

The Influence of Electrode Force on Liquid Metal Embrittlement of Third Generation Advanced High Strength Steels during Resistance Spot Welding

by

Shiyuan Song

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Author's Declaration

This thesis consists of materials all of which I authored or co-authored: see Statement of Contributions included in the thesis. This is a true copy of the thesis, including any required final versions, as accepted by my examiners.

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Statement of Contributions

This thesis was solely written by the candidate. Professor E. Biro contributed to editing the entire thesis. Additional contributions were made by other members of the Centre for Advanced Material Joining group from the University of Waterloo. The specific contributions to this work are listed as follows:

The work related to the influence of electrode force on LME of high-LME sensitive material in Chapter 4 of this thesis is based on a journal paper that has been submitted to *Journal of Materials Research and Technology*:

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The experimental design, work, analysis, and writing were conducted solely by the candidate. Mr. M. Shojaee contributed to technical discussion, formatting abstract and assisting in schematic drawings. Dr. A. R. H. Midawi contributed to technical discussion. Dr. O. Sherepenko contributed to technical discussion, formatting data, editing the paper and training on equipment and software used for this work. Dr. H. Ghassemi-Armaki (General Motors R&D) and Prof. E. Biro participated in technical discussions and editing the manuscript.

Section 4.3 of this thesis: Statistical Analysis of Factors Influencing LME Cracking Behavior of Low-LME Sensitive Material was adapted from the work done for the course Statistical Analysis and Data Visualization (ME 738i), which the candidate participated during Fall 2022. The course report was done as joint assignment between the candidate and Mr. S. F. Al Hussain. The experimental work, design, data collection, the development of original statistical analysis and write-up of the draft report were solely done by the candidate. Mr. S. F. Al Hussain contributed to the development of optimized predicting models, technical discussion, report formatting and editing. Prof. E. Biro participated in technical discussions and editing the report.

Abstract

Zinc coatings are generally applied on the surface of advanced high-strength steels (AHSS) for corrosion resistance. However, the elevated temperature during resistance spot welding process melts zinc coating and the intact contact of liquid zinc on the underlying steel substrate with the presence of tensile stress may induce liquid metal embrittlement (LME) cracking, potentially degrade the weld strength. It is evident that the alteration of welding parameters, one of which being electrode force, affects the LME response of steels. However, the mechanisms of how electrode force influences LME cracking is not fully understood. By assessing how electrode force variation affects LME cracking severity for welds in two newly designed 3G-AHSS, with low-LME and high-LME sensitivity, this work illustrated that electrode force had a two-fold effect on LME development. When welds did not exhibit expulsion, the increase of electrode force promoted heat extraction from the joint, which in-turn reduced LME cracking severity. However, when welds experienced expulsion, the increase of electrode force, accentuated electrode collapse, thus increased rapid cooling on the weld shoulder with its associated thermal stresses, increased LME cracking severity.

The welding work was performed on three heat input levels to relate the electrode force and the LME cracking severity experimentally. At each heat input level, the welding current was adjusted to compensate the influence of electrode force variation on the nugget formation and growth. Therefore, comparable nugget diameter (a measure of weld strength) was achieved regardless of various electrode force at each heat input level. The LME cracking severity of the welds made in low-LME and high-LME sensitive materials was quantified by the number of cracks, the absolute maximum crack length and the potential weld strength loss using crack index. The LME cracks were classified according to their locations on the weld where Type A cracks located at the center of the weld surface; Type B cracks located at the weld shoulder extending to the edge of the heat affected zone; Type C cracks located at the weld notch.

For both low and high LME sensitive materials, Type B cracks were observed at all levels of heat input while Type C cracks were not, and Type A cracks were only observed at moderate and high heat inputs. Welds made in the low-LME sensitive material displayed an overall less susceptibility to LME cracking (crack index below 0.15). For the high-LME sensitive material, at low heat input, the LME severity decreased from a total crack index of 0.14 to 0 when the electrode force was increased from 4.4 kN to 5.4 kN. At moderate heat input, the LME severity decreased from a total crack index of 0.67 to 0.11 when the electrode force was increased from 4.4 kN to 4.9 kN, then with a further increase of electrode force to 5.4 kN, the Total crack index increased to 0.37. At high heat input, the increase of electrode force from 4.4 kN to 5.4 kN resulted in the increase of LME cracking severity on Total crack index from 0.36 to 0.77. The observation that the increase of electrode force could result in both increased and decreased LME severity, depending on situation, contrasted with the published literature where it was seen that LME cracking

severity universally decreased with increasing electrode force. To understand the mechanisms resulting in different LME response seen in welds made in the two investigated materials and why the present results diverged from the literature, experimental analysis was first conducted on results from the tests on the low-LME sensitive material. From this analysis, it was seen that increasing electrode force promoted heat loss and the expulsion event increased with the increase of applied welding heat. However, there was not a linear relationship observed between the change of heat input, heat loss, expulsion condition and the LME cracking severity that related to variations in electrode force for welds made in the low-LME sensitive material. With the assistance of ANOVA analysis and linear regression modelling, it was seen that whether the weld exhibited expulsion had a significant effect on its LME behaviour. The results indicated that analysing both LME-free (20 out of 45 welds made) and LME-containing welds in a single dataset led to inaccurate prediction of LME crack length when LME cracking was modeled as a function of heat input, heat loss, and expulsion count. The majority of LME-free cracks were found at low heat input and the probability of these welds experiencing expulsion was low. In welds made in the low-LME sensitive material that contained LME, the LME cracking severity of LME-cracking welds was significantly influenced by the heat input, heat loss and the occurrence of expulsion from the statistical analysis. Conversely to the LME-free welds, the welds exhibiting LME cracking predominately experienced expulsion during welding.

Welds made in the high-LME sensitive material exhibited much more LME cracking than seen in the low-LME sensitive material. From all on the welds made, there was only 5 LME-free welds out of 45 welds produced. The tested factors (i.e. heat input, heat loss and expulsion condition), which had a significant impact on LME cracking behavior on low-LME sensitive material, were then experimentally correlated with the LME cracking severity for the high-LME sensitive material. The results indicated that the influence of electrode force on LME cracking depended on whether or not welds experienced expulsion. When welding with low heat input, without expulsion, LME cracking severity decreased as electrode force increased. In such cases, the increase of electrode force aided with heat extraction during welding, relieved the critical stresses required by LME cracking. In contrast, when welding with high heat input, resulting in expulsion, the increase of electrode force elevated LME cracking. It was shown that high electrode force increased the sudden indentation of the electrode into the steel substrate (electrode collapse), leading to rapid cooling of the weld shoulder. The rapid cooling increased the thermal stresses associated with the electrode collapse event, promoting LME. The results from this study show that expulsion itself (excluding its association with increased heat input) is a factor contributing to LME cracking, which highlights the importance of considering the expulsion phenomenon in designing LME resistant welding schedules.

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List of Abbreviations

AHSS	Advanced high strength steels
1G-AHSS	First generation AHSS
2G-AHSS	Second generation AHSS
3G-AHSS	Third generation AHSS
GI	Galvanized
GA	Galvannealed
EG	Electro-plating
RSW	Resistance spot welding
LME	Liquid metal embrittlement
UTS	Ultimate tensile strength
YS	Yield strength
EI	Elongation
CEY	Carbon equivalent
MFDC	Medium-frequency direct current
Type A cracks	LME cracks locate at the weld indent
Type B cracks	LME cracks locate at the weld shoulder extending to the edge of the heat affected zone
Type C cracks	LME cracks locate at the weld notch
CI	Crack index
CI T or Total CI	Crack index counting all Type A, B and C cracks
CI A	Crack index counting only Type A cracks
CI B	Crack index counting only Type B cracks
CI C	Crack index counting only Type C cracks

List of Symbols

$I_{\max}$	Expulsion current
$I_{\max}-10\%$	Low heat input (Weld nugget diameter equivalent to the baseline condition with ten percent below expulsion current)
$I_{\max}-200\text{ A}$	Moderate heat input (Weld nugget diameter equivalent to the baseline condition with expulsion current)
$I_{\max}+10\%$	High heat input (Weld nugget diameter equivalent to the baseline condition with ten percent above expulsion current)
n	Number of cracks per weld
L	Lognormal median crack length
t_s	Sheet thickness
dt	Change of welding time
t_0	Start time of welding cycle
t_1	Finish time of welding cycle
I	Welding current
R	Electrical resistance
Q_w	Total heat input during the RSW process
Q_l	Heat loss (Heat extraction)
Q_m	Heat required to melt the material which ultimately solidifies to a weld nugget
m	Nugget mass
T	Temperature
dT	Change of temperature
T_m	Melting temperature
RT	Room temperature
C_p	Specific heat capacity of steel
u	Nugget volume
d	Nugget diameter
h	Nugget height
ρ	Steel density
$F_{\text{electrode}}$	Electrode force
P	Ferrostatic pressure

LB	Before expulsion with low electrode force
LA	After expulsion with low electrode force
HB	Before expulsion with high electrode force
HA	After expulsion with high electrode force

Chapter 1 Introduction

1.1 Background

Safety issues caused by liquid metal embrittlement (LME) cracking have been reported in aerospace, automotive, chemical, power generation, oil and gas industries [1]. The severe scenarios of LME triggered mechanical failure leading to accidents causing hospitalization and death. For instance, a massive explosion, caused by a LME ruptured pipeline, resulted in twenty-eight death and thirty-six injured, in Flixborough, England in 1975 [2]. LME cracking can be consider as a brittle fracture in a solid ductile metal that occurs when the metal is exposed to a liquid metal embrittler under tensile stress [3]. Resistance spot welding (RSW) is a common welding technology used to assembly body-in-white structure. During RSW process, the zinc coated automotive steels may be subject to LME cracking, deteriorating the mechanical performance of the welded joint [4]. Global efforts have been made to understand the LME phenomenon and develop strategies for LME mitigation with the respect to the metallurgical and mechanical aspects. Alteration of welding parameters affects LME cracking by modifying zinc coating or influencing on temperature and stress profiles on the welding zone during the RSW process [1]. Electrode force is an important welding parameter, which clamps the work pieces and applies cooling during the welding process. However, its effect on LME cracking is still ambiguous, especially when expulsion occurs [5–10]. To consider the frequency of the expulsion in automotive assembly and the importance of welding parameters on LME mitigation, ongoing research is necessary.

1.2 Objectives

This thesis aims to understand the influence of electrode force on LME cracking severity during the RSW of AHSS and provides insights into impacts of expulsion on LME cracking. The specific objectives of this work are as follows:

1. Investigate the LME cracking severity of two investigated AHSS while varying electrode force in the RSW environment.
2. Explore the factors influenced by electrode force variation and correlate them to the LME cracking severity through both experimental and statistical analysis.
3. Understand the correlation between the expulsion and the LME cracking.

1.3 Criteria and Constraints

The welding parameters used in this study based on the American Welding Society (AWS) standard for resistance spot welding of automotive steels (AWS D8.9M) [11]. Details on modified welding parameters used to understand the influence of electrode force on LME cracking with compensation of nugget

diameter at each applied heat input are described in Chapter 3 of the thesis. The applied LME characterization methods, equations for calculation, statistical techniques in this work are detailed in Chapter 3. This thesis work is limited to two newly designed third generation of AHSS due to material availability and interest from the industry.

1.4 Thesis Outline

This thesis has been organized into five chapters as follows:

Chapter 1: Introduction – Introduces the background, objectives, constraints and organizations of this thesis.

Chapter 2: Literature Review – Provides an overview of concepts and preceding literature related to this thesis work including AHSS, RSW and LME. It also indicates the research gap that this thesis work intends to fill.

Chapter 3: Methodology – Details the materials used, RSW equipment and parameters, experimental procedures, equations used for calculations, LME characterization methods and statistical techniques applied in this study.

Chapter 4: Influence of Electrode Force on LME during RSW of 3G-AHSS Steels – Details investigations into the effect of electrode force on LME cracking severity of the two testing materials. The change of processing factors resulting from electrode force variation are experimentally and statistically correlated with the LME cracking severity.

Chapter 5: Conclusion and Future Work – highlights the findings from the previous chapters and proposes future work for further study.

Chapter 2 Literature Review

2.1 Advanced High-strength Steels

To minimize CO₂ emissions from vehicles, reducing harm to the environment, new emission standards have been developed which will require increased fuel economy for new light-duty vehicles compared to current vehicles on the market [12–15]. For example, as per the national greenhouse gas emissions standard for passenger cars and light trucks for model years 2023-2026 mandated by the United States Environmental Protection Agency, the maximum CO₂ emissions for new light vehicles will decrease from 103 g/km in 2023, 98 g/km in 2024, 93 g/km in 2025 and 82 g/km in 2026 and later years [12]. One of the ways that the automotive industry will achieve this goal is by reducing autobody weight by decreasing the material thickness of steels used for body-in-white construction [16]. Advanced high-strength steels (AHSS) have a multi-phase microstructure containing ferrite, pearlite, cementite, martensite, austenite, bainite and/or retained austenite, which contributes to a higher ductility, enhanced formability and crash worthiness than similar strength conventional steels with the single-phase microstructure. They can be used to decrease material thickness used for parts, thus decreasing part weight, leading to a decrease fuel consumption and emissions without compromising the vehicle safety. Approximately 40 % to 50 % of body structure of a modern vehicle is made of AHSS [17], which causes 25 % to 39 % reduction of vehicle component weight and 8 % to 10 % reduction of total vehicle weight compared with the use of conventional steels [18]. In general, the yield strength and tensile strength of AHSS are 300 MPa and 600 MPa higher than the conventional steels, respectively [19]. The following sections introduce three generations of AHSS and their applications in the automotive industry. Also, the fabrication process and microstructural characterization for quench and partitioning steels are described as it is necessary to build background understanding for the investigated materials in this work.

2.1.1 Generations of AHSS

There are three generations of AHSS, where the third generation is the latest. To achieve desired mechanical properties of steels, the alloying elements, temperature and methods for heating and cooling processes are precisely selected and controlled for dedicated microstructure transformation [20–23]. The “Banana” diagrams in Figure 2.1, depicts the evolution of steels from the conventional steels to the latest AHSS [24]. The first generation (1G) AHSS have ferrite-based microstructures and mainly include dual phase (DP) steels, transformation induced plasticity (TRIP) steels, complex-phase (CP) steels, martensitic (MS) steels and press-hardened steels (PHS) [19]. Other microstructural components such as martensite, bainite and retained austenite in 1G-AHSS provide strength and additional ductility but the variation of hardness attributing to various microstructural components may cause local formability issue while stamping the 1G-AHSS. The austenitic based microstructures of the second generation (2G) AHSS mainly include twining-

induced plasticity (TWIP) steels, Al-added lightweight with induced plasticity (L-IP) steels and shear band formation-induced plasticity (SIP) steels have improved strength and formability than the 1G-AHSS [19]. However, the high processing costs and complicated process routes for stabilizing austenitic microstructures of 2G-AHSS by adding high alloying elements (i.e. Mn, Si, Al, etc.) and requirement of low carbon sheet steels impede their wide application [20,21,23]. The invention of the third generation (3G) AHSS solves the drawbacks of the 1G and 2G AHSS where 3G-AHSS have excellent combination of strength and formability with less requirement of alloying element and process costs. The 3G-AHSS mainly comprise modified DP, TRIP and CP steels from the 1G-AHSS through optimized manufacturing techniques and primarily include TRIP-assisted bainitic ferrite (TBF) steels, carbide-free bainite (CFB) steels, quenched and partitioning (Q&P) steels and medium manganese (Med-Mn) steels [25].

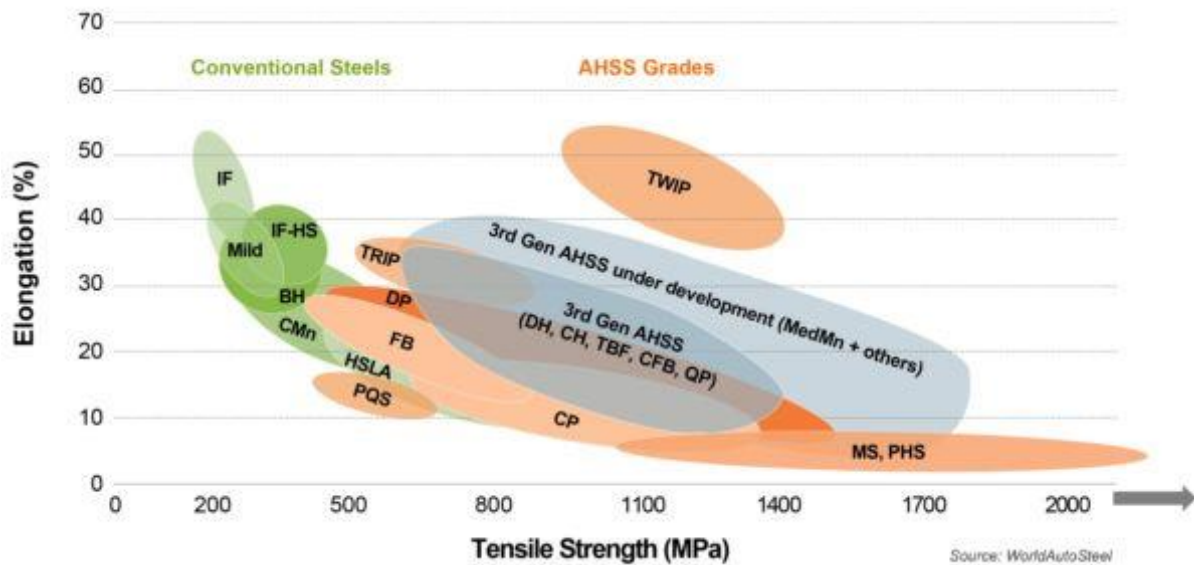


Figure 2.1: Comparison of tensile strength and elongation of conventional steels and three generations of AHSS [24]

2.1.2 Applications of AHSS in the Automotive Industry

AHSS are commonly used for safety components and structural parts in the skeletal body of a vehicle called body-in-white such as bumper beam, reinforcements, door beams, seating, chassis, and frames (See Figure 2.2) [14,15,21,26,27]. For instance, DP steels and TRIP steels have high tensile strength levels with enhanced formability and crash-energy absorption and can be used in crash zone and rear zone structures of vehicles, respectively. MS steels have highest strength range and lowest formability in the AHSS family and are suitable in the vehicle body where limited deformation is required. In addition, CP steels are generally applied in vehicle's frames because of their excellent formability and good tensile strength. AHSS are advantageous at providing same strength with significant reduction of thickness and material costs

compared to aluminum and composite materials, and accommodate to common joining and welding technology and forming methods with minor adjustment of settings [16]. The amount of AHSS, used in automotive industry as light weight material, have been consistently increasing by years. Figure 2.3 exhibits the demand of AHSS for the light vehicles produced in North American [28] and it is worthy to mention that ultra high strength steels (UHSS) is a subset of AHSS but with tensile strength above 980 MPa, 1180 MPa or 1270 MPa depending on the different standards of companies [24]. The example shows from the year of 2012 to 2025, the net pounds of AHSS including UHSS will grow by 2.3 times.



Figure 2.2: Material distribution in the body structure of Volvo XC90 [29]

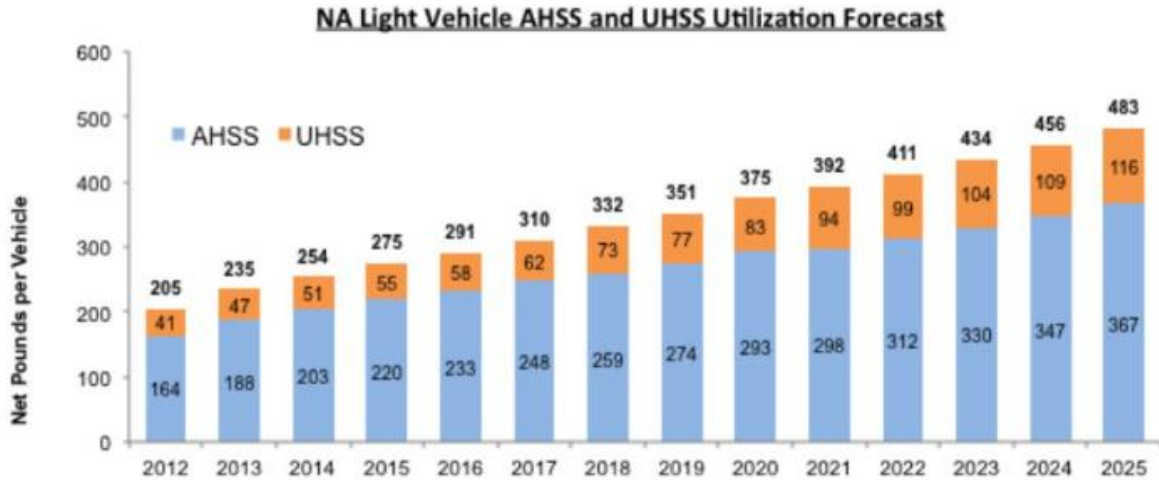


Figure 2.3: Average pounds of AHSS used in North American produced light vehicles from 2012 and forecast to the year of 2025 [28]

2.1.3 Quenching and Partitioning Steels

Q&P steels have excellent combination of high tensile strength and ductility. The microstructures of commercial produced Q&P steels contained volume fraction of 50 % to 80 % martensite, 10 % to 20 % austenite, or 20 % to 40 % ferrite and 5 % to 10 % retained austenite [25,30–32]. The micrographs of a typical Q&P steel is depicted in Figure 2.4 [33]. Quenching and partitioning (QP) is a heat treatment process containing multiple steps and firstly introduced by Speer et al. [31]. The process includes an austenitization stage (single phase austenite region or two-phase region of ferrite and austenite) and one-step or two-step QP process. Figure 2.5 shows the thermal cycle of quenching and partitioning process [25]. The steel is heated to austenitization temperature and hold isothermally for an appropriate time to produce partially or fully austenitic microstructure. After austenitization step, the steel is quenched to a temperature within the temperature range of start (M_s) and finish (M_f) of martensitic transformation to ensure contents of untransformed austenite and fresh martensite. Then, for the one-step QP process, the steel is held isothermally for a period time (partitioning process) to allow carbon transportation from carbon-supersaturated martensite to the untransformed austenite, which stabilizes austenitic microstructure by carbon enrichment and increases the strain required for martensite transformation. For a two-step QP process, after the first quenching step, the temperature is increased above M_s followed by a similar partitioning process as the one-step QP process but at a higher temperature. The tempered martensite provides a better ductility than the untampered martensite [25,26]. The increase in temperature is also beneficial for carbon diffusion from martensite to austenite by accelerating movement of carbon atoms in the microstructure. The partitioning temperature and time are varied among different manufactures but the combination of the two parameters must be adequate to produce considerable amount of carbon-enriched

austenite and hinder carbide formation. The carbide formation consumes substantial carbon and reduces the availability of carbon for carbon mitigation from martensite to austenite. Finally, for both one-step and two-step QP process, the steel is quenched below M_f temperature to complete martensite transformation [25,30,31,34–36]. Depending on the cooling rate, some bainite and additional ferrite can be produced. However, for the requirement of Q&P steel with higher strength, the reduction of ferrite content is favorable. The selected cooling rate should be high enough to diminish ferrite and bainite formation and facilitate microstructure evolution of the steel to achieve desired mechanical properties. The strength and ductility of Q&P steels can be improved by deformation-induced martensite formation from austenite through stamping (TRIP effect).

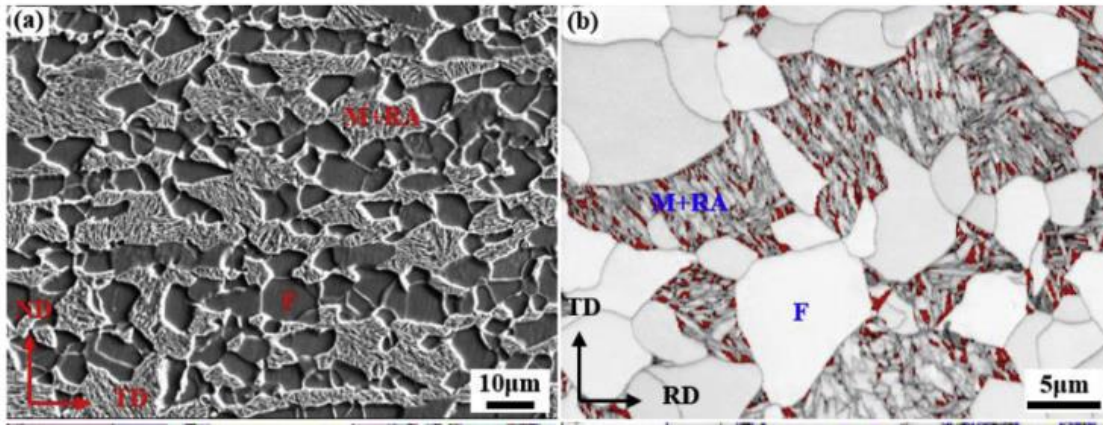


Figure 2.4: Micrographs of Q&P steel: (a) SEM image and (b) EBSD image [33]

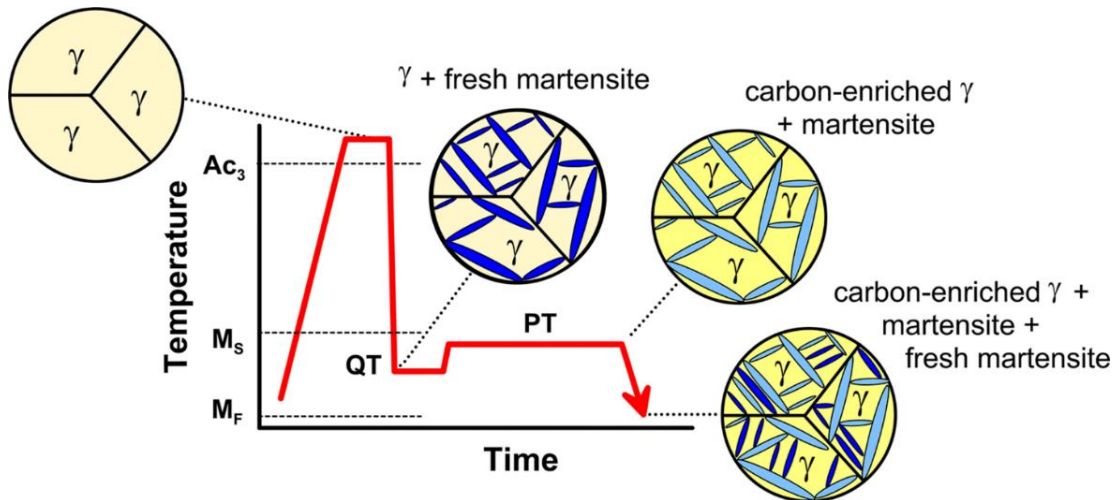


Figure 2.5: Thermal cycle of quenching and partitioning process [25]

The addition of alloying elements such as nickel, manganese, aluminum and silicon also effects austenite stabilization. The Schaeffler diagram (see Figure 2.6) shows the influence of carbon, nickel and manganese contents on austenite stabilization. Nickel has a face centered cubic (FCC) crystal structure, which is the same as austenite. The addition of Ni resists the transformation of austenite to martensite by decreasing its M_s temperature [37]. Moreover, manganese is capable of stabilizing austenite though the reduction of the availability of carbon for the formation of other phases such as ferrite, pearlite and bainite during quenching and the refinement of austenite grain [38]. The uniform and small austenite grains can resist the movement of dislocations of the grain boundary as required for phase transformation. The addition of aluminum increases stacking fault energy of the steel, which makes phase transformation from austenite to martensite energetically unfavorable [39–41]. Silicon has very low solubility in cementite and segregates at the interface of carbide, which creates kinetic barriers around carbides, retards their growth and formation [42,43] and provides availability of free carbon for austenite stabilization. Additionally, silicon suppresses austenite decomposition to bainite during partitioning process [44].

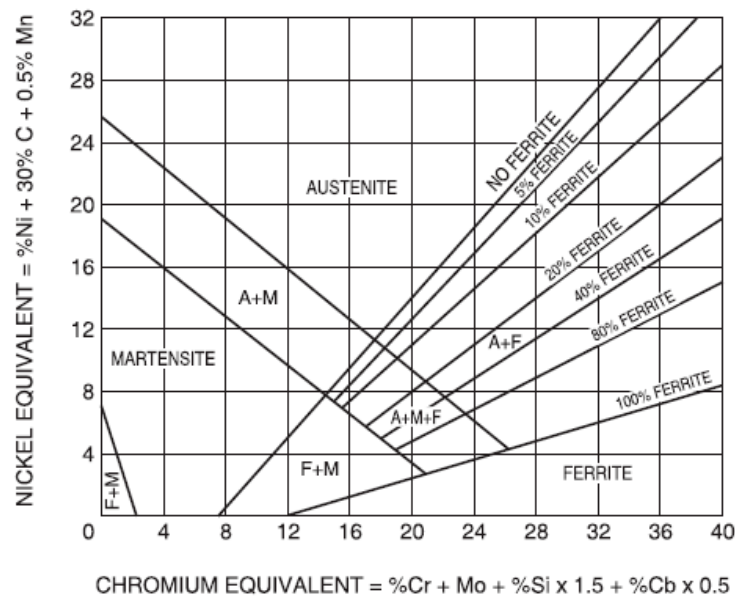


Figure 2.6: Schaeffler diagram [45]

2.1.4 Zinc Coating of AHSS

Zinc coatings are generally used for manufacturing corrosion-resistant advanced high-strength steels [46,47]. The zinc coating isolates underneath steel from corrosive environment as a barrier. Zinc is more electrochemically active than iron and it acts as a sacrificial anode to corrode preferentially. In addition, the zinc corrosion products form a barrier layer and protect the exposed steel from scratches, cuts or at the voids [48,49]. The anticorrosive coating has less influence on mechanical properties of protected steel in

stiffness, strength and formability, additionally reduces protective oil requirement in storage, improves press forming lubrication, decreases corrosion protection costs by increasing product durability compared to the use of waxes and conservation of natural resources. The zinc coating commonly was applied by hot-dipping (galvanized (GI), galvanized (GA)) or electro-plating (EG) [48].

In the hot-dipping process, steel coils after heat treatment are immersed in a molten zinc bath. Small amount of aluminum is commonly added into the zinc bath to increase the surface brightness of the coating, reduce zinc ash formation, improve zinc drainage during the coating process and inhibit reaction of iron and zinc by forming Fe_2Al_5 at the interface of the zinc coating and the steel substrate. GI coating, also called free zinc coating, comprises two layers of a pure zinc layer from the top following with a thin Fe_2Al_5 layer. After hot-dipping, the galvanized coating is sometimes annealed to diffuse iron into the zinc coating, resulting in a Zn-Fe alloyed coating. This type of coating is known as hot-dipping galvanized coating. GA coating has better weldability, higher electrical resistance, melting point and hardness owing to the presence of iron (8 wt. % to 12 wt. %) in the coating with respect to the annealing process than the GI type. [50]. However, the additional heat treatment for GA coating causes a higher processing cost than GI coating. During EG process, zinc is electrolytically bonded to steel passing through a zinc solution by an electrical current [51]. EG is a room temperature process and the microstructure, mechanical and physical properties of steel remain unchanged after coating. Compared with GI process, EG process has lower process temperature, better control of coating weight and more uniform coating thickness but a relatively higher friction coefficient and higher costs [52].

2.2 Resistance Spot Welding

Resistance spot welding (RSW), a fusion welding process, is the dominant welding technique for the body-in-white assembly. An average of 2000 to 5000 spot welds are required to assemble an automotive body [53]. There are several advantages associated with the RSW process, such as high reproductivity, feasibility of automation, very short cycle time and cost-efficiency [54]. The integrity and mechanical performance of spot welds influence the ability of a car structure to protect passengers during a crash (vehicle crashworthiness) and the comfort of driving experience without undesired noise, vibration and harshness [55]. The knowledge of RSW process and proper selection of welding parameter are essential to achieve welds with satisfied structures and properties that define vehicle's safety and performance [54,56]. The following section introduces the fundamental mechanisms of RSW process and the influence of main welding parameters on resultant properties of the weld.

2.2.1 RSW Fundamentals

RSW process interacts electrical (i.e. current and resistance) and mechanical (i.e. clamping and electrode collapse) event to join metal sheets with a solid nugget [54,56]. During the RSW process, the sheets to be joined are fitted together in a lap-joint configuration. They are then clamped by water-cooled copper electrodes. The clamping force is exerted by electrodes throughout the welding cycle. After clamping, welding current passes through the work pieces, and the sheets are fused together by the generated heat from the current flow. The heat generation during RSW follows Joule's heating that is a function of welding current square, electrical resistance and welding duration [46]. The welding current is then terminated and the electrodes extract the remaining heat from the weld to solidify the molten nugget (weld nugget) [54–58]. Finally, the electrodes retract from the sheet surface. The five stages of RSW process are showed in Figure 2.7 [59].

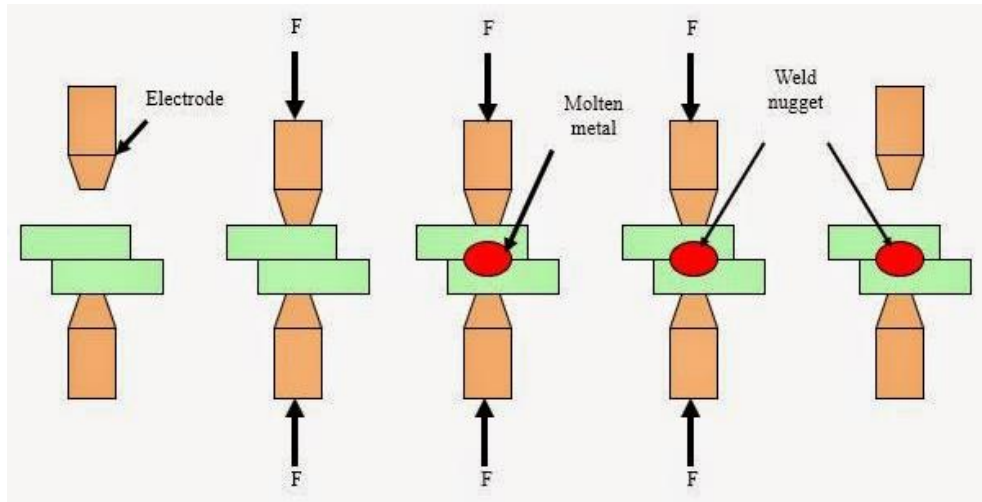


Figure 2.7: Schematic of RSW process consisting five steps of clamp, squeeze, heat, hold and release [59]

There are two types of resistance in the electrical circuit: contact and bulk resistance. The individual resistances are in series and sum of all resistance values results in the total electrical resistance of the joint. The total electrical resistance of RSW process is the summation of all of the local resistances in the weld joint, composing mainly of two contact resistances R_1 and R_5 between the sheets and the electrodes, two bulk sheet resistances R_2 and R_4 and one sheet to sheet interfacial resistance R_3 , see Figure 2.8a [60]. The total resistance can be referred as the static resistance or the dynamic resistance. The static resistance qualifies the electrode resistance at the beginning of the welding process. However, dynamic resistance takes into consideration the changes of the electrical resistance causing from the variation of surface conditions (electrode to sheet and sheet to sheet interfaces) and material temperature during the welding cycle, which is more capable to fully interpret the RSW process [60]. At the beginning of heating process,

the cumulated heat and sufficient pressure at the faying surface break down the contaminants (e.g. oxides) and then soften asperities, which reduces the sheet interfacial resistance as the sheet contact area increases (the decrease of dynamic resistance). After the completion of asperity softening, the bulk resistance of sheets becomes dominate, and the total resistance increases owing to the increase of bulk resistance with material heating. Then, the melting temperature of the material is reached and a liquid contact forms between the two sheets where the path of current flow is the longest (highest electrical resistance). However, the growth of molten nugget and electrode collapse event decreases the path of current flow causing the bulk resistance to be plateau and then decrease. In addition, the occurrence of expulsion expels molten material from the weld center and further reduces the path of current flow, consequently a sudden drop of bulk resistance (the decrease of dynamic resistance) [55,56,60].

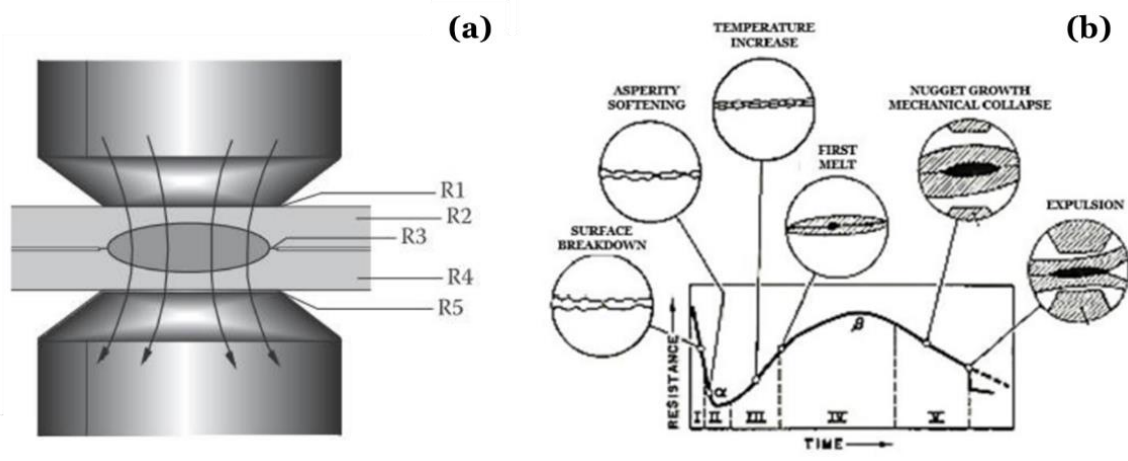


Figure 2.8: (a) Components of dynamic resistance [60]; (b) Dynamic resistance curve [55]

2.2.2 Welding Parameters

The main welding parameters of RSW process include welding current, electrode force, welding time and hold time [56,61]. The selection of proper welding parameters is vital to achieve high weld strength. The weld strength and probability of defects directly link with the nugget size. For acceptable welds, welding parameters need to be set so as to produce a nugget size within an accepted range (weldability lobe) of minimum nugget size and maximum nugget size, see Figure 2.9 [62]. The weldability lobe is determined experimentally. The minimum nugget size equals to $4(t_{\text{sheet}})^{0.5}$, where t_{sheet} is the sheet thickness and the maximum nugget size is obtained at the expulsion current [63]. A wide weldability lobe is desired since the material may be welded with a wide variation of welding parameters and results in acceptable nugget sizes [55,64,65]. If other welding parameters are held constant, prolonged welding time or elevated welding current generate more heat input in the material and produce large weld nugget [55,66]. Moreover, expulsion is subject to occur with excessive heat input, leading to inconsistent weld strength and severe

electrode indentation [54,67]. Welding with insufficient electrode force also cause oversized nugget due to the increase of heat generation (the increase of dynamic resistance) [68,69]. The application of pulsing schedules is beneficial to a slow nugget growth and delay expulsion occurrence during the RSW process [70–72]. The advantage of pulsing welding schedules from an extending welding time is the reduction of unqualified welds. In the present study, two pulsed welding schedule was adopted, and the details are introduced in chapter 3 (Methodology).

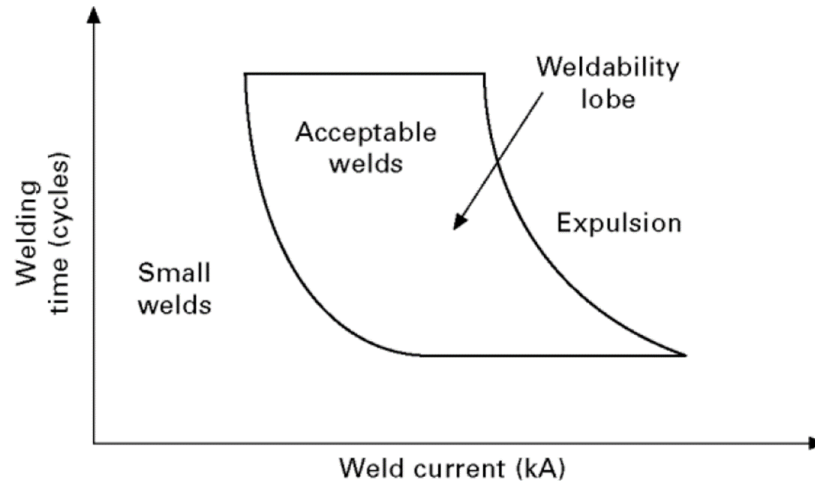


Figure 2.9: Weldability lobe for RSW process [62]

2.2.2.1 Heat Extraction during RSW Process

Cooling is a very important part of the RSW process. During welding process, the extraction of heat is necessary to ensure that the molten material remains localized at the joint interface and solidify the nugget after the termination of the welding current. Water is a common cooling agent used in RSW process. The continuous cold-water flow circulates inside of electrodes, extracting heat away from the surface of electrodes and sheets to minimize electrode wear (electrode trimming and electrode mushrooming) during heating cycle [62]. Moreover, during hold time, the cooled electrode dissipates heat from the weld zone for nugget solidification. The increase of electrode face diameter or electrode force can promote heat extraction by increasing in contact area between the electrode and the sheet surface and reducing the total resistance [43,56].

2.2.3 Expulsion Event during RSW Process

Expulsion, a phenomenon of liquid metal ejection from the electrode to the sheet interface or the faying surface, happens when welding with inappropriate welding parameters (i.e. elongated welding time, welding current exceeding the expulsion limit and insufficient electrode force), electrode misalignment and poor conditions of workpiece or electrode [55,57]. Figure 2.10 [73] shows the expulsion progression images

from nugget growth prior to expulsion initiation to the conclusion of the expulsion event using high-speed photography. Before expulsion initiation, only the bright line L_n (a visual indication of weld nugget growth) was detected at the faying interface and its length increased with the welding time. At the mean time of expulsion initiation, the other line, L_e , suddenly appeared on the right side of the line L_n , which shows the expulsion from visible at the faying surface. Subsequently, the expulsion was determined to be completed between the time of 100.50 ms to 100.75 ms, so that dark area were observed between L_n and L_e (the stop of molten material expelling, see Figure 2.10f and g).

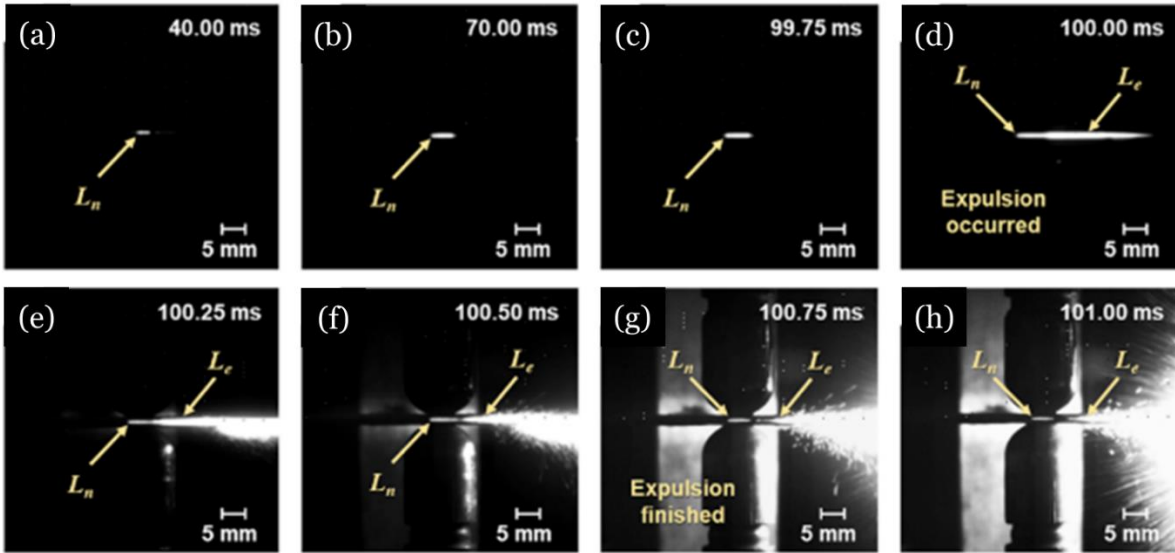


Figure 2.10: Frames from high speed imaging of the RSW cycle showing the progress of expulsion event before its initiation and after its completion from image (a) to (h) [73]

Welds experiencing expulsion have been reported to exhibit sudden electrode indentation and the formation of voids, porosity and cavities, which may be detrimental to weld strength [74,75]. Global efforts have been made to understand the mechanism causing expulsion occurrence and among those established models, geometry comparison model, force balance model and statistical model are worthy to be noted. The geometry comparison model, a finite element model, proposed that the exerted electrode force created a compressive zone around the molten nugget and if the radius of molten nugget grows larger than the compressive zone, the expulsion will happen [76,77]. The force balance model described the expulsion initiation as the force implementing on the molten nugget from the liquid expansion (ferrostatic force) exceeding from the electrode (electrode force) [57]. The corona bonding forms at the faying surface resulting from solid diffusion, which resists sheet separation and maintains nugget morphology. When a force discrepancy takes place (the ferrostatic force of weld nugget exceed the corona bond strength), the corona bonding is broken and the molten material escapes from the weld

nugget. A statistical model was developed to predict expulsion as a function of welding time and welding current with a fixed electrode force [78]. A lobe diagram is often used to visualize when expulsion is probable to determine proper welding schedule for acceptable weld size. The boundaries of the lobe diagrams are the corresponding welding time and welding current to achieve the minimum weld size and the maximum weld size (expulsion limit).

The techniques used for expulsion detection are important to understand the expulsion phenomenon with respect to the change of process parameters and the followings are two examples. Figure 2.11 [73] shows a sharp drop of resistance with expulsion occurrence. As described in section 2.2.1, when expulsion occurs, the total resistance decreases due to the sudden reduction of bulk resistance from the loss of molten material and the decreased length of current path. This method was also applied in the present work to define expulsion condition for each individual welding case. More details are discussed in the Chapter 4. Karloff et al. [74] detected expulsion non-destructively by using an ultrasound transducer installed in a welding electrode. The sudden heat loss along with molten material expelling from the welding zone during RSW process, significantly decreased the total time of flight of reflected ultrasound pulses, indicated an expulsion, see Figure 2.12. Other techniques such as observing electrode displacement and electrode collapse may help to assess the severity of expulsion [46]

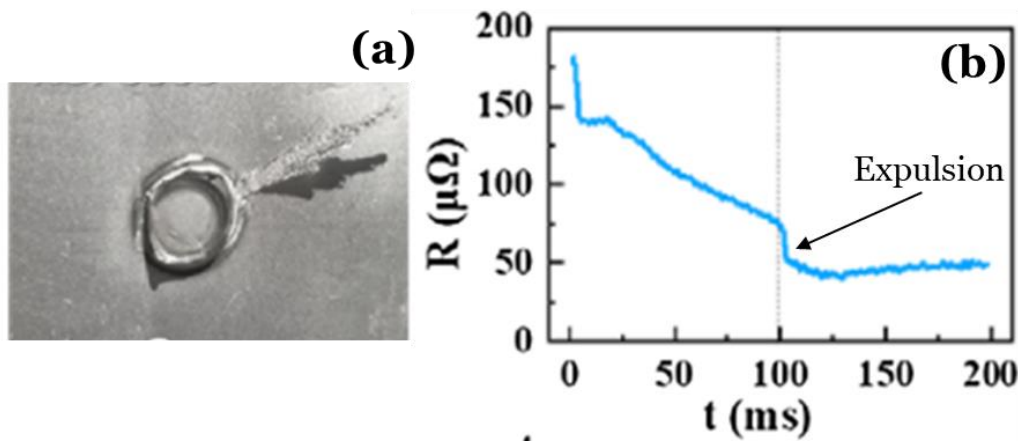


Figure 2.11: (a) Weld with expulsion; (b) A drop of resistance with expulsion [73]

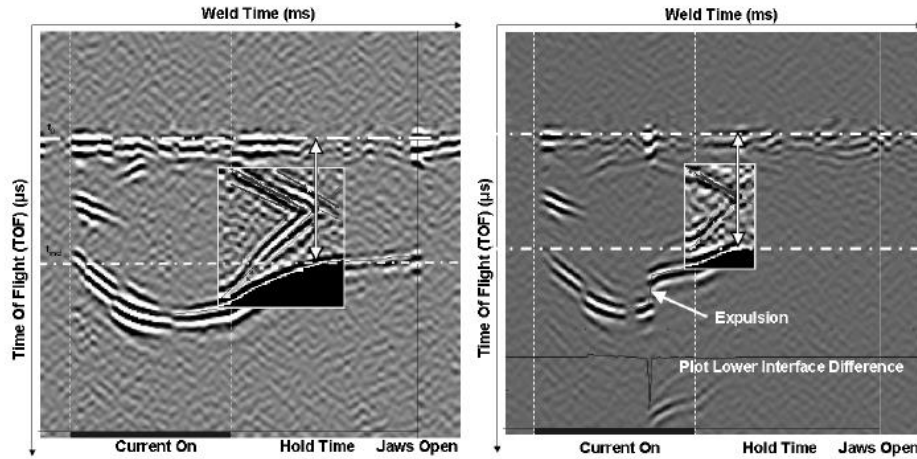


Figure 2.12: B-scan images of the inspection: expulsion-free (left) and with expulsion (right) [74]

2.3 Fe-Zn Coupled Liquid Metal Embrittlement

Liquid metal embrittlement (LME) is a cracking mechanism resulting from intimate contact of certain solid and liquid metals, such as the couples of $\text{Al}_{\text{solid}}/\text{Ga}_{\text{liquid}}$, $\text{Cu}_{\text{solid}}/\text{Bi}_{\text{liquid}}$ and $\text{Fe}_{\text{solid}}/\text{Zn}_{\text{liquid}}$ [1,79,80]. During LME, low melting point metal is melted when the temperature of the couple is raised. The liquid metal then penetrates through grain boundaries of the solid metal in the couple. The presence of the atoms from the liquid metal in the grain boundaries of the solid metal weakens those grain boundaries, which leads to brittle intergranular failure under applied tensile stress [81–84]. Safety issues caused by LME-induced material failures have been reported within nuclear, aerospace and automotive industries during material fabrication, welding, assembly and operation [1]. Therefore, it is important to understand the mechanisms of LME and the factors influencing LME behavior for developing strategies of LME elimination. The fundamentals of Fe-Zn coupled LME cracking, and the correlation of temperature, stress, susceptible microstructures, welding parameters and silicon content on LME cracking will be introduced as follows. All mentioned LME in this study refers to $\text{Fe}_{\text{solid}}/\text{Zn}_{\text{liquid}}$ couple.

During the RSW process of the zinc coated AHSS, the temperature at the sheet surface will exceed zinc melting temperature of 419 °C. The zinc coating melts and diffuses into grain boundaries of the steel acting as an embrittling agent, reducing the cohesion of grain boundaries. If there is sufficient tensile stress, the liquid zinc further propagates into the grain boundaries, causing LME cracking (see Figure 2.13), potentially degrading weld strength [4].

Lap shear test is the standard test to evaluate static spot weld strength. DiGiovanni et al. [85] examined the influence of LME cracking on weld strength by comparing changes of weld strength between zinc coated and uncoated DP980, TRIP690 and TRIP1100 joints through lap shear testing. Compared to DP980 and

TRIP690 welds, the zinc coated TRIP1100 welds experienced the largest number of LME cracks with the length greater than 400 μm and highest percentage of shoulder cracks, showing most significant strength reduction. In the lap shear test, failure initiated at sheet/sheet interface and grew towards the sheet surface while LME crack was propagating towards the sheet interface. Complete failure occurred when both types of cracks joined on the same path. The presence of zinc on the fracture surface indicated that LME cracking assisted fracture formation, see Figure 2.14a [85]. During lap shear test, the pull-out stress concentrates at the weld shoulder region, therefore; the extent of large LME cracks at the weld shoulder is critical for static weld strength loss. Conversely, DP980 and TRIP690 joints featured with relatively smaller number and size of LME cracks at the weld shoulder and were not observed notable reduction of weld strength, see Figure 2.14b [85].

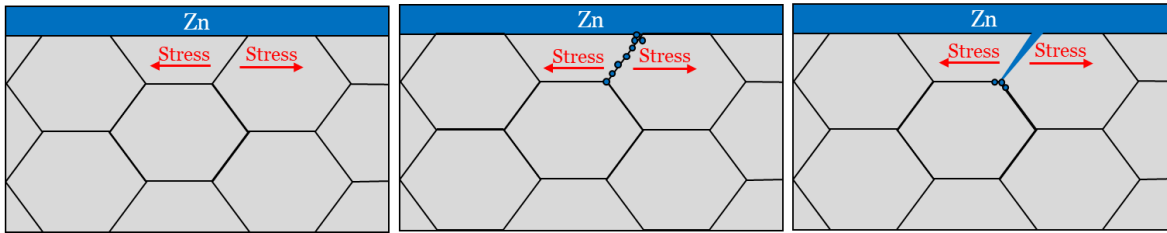


Figure 2.13: Schematic of LME crack propagation

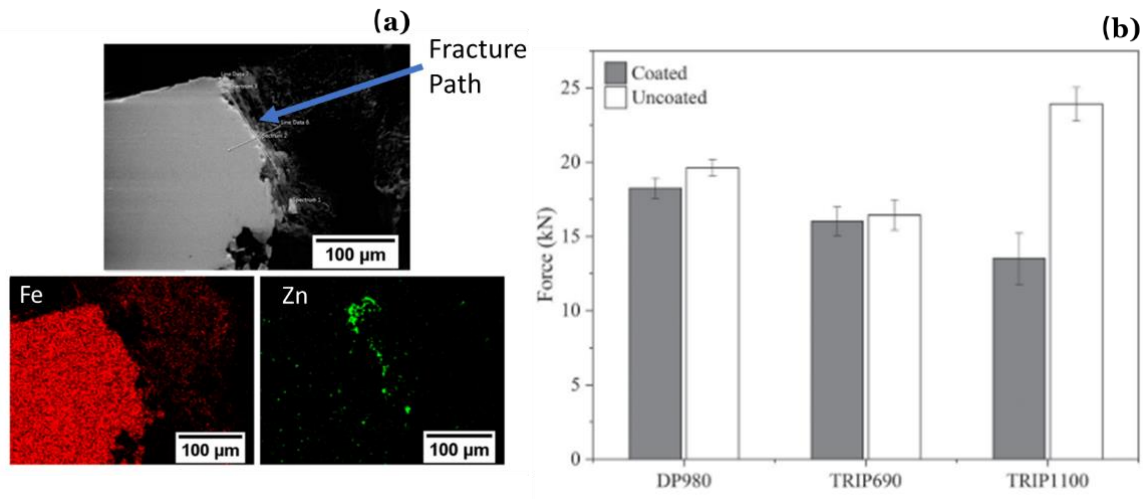


Figure 2.14: (a): SEM image of crack area and EDX maps for Fe and Zn concentrations of TRIP1100; (b) Mean lap shear strength of investigated materials [85]

2.3.1 Influence of Temperature on LME

The temperature range of RSW process is approximately between 0 $^{\circ}\text{C}$ to 1600 $^{\circ}\text{C}$. LME cracking is considered as the brittle and intergranular fracture of a comparative ductile steel, magnificently degrading

the ductility of the steel. The zinc coating of steels usually has higher melting temperature than the pure zinc attributing to the contents of Al-Fe (forms during the coating process) and Zn-Fe (forms during the annealing process) intermetallic compounds at the interface of the coating and the substrate, and they need to be melted prior to zinc penetration. As can be seen at Fe-Zn phase diagram (see Figure 2.15), LME occurs on right side of the red boundary line and a temperature of 782 °C is required to dissolve sufficient $Zn_{10}Fe_3$ intermetallic compound before LME triggers, which explains LME cracking was observed above zinc melting temperature. Beal et al. [3] and Jung et al. [86] simulated actual RSW process using hot tensile testing. During the hot tensile testing, the specimen of zinc coated steel was heated and pulled out with certain strain rate to induce LME cracking. The presence of LME cracking drastically deteriorated the ductility of the joint (reduction of fracture strain and strength) called ductility trough. The detected temperature range causing ductility trough under certain strain rate was between 600 °C and 900 °C. The ductility trough for LME formation varied in different steels, and the steel with a larger ductility trough might have worse LME cracking because of higher availability of molten zinc supporting crack propagation along grain boundaries of the steel. When the process temperature goes beyond the zinc evaporation point (approximately 907 °C), the propagation of LME cracks stopped due to discontinuous liquid zinc supply [3]. Beside of temperature, the stain rate also affects LME cracking. The severity of LME cracking increased with the increase of strain rate (the decrease of application time in hot tensile testing). It was manifested that, at high strain rate, there is insufficient time to form Fe-Zn intermetallic compounds acting as a barrier for zinc penetration [4].

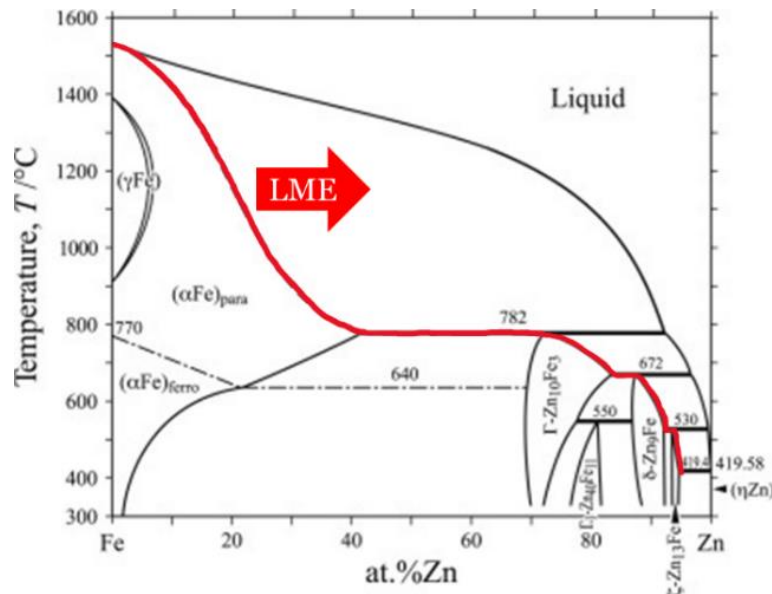


Figure 2.15: Equilibrium Fe-Zn binary phase diagram

2.3.2 Influence of Susceptible Microstructure on LME

The investigation of LME cracking behavior has been conducted on multiple types of AHSS during RSW, from which; TWIP steels, consisting of fully austenitic microstructure, exhibited higher LME sensitivity than TRIP, TBF, DP and DP high ductility (DH) steels [87,88]. DH steels have higher propensity to LME cracking than the traditional DP steels due to the extra addition of alloying elements for ductility enhancement [88]. Interstitial free (IF) steels resist to LME cracking owing to very low content of alloying elements [84]. Ghatei Kalashami et al. [81] examined the influence of ferritic and austenitic microstructures on LME cracking during hot-tensile testing on the ferritic and austenitic stainless steels. The study reported both ferritic and austenitic microstructures were susceptible to LME cracking but the extent of influence were different with regarding crack initiation and crack propagation. Ferritic microstructure was subject to LME initiation with comparatively lower crack propagation rate, which led to a larger number of smaller LME cracks. In contrast, the austenitic microstructure withstood crack initiation but had higher crack propagation rate, which resulted in fewer number of LME cracks with larger size. The austenitic specimens experienced the peak stress of 100 MPa, doubled than the ferritic specimens' (50 MPa) at the final failure during the hot tensile testing with same experiment setups. The failure mechanism of the austenitic specimens was characterized in a brittle nature at both the zinc coated and uncoated sides in line with the result from Ref. [89] that the increase of external stress promoted LME penetration. In addition, it is evident that the LME cracking triggers interannually in a brittle nature. As comparisons, the ferritic samples failed in both ductile and brittle manners, indicating their lower LME penetration rate. Furthermore, the diffusivity of zinc in austenite grain boundaries is significantly greater than in the ferrite grain boundaries, described in the ref. [84], can also elucidate the higher LME penetration rate in the austenitic specimens. Bhattacharya et al. [90] and Pant et al. [91] stated the martensitic microstructure is susceptible to LME cracking, but the reported influence of prior austenite grain size (PAGS) on the LME cracking behavior of the martensitic steels was contradictory. Bhattacharya et al. [90] reported that the large PAGS facilitated to LME infiltration and the cracked prior austenite grain boundaries were characterized with high misorientation angles. However, the study from Pant et al. [91] presented that the severe LME cracking was observed in the martensitic microstructure with small PAGS and the LME cracked prior austenite grain boundaries exhibited low boundary angles. The authors assumed that the conflicts of findings regarding to the influence of PAGS on the LME sensitivity of the martensite might be caused by the different structures of LME-cracked grain boundaries for the investigated martensitic steels but further explanation is required to prove this hypothesis. The microstructure of AHSS consists of multiple phases but the studies concerning the impact of a singular microstructure on LME cracking response have shed some light that AHSS, containing ferrite, austenite and martensite, are sensitive to LME cracking.

At the atomic level, the occurrence of LME cracking relates to grain boundary energy where less mechanical energy required to trigger LME through the grain boundary with higher energy. The grain boundary energy of random grain boundary (regular grain boundaries) increases with the increase of misorientation angle. Special grain boundary (i.e. coincidence site lattice) has less grain boundary energy than random grain boundaries resulting from simple structure and thinner thickness of grain boundary. Coincidence site lattice is quantified by the value of sigma (Σ) that equals to the area ratio of coincidence site lattice to fundamental lattice between two adjacent grains. Razmpoosh et al. [92] studied the effects of random and coincidence site lattice grain boundaries on LME cracking during hot-tensile test. The results showed LME was provoked at lower stress level with higher energy (higher misorientation angle) random grain boundaries. In addition, coincidence site lattice such as $\Sigma 3$, $\Sigma 5$, $\Sigma 7$ and $\Sigma 9$ have low grain boundary energy and created obstacles at the propagation path, which suppressed LME cracking even at the maximum applied stress level. Therefore, the material possesses larger fraction of coincidence site lattice grain boundaries may be less subject to LME cracking.

2.3.3 Influence of Stress on LME

The proposed techniques explaining LME formation with respect to grain boundary weakening are majorly classified into three categories: ductile-fracture models, brittle-fracture models and grain boundary-based model. Ductile-fracture models describe LME formation as a result of dislocation activity and plastic deformation at the crack tip, driven by liquid zinc diffusion [93–96]. However, the ductile-fracture models do not explain the mechanism that liquid zinc leads to LME crack propagation. Brittle-fracture models emphasize the brittle nature of LME cracking and the continuous LME crack initiation and propagation resulted from the stress-assisted solid and liquid atoms interaction at the crack tip [97–100]. Compared with ductile-fracture models, the brittle-fracture models do not correlate LME cracking with the role of dislocations. In grain boundary-based models, tensile stress assists liquid zinc from diffusing to the grain boundary and reduces cohesion of the grain boundary, consequently; crack nucleates and rapidly penetrates into the material [84,89,101]. The grain boundary-based models consider the brittle nature of LME cracking and the function of tensile stress on grain boundary diffusion of liquid zinc, which best describes the LME phenomenon.

During RSW process, the tensile stress profile at various weld location is distinct resulting from different mechanical and thermal effects. The locations are classified as the center of electrode indentation (location A), shoulder of weld extending to the edge of heat affected zone (location B) and the sheet notch (location C). Location A experiences compressive stress during squeeze time and welding time. Tensile stress initiates at the beginning of hold time and then increases for a period of time due to fast cooling [5,102–104]. Subsequently, tensile stress converts to compressive stress until the end of hold time because of

continuous heat dissipation. Upon electrode removal, the material reheating and extraction produce tensile stress. Temperature of location A gradually increases during welding time, and then decreases during hold time, finally rises slightly up as electrode retraction [5,102–104]. DiGiovanni et al. [104] compared the influence of B-nose (a semi-spherical shaped electrode with a flat contact surface), truncated cone and radius types of electrode geometries (see Figure 2.16a, b and c, respectively) on LME severity of GI coated TRIP1100 steel with comparable nugget size for all samples. The B-nose and truncated cone types both had 7mm tip but varied with shoulder shapes. Compared to the B-nose type of electrode, welds produced with truncated cone type of electrodes had LME cracking severity similar at the location A but worse at the location B, see Figure 2.16a and b. Throughout the welding time, electrode collapse occurs when the material underneath electrode face is gradually softened by the elevated temperature. The geometry of weld shoulder is critical on the tensile stress profile since it influences the approach of material extraction and the impact of mechanical force on the weld shoulder during electrode collapse event. With the progress of electrode collapse, the temperature of material momentarily contacting with the electrode shoulder suddenly decreases due to the cooling effect and then further increases by continuous heat input. The cooled material instantaneously shrinks and leads tensile stress to the adjacent material. However, the stress on the adjacent material become compressive when it contacts with the electrode. The generation of tensile stress at the region between the top weld shoulder and the edge of heat affected zone depends on the level of material contraction and applied mechanical force on the weld shoulder. The study shows that the steep shoulder of truncated cone type electrode exerted higher mechanical pressure and efficient cooling on the weld shoulder path during electrode collapse, which induced more severe LME cracking in the welds and the region tolerating highest tensile stress shifted away from the weld shoulder, see Figure 2.17. On the contrary, the curvature of B-nose electrode type had less mechanical pressure on the weld shoulder region and the peak tensile stress was observed at the top end of weld shoulder. During hold time, the temperature of shoulder area decreases by cooling effect. The edge of heat affected zone shows an overall increase in temperature during welding time and decrease in temperature during hold time [5]. Furthermore, samples welded with a radius type electrode suffered least severity of LME cracking and mild electrode indentation, see Figure 2.16c. It was seen that tensile stress was only detected at the beginning of welding time when the electrode was pressing into the material, and then compressive stress was dominant owing to consistent electrode contact with the material and cooling effect through the rest of welding cycle. In general, LME cracks, located at weld notch, appears due to the dual effect of high weld time and short hold time or under unexpected welding condition such as electrode misalignment [7,57]. The tensile stress at the weld notch is attributed to force produced sheet separation and adjacent material contraction [95]. Moreover, it is believed that the formation of LME cracks at the location B are mostly influenced by the process parameters

such as variation of electrode force and electrode geometry (external tensile stress on the weld shoulder) [5,104,105]. Location A is detrimental to LME cracking with higher RSW heat input [103].

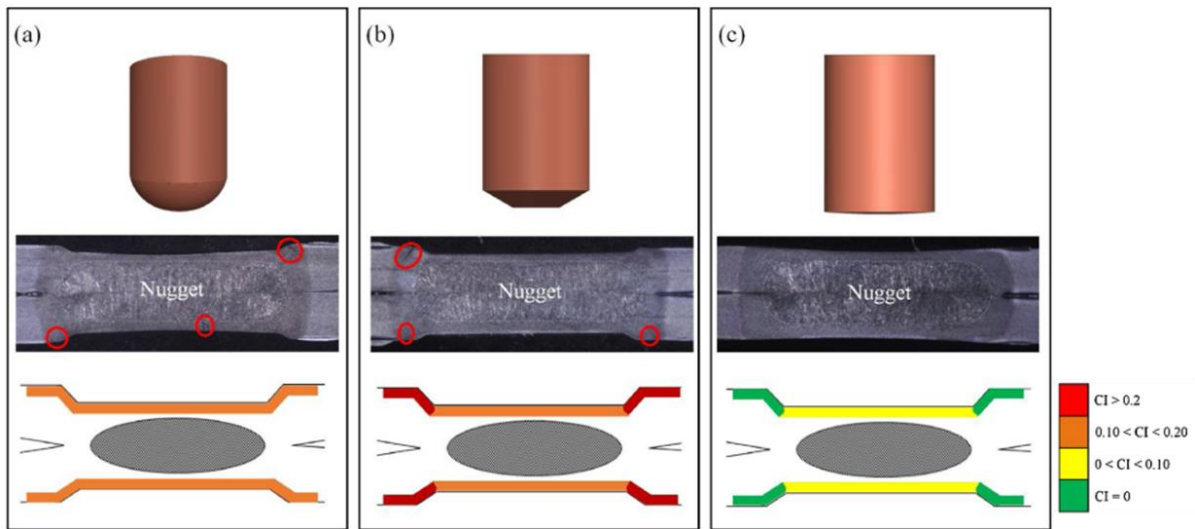


Figure 2.16: Images of electrode geometry, weld cross-section and local LME severity (larger CI value means more severe LME cracking) for (a) B-nose type; (b) truncated cone type; (c) radius type [104]

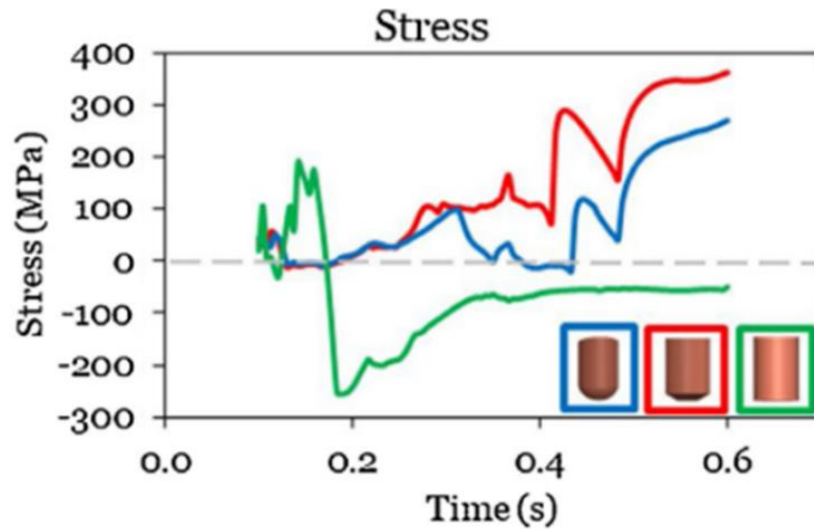


Figure 2.17: Stress profile during welding cycle for B-nose (blue line), truncated cone (red line) and radius electrode geometry (green line) [104]

DiGiovanni et al.'s work [104] on electrode geometry demonstrated the link between stress and LME cracking severity, due to influencing Zn transport on LME penetration. The stress during the welding process may also be modified using welding parameters. For instance, alteration of current profile (e.g. ramping current and pulsing current), prolonged hold time and the increase of electrode force or size

mitigate LME cracking (less availability of molten zinc and the decrease of tensile stress) by reducing process temperature or promoting heat extraction in the welding cycle. The specifics of which will be discussed in future sections.

2.3.4 Influence of Silicon Content on LME Cracking

As mentioned in subsection 2.1.3, the addition of silicon in Q&P steels stabilizes austenite phase to enhance ductility by inhibiting carbide nucleation and bainite formation during steel fabrication process [42–44]. However, studies have shown the increase of silicon content in steel led to excessive welding heat generation [106–108], the decrease in the number of low energy $\Sigma 3$ boundaries, and the retardation of iron diffusion to zinc coating from the steel [106]. The study from Hong et al. [109] reported the LME cracking severity of the weld shoulder increased with the increase of silicon content from 0 to 1.52 wt. % in the EG coated TWIP steel. It was observed that the silicon content delayed the Fe-Zn alloying reaction by hindering the Fe diffusion to the zinc coating during the heat treatment and the welding process, which exposed the underlay steel substrate to the liquid zinc and facilitated LME cracking propagation. Kalashami et al. [107] emphasized that the increase of silicon content of GI coated DP steel from 0.7 wt.% to 1.8 wt. % considerably increased LME cracking severity by extensive process heat generation (the increase of bulk resistance of the material), resulting in early expulsion during the heating cycle and generated more tensile stress (thermal shock) during the hold time and after the electrode retraction. On metallurgical aspects, higher silicon content (1.8 wt. %) enhanced decarbonization process on the steel surface adjacent to the zinc coating (higher diffusion coefficient of carbon) and caused deeper full ferritic layer with large grain size, Figure 2.18. Also, the increase of internal oxides density was observed in the DP steel with higher silicon content. Dong et al. [97] compared the LME cracking severity of GI coated Q&P steel with the silicon content of 0.8 wt. % and 1.8 wt. %. The results also showed that the GI coated Q&P steel with higher silicon content had thicker decarbonization layer during the intercritical annealing stage and was more susceptible on LME cracking during the welding process than the counterpart. In the present work, the silicon content is 1.02 wt. % for low-LME sensitive material and 1.53 wt. % for high-LME sensitive material. The two investigated materials were suspected to behave differently in LME cracking with account for silicon content and the material with higher silicon content may be prone to LME cracking.

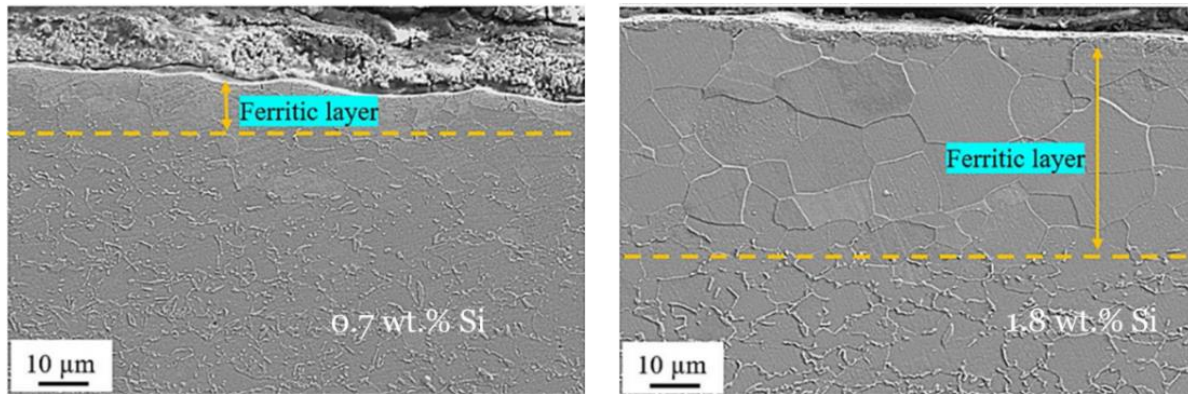


Figure 2.18: SEM micrographs indicating the ferritic layer of 0.7 wt.% silicon content investigated DP steels: (left) and 1.8 wt.% silicon content (right) [107]

2.3.5 Influence of Welding Parameters on LME

Previous research on LME cracking have pointed out that welding parameters and boundary conditions, such as electrode geometry [104,110–112], electrode force [5–10], welding current [6,103,113], and hold time [5,6,114] influenced the severity of LME cracking by change the temperature, stress, and morphology evolution in the welding area. The LME mitigation strategies can be grouped into two categories through modification of coating effect (pre-pulsing [6,113]) and heat transfer (multi-pulsing current [66,113], ramping current [103], prolonged hold time [114], radius-type electrodes [104], larger electrode tip area [110,111] and the increase of electrode force [5–10]).

Kim et al. [6] applied a short pre-pulse with low current before a main pulse in the welds of GA coated TRIP590 steel and illustrated that pre-pulse melted zinc coating and squeezed it to the circumference of the weld, mitigating LME cracking. The study from Wintjes et al. [113] conducted a low pre-pulse with short time before a main two pulsed current resulting in LME cracking reduction in the welds of GI TRIP1100 steel by stabilizing zinc coating with iron diffusion from the steel substrate instead, and decreasing requirement of heat input. Both Ashiri et al. [66] and Wintjes et al. [113] demonstrated pulsing current reduced LME cracking but the optimum pulsing current setting was a long first pulse following by a short second pulse from Ashiri et al.'s study and two even pulsed currents from Wintjets et al.'s [113]. DiGiovanni et al.'s [103] study reported welding with a ramping down current exhibited less LME active region at the indent surface and the weld shoulder, which efficiently reduced LME cracking. Böhne et al. [114] stated that an elongated hold time is required when welding with high heat input to sufficiently extract the remaining heat from the weld and prevent the weld from LME cracking.

The geometry of radius-type electrodes had reduced mechanical effect resulting from severe electrode collapse on the weld shoulder and consistently extracted heat from the weld. The temperature and stress history on the weld by adopting B-nose and truncated cone types of electrodes have been discussed in the 2.3.2 section. The implementation of electrode with larger tip area increased the contact interface of electrode and the sheet surface, which promotes heat extraction from the weld. Böhne et al. [110] investigated the influence of electrode face diameter on LME cracking of two stack-up configuration of EG coated DP1200 and GI coated mild steel with 310 MPa ultimate tensile strength, with four different welding time. B-nose and radius types of electrode geometries with 16 mm electrode diameter were selected for the study. Electrode face diameters of 5.5 mm and 8.0 mm was adopted for B-nose type. In addition, welding current was also altered to compensate the change of heat input (similar nugget diameter) from varied welding conditions. The results showed the increase of electrode diameter reduced weld indent depth and sheet separation, which mitigated LME cracking even with elongated welding time and the decrease of stress concentration was observed at the electrode to sheet interface. Furthermore, Murugan et al. [111] investigated effects of electrode design parameters (i.e. radius of curvature and tip diameter) for B-nose and radius type electrodes on LME cracking of GA coated TRIP steel. The study proved that radius type electrode was more beneficial on LME suppression than the B-nose type due to lower geometric singularity and larger contact area at the electrode to sheet interface (lower stress and larger heat dissipation). Additionally, B-nose type electrode with larger tip diameter exhibited less LME cracking. B-nose type electrodes with 6 mm and 8 mm tip diameters represented similar initial contact area of electrode to sheet interface but the final contact area was larger with the 8 mm tip diameter. For the radius type electrode, both initial and final contact area increased with the increase of radius of curvature. With regards to the extent of electrode indentation and sheet separation, the investigated B-nose type electrode generated more severe electrode indentation and sheet separation than the radius type electrode, but the severity decreased with the increase of tip diameter. The accomplished studies regarding to the influence of electrode force on LME cracking from the literature are specified in the following section to link with the present work.

2.4 Summary of Previous Work Regarding to Electrode Force Variation on LME

The body of literature, describing the influence of electrode force on LME cracking, showed that LME cracking severity decreased linearly as electrode force increased, which the authors attributed to the decrease in LME cracking causing by heat transfer as electrode force increased [5,6,8,10]. Although it is accepted that the increase of electrode force could increase the mechanical stress at the area under the electrode (especially when the electrode collapse takes place), DiGiovanni et al. [104] described that the dominant tensile stresses responsible for LME cracking was thermally, and not mechanically, generated. Choi et al. [5] represented that the maximum crack length decreased with increase in electrode force.

Barthelmie et al. [8] reported the higher electrode force led to smaller average crack length. The research from Meschut et al. [9] did not observe any LME cracks in their electrode force study, owing to the usage of high LME resistant materials. Kim et al. [6] introduced a metric they called the Surface Cracking Ratio (although the methodology to calculate it was not described) to estimate LME cracking and illustrated an increase in electrode force caused a decrease in Surface Cracking Ratio. Siar et al.[7] perceived that a decrease of electrode force led to an increasing quantities of small size type A and B cracks with 5° electrode misalignment. DiGiovanni et al. [10] indicated that the increase of electrode force decreased the severity of LME cracking severity using a statistical LME cracking indicator called Crack Index at three investigated heat input levels. Except for Choi et al.'s work, none of the electrode force studies compensated for nugget diameter change from electrode force variation. Barthelmie et al., Meschut et al., and Choi et al. investigated on dissimilar materials. Moreover, the studies from Kim et al., Siar et al. and DiGiovanni et al. manipulated on similar materials. The applied heat input during welding process in the aforementioned studies was below the expulsion limit. However, none of those electrode force studies correlated LME cracking severity with the expulsion occurrence, which is frequently observed in the automotive assembly.

Table 2.1: Literature research gap regarding the influence of electrode force on LME

	Meschut et al. [105]	Kim et al. [102]	Barthelmie et al. [104]	Choi et al. [93]	Siar et al. [103]	DiGiovanni et al. [106]
Accounted for expulsion influence on LME when varying electrode force	No	No	No	No	No	No
Compensation of nugget diameter change when varying electrode force	No	No	No	Yes	No	No
Applied welding heat input on or above expulsion limit when study the electrode force influence on LME	No	N/A	N/A	No	N/A	No
Classified LME cracking types according to their locations at the weld (i.e. Type A, B, C crack)	No	No	No	Yes	Yes	Yes
Used statistically viable method for crack severity evaluation	No	Yes	Yes	Yes	No	Yes
Influence of electrode force on LME is the main focus of the study	No	No	No	Yes	No	No
Similar joints	No	Yes	No	No	Yes	Yes

2.5 Deficiencies from Previous Work

The reviewed literature shows the important role of 3G-AHSS in the development of light vehicle owing to their improved mechanical properties and lower fabrication cost than the 1G-AHSS and 2G-AHSS with satisfaction of safety and GHG emission requirements. However, during RSW process, with the presence of liquid zinc and tensile stress, the 3G-AHSS may suffer from LME cracking, potentially deteriorate their mechanical properties. The global efforts have been made to explore solutions for LME mitigation.

Electrode force is one of vital process parameters and its alteration changes the amount of heat accumulation and dissipation of the weld during the welding cycle, influencing LME cracking severity. With regards to the preceding literature review, a comprehensive study of the influence of electrode force on LME cracking is necessary. The study must be done using a range of heat inputs used in automotive assembly, compensation the role of electrode force on nugget size, statistical assessment of LME cracks, and include the occurrence of expulsion, which is frequently observed industrial welding processes. This research aims to understand the role of heat extraction and expulsion on the effect that electrode force has on LME formation during RSW of 3G-AHSS. The motivation of conducting this study was to disclose the mechanisms causing different LME response of the investigated materials while varying electrode force during RSW process compared to the existing literature.

Chapter 3 Methodology

The purpose of this research was to understand how electrode force impacts LME cracking during RSW. LME cracking severity was correlated with various factors such as nugget diameter, nugget height, nugget shape, heat input, heat loss (heat extraction), relative heat loss (the ratio of heat loss to heat input), expulsion and weld indent depth to discover the dominant factors controlling LME cracking with electrode force variation. Two materials, known as low and high LME sensitive, were used in this work. Firstly, the welding work was performed, and LME cracking severity was assessed on both investigated materials to observe the influence of electrode force variation on their LME cracking behaviors. Secondly, changes of nugget morphology, melting heat and heat loss resulting from electrode force variation were studied for low-LME sensitive material. In addition, heat input, relative heat loss and expulsion were correlated with its LME cracking severity. Finally, with the assistance of statistical analysis, the factors, showing strong relationship with LME cracking severity while varying electrode force on the low-LME sensitive material, were focused on the high-LME sensitive material to reduce redundant testing parameters. This chapter describes the chemical and mechanical properties of the investigated materials, the used RSW techniques and parameters, equations used to analyze LME cracking and characterize the welding process and sample preparation methods in the present work. Data analysis techniques, such as applied statistical methods and software are also presented.

3.1 Materials

This study was carried on two hot-dipped galvanized 3G-AHSS steels with 1.4 mm sheet thickness and their mechanical properties with corresponding tolerance of measurement (i.e. ultimate tensile strength (UTS), yield strength (YS) and total elongation (El)) and chemical compositions can be found in Table 3.1; Table 3.2 and Table 3.1, respectively. As described in chapter 2, alloying elements such as nickel [37], manganese [38], aluminum [26,39,40] and silicon [42–44] assist austenite stabilization. The amount of a particular chemical that has been added to the steel, is critical on its properties. For example, the differences in silicon additions of the two investigated steels may explain their different LME sensitivity [106–108,115].

Table 3.1: Mechanical properties of the investigated materials

Materials	Mechanical properties		
	YS (MPa)	UTS (MPa)	Total EI (%)
Low-LME sensitive	664 $\pm$ 8	1010 $\pm$ 3	16.5 $\pm$ 0.8
High-LME sensitive	634 $\pm$ 8	1015 $\pm$ 6	19.9 $\pm$ 0.8

Table 3.2: Chemical composition of low-LME sensitive material

Material	Chemical composition (wt. %)							
Low-LME sensitive	C	Mn	P	S	Si	Cu	Ni	Cr
	0.24	1.98	0.009	0.003	1.02	0.005	0.02	0.19
	Al	Mo	V	Nb	B	Ti	Ca	N
	0.5	0.001	0.004	0.03	0.0006	0.005	0.0002	0.005

Table 3.3: Chemical composition of high-LME sensitive material

Material	Chemical composition (wt. %)							
High-LME sensitive	C	Mn	P	S	Si	Cu	Ni	Cr
	0.23	2.16	0.005	0.002	1.53	0.012	0.007	0.02
	Al	Mo	V	Nb	B	Ti	Ca	N
	0.03	0.003	0.003	0.001	0.0003	0.003	0.001	0.005

3.2 Resistance Spot Welding

All welds in this study were made as two-sheet same-to-same welds constructed in the lap joint configuration. Welding was performed using a 125 kVA medium-frequency direct current (MFDC) robotic spot welder timed by Bosch Rexroth controller. The system was capable of a maximum electrode force of 5.5 kN, applied by a servo electric mechanical system. The welder used an industrial C-type welding gun positioned by a Fanuc robotic arm, and the electrodes were water-cooled with 6 L/min flow rate according to American Welding Society (AWS) standard: D8.9M [11]. Resistance Welding Manufacturing Alliance

(RWMA) 5/8" Type B, dome-shaped electrodes with a face diameter of 7 mm and a flat contact face were used, as shown in Figure 3.1. The baseline welding schedule followed the specification given by AWS D8.9M [11]. The standard (baseline) electrode force used to make the welds in the study was 4.9 kN, additionally; welds were also made with a low electrode force (4.4 kN) and a high electrode force (5.4 kN) conditions. The modified electrode force conditions shared same welding schedule with the baseline 4.9 kN electrode force condition, which can be found in Table 3.4. A Kistler 9183 force sensor and Kistler B 507 amplifier were implemented to record electrode force from each welding during this work. Welding current was applied in a two-pulse schedule with 167 ms welding time each and 33 ms cooling time between the two pulses. Figure 3.2 shows a schematic for dual-pulse welding parameters.

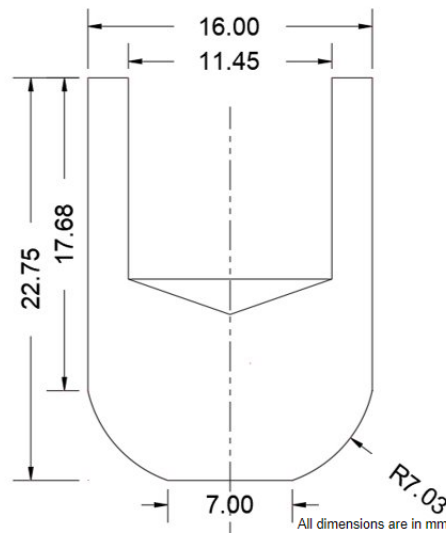


Figure 3.1: RWMA 5/8" Type B, dome-shaped electrode with 7 mm face diameter [104]

Table 3.4: Resistance spot welding parameters [11]

Electrode Force (kN)	Squeeze Time (ms)	Pulse Schedule (ms)	Hold Time (ms)
4.4	167	167-33-167	167
4.9 (Baseline)			
5.4			

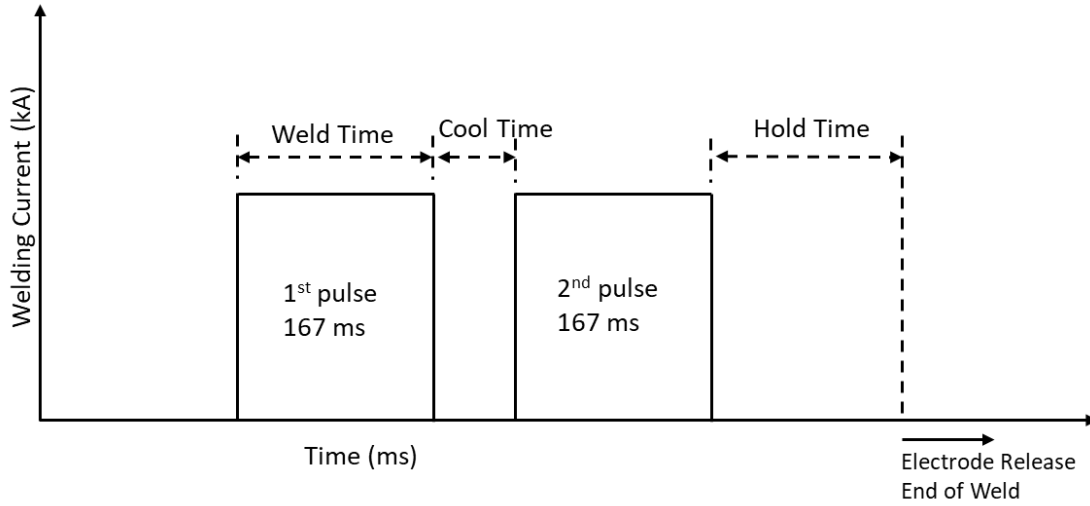


Figure 3.2: Schematic for dual-pulse resistance spot welding

Tip dressing and stabilization procedures were applied on each pair of new electrodes before producing test samples to ensure the electrode face condition did not change as welding progressed. The weldability of material indicates a range of acceptable weld size starting at the minimum weld size (MWS) and ending with the weld size resulting from expulsion and the corresponding welding currents are minimum welding current ($I_{\min}$) and maximum welding current ($I_{\max}$), respectively. The MWS for the two materials is 4.73 mm. To obtain $I_{\min}$ and $I_{\max}$ currents, a welding current was initially selected to produce the weld size with 0.5 mm smaller than the MWS and then the welding current was increased by the increment of 100 A until the target was met. The electrode stabilization current (I_{sws}), minimum and maximum welding currents for the two investigated materials with the baseline electrode force of 4.9 kN condition are listed in Table 3.5.

Table 3.5: I_{sws} , $I_{\min}$ and $I_{\max}$ of the investigated materials

Material	I_{sws} (kA)	$I_{\min}$ (kA)	$I_{\max}$ (kA)
Low-LME sensitive	6.9	5.7	9.3
High-LME sensitive	9.9	9.5	10.8

3.3 Selection of Welding Current with Varied Electrode Force

As heat generation decreases as an increase in electrode force [5], welding currents for a low (4.4 kN) and high (5.4 kN) electrode forces were adjusted, so that the nugget diameter of the welds made with the modified electrode forces matches those welds made with the baseline electrode force of 4.9 kN. The nugget diameter was used as a proxy measure for heat input forming weld nugget and used to calculate the heat

loss. Otherwise, the welding parameters were designed so that all welds made at each heat input level were made using the same generated heat input, regardless of the electrode force. The equations used to calculate welding heat are described in the following section. Three levels of heat inputs were first determined with respect to expulsion current, $I_{\max}$, for the baseline electrode force condition of 4.9 kN. The three heat input levels were: low heat input ($I_{\max}-10\%$), moderate heat input ($I_{\max}-200\text{ A}$), and high heat input ($I_{\max}+10\%$). Then, the nugget diameter obtained from each heat input level was measured to determine the required welding current with the modified electrode force conditions of 4.4 kN and 5.4 kN. For example, high heat input welds are noted as $I_{\max}+10\%$, regardless of how expulsion current changed with the electrode force conditions of 4.4 kN and 5.4 kN. By doing this, the welds made at 4.4 kN, 4.9 kN and 5.4 kN electrode forces, all had same nugget diameter as the $I_{\max}+10\%$ baseline condition of 4.9 kN electrode force condition. The welding currents and nugget diameters used for all welding conditions can be seen in Tables 3.6 and 3.7. Once the welding parameters were defined, five welds were made at each condition for LME cracking analysis. The definition of each of the dimensions used to define the weld geometry (nugget diameter, nugget height and indent depth) may be found Figure 3.3. The indent depth was calculated by the difference between the sheet thickness and half of the total thickness of the indentation region of the joint as measured from the cross-section of the weld.

Table 3.6: The selection of welding current with altering electrode force at various heat input levels for the low-LME sensitive material

	$I_{\max}-10\%$ (Low heat input)		$I_{\max}-200\text{ A}$ (Moderate heat input)		$I_{\max}+10\%$ (High heat input)	
	Welding current (kA)	Average nugget diameter (mm)	Welding current (kA)	Average nugget diameter (mm)	Welding current (kA)	Average nugget diameter (mm)
Baseline (4.9 kN)	10.2	6.4	11.1	6.9	12.4	7.1
Increase electrode force (5.4 kN)	10.4		11.8		13.1	
Decrease electrode force (4.4 kN)	9.8		11.1		12.3	

Table 3.7: The selection of welding current with altering electrode force at various heat input levels for the high-LME sensitive material

	$I_{\max}-10\%$ (Low heat input)		$I_{\max}-200\text{ A}$ (Moderate heat input)		$I_{\max}+10\%$ (High heat input)	
	Welding current (kA)	Average nugget diameter (mm)	Welding current (kA)	Average nugget diameter (mm)	Welding current (kA)	Average nugget diameter (mm)
Baseline (4.9 kN)	9.7	5.3	10.6	7.2	11.9	7.4
Increase electrode force (5.4 kN)	9.8		11.4		12.1	
Decrease electrode force (4.4 kN)	9.6		10.7		11.4	

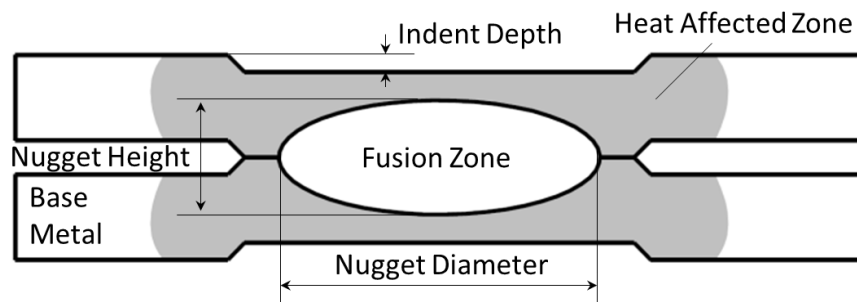


Figure 3.3: Schematic showing nugget diameter, nugget height and indent depth from the weld cross-section

After weld construction, the nugget diameter of welds made with all alternate electrode force using the currents from Tables 3.6 and 3.7 were measured. From Figure 3.4 for low-LME sensitive material and Figure 3.5 for high-LME sensitive material, it may be seen that there was no significant difference between the nuggets produced at each heat input levels, regardless of welding force. The nugget diameter was also observed an increase with the increase of heat input. The smallest nugget size was observed at low heat input, medium nugget size for moderate heat input and the largest nugget size was formed at high heat input.

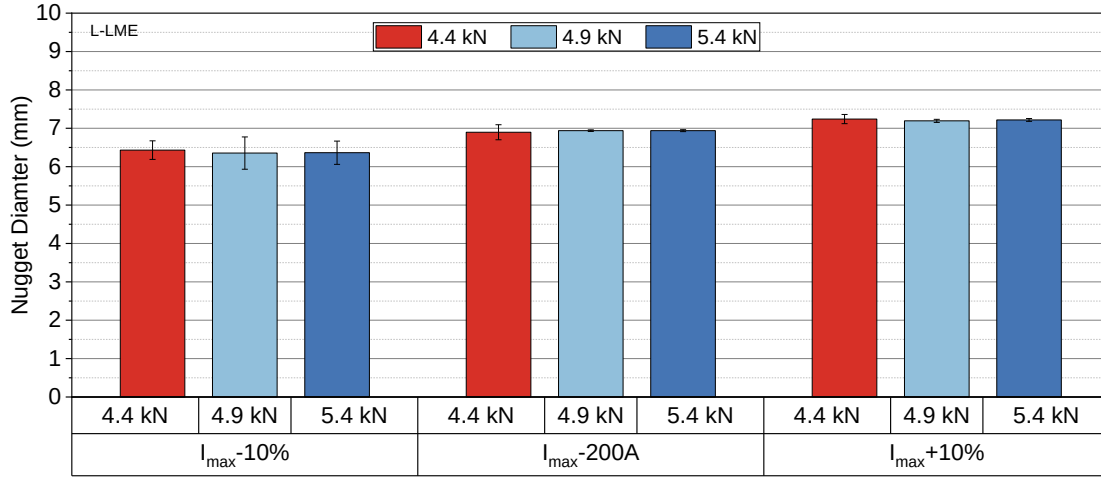


Figure 3.4: Measurements of nugget diameter for the low-LME sensitive material

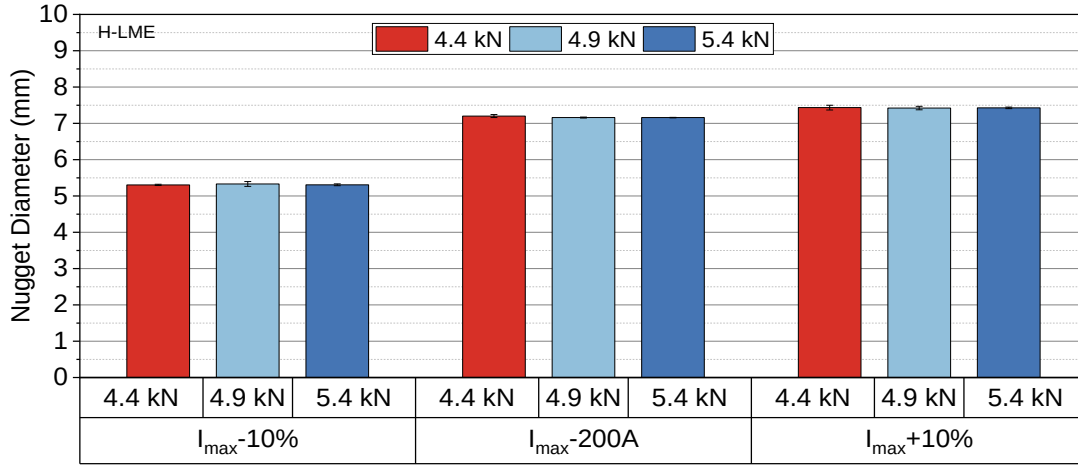


Figure 3.5: Measurements of nugget diameter for the high-LME sensitive material

3.4 Calculations of Heat Generation and Heat Extraction

The total heat input (Q_w) during the RSW process was calculated by the Joules' law as per Equation 1 below [55]:

$$Q_w = \int_{t_0}^{t_1} I^2 \cdot R \cdot dt \quad (\text{Eq.1})$$

Where t_0 , t_1 , I , R and dt denote start time of welding cycle, finish time of welding cycle, welding current, electrical resistance, and the change of welding time, respectively. The electrical resistance was calculated by dividing the instantaneous voltage by the instantaneous current. A data acquisition system, integrated into the weld controller, recorded the instantaneous welding current and voltage at a frequency on 1 kHz.

The heat loss (Q_l) was defined as a difference between the total heat input (Q_w) and melting heat (Q_m) in Equation 2 below [55]:

$$Q_l = Q_w - Q_m \quad (\text{Eq.2})$$

Q_m is the calculated heat required to melt the material which ultimately solidifies to a weld nugget. It was calculated as below [55]:

$$Q_m = m \cdot \int_{T_R}^{T_m} C_p (T) dT \quad (\text{Eq.3})$$

Where m is nugget mass, C_p is specific heat capacity of steel, as calculated using JMatPro 12.4. T_m and T_R are the melting temperature and the ambient temperature (25 °C), respectively. It was assumed that the weld nugget was ovoid in shape to estimate the nugget mass (m) as derived from equation 4 and 5 below since only ordinary nugget shape was observed from the cross-sectional views of welds [55]:

$$u = \frac{4}{3}\pi \cdot \frac{d^2}{4} \cdot \frac{h}{2} \quad (\text{Eq.4})$$

$$m = u \cdot \rho \quad (\text{Eq.5})$$

Where d and h are nugget diameter and nugget height, respectively, depicted in Figure 3.3. u is the nugget volume and ρ is the steel density ($7.85 \times 10^{-3} \text{ g/mm}^3$ [116]). Micrographs of the weld cross-sections were taken using an automated Clemex CMT microscope. Carl Zeiss Blue software package was used to measure the nugget height, nugget diameter and weld indent depth from the etched weld cross-section with 2 % nital solution after LME crack counting.

3.5 Classification of Expulsion and Non-expulsion Conditions

The investigation of the influence of expulsion on LME cracking was characterized by counting the number of welds in each group of five, made with the same welding parameters, that exhibited an expulsion event. Dynamic resistivity curves were plotted according to auto-generated values of resistance and time interval after each welding cycle, and a sharp decrease of resistance on the dynamic resistivity curve indicated expulsion (see Figure 3.6). Whether or not a particular welding condition produced expulsion was determined by how many welds of the set of five made at each condition experienced expulsion. Conditions that produced no welds with expulsion were denoted as the “No Expulsion” condition and the conditions that produced welds with one or more cases of expulsion were denoted as “Yes Expulsion” condition.

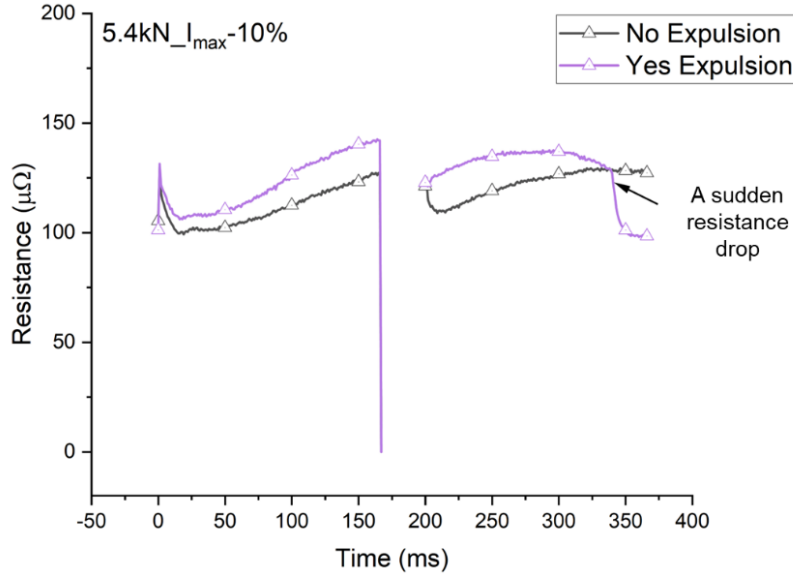


Figure 3.6: A example of defining No and Yes expulsion conditions

3.6 Quantification of LME Cracking Severity

The distribution of LME cracks was viewed from the weld cross-section as defined by the cutting plane passing through the weld center and crossing most severe visible surface cracks, described by DiGiovanni et al. [117]. The cross-sectioned welds were then hot mounted and polished using standard metallographic methods to a 1 μm diamond final polish. Crack locations were described as per Choi et al.'s [5] classification which divided cracked regions to the electrode indent of the weld (Type A), the weld shoulder extending to the edge of the heat affected zone (Type B), and the weld notch (Type C). The quantity and length of Type A, B, and C cracks (see Figure 3.7) were measured on the unetched surface using a Carl ZEISS Axio Vert. A1 optical microscope at magnifications of 200x. The crack length was defined as the straight-line distance from the cracking opening at interface of the zinc coating and the steel substrate side to the crack tip. The LME cracking severity was characterized as per crack index (CI), developed by Wintjes et al. [118], see Equation 6 below.

$$CI = \frac{nL}{t_s} \quad (\text{Eq.6})$$

Where n is the number of measured cracks per weld, L is the lognormal median crack length, and t_s is the sheet thickness. The crack index can be thought as a ratio of the total length of cracks in the weld cross-section to the sheet thickness and explains the strength loss caused by LME cracks. The error bars shown on graphs indicate the 95 % confidence interval of the data.

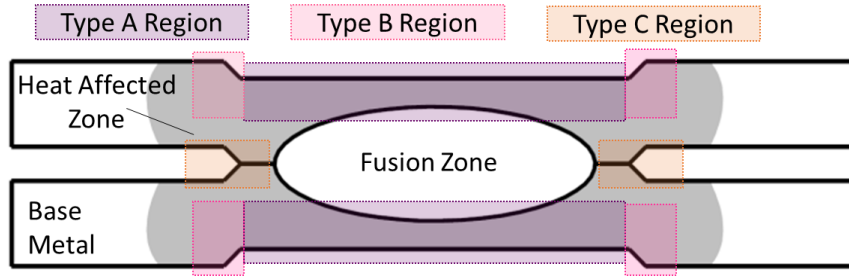


Figure 3.7: Schematic of different types of LME cracks viewing from the weld cross-section

3.7 ANOVA Analysis and Optimized Predictive Model on LME Cracking

The analysis of variance (ANOVA), and linear regression modelling were done using Minitab software with a 0.05 significant level to theoretically evaluate the influence of electrode force, heat input, heat loss and expulsion occurrence (input variables) on the LME cracking severity (output variable) of the resistance spot welded low-LME sensitive material based on the obtained experimental data. Several metrics such as outlier removal, forwards selection, backwards elimination and stepwise selection were adopted to improve the predictivity of the original model. In addition, LME-free and LME-cracking welds were separately analyzed to study the distribution of LME-free welds amongst various heat input levels and their influence on the model predictivity for LME cracking severity as a function of input variables. The crack index, developed by Wintjes et al. [118], comprehensively evaluates the severity of LME cracking and correlated to the loss of weld strength, consisting the number of crack length, lognormal median of crack length and the sheet thickness. On the other hand, the contribution of multiple factors in the calculation of crack index makes the data distribution of crack index complicated to be defined. Wintjes et al. [118] manifested that the data of crack length were better described by the lognormal distribution than the normal distribution by graphing and comparing the confidence (error bar) of the lognormal mean and lognormal median to the arithmetic mean of the crack length data. The results showed lognormal mean and lognormal median had higher confidence than the arithmetic mean, and the lognormal distribution was much more accurate to describe the population of the crack length data. Therefore, the lognormal crack length was selected for the statistical analysis. The analysis of ANOVA and linear regression modelling assume that the data are normally distributed. If the data are non-normally distributed, then the accuracy of the analysis will decrease. The bootstrap method was used to develop a normally distributed data set for Lognormal crack length named the geometric mean crack length as the indicator of LME cracking severity instead of Total crack index. The bootstrap method ran with 1000 resampling processes and the final data set consisted with 45 data points including 9 welding conditions and 5 repetitions for each condition. The low ($I_{\max}-10\%$), moderate ($I_{\max}-200\text{ A}$) and high ($I_{\max}+10\%$) heat input was coded as -1, 0 and +1 heat input, respectively.

Chapter 4 Influence of Electrode Force on LME during RSW of 3G-AHSS Steels

In this chapter, a comparative study of the influence of electrode force on LME cracking severity during resistance spot welding of low-LME sensitive and high-LME sensitive materials is reported to fill the existed gap in the literature noted in chapter 2. Welds were made with various electrode forces at low, moderate and high heat input levels while the achieved nugget diameter of the welds at each individual heat input level was kept constant by altering welding current. The applied welding schedule and parameters for the RSW process and equations used in this work are listed in chapter 3. Then, the LME cracking severity was measured and compared between and within different heat input levels. Multiple processing factors were experimentally examined and correlated with the LME cracking severity for the low-sensitive material. The statistical analysis was carried out to compare with the experimental observations in the significance of those factors affecting LME cracking severity. The factors that most influenced LME for the low-LME sensitive material were then examined in the context of LME cracking in welds made in the high-LME sensitive material to fully understand how force influence LME cracking in steels.

4.1 Correlation of LME Cracking Severity with Electrode Force Variation

In the present study, welds were made at the low ($I_{\max}-10\%$), moderate ($I_{\max}-200\text{ A}$) and high ($I_{\max}+10\%$) heat input, and the corresponding LME severity of the welds were measured using the crack index (see Figure 4.1 for low-LME sensitive material and Figure 4.2 for high-LME sensitive material and crack distribution (see Figure 4.3 and Figure 4.4 for low and high LME sensitive material, respectively) are interpreted as follows.

For low-LME sensitive material, at low heat input, only a few numbers of Type B cracks in a group of 5 welds were observed (0 cracks, 5 cracks, and 1 crack at 4.4 kN, 4.9 kN, 5.4 kN of electrode force condition, respectively, see Figure 4.1 and Figure 4.3a, b and c). In addition, the absolute maximum crack length per 5 welds was relatively small (0 μm , 76.6 μm and 12.8 μm at 4.4 kN, 4.9 kN, 5.4 kN electrode force condition, respectively, see Figure 4.3a, b and c). At moderate heat input, both Type A and Type B cracks were observed (see Figure 4.1). As the electrode force increases, the number of Type A cracks per 5 welds decreased but the absolute maximum crack length increased (7 cracks and 46.7 μm in welds made with the electrode force of 4.4 kN, 4 cracks and 82.8 μm in welds made with the electrode force of 5.4 kN, see Figure 4.3d, e and f). Both the number and absolute maximum crack length of Type B cracks per 5 welds increased when the electrode force increased from 4.4 kN to 4.9 kN (11 cracks and 19.1 μm at the 4.4 kN electrode force condition, 22 cracks and 50.7 μm at the 4.9 kN electrode force condition, see Figure 4.3 d and e). When the electrode force further increased to 5.4 kN, both the number and the absolute maximum crack

length per 5 welds of Type B cracks decreased to 1 crack with the crack length of 17.1 μm , see Figure 4.3f. At high heat input, only a few number of Type A cracks were observed at the 4.4 kN and 5.4 kN electrode force conditions and the absolute maximum crack length of Type A cracks per 5 welds was larger at the electrode force of 4.4 kN condition, see Figure 4.3g, h and i (3 cracks and 210.5 μm in the welds made with the electrode force of 4.4 kN, 8 cracks and 150.2 μm in the welds made with 5.4 kN of electrode force). As regards to Type B cracks, as the electrode force increased from 4.4 kN to 4.9 kN, the number of Type B cracks per 5 welds significantly increased also accompanying with the increase of the absolute maximum crack length, see Figure 4.3g and h (6 cracks and 22.6 μm at the electrode force of 4.4 kN condition, 47 cracks and 98.1 μm at the electrode force of 4.9 kN condition). However, both the number and the absolute maximum crack length of Type B cracks per 5 welds drastically decreased to 3 cracks and 15.3 μm when the electrode force further increased to 5.4 kN, see Figure 4.3i. It can be seen, at all heat input levels for the low-LME sensitive material, the increase of electrode force did not linearly result in LME reduction contradicting to the literature [5,6,8,10].

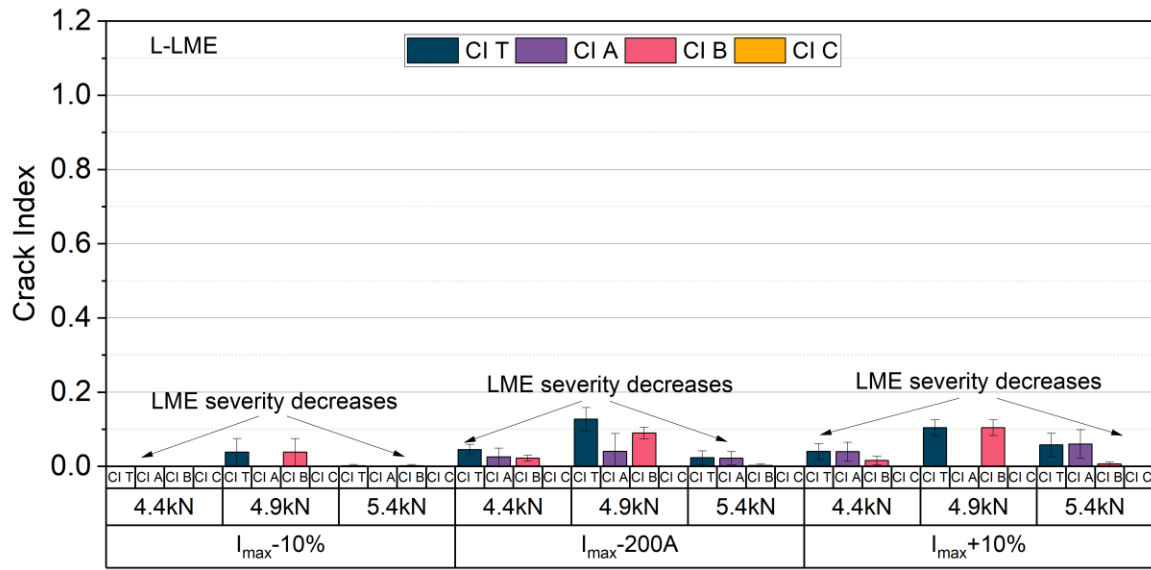


Figure 4.1: Crack Index of Total and Individual Type crack at various heat input levels with electrode force variation for low-LME sensitive material

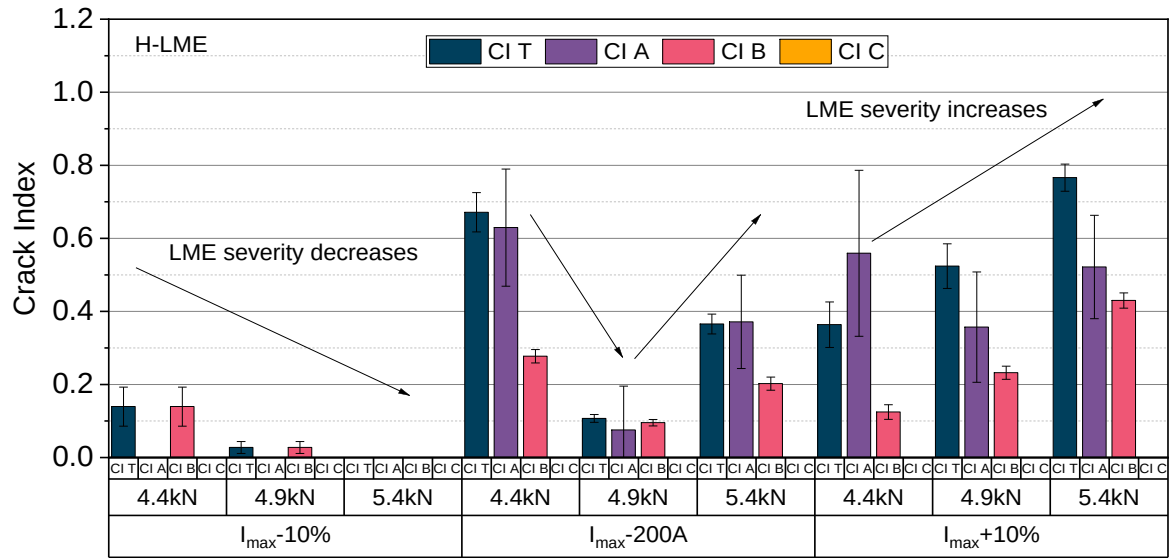


Figure 4.2: Crack Index of Total and Individual Type crack at various heat input levels with electrode force variation for high-LME sensitive material

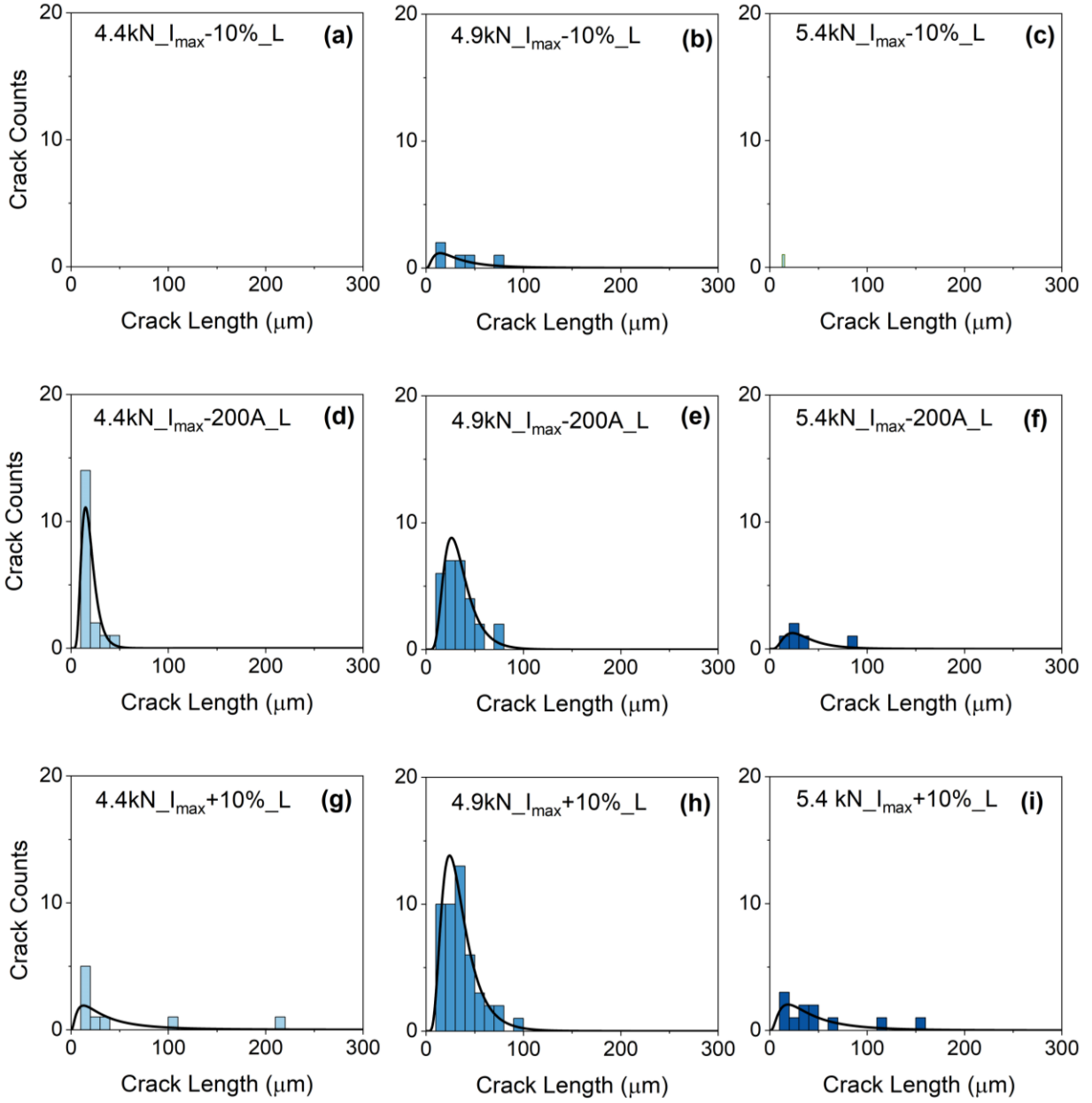


Figure 4.3: Crack distribution of low-LME sensitive material with electrode force variation at (a)(b)(c) low; (d)(e)(f) moderate and (g)(h)(i) high heat input

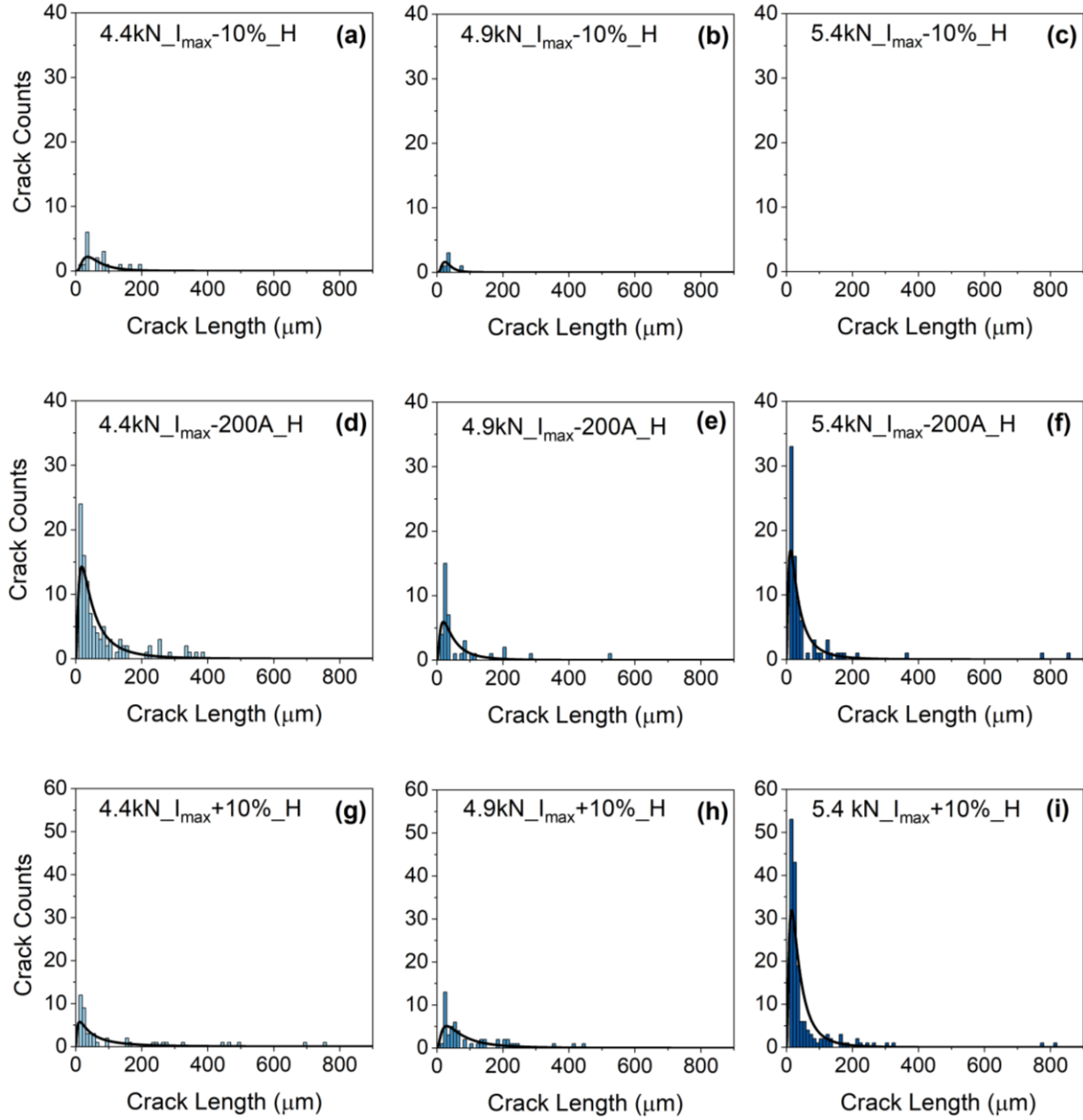


Figure 4.4: Crack distribution of high-LME sensitive material with electrode force variation at (a)(b)(c) low; (d)(e)(f) moderate and (g)(h)(i) high heat input

For high-LME sensitive material, at low heat input level, only Type B cracks were detected (see Figure 4.2) and the increase of electrode force from 4.4 kN to 5.4 kN led to a reduction of both the number and the absolute maximum length of Type B cracks per 5 welds (see Figure 4.4a, b and c), which agrees with the literature [5,6]. At moderate heat input level, both Type A and Type B cracks were observed (see Figure 4.2). At low electrode force (4.4 kN), there were many Type A cracks per 5 welds (32 cracks, see Figure 4.4d). As the electrode force increased to the moderate level (4.9 kN), the number of Type A cracks per 5 welds significantly decreased to 1 crack, see Figure 4.4e. A further increase of electrode force to 5.4 kN

increased the number of Type A cracks per 5 welds to 15 cracks, see Figure 4.4f. With respect to the absolute maximum crack length per 5 welds of Type A cracks, it linearly increased with the increase of electrode force from 389.6 μm at the electrode force of 4.4 kN condition to 854.2 μm at the 5.4 kN electrode force condition, see Figure 4.4d, e and f. For Type B cracks, the low electrode force (4.4 kN) condition resulted in the largest number of Type B cracks per 5 welds with relatively large absolute maximum crack length (69 cracks and 250.1 μm , see Figure 4.4d). As the electrode force increased to 4.9 kN, the number of Type B cracks per 5 welds decreased to 37 cracks but the absolute maximum crack length increased to 284.4 μm , see Figure 4.4e. When the electrode force further increased to 5.4 kN, welds experienced similar number of Type B cracks (66 cracks per 5 welds) compared with the low electrode force condition (4.4 kN) but the absolute maximum crack length of Type B cracks significant decreased to 93.1 μm , see Figure 4.4f. At high heat input, the number of Type A cracks per 5 welds increased with the increase of electrode force from 14 cracks at 4.4 kN electrode force condition to 31 cracks at 5.4 kN electrode force condition, see Figure 4.4g, h and i. The absolute maximum crack length was considerable at high heat input per 5 welds and it increased with the increase of electrode force (4.4 kN to 5.4 kN) from 755.3 μm to 818.6 μm , see Figure 4.4g, h and i. As the increase of electrode force (4.4 kN to 5.4 kN), both the number and the absolute maximum crack length of Type B cracks per 5 welds increased from 32 cracks and 159.6 μm to 129 cracks and 165.9 μm , see Figure 4.4g, h and i.

From the crack distribution of the low and high LME sensitive materials, the severity of LME cracking could not be singularly drawn from either the number of cracks or the absolute maximum crack length at higher heat input levels, since the two metrics had different results in the severity of LME cracking (see Figures 4.3 and 4.4). Also, the appearance of multiple types of LME cracks complicated the indication of total LME cracking severity and the individual evaluation metric such as the number of cracks and the absolute maximum crack length did not satisfy the needs for an explicit conclusion. Therefore, the crack index was calculated from the measured cracks to quantify LME cracking severity of welds at each welding condition (see Figures 4.1 and 4.2). For Low-LME sensitive material (see Figure 4.1), at low heat input, Total crack index and Type B crack index were identical since only Type B cracks were observed at low heat input. The LME cracking severity increased from 4.4 kN to 4.9 kN electrode force and then decreased when electrode force was increased from 4.9 kN to 5.4 kN. At moderate heat input, both Total and Type B cracking severity increased from the electrode force level of 4.4 kN to 4.9 kN and then decreased when electrode force was increased to 5.4 kN. In addition, there was no significant change of Type A cracking severity when electrode force was varied. At high heat input, both Total and Type B cracking severity increased from the electrode force level of 4.4 kN to 4.9 kN and then decreased with the electrode force of 5.4 kN. Furthermore, Type A cracks were observed when welding with electrode forces of 4.4 kN to 5.4 kN and welds did not suffer Type A LME cracking at the 4.9 kN electrode force condition. For high-LME

sensitive material (see Figure 4.2), at low heat input, only Type B cracks were observed, so the Total crack index and the crack index of Type B cracks were equal. The LME cracking severity, as measured by crack index, decreased with the increase of electrode force. At moderate heat input, there was no correlation between the electrode force level and the LME cracking severity. However, the largest Total crack index, as well as contributions from Type A and Type B cracks were seen at the lowest electrode force (4.4 kN) level and decreased when electrode force was increased from 4.4 kN to 4.9 kN; LME cracking severity then further increased from the electrode force level of 4.9 kN to 5.4 kN. At high heat input, there was no significant effect of electrode force on Type A cracking severity. However, both Total and Type B crack indexes increased linearly with the increase of electrode force from 4.4 kN to 5.4 kN.

From the present observations of LME cracking severity for welds in the low-LME sensitive material, there was not a linear correlation between electrode force and LME cracking severity. It should be noted though that the crack index, measured for all of the welds made in the low-LME sensitive material, was less than 0.15 at all heat input levels. The low cracking index shows that this material had a general resistance to LME cracking. Regarding the high-LME sensitive material, at low heat input, increasing electrode force decreased LME cracking, which agrees with the literature. However at moderate and high heat input levels, the increase of electrode force did not cause a linear reduction of LME cracking severity, which contradicts the literature [5,6,8,10]. It was also noticed that, at high heat input, LME cracking severity increased with the increase of electrode force for the high-LME sensitive material.

4.2 Influence of Electrode Force Variation on the Low-LME Sensitive Material

In this section, the influence of electrode force on LME cracking severity was examined while accounting for nugget shape, nugget height, heat input, heat loss, relative heat loss and expulsion events from the experimental observation. These results were analysed using statistical methods in Section 4.3. The understanding of the factors influencing LME cracking severity as electrode force is varied, will be used to analyse the LME cracking severity for high-LME sensitive material in Section 4.4.

4.2.1 Correlation of Nugget Shape and Nugget Height with Electrode Force Variation

The micrographs of cross-section of the welds made with various electrode forces are depicted in the Figure 4.5. There were no significant changes of nugget shape with electrode force variation. From Figure 4.6, the average nugget height of welds made with the 5.4 kN electrode force condition was observed a significant reduction than other two electrode force conditions at same heat input level.

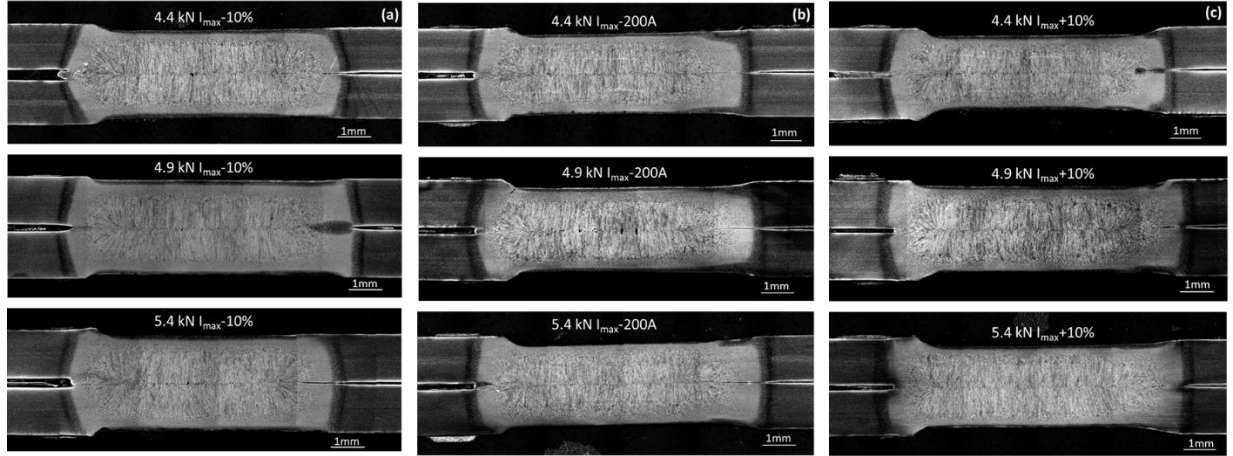


Figure 4.5: Nugget shape observations from weld cross sectional view of low-LME sensitive material at (a) low heat input; (b) moderate heat input and (c) high heat input

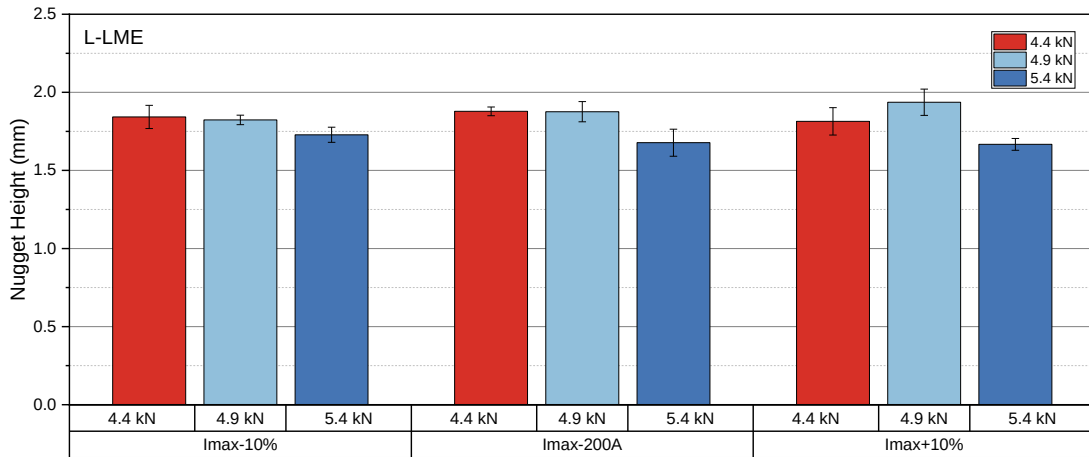


Figure 4.6: Measurements of nugget height for the low-LME sensitive material

4.2.2 Correlation of Melting Heat and Heat Loss (heat extraction) with Electrode Force Variation

The influence of electrode force on melting heat (measured by Eq. 4) and heat loss (measured by Eq. 3) by electrode force variation can be seen in Figure 4.7. The amount of average heat loss at each welding condition is shown by the height of reddish bar on the plot and the height of blue colored area above is the amount of melting heat. The corresponding amount of heat loss and melting heat can be read from the y-axis. There was no significant change of melting heat among varied electrode force conditions at each heat input regime, which indicated comparable nugget volumes. In terms of heat loss, at low heat input, the electrode force variation did not introduce a significant change of heat loss. At moderate and high heat input

levels, largest amount of heat was extracted from the welds with the electrode force of 5.4 kN than the conditions of 4.4 kN and 4.9 kN electrode force.

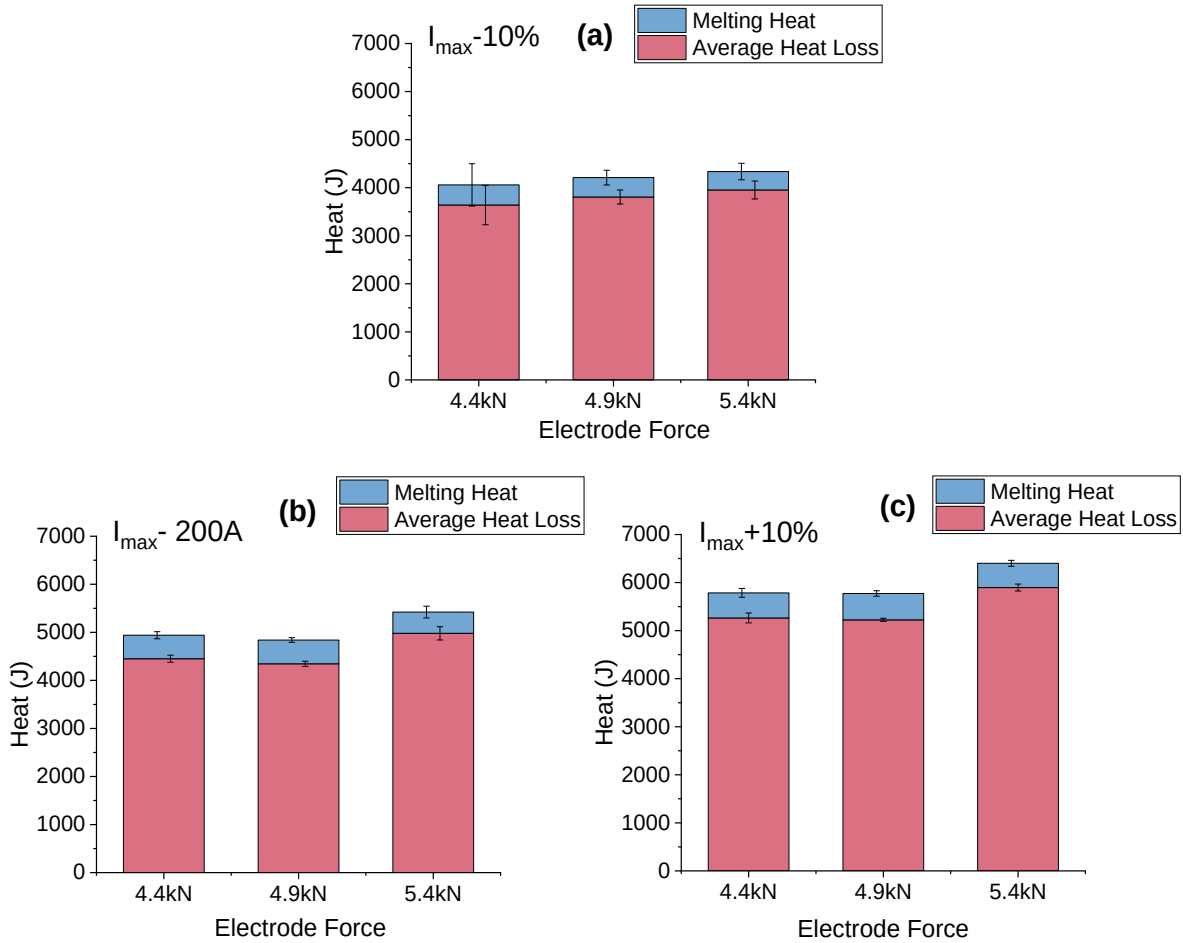


Figure 4.7: Change of melting heat and heat loss with electrode force variation at (a) low heat input; (b) moderate heat input and (c) high heat input for low-LME sensitive material

4.2.3 Correlation of Heat Input and LME Cracking Severity

The interaction effects of heat input and electrode force on LME cracking severity for the investigated low-LME sensitive material is shown in Figure 4.8. At low heat input, LME cracking did not show significant response from the increase of electrode force and an overall minor LME cracking was observed in this regime. In addition, at moderate and high heat inputs, the increase of electrode force from low to moderate level increased LME cracking severity and then decreased when the highest electrode force was applied, but LME cracking severity in all cases remained low. The effect of heat input and electrode force on the investigated low-LME sensitive material agreed with the observations from Meschut et al.'s study [9] that

a low-LME sensitive material showed a strong resistance to LME cracking and there was a lack of LME response with electrode force variation.

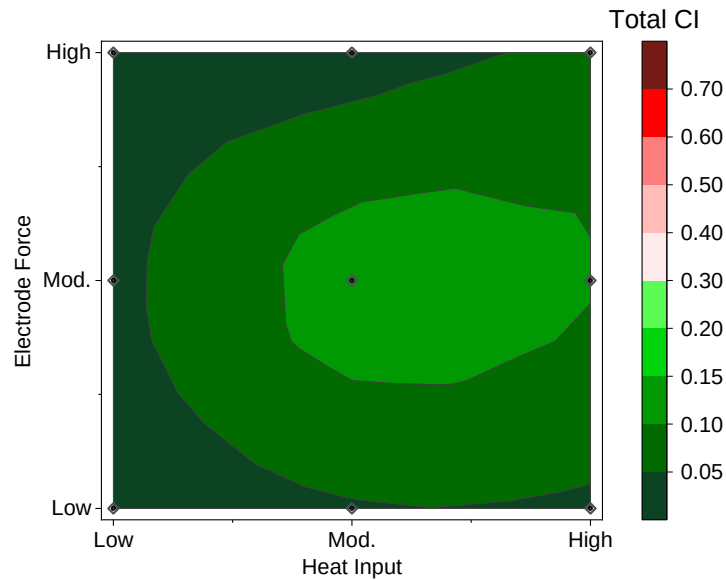


Figure 4.8: Effects of electrode force on LME cracking at various heat input levels for low-LME sensitive material

4.2.4 Correlation of Heat Loss and Relative Heat Loss with LME cracking

The correlation of heat loss and LME cracking severity is exhibited in Figure 4.9. At all three heat input levels, LME cracking severity did not directly correlate with the change of heat loss. At low heat input, the heat loss at various electrode force levels was comparable and so the LME cracking severity was. At moderate and high heat inputs, the heat loss was the highest at the highest electrode force level but the LME cracking severity was not the largest. However, the increase of heat loss was observed with the increase of heat input. So, if absolute heat loss is analyzed as a factor contributing to LME, it will be correlated to the heat input. Therefore, the relative heat loss (the absolute heat loss), defined as the ratio of heat loss to heat input, was compared with the LME cracking severity for each welding condition, see Figure 4.10. At low heat input, there was no significant change of relative heat loss with electrode force variation and no correlation between relative heat loss and LME cracking severity was observed (see Figure 4.9a). At moderate and high heat inputs, the largest relative heat loss was observed in the welds made with 5.4 kN electrode force but the LME cracking severity was not the least at this condition (see Figure 4.9b and c). It can be seen that there was no direct relationship between the relative heat loss and the LME cracking severity at various heat input regimes.

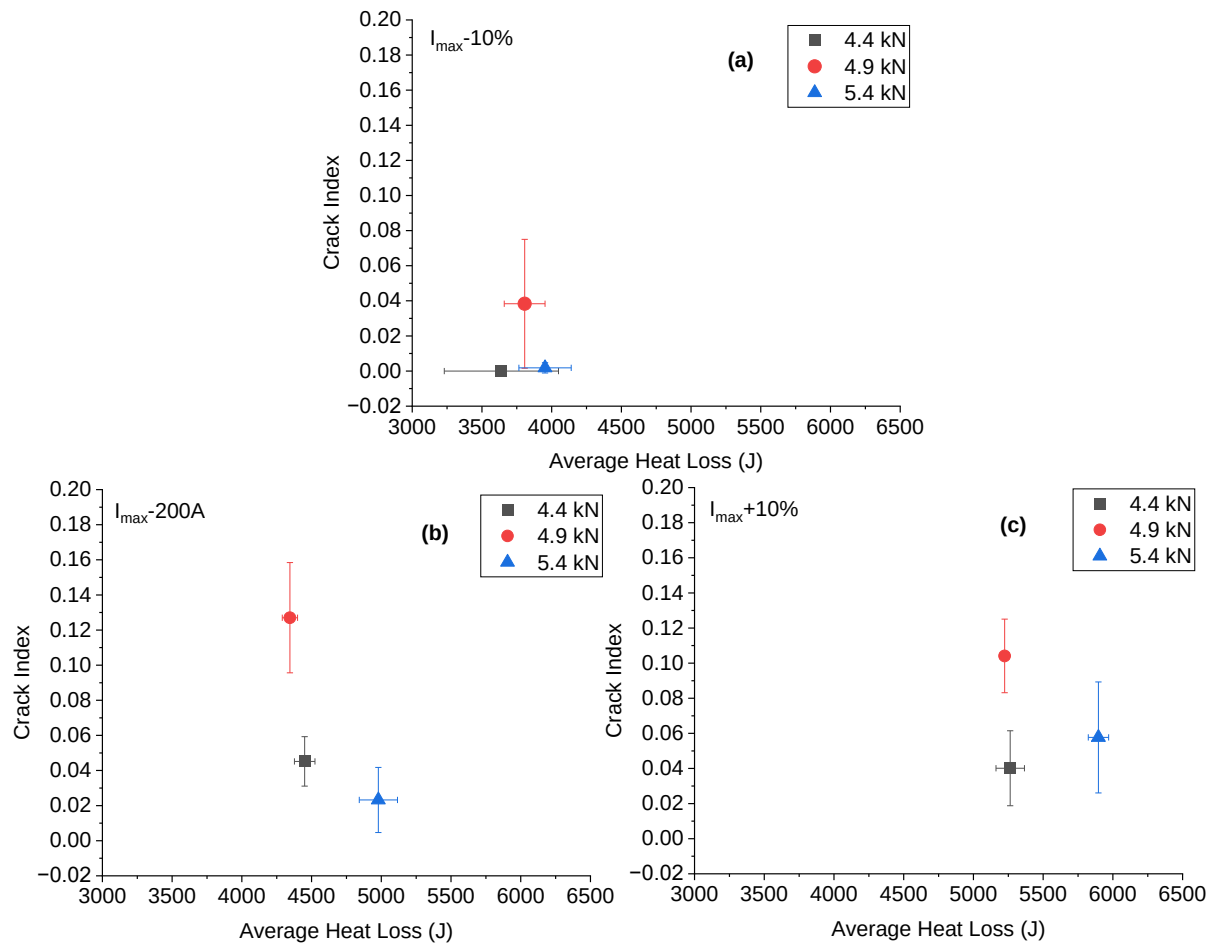


Figure 4.9: Correlation of heat loss and total crack index at (a) low heat input; (b) moderate heat input and (c) high heat input

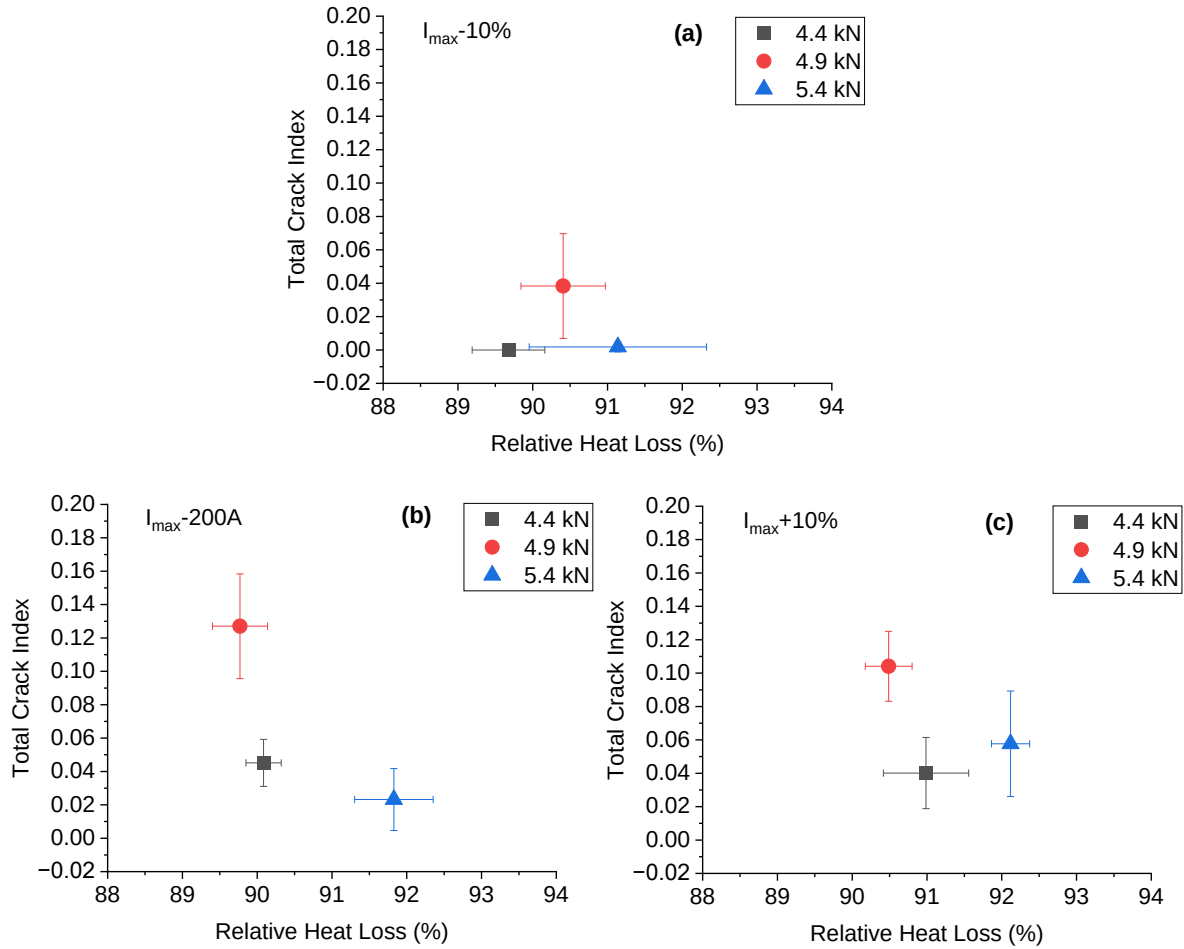


Figure 4.10: Correlation of relative heat loss and total crack index at (a) low heat input; (b) moderate heat input and (c) high heat input

4.2.5 Correlation of Expulsion and LME Cracking

Preceding research shows that welding with expulsion altered welding condition and welds experiencing expulsion had a high likelihood of experiencing LME, leading to an assumed connection between expulsion and LME cracking [74,75]. During the welding process of this study, expulsion was observed at some welding conditions. A hypothesis was made that the expulsion event might have influence on LME cracking severity. Therefore, the number of expulsion occurrence in a group of five welds generated at each welding condition was counted and correlated to the LME cracking severity as shown in Figure 4.11. LME cracking did not linearly increase with the increase of the number of expulsion but the no expulsion condition showed less LME cracking severity than the ones with expulsion (i.e. welding with electrode force of 4.4 kN and low heat input, Figure 4.11a). When higher heat input was applied in the welding process, as seen in Figure 4.11b and c, a larger number of expulsion events occurred and more severe LME cracking was observed.

For example, three, four and five numbers of expulsion were observed at moderate heat input; five and seven numbers of expulsion were observed at high heat input.

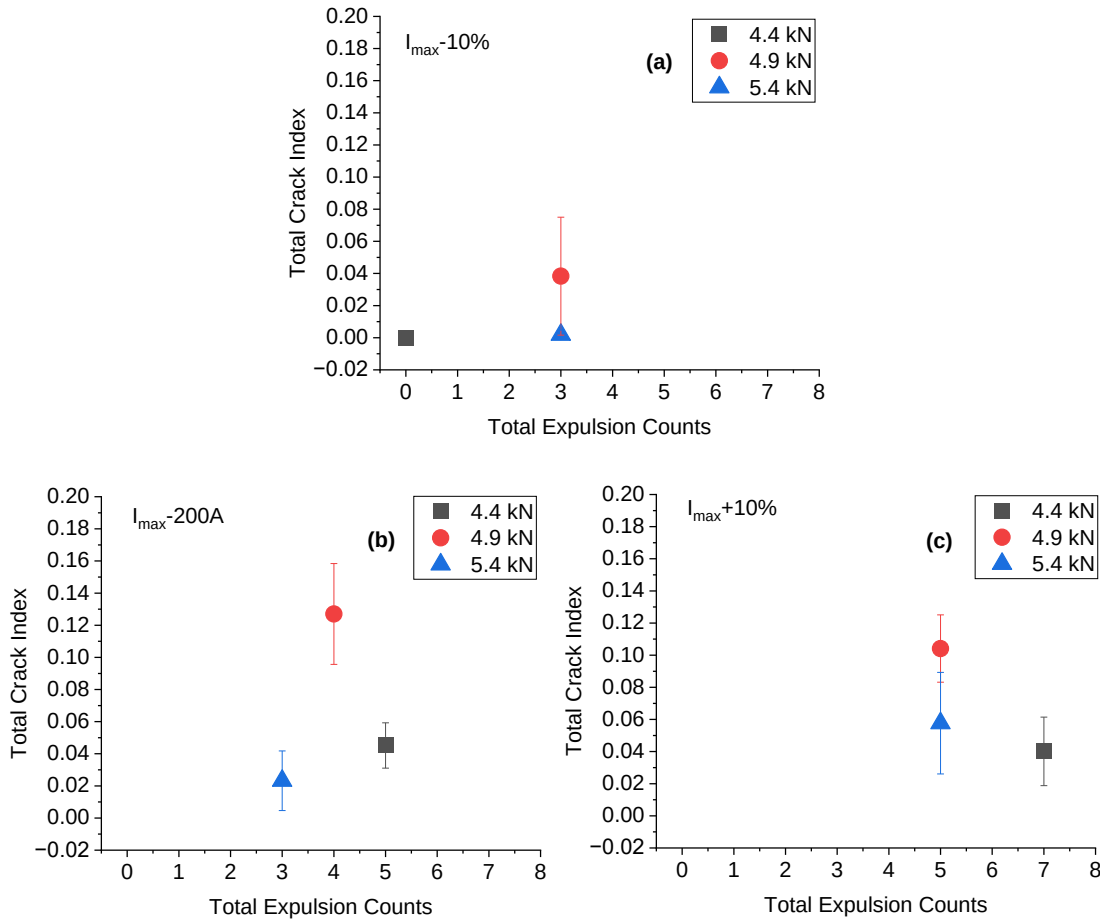


Figure 4.11: Correlation of expulsion counts with LME cracking severity for low-LME sensitive material at (a) low; (b) moderate and (c) high heat input

4.3 Influence of Welding Parameters and Conditions on LME Cracks

The purpose of this statistical analysis is to study the effects of the following factors on the LME cracking severity, as measured in-terms of representative crack length (log normal median or as referred to in the text, geo mean), of the low-LME sensitive material: electrode force, heat input, heat loss, and expulsion event. The analysis was done using the ANOVA analysis and the linear regression modelling. When conducting the two statistical techniques, it is important that the dataset being tested is normally distributed and has homogeneous variance, since they are the fundamental assumptions for the two statistical techniques. The details will be explained as follows.

4.3.1 Tests for Normality and Homogeneity of Variance

In statistics, the residual of a variable is the difference between the observed and the predicted value. The normality of residuals indicates that the dataset of the variable is normally distributed. In addition, both the ANOVA analysis and the linear regression method assume that the testing dataset follows a normal distribution and has homogeneous variances. Homogeneous variance means the variance of the residuals is constant across all groups of dataset. Normal distributed dataset with homogeneous variance ensures the precision of the analysis results regarding the estimation and significance (i.e. unbiased estimates of the regression coefficients, correct p-values and confident interval). Anderson-Darling and Ryan-Joiner tests were applied to test the normality of residuals. The Anderson-Darling test is powerful in detecting the departures of normality in tails of the data distribution but is sensitive to the outliers. The outliers are often subject to occur on the tails of a distribution and can lead to inaccurate estimation of the data distribution using the Anderson-Darling test. The Ryan-Joiner test addresses the sensitivity of outliers in the Anderson-Darling test and it is robust in evaluating the normality of the data distribution if the outliers exist. The dataset is composed of 45 data points and an individual data point is corresponding to the geo mean crack length of an investigated weld obtained from the experiment. As previously mentioned, there were total 9 welding conditions and 5 welds were produced for each welding condition. The null hypothesis for the Anderson-Darling test is that the residuals of the data points are normally distributed and the level of significance for this test was selected to be 0.05. The null hypothesis for the Ryan-Joiner test is that the residuals of given dataset follow normal distribution. The 0.05 level of significance was selected for the Ryan-Joiner test. In Figure 4.12a, the p-value of Anderson-Darling test is 0.095 (greater than 0.05), which the Anderson-Darling test does not reject the null hypothesis. Therefore, the residuals of dataset were approved to be normally distributed by the Anderson-Darling test. Furthermore, the null hypothesis for Ryan-Joiner test is not rejected, demonstrating by the p-value greater than 0.1 (greater than the 0.05 level of significance), see Figure 4.12b. Therefore, the normality of dataset for Geo Mean crack length is established.

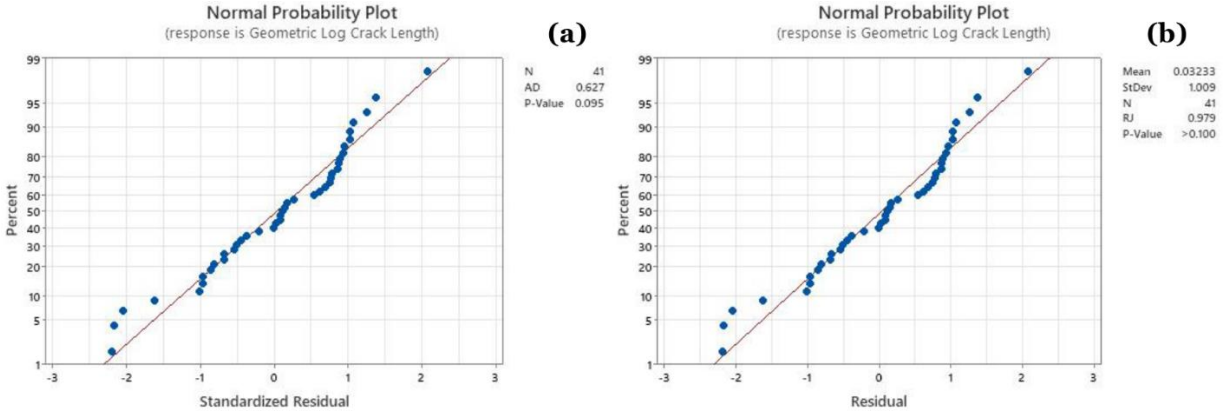


Figure 4.12: Residual normality tests of geometric mean crack length: (a) Anderson-Darling and (b) Ryan-Joiner

Levene’s test was used to verify the homogeneity of the data variances. The null hypothesis for the Levene’s test is that the variances of the data are equal. Its significance level was set to be 0.05. Table 4.1 shows the p-value for the Levene’s test is 0.385 (greater than 0.05) and the null hypothesis is not rejected. Therefore, the condition for homogeneity of the data variance was met. Furthermore, the dataset also has similar means, examined by the multiple comparisons test. The null hypothesis for the multiple comparisons test is that there is no significant difference of the data means and the level of significance for the test is 0.05. As shown in Table 4.1, the p-value for the multiple comparisons test is 0.783 (greater than 0.05) and the null hypothesis is not rejected.

Table 4.1: Summary of tests for homogeneity of variance (Levene’s test) and similar means (multiple comparison test)

Tests		
Method	Test	
	Statistic	P-Value
Multiple comparisons	—	0.783
Levene	0.98	0.385

4.3.2 Influence of Processing Factors on Geo Mean Crack Length

The experimental work, conducted on the low-LME sensitive material, indicated that the increase of electrode force did not reduce LME cracking severity, which conflicted to the observations from the literature [6–8,10]. It is believed that the processing factor changed owing to the electrode force variation and affected LME cracking severity. Before any statistical analysis was proceeded, the influence of these processing factors was visualized by graphing them against the geometric mean crack length. The correlation of geometric mean crack length with the change of electrode force, heat input, heat loss and

expulsion condition was depicted by the main effects plot, see Figure 4.13. The main effects plot uses the mean value of the data (geometric mean crack lengths) to provide an average response for each level of a factor. With the increase of electrode force from 4.4 kN to 4.9 kN, the geometric mean crack length increased and then decreased when the electrode force was further increased to 5.4 kN. In addition, the implementation of higher heat input and occurrence of expulsion during the welding cycle resulted in greater geometric crack length. The relationship between geometric mean crack length and heat loss was not directly seen from the main effects plot. Then, the heat loss and geometric mean crack length were replotted in a contour map with respect to the heat input, see Figure 4.14. From the contour map, at low heat input, there was no significant change of heat loss with the increase of electrode force, but, at the higher heat input levels, the increase of electrode force caused the increase of heat loss. Furthermore, as can be seen in Figure 4.15, large geometric mean crack length was observed at high heat loss which illustrated that heat loss did not always mitigate LME cracking. The sequence of statistical methods will performed as follows to establish an optimal model to correlate geometric mean crack length with electrode force, heat input, heat loss and expulsion condition.

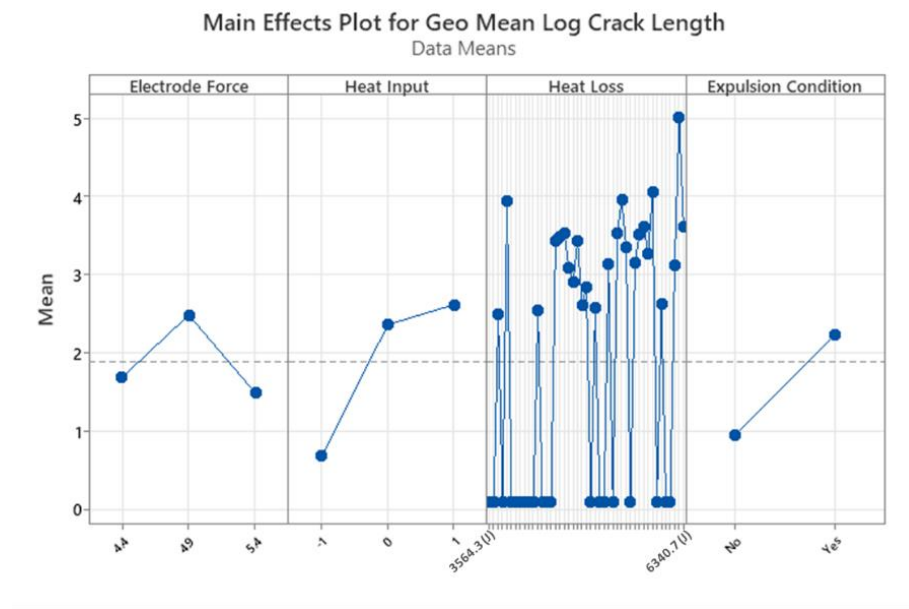


Figure 4.13: Main effect plots correlating geometric mean crack length with electrode force, heat input, heat loss, and expulsion condition

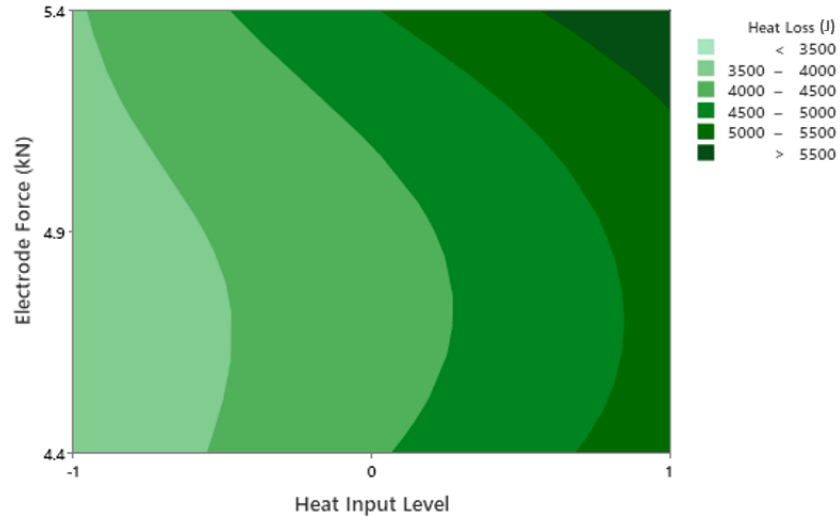


Figure 4.14: Contour plot to show the Interactions of electrode force, heat input, and heat loss

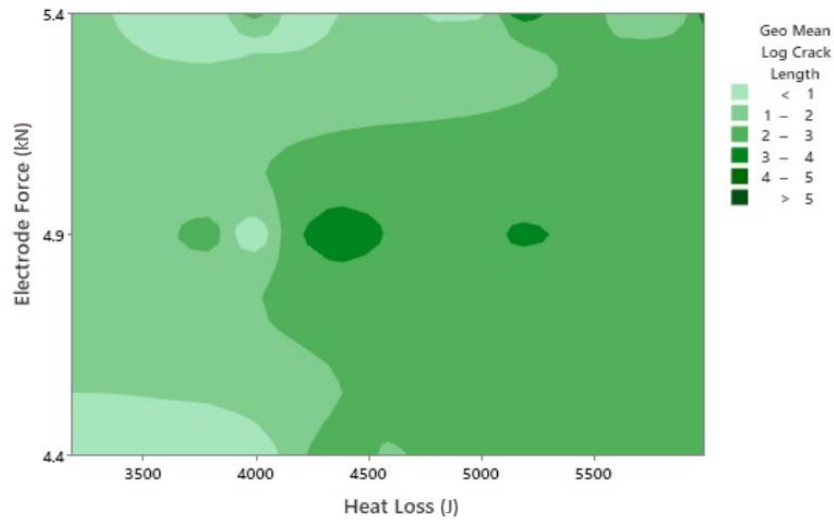


Figure 4.15: Contour plot to show the interactions of electrode force, heat loss and geometric mean crack length

4.3.3 Results of Initial ANOVA Analysis

The outcomes of the linear regression model, considering the factors of electrode force, heat input, heat loss, expulsion condition and all their interaction terms, are listed in Table 4.2. The R^2 value in this analysis is only 41.2 % and the adjusted R^2 is even lower at 13.7 %. This low R^2 value of 41.2 % indicates that the model is only capable of explaining approximately half of the observed variation in the response using the factors considered. The large disparity between the R^2 and the adjusted R^2 value also points to overfitting of the model.

Table 4.2: Results of first ANOVA test

Model Summary

S	R-sq	R-sq(adj)	R-sq(pred)
1.54993	41.20%	13.77%	*

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	14	50.506	3.6076	1.50	0.170
Heat Input	1	1.430	1.4299	0.60	0.446
Heat Loss	1	4.827	4.8269	2.01	0.167
Electrode Force	1	1.483	1.4827	0.62	0.438
Expulsion Condition	1	0.994	0.9939	0.41	0.525
Heat Input*Heat Loss	1	5.544	5.5444	2.31	0.139
Heat Input*Electrode Force	1	4.194	4.1937	1.75	0.196
Heat Loss*Electrode Force	1	1.058	1.0579	0.44	0.512
Heat Input*Expulsion Condition	1	1.150	1.1496	0.48	0.494
Heat Loss*Expulsion Condition	1	7.784	7.7842	3.24	0.082
Electrode Force*Expulsion Condition	1	0.285	0.2855	0.12	0.733
Heat Input*Heat Loss*Electrode Force	1	0.327	0.3268	0.14	0.715
Heat Input*Heat Loss*Expulsion Condition	1	5.236	5.2363	2.18	0.150
Heat Input*Electrode Force*Expulsion Condition	1	3.997	3.9970	1.66	0.207
Heat Loss*Electrode Force*Expulsion Condition	1	3.419	3.4192	1.42	0.242
Error	30	72.068	2.4023		
Total	44	122.574			

Table 4.3 shows the variance inflation factor values (VIF) for the coded coefficients used in the linear regression modelling. A VIF value greater than 5 indicates multicollinearity. Heat input and heat loss have VIF values of 13.11 and 14.21, respectively, indicating that these two variables are highly correlated to one another. Multicollinearity in the model is problematic since it makes it difficult to estimate the effect that each of the independent variables have on the geometric mean crack length if they are very closely related. In Figure 4.16, this multicollinearity issue is indicated by a pearson correlation coefficient of 0.926 (a strong positive correlation) between the heat input and the heat loss. To minimize the multicollinearity effect and improve the predictivity of the linear regression model, the relative heat loss (absolute heat loss) was introduced. Table 4.4 shows a significant decrease of variance inflation factor value (multicollinearity effect).

Table 4.3: Variance inflation factor values

Term	Coef	SE Coef	T-Value	P-Value	VIF
Constant	1.511	0.499	3.03	0.004	
Heat Input	1.729	0.814	2.12	0.040	13.11
Heat Loss	-1.142	0.848	-1.35	0.185	14.21
Electrode Force	0.216	0.322	0.67	0.507	2.05
Expulsion Condition					
Yes	0.510	0.609	0.84	0.407	1.47

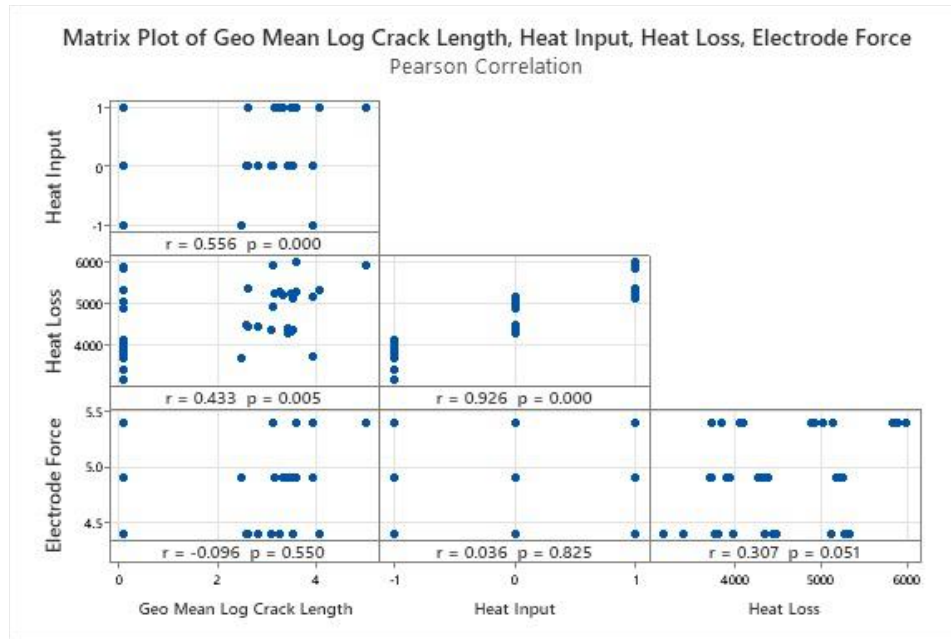


Figure 4.16: Pearson correlation of electrode force, heat loss and geometric mean crack length

Table 4.4: Variance inflation factor values with updated relative heat loss term

Term	Coef	SE Coef	T-Value	P-Value	VIF
Constant	1.472	0.512	2.87	0.006	
Heat Input	0.781	0.287	2.72	0.010	1.59
Relative Heat Loss	-0.340	0.345	-0.99	0.329	2.30
Electrode Force	0.122	0.316	0.39	0.701	1.94
Expulsion Condition Yes	0.563	0.627	0.90	0.375	1.53

To further improve the accuracy of the model, a Grubbs' test is conducted on the data to identify any outliers. The outliers are the data points that fall outside of a typical data range, which could be caused from measurement error or extreme values. The existence of outliers can significantly influence on the accuracy of the conclusion drawn from a statistical analysis and it is necessary to omit the outliers. 4 data points are identified as outliers (Figure 4.17) across various electrode force and heat input levels, and these points were eliminated in the subsequent analysis.

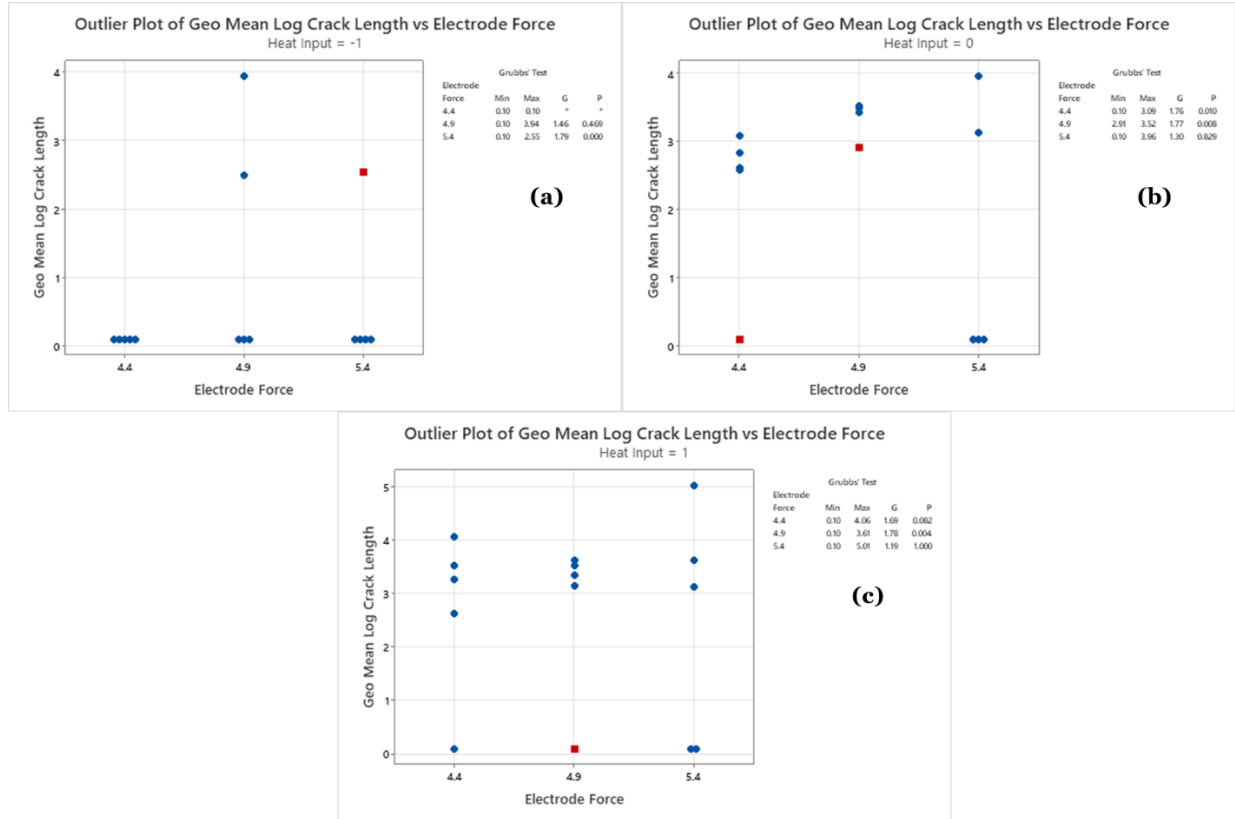


Figure 4.17: Results of Grubbs' Outlier Test: (a) low heat input data, (b) moderate heat input data and (c) high heat input data

The ANOVA analysis was redone after elimination of outliers and the substitution of the relative heat loss term for the heat loss term. The results for the updated ANOVA test are summarized in Table 4.5. The R^2 and the adjusted R^2 increased to be 82.8 % and 70.1 %, respectively. To compare with the initial model with 41.2 % of the R^2 and 13.8 % of the adjusted R^2 , the updated model shows that the model is better able predict the influence of the variables. However, the R^2 predictive value is unavailable, indicating that the model may be overfit. Hence, several optimization methods were adopted to eliminate the statistically insignificant terms and improve the accuracy of the predictive model.

Table 4.5: Results of updated ANOVA analysis with outlier removal and the substitution of relative heat loss

Model Summary					
	S	R-sq	R-sq(adj)	R-sq(pred)	
	0.925415	82.82%	70.12%	*	
Source					P-Value
Regression					0.000
Heat Input					0.926
Electrode Force					0.766
Relative Heat Loss					0.483
Expulsion Condition					0.891
Heat Input*Heat Input					0.502
Electrode Force*Electrode Force					0.093
Relative Heat Loss*Relative Heat Loss					0.001
Heat Input*Electrode Force					0.936
Heat Input*Relative Heat Loss					0.610
Electrode Force*Relative Heat Loss					0.955
Heat Input*Expulsion Condition					0.777
Electrode Force*Expulsion Condition					0.927
Relative Heat Loss*Expulsion Condition					0.362
Heat Input*Electrode Force*Relative Heat Loss					0.283
Heat Input*Electrode Force*Expulsion Condition					0.960
Heat Input*Relative Heat Loss*Expulsion Condition					0.800
Electrode Force*Relative Heat Loss*Expulsion Condition					0.960

4.3.4 Optimization of Predictive Model

The objective of model optimization is to reduce the statistically insignificant terms considered while not compromising the accuracy and predictive ability of the model. Three methods including backwards elimination, forwards selection and stepwise regression were implemented for the model optimization. The final ANOVA results obtained from these optimization methods can then be compared to decide which method is most appropriate to stimulate the relationship of those input and output variables.

4.3.4.1 Forwards Selection Method

The forwards selection method starts off with no terms in the model and adds one term at a time in the order of statistical significance until the model accuracy is no longer improved by adding further terms. The results for the ANOVA analysis with forward selection are given in Table 4.6. The new R^2 value is 82.2 %, which is slightly lower than the updated model (82.8 %). However, the adjusted R^2 value is improved by 3.5 % resulting in a final value of 73.6 %. The number of interaction terms involved in the model is also reduced to 9 (compared to 13 interaction terms in the updated model). In addition, the performance of the predictive model is evaluated to be 63.4 % (predictive R^2), which suggests that the accuracy of the predictive model is improved.

Table 4.6: Results of forward selection method

S	R-sq	R-sq(adj)	R-sq(pred)
0.869068	82.21%	73.64%	63.99%

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	13	94.235	7.2488	9.60	0.000
Heat Input	1	0.231	0.2310	0.31	0.585
Electrode Force	1	17.504	17.5038	23.18	0.000
Relative Heat Loss	1	14.180	14.1803	18.77	0.000
Expulsion Condition	1	0.365	0.3651	0.48	0.493
Heat Input*Heat Input	1	0.599	0.5986	0.79	0.381
Electrode Force*Electrode Force	1	4.413	4.4133	5.84	0.023
Relative Heat Loss*Relative Heat Loss	1	18.993	18.9932	25.15	0.000
Heat Input*Electrode Force	1	0.388	0.3880	0.51	0.480
Heat Input*Relative Heat Loss	1	4.653	4.6532	6.16	0.020
Electrode Force*Relative Heat Loss	1	0.006	0.0063	0.01	0.928
Heat Input*Expulsion Condition	1	1.480	1.4801	1.96	0.173
Relative Heat Loss*Expulsion Condition	1	12.386	12.3856	16.40	0.000
Heat Input*Electrode Force*Relative Heat Loss	1	9.214	9.2140	12.20	0.002
Error	27	20.393	0.7553		
Total	40	114.627			

4.3.4.2 Backwards Elimination Method

The backward elimination method starts off with all factors and the corresponding interaction terms in the model and eliminates one term at a time in the order of statistical significance until the model accuracy is no longer improved by removing further terms. The outcomes of the ANOVA analysis with backwards elimination method are given in Table 4.7. The R^2 value generated with this model is 81.7 %, which is slightly lower (1.1 % lower) than the original model (82.8 %). However, the adjusted R^2 value is increased by 3.7 % at 73.8 %. The number of interaction terms in the model is also reduced to 8, from 13 in the updated model. Similar to the model generated by the forwards selection method, the predictive R^2 is available for the model established with the backwards elimination method, which is 64.5 %.

Table 4.7: Results of backward elimination method

S	R-sq	R-sq(adj)	R-sq(pred)
0.865843	81.69%	73.84%	64.47%

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	12	93.636	7.8030	10.41	0.000
Heat Input	1	0.597	0.5969	0.80	0.380
Electrode Force	1	17.114	17.1137	22.83	0.000
Relative Heat Loss	1	13.617	13.6171	18.16	0.000
Expulsion Condition	1	0.209	0.2093	0.28	0.601
Electrode Force*Electrode Force	1	7.380	7.3802	9.84	0.004
Relative Heat Loss*Relative Heat Loss	1	21.965	21.9651	29.30	0.000
Heat Input*Electrode Force	1	0.922	0.9216	1.23	0.277
Heat Input*Relative Heat Loss	1	4.129	4.1287	5.51	0.026
Electrode Force*Relative Heat Loss	1	0.030	0.0303	0.04	0.842
Heat Input*Expulsion Condition	1	2.661	2.6608	3.55	0.070
Relative Heat Loss*Expulsion Condition	1	12.673	12.6727	16.90	0.000
Heat Input*Electrode Force*Relative Heat Loss	1	8.808	8.8082	11.75	0.002
Error	28	20.991	0.7497		
Total	40	114.627			

4.3.4.3 Stepwise Selection Method

The stepwise selection method is a feature selection technique that starts off with an empty model and then eliminates or adds one term at every step based on the statistical significance until the model accuracy is no longer improved. The summary of the ANOVA analysis with the stepwise selection method is given in Table 4.8. The new R^2 value is 80.3 %, which is slightly lower (2.6 % lower) than the original model (82.8 %). However, the adjusted R^2 value is improved by 2.7 % at 72.8 %. The number of interaction terms of the model has also decreased to 7, compared to 13 terms in the updated model. Moreover, the predictive R^2 for this model is 63.1 %.

Table 4.8: Results of stepwise selection method

S	R-sq	R-sq(adj)	R-sq(pred)
0.883292	80.26%	72.77%	63.14%

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	11	92.001	8.3637	10.72	0.000
Heat Input	1	17.336	17.3364	22.22	0.000
Relative Heat Loss	1	15.944	15.9437	20.44	0.000
Electrode Force	1	9.664	9.6642	12.39	0.001
Expulsion Condition	1	10.686	10.6861	13.70	0.001
Relative Heat Loss*Relative Heat Loss	1	20.410	20.4100	26.16	0.000
Electrode Force*Electrode Force	1	9.457	9.4573	12.12	0.002
Heat Input*Electrode Force	1	5.672	5.6723	7.27	0.012
Relative Heat Loss*Electrode Force	1	3.030	3.0301	3.88	0.058
Relative Heat Loss*Expulsion Condition	1	14.512	14.5119	18.60	0.000
Electrode Force*Expulsion Condition	1	1.565	1.5649	2.01	0.167
Relative Heat Loss*Electrode Force*Expulsion Condition	1	11.328	11.3279	14.52	0.001
Error	29	22.626	0.7802		
Total	40	114.627			

4.3.4.4 Summary of Model Optimization

The results of all three selected techniques (forwards selection, backwards elimination and stepwise selection) exhibit that the optimization process improved the accuracy and predictivity of the model by removing the statistically insignificant terms. Comparing the results of those methods, the R^2 value of the model applying the forwards selection method (82.2 %) was slightly higher than both backwards elimination (81.7 %) and stepwise selection (80.3 %) methods. However, the model adopting the backwards elimination method has the highest values of adjusted R^2 and the R^2 predictive (73.8 % and 64.5 % respectively). Also, the model using the backwards elimination methods has fewer number of interaction items (8) than the model using the forwards selection method (9 interaction items), which is advantageous in the simplicity, less overfitting and easiness of interpretability for the model.

The results of the optimized model using backwards elimination method can be found in Table 4.7. With a R^2 of 81.7 %, an adjusted R^2 of 73.8 %, and a predictive R^2 of 64.5 %, this model displays a clear improvement compared with the results from the first ANOVA analysis. Table 4.9 summarizes all factors and interaction effects that are considered statistically significant with p-values less than 0.05. Only two out of the four individual factors are considered significant (electrode force and relative heat loss) on the influence of the geometric mean crack length. Therefore, the residuals of the model established by the backwards elimination method against the fitted values was analyzed to understand the feature of the data

points and develop feasible optimization steps for the subsequent analysis. The fitted values are the predicted values of the response variable of the model and the response variable is the geometric mean crack length.

Table 4.9: Statistically significant factors and interaction effects of backward elimination model

Term	P-Value
Electrode Force	0.000
Relative Heat Loss	0.000
Electrode Force * Electrode Force	0.004
Relative Heat Loss * Relative Heat Loss	0.000
Relative Heat Loss * Heat Input	0.026
Heat Input * Expulsion Condition	0.070
Relative Heat Loss * Expulsion Condition	0.000
Relative Heat Loss * Heat Input * Electrode Force	0.002

4.3.5 Analysis of Residuals

To understand the quality of the backward elimination model, the model residuals were analyzed. The analysis revealed some problematic tendencies of data, see Figure 4.18. When plotted against their fit value (the result of the model), the residual values ideally should be randomly scattered, centered around the zero line with no clear clusters of points or directional trends. On the contrary, the residuals of the data points express two clearly separate clusters with negative linear trends. To investigate the reasons for this unusual behavior, the two clusters were isolated and the corresponding data points from each cluster were studied.

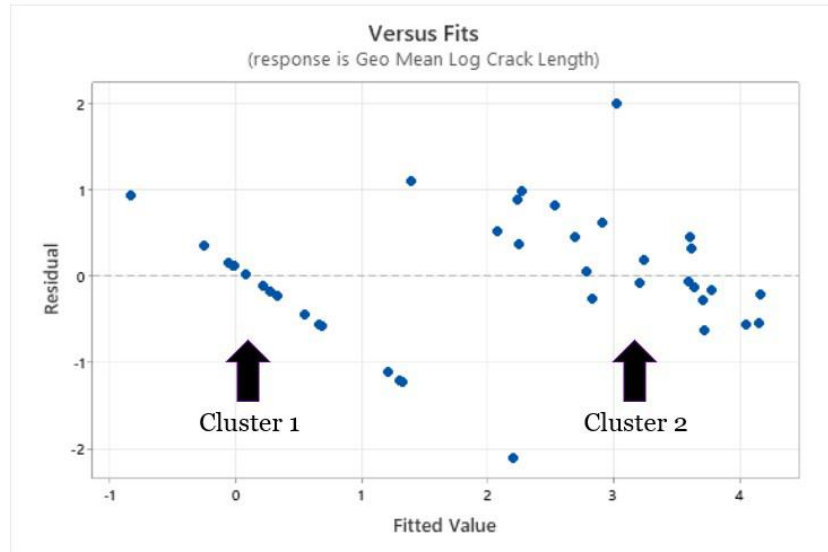


Figure 4.18: The plot of residuals versus fitted values

To understand what was behind the clustering in the residual values, the points from each cluster were separated and the LME crack length associated with each point was tabulated, see . The values clearly show that cluster 1 is made up entirely of zero values (LME-free welds) while cluster 2 consists of all the non-zero values of LME crack length. With this discovery, the two datasets were separately studied to understand their impacts on the predictive model correlating the processing variable to the LME cracking severity.

Table 4.10: Two clusters of crack length values

Cluster 1 crack length (μm)	Cluster 2 crack length (μm)
0	12.1
0	51.4
0	12.8
0	13.6
0	13.1
0	21.9
0	17.1
0	31.0
0	33.9
0	32.7
0	18.4
0	30.8
0	23.1
0	52.2
0	26.0
0	34.1
0	58.0
0	13.8
0	37.1
0	28.4
	23.3
	33.5
	22.6
	37.3
	150.2

4.3.5.1 Analysis of Cluster 1 (Zeros, LME-free Welds)

Cluster 1 clearly shows that the zero values of geometric mean crack length in the model has an undesirable effect on residuals of the model, as may be seen from their highly linear (non-random) arrangement in Figure 4.18. Out of the 45 total data points across 9 conditions considered, 20 of these points (44.4 % of all points) were LME crack free (0 crack length). To better understand what is causing these zero crack length

values, the probability of LME-free welds at each welding condition was calculated, see Table 4.11. The probability matrix shows that 60 % of LME-free welds made with the low heat input and the probability of LME-free welds decreases to 20 % at both the moderate and the high heat input. It can be explained by the less availability of the molten zinc and lower tensile stress associated with low heat input during the RSW process. Furthermore, Table 4.11 also expresses, at low heat input, largest number of LME-free welds were produced with the low electrode force level. However, at higher heat input levels, the largest LME-free welds were produced with the high electrode force level. There was no correlation between the number of LME-free welds and the electrode force level.

Table 4.11: Probability of LME crack-free welds across different heat input levels

		Electrode Force			SUM
		Low	Medium	High	
Heat	Low	0.25	0.15	0.2	0.6
	Medium	0.05	0	0.15	0.2
	High	0.05	0.05	0.1	0.2
	SUM	0.35	0.2	0.45	1

As described in the literature, expulsion is prone to happen at higher heat input levels and could weaken the weld strength and reduce the weld quality [74,75]. It was suspected expulsion event had influence on the probability of LME-free and LME-cracking welds. A binary logistic regression model was used to calculate the probability of a weld containing LME cracks with and without expulsion, see Figure 4.19. It can be seen a significant correlation between expulsion condition and the likelihood of a weld exhibiting LME cracking, as the probability increases from 25 % at the no expulsion condition, to 66.7 % when expulsion occurs. Since the R^2 value of this binary logistic regression was quite low at 52.4 %, the dataset was examined to see the reflected trend. Table 4.12 validates this observation by showing: Out of the 33 welds where expulsion was observed, 22 of them resulted in the weld containing LME cracks (67 %), and the remaining 11 welds were LME-free (33 %). Also, out of the 12 welds where no expulsion was observed, 3 resulted in the weld containing LME cracks (25 %), and 9 did not (75 %). This suggests that reducing expulsion is an important factor in reducing the likelihood of LME crack formation.

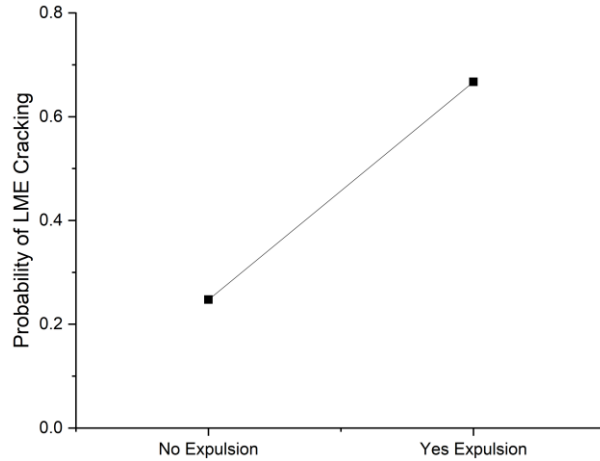


Figure 4. 19: Probability of LME cracking with the occurrence of expulsion

Table 4.12:Percentage of welds with expulsion condition that resulted in LME cracking

		Expulsion Condition	
		Yes	No
LME Crack	Present	67%	25%
	Not Present	33%	75%
		100%	100%

4.3.5.2 Analysis of Cluster 2 (Welds containing LME cracks))

Figure 4.20 shows the probability plot for the cluster 2 residuals, which can be used to validate the normality of the geometric mean crack lengths after eliminating the zero values (data from Cluster 1). With a p-value greater than 0.1 (greater than the 0.05 significance level), the Ryan-Joiner test validate the normality of cluster 2 dataset.

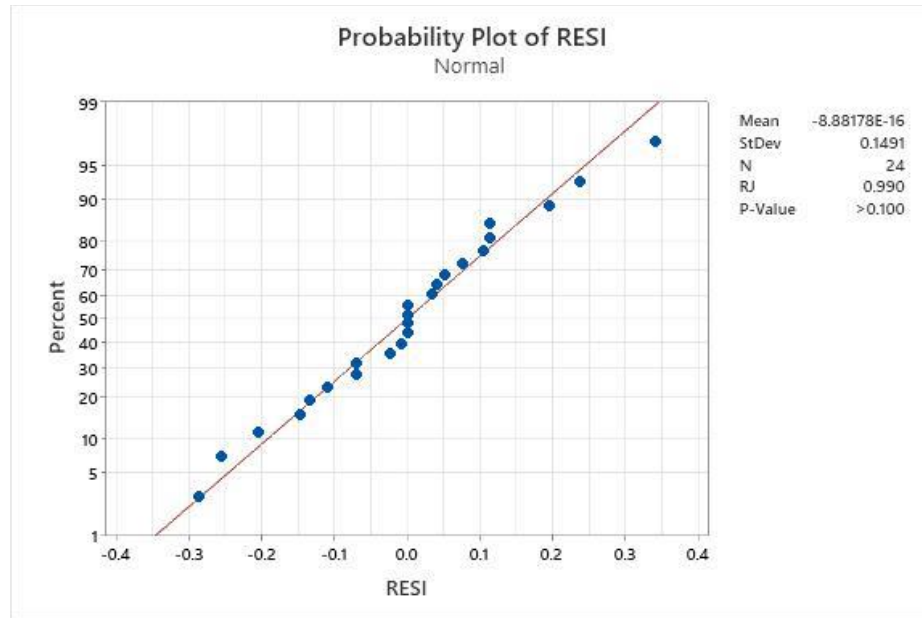


Figure 4.20: The probability plot of the residuals for the cluster 2 data points

The plot of cluster 2 residuals with respect to the fitted values is shown in Figure 4.21a. The residuals of cluster 2 data points are randomly distributed and no longer exhibits apparent clustering, which is preferred in the statistical analysis in the aspects of more accurate inferences and less risk of bias from the non-randomly distributed dataset. The random distribution of the residual remains and was confirmed by plotting residuals against the measured response (geometric mean crack length against the measured crack length, see Figure 4.21b) and other factors (heat input and electrode force, see Figures 4.22a and b, respectively).

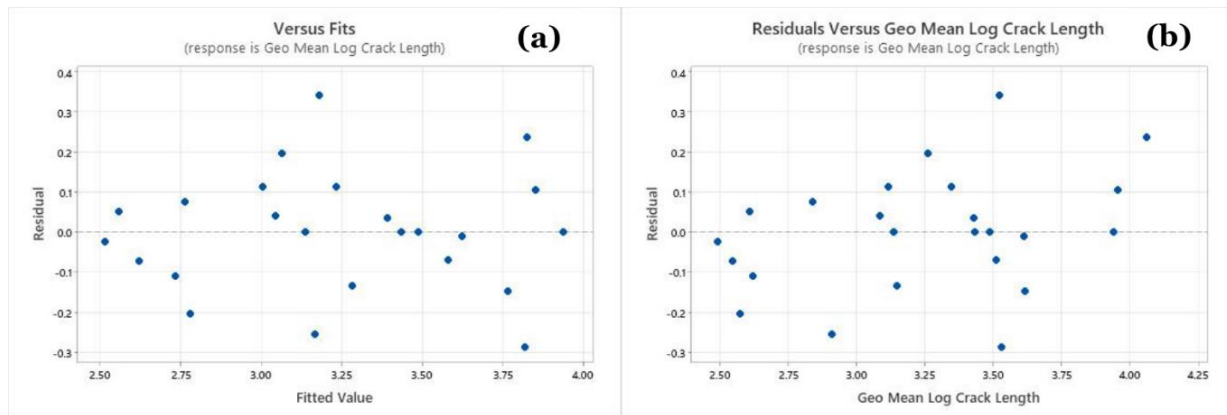


Figure 4.21: Residuals versus (a) relative heat loss and (b) geometric mean crack length

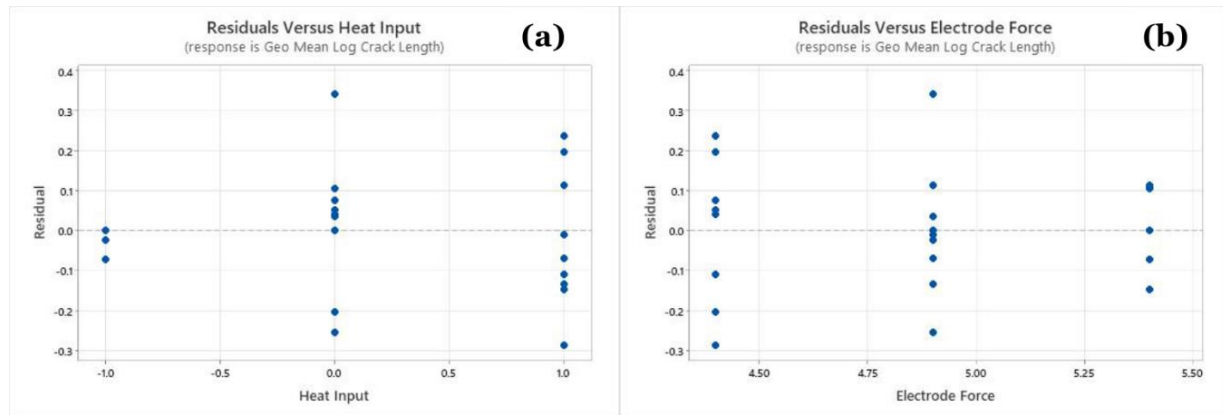


Figure 4.22: Residuals versus (a) heat input and (b) electrode force

After confirming the normality and randomness of the data distribution for the welds with LME cracking (Cluster 2), the linear regression and the ANOVA analysis were redone to study the effect of those points on the original predictive model and their isolated LME cracking behavior causing from the change of input variables. The results of this analysis are displayed in Table 4.13, and show a significant improvement over the model with the zero crack length values included. The R^2 value of 90.3 % and the adjusted R^2 value of 77.8 % are both higher than the original model as well as the optimized models. This indicates that the model without zero crack length values is more accurate due to less disturbance from zero crack length. The regression model of the cluster 2 was optimized and its residuals were graphed to determine and analyze the quality of the analysis.

Table 4.13:ANOVA analysis of cluster 2

S	R-sq	R-sq(adj)
0.218174	90.34%	77.78%

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	13	4.45145	0.34242	7.19	0.002
Heat Input	1	0.21104	0.21104	4.43	0.061
Electrode Force	1	0.05728	0.05728	1.20	0.298
Relative Heat Loss	1	0.02420	0.02420	0.51	0.492
Expulsion Condition	1	0.17820	0.17820	3.74	0.082
Heat Input*Heat Input	1	0.03507	0.03507	0.74	0.411
Electrode Force*Electrode Force	1	0.22113	0.22113	4.65	0.057
Relative Heat Loss*Relative Heat Loss	1	0.20811	0.20811	4.37	0.063
Heat Input*Electrode Force	1	0.47002	0.47002	9.87	0.010
Heat Input*Relative Heat Loss	1	0.03540	0.03540	0.74	0.409
Electrode Force*Relative Heat Loss	1	0.08516	0.08516	1.79	0.211
Heat Input*Expulsion Condition	1	0.22540	0.22540	4.74	0.055
Electrode Force*Expulsion Condition	1	0.12297	0.12297	2.58	0.139
Heat Input*Electrode Force*Relative Heat Loss	1	0.14293	0.14293	3.00	0.114
Error	10	0.47600	0.04760		
Total	23	4.92745			

Similar to the original model, the model was optimized using the backwards and forwards selection methods, the results of this analysis are shown in Tables 4.14 and 4.15. From these results, it is evident that the backwards elimination method is the more appropriate choice for the predictive model, with a higher R^2 value of 89.6 % and also a higher adjusted R^2 value of 78.3 %. According to this cluster 2 model, the only individual factor that has a statistical significance on the geometric mean crack length is expulsion condition, and the only interaction effects with a statistical significance are: Relative Heat Loss*Relative Heat Loss, Heat Input*Electrode Force, and Electrode Force*Expulsion Condition.

Table 4.14: Optimization results of cluster 2 model using backwards elimination method

S	R-sq	R-sq(adj)
0.215547	89.63%	78.31%

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	12	4.41639	0.36803	7.92	0.001
Heat Input	1	0.18783	0.18783	4.04	0.070
Electrode Force	1	0.13553	0.13553	2.92	0.116
Relative Heat Loss	1	0.02870	0.02870	0.62	0.449
Expulsion Condition	1	0.23487	0.23487	5.06	0.046
Electrode Force*Electrode Force	1	0.20916	0.20916	4.50	0.057
Relative Heat Loss*Relative Heat Loss	1	0.76024	0.76024	16.36	0.002
Heat Input*Electrode Force	1	0.50270	0.50270	10.82	0.007
Heat Input*Relative Heat Loss	1	0.10976	0.10976	2.36	0.153
Electrode Force*Relative Heat Loss	1	0.05610	0.05610	1.21	0.295
Heat Input*Expulsion Condition	1	0.20391	0.20391	4.39	0.060
Electrode Force*Expulsion Condition	1	0.37319	0.37319	8.03	0.016
Heat Input*Electrode Force*Relative Heat Loss	1	0.20148	0.20148	4.34	0.061
Error	11	0.51106	0.04646		
Total	23	4.92745			

Table 4.15: Optimization results of cluster 2 model using forwards selection method

S	R-sq	R-sq(adj)
0.234110	85.54%	74.42%

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	10	4.21495	0.42150	7.69	0.001
Heat Input	1	0.55081	0.55081	10.05	0.007
Electrode Force	1	0.00147	0.00147	0.03	0.873
Relative Heat Loss	1	0.12375	0.12375	2.26	0.157
Expulsion Condition	1	0.22360	0.22360	4.08	0.065
Heat Input*Heat Input	1	0.56981	0.56981	10.40	0.007
Electrode Force*Electrode Force	1	0.26884	0.26884	4.91	0.045
Heat Input*Electrode Force	1	1.48164	1.48164	27.03	0.000
Heat Input*Relative Heat Loss	1	0.55205	0.55205	10.07	0.007
Electrode Force*Relative Heat Loss	1	0.92485	0.92485	16.87	0.001
Heat Input*Expulsion Condition	1	0.89656	0.89656	16.36	0.001
Error	13	0.71250	0.05481		
Total	23	4.92745			

The factors used for the linear regression model was further reduced to only include the processing factors during the welding cycle. The relative heat loss term was developed by the user and was not included. The results in Table 4.16 showed that the heat input and electrode force factors were significant, along with the interaction term between heat input and expulsion condition. However, the R^2 and adjusted R^2 values decreased significantly after removing the relative heat loss term. The middle part of the residuals plot in Figure 4.23 suggests that elimination of relative heat loss term was not feasible to further improve the linear regression model. In order to address the decrease of predictivity of the linear regression model, the relative heat input was standardized (subtract the mean, then divide by the standard deviation) and the interaction terms of relative heat loss but without considering the relative heat loss factor itself were added back for another run. As a result, the ANOVA analysis, Table 4.17, shows a significantly improved R^2 of 88.1 %, and adjusted R^2 of 79.0 %. This model has a relatively high R^2 and the highest adjusted R^2 values compared with previously calculated models, therefore, it was selected to be the most optimal model. The model shows that the heat input and the expulsion condition terms are the statistically significant factors that control LME indicting by the change of geometric mean crack length, with expulsion condition being the most statistically significant with a p-value of 0.015. The standardized equation for this regression for both the Yes and No expulsion conditions is given in Table 4.18. The minus and plus sign in front of each coefficient on the right side of the equation indicate that its relating variable has negative and positive correlation on the geometric mean crack length, respectively. This equation along with the results of its corresponding ANOVA analysis provides insights on the overall impact of the testing factors on LME as regards to the geometric mean crack length for the following section.

Table 4.16: ANOVA analysis without relative heat loss term for cluster 2 data set

Model Summary

S	R-sq	R-sq(adj)	R-sq(pred)
0.371837	52.30%	35.46%	*

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	6	2.5770	0.4295	3.11	0.031
Heat Input	1	0.9223	0.9223	6.67	0.019
Electrode Force	1	0.6762	0.6762	4.89	0.041
Expulsion Condition	1	0.3606	0.3606	2.61	0.125
Heat Input*Heat Input	1	0.6077	0.6077	4.40	0.051
Heat Input*Electrode Force	1	0.3150	0.3150	2.28	0.150
Heat Input*Expulsion Condition	1	1.3769	1.3769	9.96	0.006
Error	17	2.3505	0.1383		
Lack-of-Fit	4	0.6097	0.1524	1.14	0.381
Pure Error	13	1.7408	0.1339		
Total	23	4.9275			

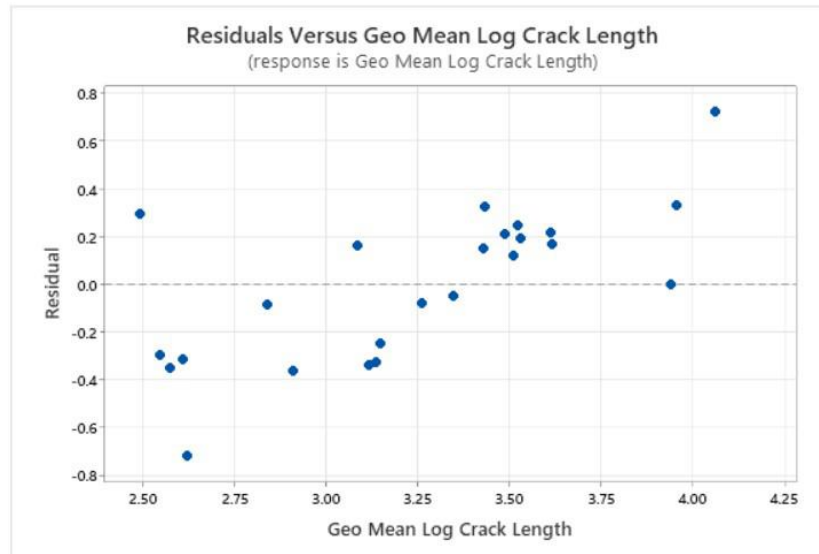


Figure 4.23: The plot of residuals versus geometric mean crack length excluding relative heat loss term for cluster 2 dataset

Table 4.17: Results of normalizing relative heat loss term and only considering relative heat loss interaction effects

Model Summary

S	R-sq	R-sq(adj)	R-sq(pred)
0.212372	88.10%	78.95%	*

Analysis of Variance

Source	DF	Adj SS	Adj MS	F-Value	P-Value
Regression	10	4.3411	0.43411	9.63	0.000
Heat Input	1	0.3377	0.33772	7.49	0.017
Electrode Force	1	0.1736	0.17357	3.85	0.072
Expulsion Condition	1	0.3558	0.35577	7.89	0.015
Electrode Force*Electrode Force	1	1.1369	1.13689	25.21	0.000
RHL*RHL	1	1.4102	1.41023	31.27	0.000
Heat Input*Electrode Force	1	0.6018	0.60182	13.34	0.003
Heat Input*RHL	1	1.6688	1.66884	37.00	0.000
Heat Input*Expulsion Condition	1	0.4265	0.42645	9.46	0.009
Electrode Force*Expulsion Condition	1	0.3918	0.39180	8.69	0.011
Heat Input*Electrode Force*RHL	1	1.3303	1.33028	29.49	0.000
Error	13	0.5863	0.04510		
Total	23	4.9275			

Table 4.18: Final regression model only accounting for cluster 2 data points

Expulsion Condition		
No	Geo Mean Log Crack Length	$= 1.993 - 0.685 \text{ Heat Input} + 0.567 \text{ Electrode Force}$ $- 0.4551 \text{ Electrode Force} * \text{Electrode Force}$ $+ 0.622 \text{ RHL} * \text{RHL} - 0.3107 \text{ Heat Input} * \text{Electrode Force}$ $- 0.686 \text{ Heat Input} * \text{RHL}$ $+ 0.582 \text{ Heat Input} * \text{Electrode Force} * \text{RHL}$
Yes	Geo Mean Log Crack Length	$= 3.1889 + 0.1251 \text{ Heat Input} - 0.443 \text{ Electrode Force}$ $- 0.4551 \text{ Electrode Force} * \text{Electrode Force}$ $+ 0.622 \text{ RHL} * \text{RHL} - 0.3107 \text{ Heat Input} * \text{Electrode Force}$ $- 0.686 \text{ Heat Input} * \text{RHL}$ $+ 0.582 \text{ Heat Input} * \text{Electrode Force} * \text{RHL}$

4.3.5.3 Implication of Statistical Analysis

As can be seen in Figure 4.24, the geometric mean crack length decreased as the number of welds containing cracks for a particular condition decreased. At the electrode force level of 5.4 kN, the number of welds not exhibiting LME cracking increased, compared with other two electrode force conditions among three heat input regimes but the welds experiencing LME cracking with the electrode force of 5.4 kN showed large geometric mean crack length. Figure 4.25 shows the interaction effects of electrode force and heat input on geometric mean crack length for the LME-cracking welds (Cluster 2) along with occurrence of expulsion. At low heat input, the geometric mean crack length increased with the increase of electrode force from 4.4 kN to 4.9 kN then further decreased with the electrode force of 5.4 kN. At moderate heat input, the geometric mean crack length linearly increased with the increase of electrode force. At high heat input, welds with electrode force of 4.4 kN and 5.4 kN conditions had similar geometric mean crack length and were larger than the condition of 4.9 kN electrode force. Moreover, the contour plot (see Figure 4.26) correlated geometric mean crack length with effects of electrode force and heat input for the welds tolerating LME cracking but without expulsion. The cracking welds without expulsion only observed at the low and moderate heat inputs and the implementation of higher electrode force resulted in an increase of geometric mean crack length. Overall, it is worth noting that at high electrode force level, the geometric mean crack length was large regardless the decrease of the number of LME-cracking welds (see Figure 4.24). When expulsion happened, the increase of electrode force exaggerated larger geometric mean crack length at higher heat input level (see Figure 4.25). Furthermore, the welds with LME cracking accompanied with expulsion at high heat input. A study from DiGiovanni et al. [85] showed the frequent LME cracks with large length caused a significant decrease of lap shear strength of welds owing to the contribution of fracture propagation under external loads by LME cracks. According to statistical results of the current work, it is expected to see a pronounced static strength loss of the investigated 3G-AHSS welds when welding at

higher heat input levels with expulsion. Under such welding conditions, larger number of deep LME cracks will form.

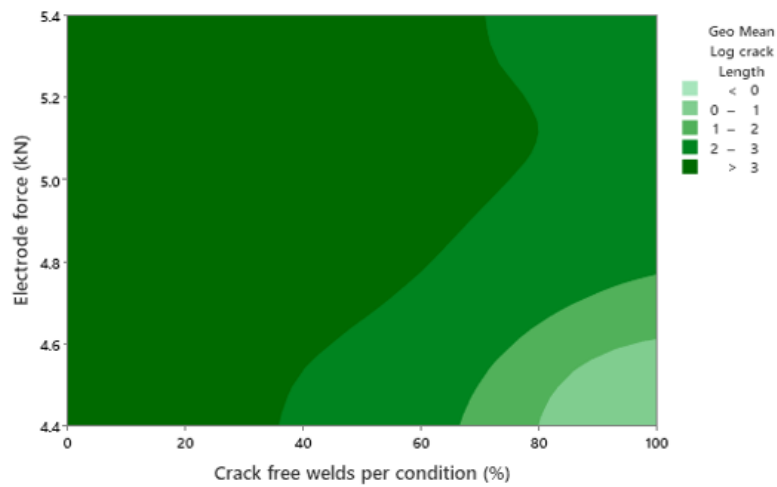


Figure 4.24: Contour plot to correlate geometric mean crack length with electrode force and percentage of crack free welds per condition

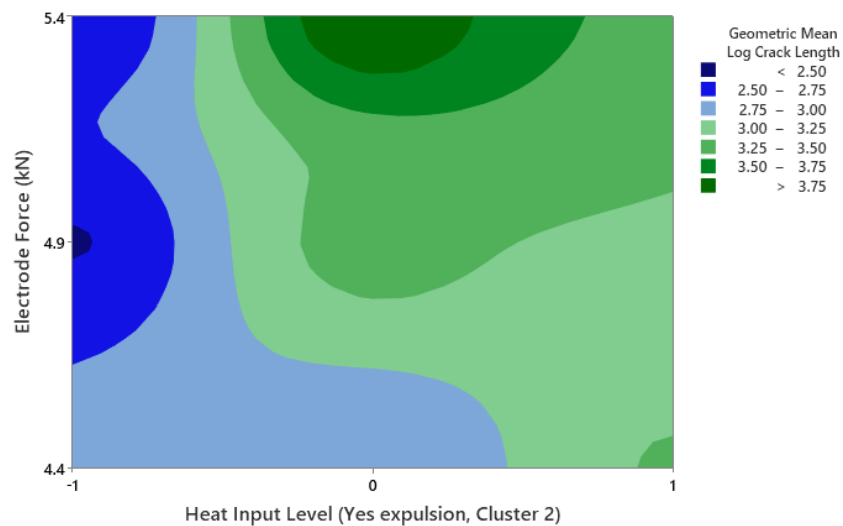


Figure 4.25: Contour plot to Correlate geometric mean crack length with electrode force and heat input at Yes expulsion condition for cluster 2

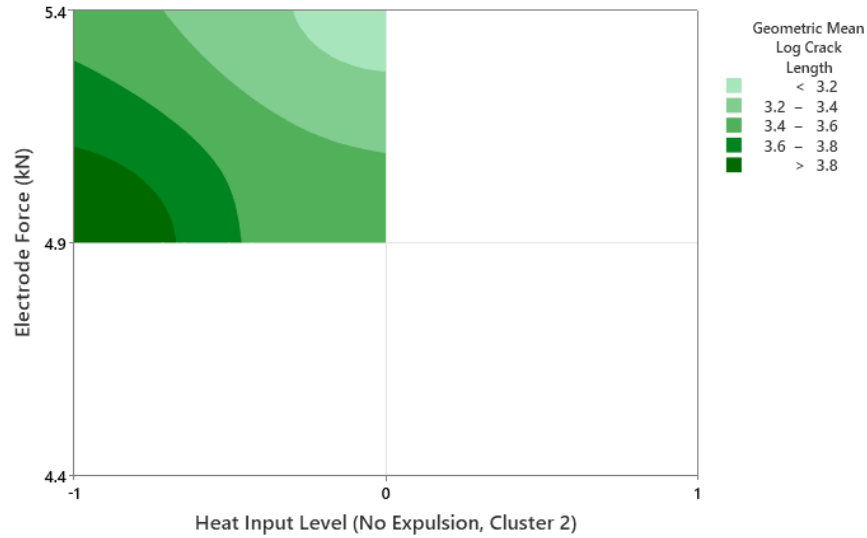


Figure 4.26: Contour plot to correlate geometric mean crack length with electrode force and heat input at No expulsion condition for cluster 2

4.4 Mechanism Linking Electrode Force and LME Severity

The influence of electrode force on LME cracking severity for the low-LME sensitive material followed a similar trend in both experimental and statistical analysis. Welds made using 4.9 kN of electrode force and had expulsion exhibiting more severe LME cracking, indicated by the largest crack index (the indicator of LME cracking severity in the experimental analysis) and geometric mean crack length (the indicator of LME cracking severity in the statistical analysis), than the welds made at other electrode force levels that were expulsion-free. The statistical analysis, applying the ANOVA and the linear regression techniques, also revealed the relations between the input variables (processing factors: heat input, heat loss, electrode force and expulsion condition) and the output variable (geometric mean crack length) accounted for the full data points, LME-free welds and LME-cracking welds. The established model only considering LME-cracking welds made in low-LME sensitive material represents strong relationship (improved predictivity) between the input and output variables. Unlike the case of the low-LME sensitive steel where 44 % of the welds were crack-free, only 5 LME-free welds (11%) were found in the welds made in the high-LME sensitive material. Also, both the number and length of the LME cracks in the welds increased significantly for the high-LME sensitive material, see Figures 4.3 and 4.4. The work on the low-LME sensitive material acted as a screening study to understand which factors were influential in influencing LME. By examining how the LME cracking severity of welds made in the high-LME sensitive material changed in response to the variables observed to control LME indicating by geometric mean crack length from the study of welds in the low-LME sensitive material, the connection between variables (i.e. heat input, heat loss and expulsion condition) and LME crack formation will be understood. Figure 4.27 depicts the progress of LME analysis

transitioning from the low-LME sensitive material to the high-LME sensitive material. The correlation of heat input, heat loss and expulsion condition with LME cracking severity for the high-LME sensitive material will be discussed in the following subsections.

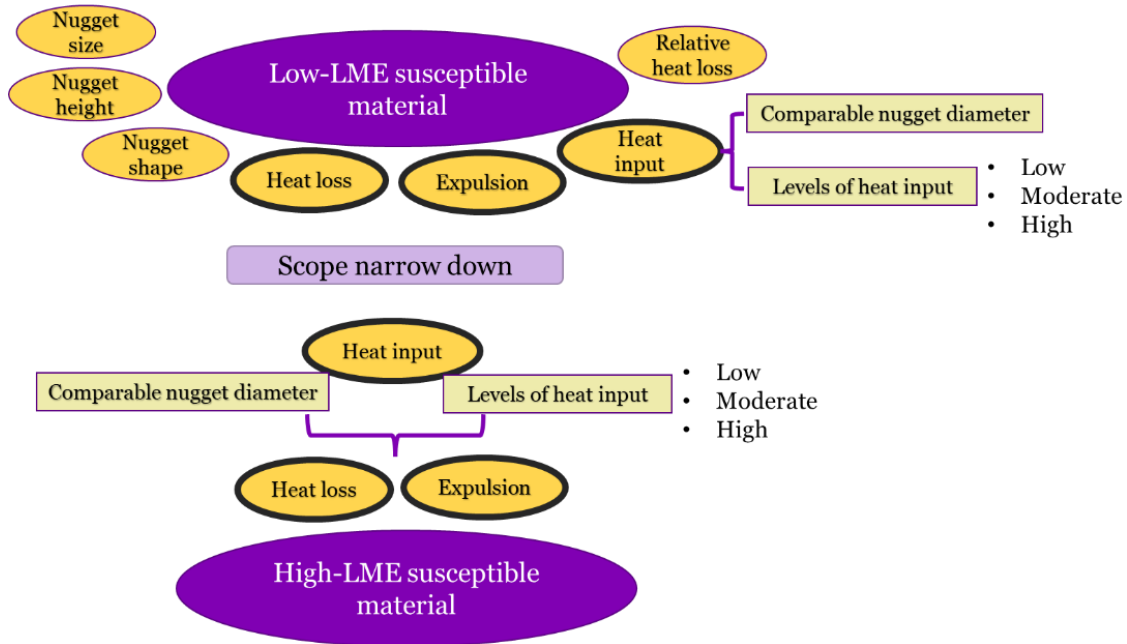


Figure 4.27: Schematic showing analysis progress from low-LME sensitive material to high-LME sensitive material

4.4.1 Correlation of LME Cracking Severity with Heat Input

The interacting effects of heat input (represented by welding current) and electrode force on LME cracking are summarized in Figure 4.28a showing that increasing heat input increased LME cracking severity, however increasing electrode force could mitigate LME cracking, even at high heat input [10]. It is worth mentioning that the effect of electrode force on heat generation was not accounted for in that work. Otherwise welding current (at each current setting) was held constant regardless of electrode force variation. Also, all of the tests done to understand the relationships between heat, force and LME were done below the expulsion current, so the effect of expulsion was not accounted for. As mentioned in the chapter 3 (methodology), in the present study, welding current was changed to maintain constant heat input (as measured by nugget diameter), at each heat input level, when electrode force was changed. For the high-LME sensitive material, the observed effects of heat input and electrode force on LME cracking diverged from the literature [5–8,10] at the moderate and high heat input levels, as can be seen in Figure 4.28b. Although, it should be noted that at low heat input, LME cracking decreased with the increase of electrode force, in-line with the literature [5–8,10]. At moderate heat input, welds with both low electrode force and

high electrode force experienced high LME cracking. At high heat input, LME cracking increased when the electrode force increased.

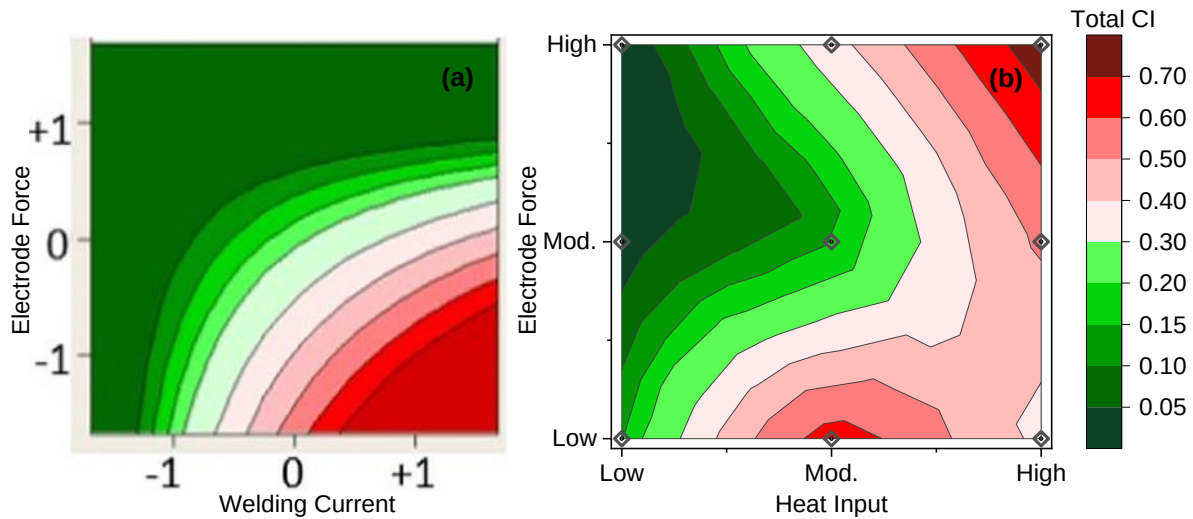


Figure 4.28: Effects of electrode force on LME cracking at various heat input levels: (a) summary from the literature, reprint permitted [10]; (b) observations of the high-LME sensitive material (Note: the diamond icons in plot (b) represents experimental conditions.)

The heat map in Figure 4.29 correlates heat loss with electrode force at various heat input levels of the present work. At all heat input levels (low, moderate and high), heat loss increased with the increase of electrode force, which agrees with the literature [5–8,10]. This link between electrode force and heat loss is due to the electrode contact area enlarging as electrode force increases. The increase in contact improves the cooling effect i.e. larger amount of heat loss by increasing area and decreasing contact resistance [5,104]. At low heat input, the increase of heat loss mitigated LME cracking. However, increasing heat loss did not reduce LME cracking in welds made with moderate and high heat input not in line with the literature [5–8,10]. The expulsion phenomenon was perceived during the welding process at those heat input conditions and it will be correlated with the LME cracking severity as follows.

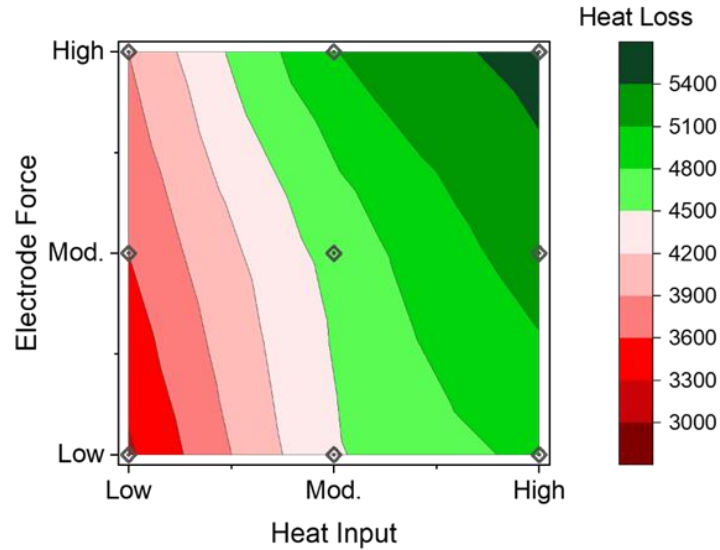


Figure 4.29: Correlation of electrode force to heat loss at low, moderate, and high heat input levels

4.4.2 Correlation of LME Cracking with Expulsion

High-LME cracking has been seen in welds that experienced expulsion from the literature [119] and in the present work shown in Section 4.3. Although the explicit link between expulsion and LME cracking has not been made, expulsion may contribute to LME formation, separate from the contribution from increasing heat input. For the high-LME sensitive material zero, three, four and five expulsions were detected within per five welds (denoted as “Yes Expulsion” condition) and conditions that produced no welds with expulsion were denoted as the “No Expulsion” condition as described in chapter 3 (methodology), see Figure 4.30. Expulsion was not observed for any of the conditions made with low heat input. In these welds, the increase in electrode force enhanced cooling effect (see Figure 4.29) and reduced LME cracking, as described by literature [5–8,10]. At the moderate heat input level, LME formation was decoupled from heat extraction. It was seen that LME cracking decreased as heat extraction increased when electrode force was increased from 4.4 kN to 4.9 kN. However, it was also seen that LME cracking increased with further increases in electrode force to 5.4 kN, even though this further increased heat extraction. As regards to the expulsion event, the welds made using electrode forces of 4.4 kN and 5.4 kN experienced expulsion, whereas the welds made with an electrode force of 4.9 kN did not. At the high heat input level, expulsion occurred at all electrode force conditions. At this heat input, although increasing electrode force still increased heat loss (see Figure 4.29), LME cracking increased with increasing electrode force. These results show that below the expulsion limit, the relation between electrode force and LME cracking is controlled by heat loss, where increasing electrode force, increases heat loss and decreases LME cracking. However, when welding above the expulsion limit, there is another phenomenon that is occurring that exasperates LME cracking with increasing electrode force, which is more influential than heat loss.

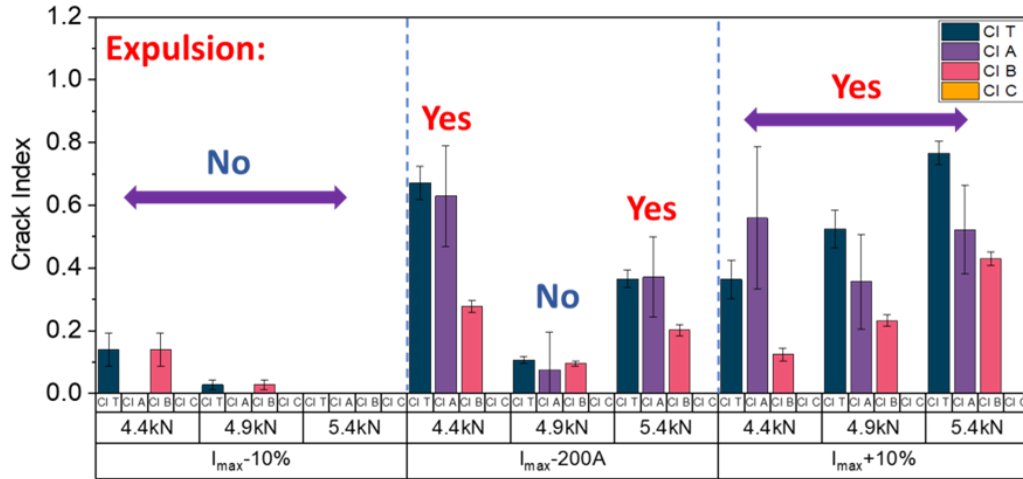


Figure 4.30: No Expulsion case and Yes Expulsion case among various heat input levels with electrode force variation

4.4.3 Processes Linking Expulsion to LME Formation

In the present work, welds made with moderate heat input that experience expulsion showed much higher B Type cracking severity than the weld that did not experience expulsion, whereas the increase of A Type cracking in welds experiencing expulsion was not as great and C Type cracking was not observed at all testing conditions, see Figures 4.2 and 4.30. This indicates that the severity of Type B cracks was driven by expulsion event. It is known that the formation of B Type cracks is due to thermal stress generated from the rapid cooling that occurs when the electrode suddenly sinks into the steel (electrode collapse), due to the decrease in hot strength of the steel surface on heating [104]. When welds were expulsion-free, the electrode force was countered by both the material strength and the ferrostatic pressure of the molten metal, enclosed in the corona bond, so the rate and depth of electrode collapse is purely a function of the material hot strength [120]. However, expulsion disrupts this natural equilibrium influencing electrode position. The schematic in Figure 4.31a compares influences of electrode collapse on welds without expulsion (left half) and with expulsion (right half). The effect of expulsion on LME cracking can be divided in mechanical and thermo-mechanical components, acting simultaneously. During expulsion, liquid metal is rapidly lost from the molten nugget. Due to speed of this event, it would be expected that this would lead to a rapid drop in the ferrostatic pressure of nugget, disrupting the mechanical equilibrium between the weld joint and electrodes. This sudden change would in-turn affects the electrode collapse event. As such, the rapid reduction in counterforce from the nugget against the electrode leads to a sudden electrode collapse and thus rapid tensile deformation at the sheet surface [120,121]. The link between this rapid collapse and LME was even speculated by Böhne et al. [114] who noted that faster deformation can be associated with more LME cracks formation. An increase in collapse deformation rate during expulsion may similarly cause more

severe LME cracking [121]. As well, this sudden and deep deformation will increase of contact area between the electrode and the weld shoulder, greatly enhancing heat extraction, developing more tensile stress at the affected area. The connection between the expulsion event and development of high thermal stresses from the rapid electrode collapse may be described as the “thermo-mechanical” component of the expulsion effect on LME cracking. The effect of expulsion on electrode indent can be visualized by comparing cross-sections of welds with low, moderate and high electrode forces at moderate heat input level, presented in Figure 4.31b, c, and d, respectively. At low (4.4 kN) and high (5.4 kN) electrode force levels, welds experienced expulsion and had deeper indents along with more severe LME cracking (see Figures 4.2 and 4.30) than the weld without expulsion made with moderate (4.9 kN) electrode force.

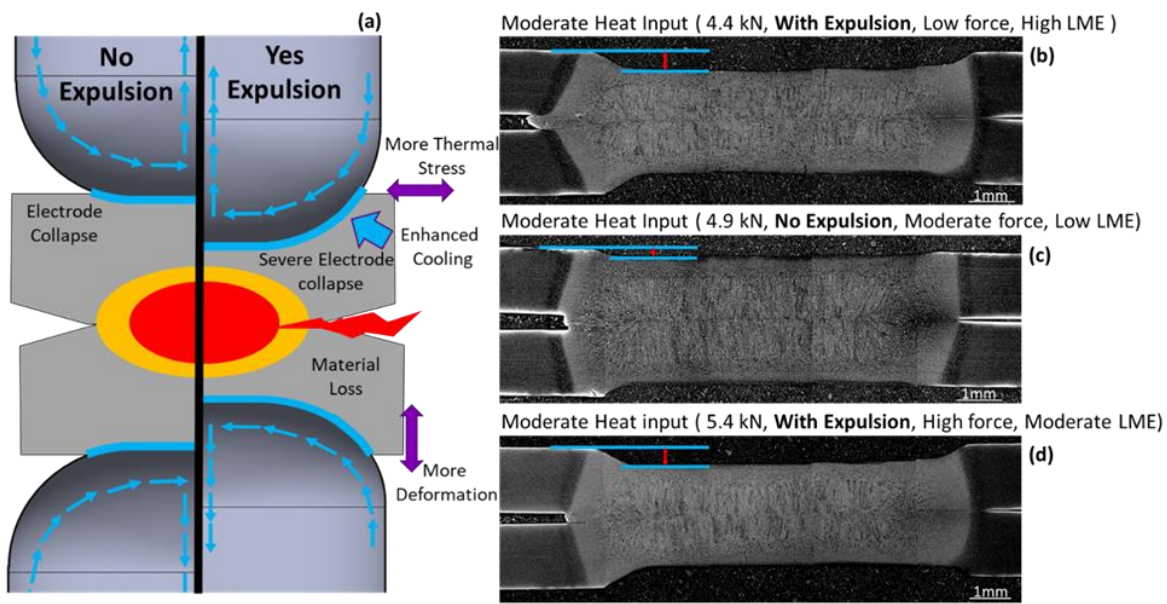


Figure 4.31: (a) Schematic showing a comparison of expulsion-free weld and weld with expulsion; (b) (c) (d) cross-sectional weld images with the denotation of electrode indent depth at moderate heat input level with 4.4 kN, 4.9 kN, 5.4 kN electrode force, respectively

The influence of electrode force on expulsion can be described by analyzing the balance of forces acting on the molten metal for two conditions: “low electrode force” and “high electrode force” as shown in Figure 4.32. Where $F_{\text{electrode}}$ is the electrode force; P is the ferrostatic pressure within the nugget resisting the electrode force; subscripts of LB and LA, as well as, HB and HA mean before and after expulsion for the low electrode force case, and before and after expulsion for the high electrode force case, respectively. Higher electrode force leads to increase in ferrostatic pressure inside the nugget and increases the strength of the corona bond [120]. When the corona bond is breached, more material is lost in the expulsion at high force as compared to the expulsion at low force due to higher pressure of the liquid, resulting in a deeper indent. This hypothesis is in-line with the observations of electrode indent depth change with change in

electrode force, as can be seen in Figures 4.33 and 4.34c, showing that in welds that experience expulsion, higher force leads to significantly deeper electrode indents at the high heat input level.

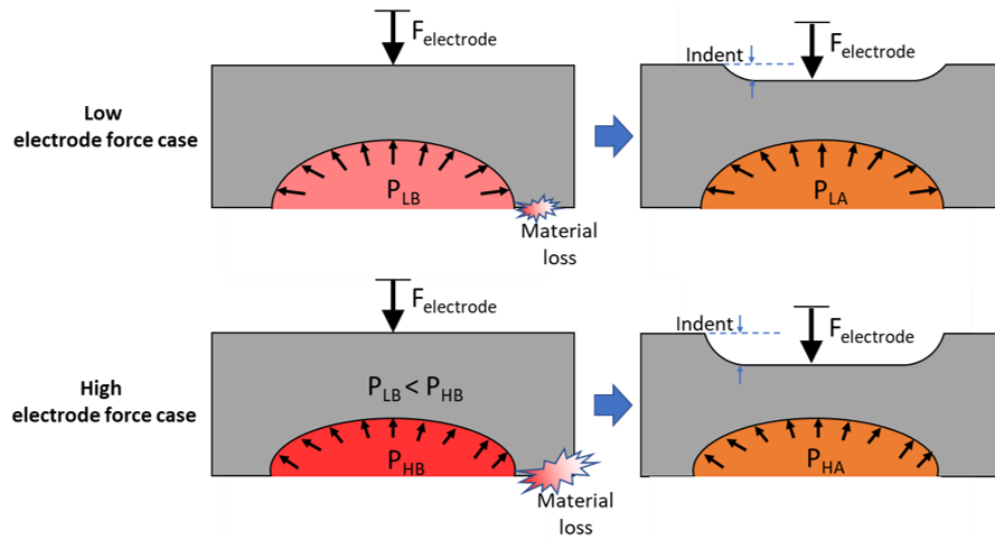


Figure 4.32: Schematic of expulsion at low and high electrode force conditions at the high heat input level

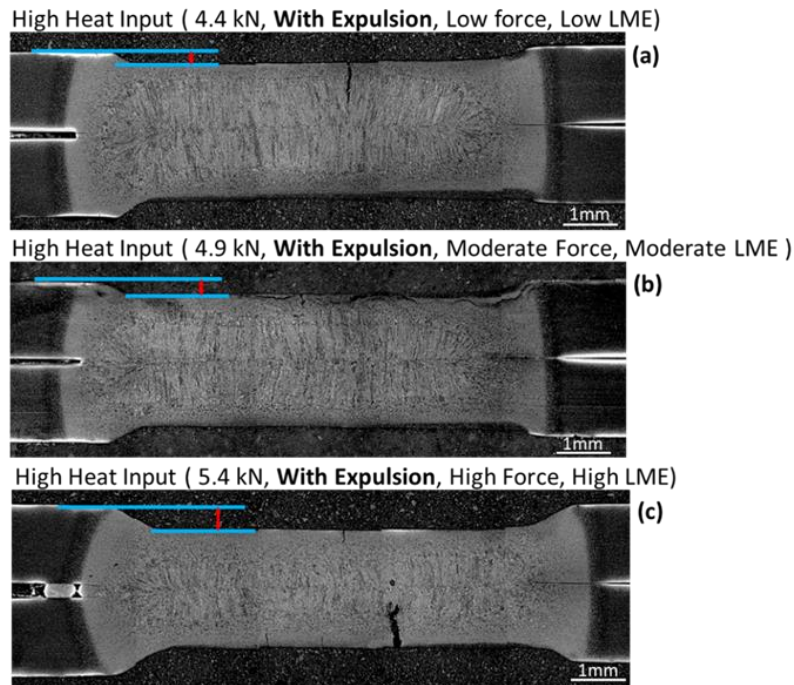


Figure 4.33: Cross-sectional images of welds with expulsion at the high heat input level, (a), (b) and (c) with the electrode force of 4.4 kN, 4.9 kN, 5.4 kN, respectively

LME cracking severity and electrode indent depth may be correlated. Different relations may be seen in welds made at heat input levels that did and did not experience expulsion. In welds without expulsion (at

low heat input) electrode indent depth was low, and so was the LME cracking severity (Figure 4.34a). The embedded figure in Figure 4.34a, at a higher scale to show the data trends, reveals that despite a significant (but small) increase of electrode indent depth between the welds made with electrode forces of 4.4 kN and 5.4 kN, no increase of LME cracking severity was observed. Conversely, LME cracking severity decreased from a crack index of 0.14 for welds made with an electrode force of 4.4 kN to crack index of 0 (crack-free) for welds made with an electrode force of 5.4 kN. In this case, higher heat extraction caused by higher electrode force was a predominant factor decreasing LME cracking severity, in-line with the observations from literature[5–8,10]. In welds with expulsion at high heat input (Figure 4.34c), LME cracking severity increased linearly with indent depth. The same trend was also seen for welds made with moderate heat input (Figure 4.34b). In the welds that experienced expulsion, higher electrode force led to deeper indent allowing more contact between the electrode and the sheet surface at the weld shoulder, which increased rapid cooling during electrode collapse leading to higher tensile stresses and more severe LME cracking.

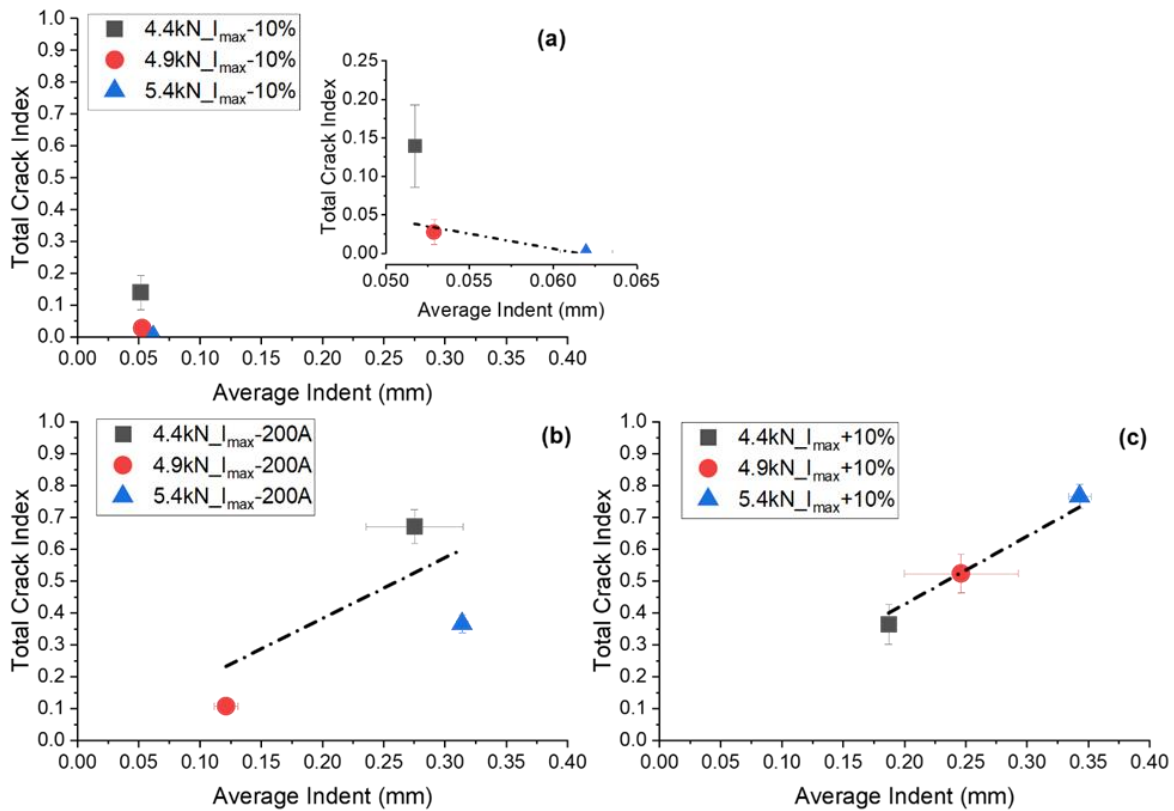


Figure 4.34: Total crack index as a function of electrode force and average indent depth at the (a) low; (b) moderate and (c) high heat input, respectively

Chapter 5 Conclusion and Future Work

5.1 Conclusion

This study investigated influence of electrode force on LME cracking severity of two types of 3G-AHSS (low-LME sensitivity and high-LME sensitivity) at the low (below expulsion limit), moderate (near expulsion limit) and high (above expulsion limit) welding heat input levels during RSW. The welding parameters were selected so that the nugget diameter of all welds made with each respective level of heat input was similar, regardless of electrode force used, to prevent from the change of weld strength attributed to the variation of the welding parameter. It was achieved by altering the welding current while varying the electrode force. The two highlighted contributions of this study in the aspect of LME mitigation through the change of welding parameters are that expulsion exasperates LME cracking due to its effect on electrode collapse during the welding process and the presence of LME cracking in low-LME sensitive material is highly determined by the expulsion occurrence. The above conclusions were derived from two distinct set of experiments. The first study statistically analyzed the factors that affected LME crack length in a low-LME sensitive material. The second study applied the outcomes of the statistical analysis on observations of LME cracking severity for a high-LME sensitive material to understand the mechanism relating the significant process parameters identified in the statistical analysis to the mechanisms responsible for LME formation.

The experimental observations on the low-LME sensitive material indicates an overall low LME severity across various welding conditions, shown by its generally low cracking severity; having a crack index value below 0.15. In addition, the LME cracking severity was not linearly correlated with the change of heat input, heat loss and expulsion event. In the statistical analysis of the LME cracks in the welds made in the low-LME sensitive material, Geo Mean crack length (the output variable) was used as an indicator of LME cracking severity instead of the crack index since a normally distributed dataset is required for the accuracy of statistical analysis. It is worth mentioning that the LME cracking severity represented by the geometric mean crack length and the crack index followed the same trend with the change of processing factors. The input variables of the statistical analysis were heat input, heat loss and expulsion event and the adopted statistical techniques were the ANOVA analysis and the linear regression modelling.

The original linear regression model correlating the input and output variables showed very low predictivity (R^2 of 41.2 % and adjusted R^2 of 13.8 %). The linear regression model then was improved by a sequence of optimization methods including outlier removal, the use of normalized values, backwards elimination, forwards selection and stepwise selection, resulting in an increase of R^2 and adjusted R^2 value to be 81.7 % and 73.8 %, respectively. However, the residuals analysis on the optimized model showed a clear gathering issue (two clusters of data points). To investigate this phenomenon further, the data values corresponding

to the individual residual clusters (denoted as cluster 1 and cluster 2) were identified and isolated for separate analysis.

Analysis of cluster 1 (geometric mean lengths of LME-free welds) showed that LME cracking was much less likely to be present in welds with low heat input compared to those made with medium or high heat inputs. At the low heat input, 80 % of the welds were LME-free, while only 27 % of the welds in the medium and high heat input conditions were LME-free. Further analysis showed that the occurrence of expulsion was a significant factor in the likelihood of LME formation, with welds being approximately 42 % more prone to have LME cracks when expulsion was observed. Moreover, the expulsion was subject to take place at higher heat input levels.

The ANOVA analysis and linear regression model with similar optimization methods as applied on the original predictive model were redone on the cluster 2 (geometric mean crack lengths of LME-cracking welds). The resultant model expressed a closer relationship between the input and the output variables than the original model owing to its higher R^2 (89.6 %) and adjusted R^2 (78.3 %) and randomly distributed residuals of data points. The predictive model of cluster 2 disclosed that both heat input and expulsion condition had significant influence on geometric mean crack length.

The results of experimental and statistical analysis for low-LME sensitive material provided insights of electrode force impact on LME cracking. At low heat input, welds were less prone to inflict LME cracking and expulsion. However, welds made with low heat input possess lower weld strength owing to the formation of smaller nugget diameter compared with a higher heat input level. The increase of heat input is necessary to produce welds with higher strength (larger nugget diameter) while expulsion becomes favorable. With the occurrence of expulsion, the increase of electrode force could be detrimental to the overall LME cracking severity, leading to an increase of the number of welds with LME cracking or to a fewer deeper cracks. The influence of heat input, heat loss and expulsion condition causing from electrode force variation was then explicitly examined for the high-LME sensitive material.

The electrode force study for high-LME sensitive material shows Type B cracks were observed along with all three heat inputs levels while Type C cracks were absent. In addition, Type A cracks appeared at both moderate and high heat inputs. Multiple types and larger number of LME cracks were induced at higher heat input levels. When welds were made at low heat input, LME severity decreased from a Total CI of 0.14 to 0 when electrode force was increased from 4.4 kN to 5.4 kN. However, that same increase in electrode force resulted in an increase in LME (Total CI increased from 0.36 to 0.77) when welds were made at high heat input. When moderate heat input was applied, the Total CI decreased from 0.67 to 0.11 when electrode force was increased from 4.4 kN to 4.9 kN and then the Total CI increased to 0.37 when

the electrode force was further increased to 5.4 kN. The observations showed that the severity of LME cracking did not always decrease with the increase of electrode force. The decrease of LME cracking with an increase of electrode force only was observed at low heat input where expulsion did not occur. At moderate heat input, the welds with expulsion possessed higher LME cracking than expulsion-free welds, deviating from the trend seen in the welds made with low heat input. At high heat input, expulsion happened at all electrode force conditions and the severity of LME cracking increased with increased electrode force. LME cracking was dominated by heat extraction at low heat input. At moderate heat input conditions where both expulsion and non-expulsion conditions were seen, LME was both heat transfer and electrode collapse controlled. Finally, in welds made with high heat input, LME cracking was dominated by electrode collapse which had a much stronger effect controlling LME than did heat loss during the welding cycle. As regards as the correlation of electrode indent depth and the LME cracking severity, at low heat input, electrode indent depths and LME cracking severity were low and the increase of electrode force increased electrode indent but decreased LME cracking severity. At moderate heat input, welds with expulsion experienced deeper indent with more severe LME cracking. At high heat input, the increase of electrode force significantly increased electrode indent depth and LME cracking. In automotive assembly, expulsion often occurs during RSW. Under such circumstances, LME will be more severe and its severity will increase with increased electrode force. Therefore, when using high electrode force to mitigate LME during welding, welding engineers must validate whether or not they are welding in expulsion conditions.

5.2 Future Work

This work has shown the influence of electrode force on LME cracking for two types of 3G-AHSS. The results from this study show that expulsion itself (excluding its association with increased heat input) is a factor contributing to LME cracking. When welding below the expulsion current, high electrode force decreased LME cracking severity. On the other hand, when welding above the expulsion current, more severe LME cracking was observed at high electrode force. In addition, severe Type B LME cracking was observed at the expulsion condition due to tensile stress generation at the weld shoulder region from sudden electrode collapse. The study from DiGiovanni et al. [85] indicates that Type B LME cracking was critical on weld static strength loss through lap shear test since the cracking location was on the fracture path and the increase in number of Type B cracks with large size promoted a weld failure. Therefore, it is worth to conduct mechanical tests such as lap shear test and cross tension test on the welds with and without expulsion to comparative study the influence of expulsion on the weld strength loss and the fracture feature through the bridge of LME cracking behavior at the weld shoulder region in the future work. The studies of silicon content effect on LME cracking by Kalashami et al. [107] and Dong et al. [108] illustrate that higher silicon content resulted in severe LME cracking due to earlier expulsion and the promotion of

decarbonization and internal oxidation at the interface of zinc coating and the substrate steel during the intercritical annealing process. The investigated materials in this work possess 1.02 wt. % (low-LME sensitive material) and 1.53 wt. % (high-LME sensitive material) silicon content. A hypothesis can be made that the different LME cracking response of the two materials is caused from their different silicon composition. To verify this hypothesis, SEM and TEM micrographs can be taken to compare the thickness of the decarbonization layer (ferrite layer) of the welds made in the two materials after applying the same welding parameters. It is expected to see the high-LME sensitive material has thicker ferrite layer near the surface of the steel substrate. Furthermore, the feasibility of the research observations with the respect of the influence of electrode force on LME cracking severity can be tested on various AHSS and zinc coatings.

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