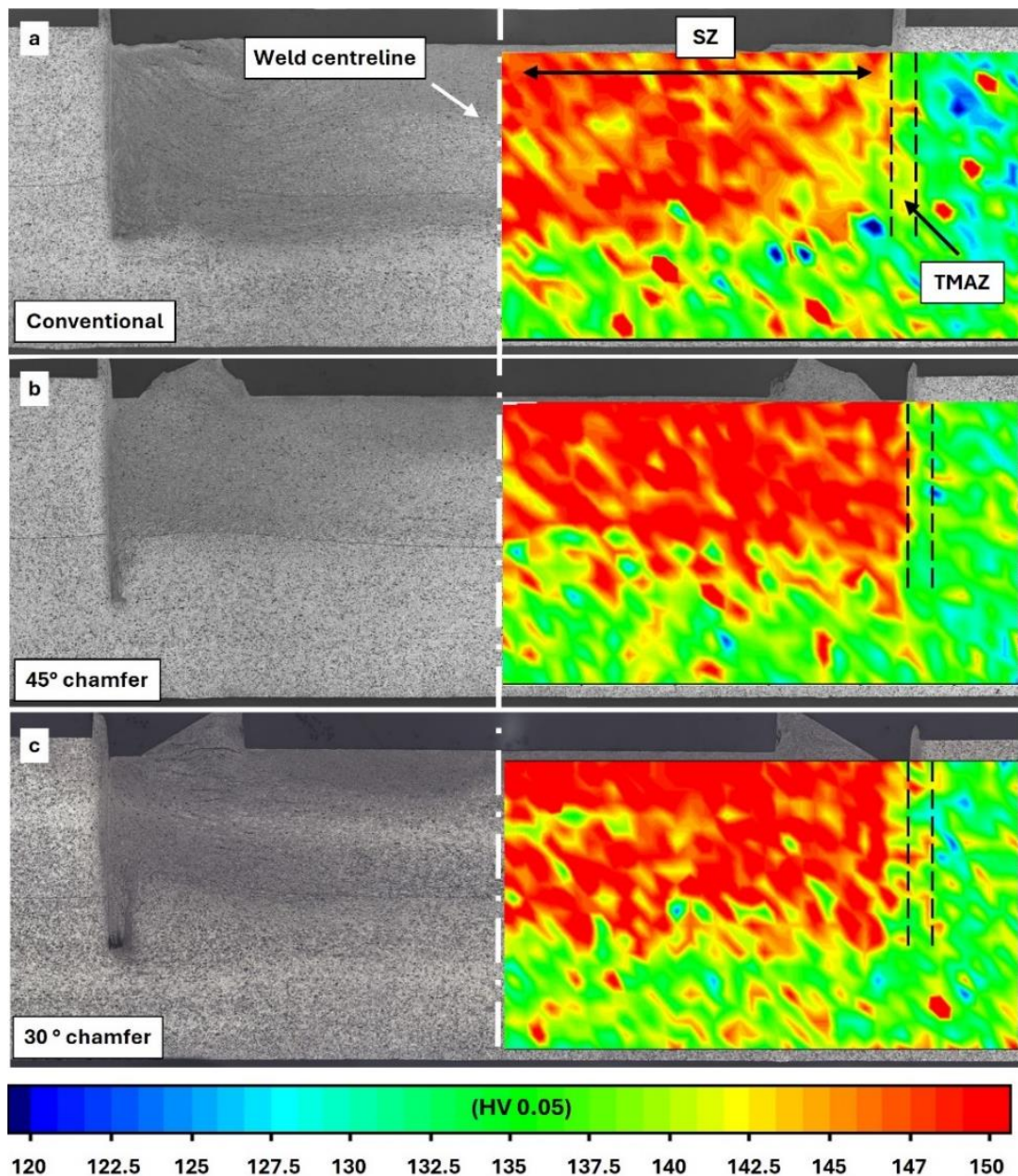


Figure 6-14: Vickers hardness maps of 0.8 volume ratio welds overlaid on cross sectional micrographs a) conventional shoulder, b) 45° shoulder, c) 30° shoulder

6.3.6 Tensile lap-shear testing and failure modes

The results of tensile lap shear testing of welds produced with each shoulder variant and volume ratio are provided in Figure 6-15 and Table 6-2. Each shoulder variant demonstrated a significant improvement in terms of ULSF over the previous generation of tools discussed in chapter 4. To confirm the positive influence of the surface indentation, welds were produced using the conventional shoulder, with and without the indent.

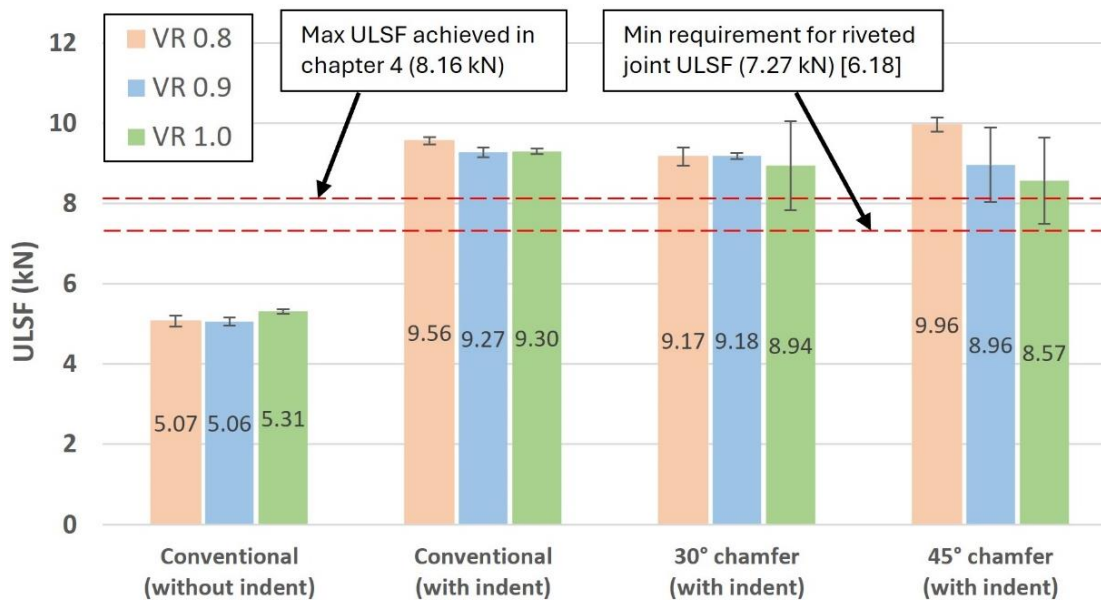


Figure 6-15 Average ULSF of welds produced with various shoulder geometry and volume ratio combinations. Basic welding parameters: 1.5 s welding time, 2000 rpm rotational speed, 2.25 mm plunge depth (error bars denote standard deviation, n = 3)

A maximum ULSF of 5.3 kN was achieved for the un-indented welds; the addition of the indent increased this by 80% to 9.56 kN. This can be attributed to the improved bonding at the SZ/TMAZ interface which is confirmed by the change of failure mode from plug pull-out (PPO) to 45° shear through the weld (TWS). For a detailed description of the different failure modes and how they relate to weld quality, the reader can refer back to chapter 3.

The highest ULSF of 9.96 kN was achieved using the 45° shoulder, representing a 4.2% increase over the conventional shoulder. This can be attributed to a combination of improved bonding at the SZ/TMAZ interface and a lower hook height than the conventional welds. The performance of the 30° welds was close to that of the conventional welds, which is consistent with similarities in microstructure discussed in section 6.3.3.2.

Volume ratio did not obviously influence lap shear performance, with ULSF either remaining constant or decreasing as volume ratio was increased. This is due to the increased hook height observed at higher volume ratios, which has been widely reported as detrimental to weld strength [6.2,6.12–6.15].

Table 6-2 Tabulated results of tensile lap-shear testing and failure modes

Shoulder design	I.D	Volume ratio	ULSF (kN)	Standard deviation (kN)	Sample failure mode		
					1	2	3
Conventional (without indentation)	Pa	0.8	5.07	0.13	PPO	PPO	PPO
	Pb	0.9	5.06	0.10	PPO	PPO	PPO
	Pc	1.0	5.31	0.06	PPO	PPO	PPO
Conventional (with indentation)	P1	0.8	9.56	0.09	TWS	TWS	TWS
	P2	0.9	9.27	0.13	TWS	TWS	TWS
	P3	1.0	9.30	0.07	TWS	TWS	TWS
30° chamfer	P4	0.8	9.17	0.22	TLI	TLI	TLI
	P5	0.9	9.18	0.08	TLI	TLI	TLI
	P6	1.0	8.94	1.10	TLI	TLI	TLI
45° chamfer	P7	0.8	9.96	0.17	TLI	TLI	TLI
	P8	0.9	8.96	0.93	TLI	TLI	TLI
	P9	1.0	8.57	1.08	TLI	TLI	TLI

PPO, TWS, and through the lap interface (TLI) failure modes were observed during tensile lap shear testing, of which a detailed description can be found in sections 3.3.2 and 4.3.4. Characteristic force-displacement curves for each failure mode are presented in Figure 6-16.

PPO occurred only in conventional welds produced without indentations; this was the result of poor bonding at the SZ/TMAZ interface (see Figure 6-3b). This is further evidenced by the jagged force-displacement curve (Figure 6-16) generated as the crack propagated through the volumetric defects (Figure 6-3b).

All conventional welds with indentations experienced TWS failure at high ULSF. This proves that the lap interface was not a weak point of these welds and confirms the bonding line visible in Figure 6-9 as a diffusion bond, as agreed by Li et al. [6.8] and Wang et al. [6.9]. Figure 6-17a shows a sectioned TWS failure of a conventional weld (P1). The initial crack opened at the hook tip of the tensile face (Figure 6-17c) and travelled up through the SZ/TMAZ interface and around the weld periphery. This can be identified in the force-displacement curve (Figure 6-16) by the plateau in force (initial crack), followed by an erratic decline as the crack advances around the weld

periphery prior to the secondary shear through the SZ. A small tunnel defect can be identified in Figure 6-17b, although this did not interfere with the propagation path of the secondary crack.

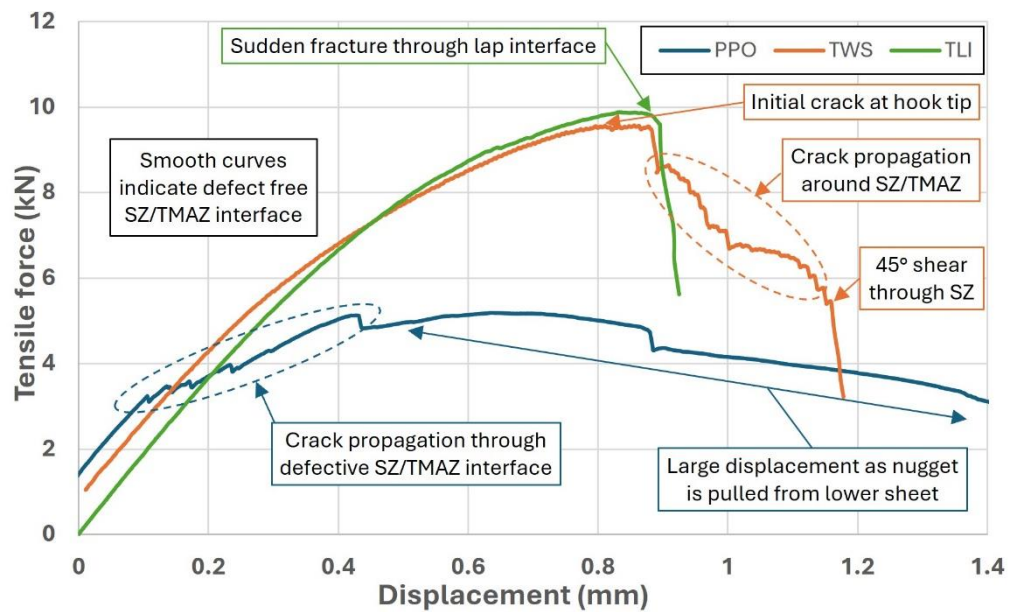


Figure 6-16 Characteristic force-displacement curves for each failure mode. Data acquired from samples Pa-1 (PPO), P1-1 (TWS), and P7-1 (TLI)

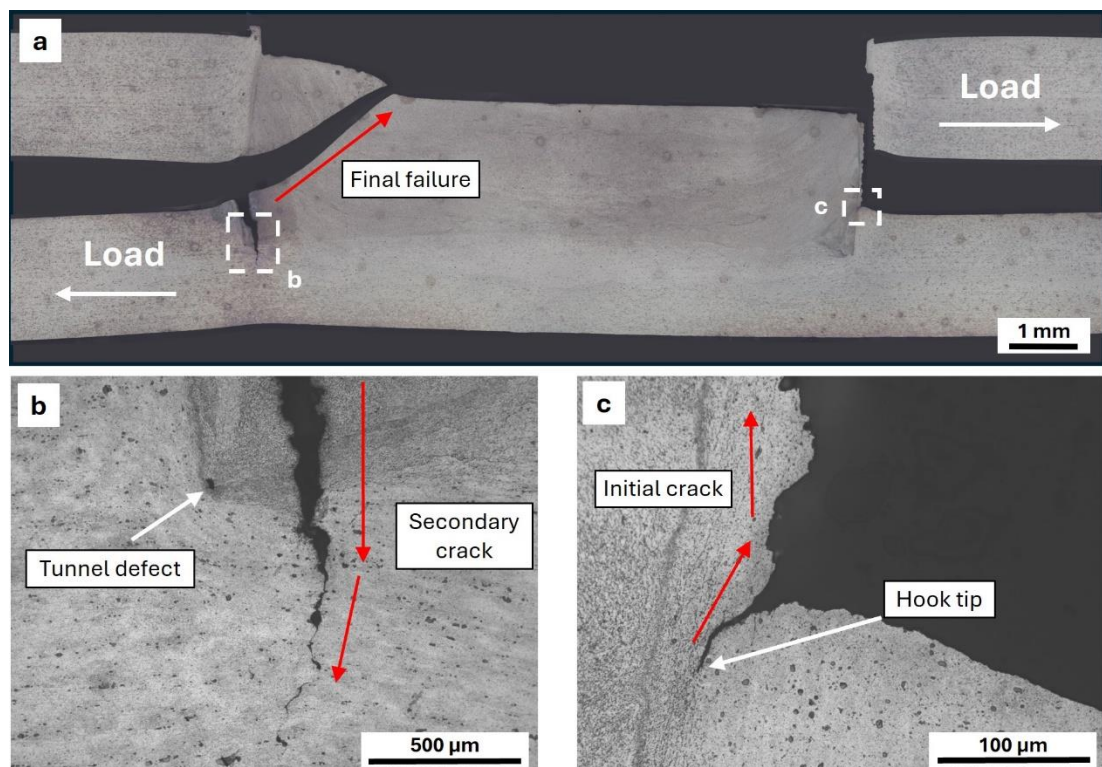


Figure 6-17 TWS failure of a conventional weld of parameter set P1 a) cross section of failed weld, b) tunnel defect region with secondary crack, c) initial crack site at tensile hook tip

All welds produced using chamfered shoulders failed in the TLI mode (Figure 6-18a), regardless of chamfer angle or volume ratio. This confirms that the lap interface remains the primary weakness of these welds, despite the addition of the indentation step and variations in volume ratio. As in TWS failure, the initial crack opened at the tensile hook tip and propagated up through the SZ/TMAZ interface (Figure 6-18c), while a secondary crack advanced through the bottom sheet (Figure 6-18b). As the force-displacement curve confirms (Figure 6-16), the final TLI failure occurred much more suddenly, and with little warning compared to PPO and TWS failure modes.

Despite the lack of bonding at the lap interface, TLI failure occurred in the 45° welds at higher loads than TWS failure occurred in the conventional welds. From Figure 6-18c, it can be seen that the initial crack had advanced only halfway up the SZ/TMAZ interface at the point of final failure through the lap interface. This provides conclusive evidence that the 45° shoulder improved the strength of the SZ/TMAZ interface; further parameter optimisation to improve bonding at the lap interface would likely yield even higher ULSEF.

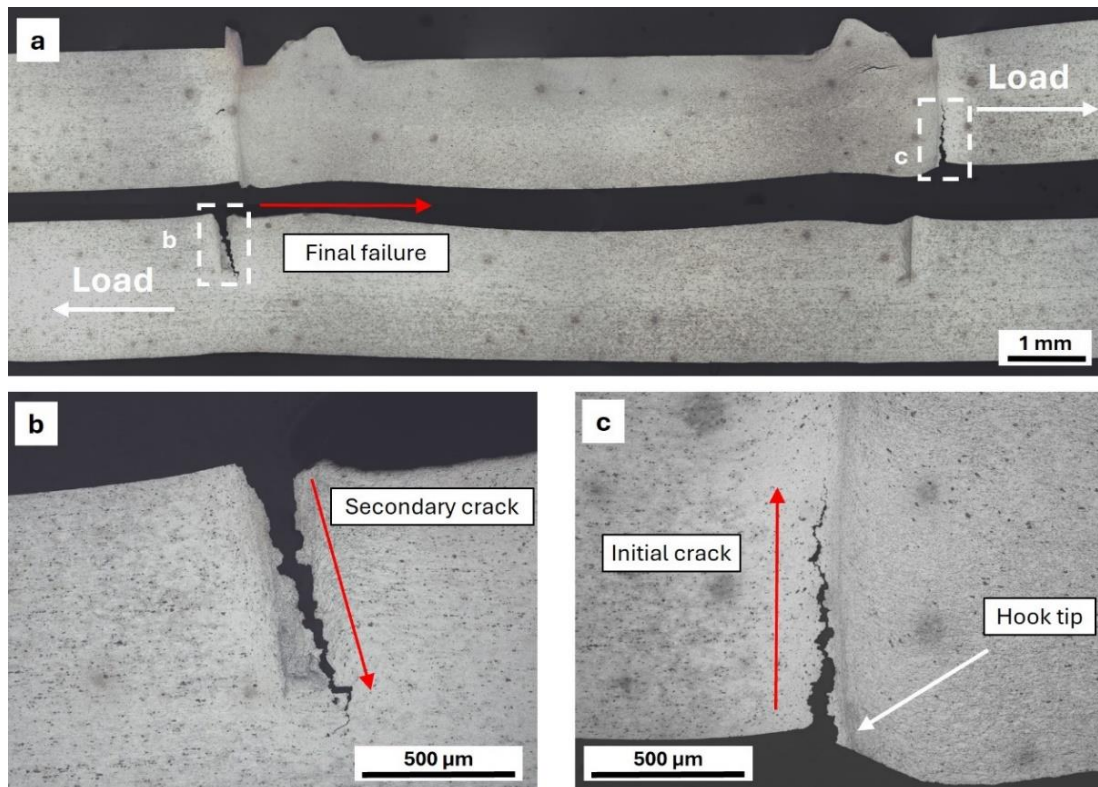


Figure 6-18 Tensile lap-shear failure mode of shear failure through the lap-interface (TLI) a) cross section of failed weld, b) secondary crack through bottom sheet c) initial crack at tensile hook tip

6.4 Conclusions

This investigation has built upon the outputs of chapter 4 by further exploring the chamfered shoulder concept in combination with modified volume ratios first simulated in chapter 5. All welds were produced using a welding time of 1.5 s, 46% faster than any previously reported weld of AA2024-T3. Defect free welds were achieved with a conventional shoulder by implementing a surface indentation technique in conjunction with a volume ratio of ≥ 0.9 . The 45° shoulder demonstrated the highest ULSF of 9.96 kN, 4% higher than the conventional shoulder (9.56 kN) and 22% higher than the results obtained in chapter 4. The following key conclusions can be drawn from this work:

1. High strength, defect free welds of AA2024-T3 were produced at a very short welding time of 1.5 s.
2. Modifying the end position of the probe and shoulder to 0.2 mm below the original sheet's level compensates for material lost between tool gaps. This led to an 80% increase in ULSF and the elimination of all volumetric defects along the SZ/TMAZ interface.
3. It has been proven that chamfer angle does influence the pressure distribution based on the diffusion bonding seen in the 30° welds, but not the 45° welds.
4. Increasing volume ratio is an effective way of eliminating tunnel defects, however, doing so also leads to larger hook height that impair ULSF.
5. The 45° shoulder increased the strength of the SZ/TMAZ interface, leading to a 4% increase in ULSF. If bonding at the lap interface can be improved through further parameter optimisation, the lap shear strength can likely be increased further.

6.5 References

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Chapter 7. Conclusions and future work

7.1 Conclusions

The research presented in this thesis has sought to address a critical barrier that hinders the industrial adoption of RFSSW, namely the long welding time needed to join high strength aluminium alloys. Generally reported within the range of 3 s – 6 s, welding time for RFSSW is significantly longer than the 1 s – 2 s required by the competitor process, resistance spot welding. An open question remains, therefore, as to the industrial viability of RFSSW in high production rate environments. In the current work, an experimental investigation was conducted to identify and understand the influence of reducing welding time on weld microstructure and mechanical properties. A series of additional experimental and numerical studies were carried out to methodically address the issues that emerged.

The alloy used throughout this work was AA2024-T3, a high strength aluminium alloy commonly used within the aerospace industry. No prior RFSSW research of this alloy has addressed welding times shorter than 2.8 s. Even at these moderate durations, researchers rarely report in detail on welding defects and microstructure, meaning little is known about weld quality under these conditions. The initial investigation within the current work aimed to reduce welding time to the minimum value achievable by the available welding machine (Harmes and Wende RPS100), which was found to be 0.75 s. Before any modifications to the welding tools were made, a range of welding times between 3 s and 0.75 s were explored in combination with various rotational speeds. It was found that, as welding time reduced and less frictional heat was generated, the flowability of the workpiece was impeded and substantial defects began to form within the weld, principally the widely reported tunnel defect, and weak bonding/voids along the SZ/TMAZ interface. While ULSF was negatively impacted, dropping from 9.21 kN at 3 s welding time to 6.37 kN at 0.75 s welding time, all joints exceeded the strength requirements of comparable resistance spot welds and showed promise for further investigation.

The weak bonding and voids identified during the short duration weld investigation highlighted the need to improve material flow to the periphery of the weld during the refill stage. Two novel shoulder designs were developed for this objective: one with 0.5 mm deep grooves on the contact face, and another with a 45° chamfer on the inside rim. While the grooved shoulder provided no significant advantage over the conventional shoulder in terms of defect evolution or mechanical performance, the chamfered shoulder led to a number of significant findings. By providing a more direct path for material to flow towards the weld periphery during refill, the chamfered shoulder redistributed the pressure applied by the probe, from the lap interface to the SZ/TMAZ interface. This resulted in a significant improvement in bonding at the SZ/TMAZ interface at all welding times, and the complete elimination of volumetric defects in this region. An increase in ULSF of 17% and 27% were observed at welding times of 0.75 s and 1.5 s respectively, compared with the conventional shoulder. Additionally, ULSF was unaffected by the reduction in welding time from 1.5 s to 0.75 s, meaning the chamfered design provides a degree of insensitivity to changes in process parameters. However, the chamfered shoulder also increased the size of the tunnel defect and led to poor bonding at the lap interface due to the loss of forging pressure in this region. The large tunnel defects provided a short path for crack propagation from the hook tip towards the lower weld surface, leading to premature failure in a rarely reported fracture mode of 45° shear through the lower sheet.

To better understand the formation mechanism of the tunnel defect, a fully coupled thermomechanical model of the entire RFSSW process was developed to visualise the material flow in three dimensions; Abaqus Explicit and the coupled Eulerian-Lagrangian technique were adopted for the task. When compared with experimentally generated thermocouple data, the model accurately predicted the temperature trend and peak at the edge of the SZ throughout the plunge stage under different welding conditions. Areas of predicted high and low plastic strain were found to correlate well with the level of grain refinement in experimental weld microstructures, and similar patterns of material flow could be observed in both simulated and experimental welds. The tunnel defects were also captured in sufficient

resolution and the model accurately predicted an increase in tunnel size as welding time was reduced; this relationship was also observed in the experimental results. By visualising the flow of material in the simulation, it was concluded that the tunnel defect forms approximately halfway through the refill stage, as a result of a low volume ratio. If the shoulder retracts too far before the probe begins to refill material, a gap is left beneath the shoulder. By the point at which the refill begins and material flows into this gap, the shoulder has retracted too far to apply meaningful forging pressure. The material is allowed to fold upon itself, creating the tunnel defect. By adjusting the volume ratio from 0.8 to 1.0, the gap beneath the shoulder was avoided and the model predicted a complete elimination of the tunnel.

Building on the promise of the chamfered shoulder and the knowledge delivered by the numerical model, a further experimental study explored the influence of tool path (i.e. end position and volume ratio) on the microstructural and mechanical properties of conventional and chamfered shoulder welds. The most significant improvement was achieved through implementing an indent on the weld surface by programming the end position of the shoulder and probe as 0.2 mm below the surface of the top sheet. This compensated for material lost through gaps in the tooling and applied additional forging pressure to the weld. Using a conventional shoulder and a welding time of 1.5 s, all volumetric defects along the SZ/TMAZ interface were eliminated, although tunnel defects were still present. ULSF was increased by 80% to 9.56 kN, compared to reference samples without indents. This was increased further to 9.96 kN using a 45° chamfered tool due to strengthening of the SZ/TMAZ interface.

Finally, as predicted by the numerical model, it was proven through experimentation that the volume ratio can be optimised to avoid the formation of tunnel defects. With a volume ratio of 0.8, tunnels were found in welds produced with all shoulder variants. However, it was concluded that increasing the ratio to 1.0 was effective at entirely eliminating the tunnel defect from forming in welds produced with all shoulder variants. These findings constitute the first examples of high strength, defect free RFSSW joints of AA2024-T3 aluminium at welding times comparable with existing mass production joining technologies.

7.2 Recommendations for future work

Although the current work has made important progress by exploring the relationship between RFSSW tool geometry, process parameters, weld microstructure, and mechanical joint performance, a number of research areas remain to be addressed.

7.2.1 Fatigue assessment

While the results presented in this thesis provide strong evidence of high-quality welds, the primary evaluation method was static lap shear testing. In an industrial setting, particularly when considering safety/mission critical aerospace applications, fatigue performance must be conclusively demonstrated and understood. Research into the fatigue life of short duration welds, especially comparing the relative performance of conventional and chamfered shoulders, would be of significant scientific and industrial relevance. The conclusions made regarding the influence of the chamfer on hook geometry would be of particular interest in this matter, as it is well understood that large hooks act as stress concentrators in RFSSW joints. As the chamfer generally produced flat hooks, it is envisioned that fatigue life will be positively affected.

7.2.2 Dissimilar alloys and metals

A common occurrence in industrial applications is the need to create dissimilar joints, either between different alloys of the same metal, or different metals entirely. This is a key advantage of RFSSW. Aircraft structures, for example, commonly use rivets to join 2000 series aluminium fuselage skins to 7000 series stiffeners. The automotive industry is aware of RFSSW for its potential to join aluminium to copper for electric vehicle battery applications. Connecting light aluminium components to steel frames is another automotive application to which RFSSW is well suited, yet it is addressed in almost no scientific literature. Any of these applications' present opportunities for meaningful research contributions.

Appendix A. Dissemination activities

A 1. Journal publications

1. J. Draper, S. Fritsche, S. de Traglia Amancio-Filho, A. Galloway, and A. Toumpis, "Exploring a novel chamfered tool design for short duration refill friction stir spot welds of high strength aluminium," *The International Journal of Advanced Manufacturing Technology*, vol. 131, no. 12, pp. 5867–5879, Apr. 2024, doi: 10.1007/s00170-024-13336-x.
2. J. Draper, S. Fritsche, A. Garrick, S. T. Amancio-Filho, A. Toumpis, and A. Galloway, "Exploring the boundaries of refill friction stir spot welding: influence of short welding times on joint performance," *Welding in the World*, 2024, doi: 10.1007/s40194-024-01721-9.
3. S. Fritsche, J. Draper, A. Toumpis, A. Galloway, and S. T. Amancio-Filho, "Refill friction stir spot welding of AlSi10Mg alloy produced by laser powder bed fusion to wrought AA7075-T6 alloy," *Manufacturing Letters*, vol. 34, pp. 78–81, Oct. 2022, doi: 10.1016/j.mfglet.2022.09.010.

A 2. Conference presentations

1. J. Draper, S. Fritsche, S. de Traglia Amancio-Filho, A. Galloway, and A. Toumpis "Numerical modelling as a predictive tool to reduce defects in refill friction stir spot welded joints," 77th IIW Annual Assembly and International Conference on Welding and Joining, IIW2024, Rhodes, Greece, 7-12 July 2024.