

CRANFIELD UNIVERSITY

STEPHAN ALFRED EGERLAND

SPLIT ANODE CALORIMETRY OF LOW CURRENT TIG
ARCS

SCHOOL OF AEROSPACE, TRANSPORT AND
MANUFACTURING
Welding Engineering

PhD

Academic Year: 2012 - 19

Supervisors:

Dr Paul COLEGROVE

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Abstract

Various techniques have been used to understand the distribution of energy within a Tungsten Inert Gas (TIG) Welding arc. Spectroscopy, electrostatic probe measurement, split anode calorimetry, as well as advanced numerical modelling techniques to a varying degree have found their application in scientific research - deployed to improve the comprehension of phenomena observed in TIG arcs interacting with the parent material to join. While spectroscopic methods have the advantage of being able to determine arc energy density variations as a function of the temperature of electrons and other charged species in the plasma, it is difficult to determine the power or heat flux arriving at the anode or workpiece. The electrostatic probe technique, conversely to spectroscopy, is reckoned among the invasive methods in arc plasma investigations. That is, the subject of investigation is, to considerable degree, affected in its structure by interacting with the biased probe wires transferred through the plasma plume which may finally lead to substantial deviations from the real conditions actually present inside the arc. Numerical modelling, which is based on assumptions simplifying the real arc plasma conditions to a certain degree, still require the empirical experiment for validation of the results achieved by the model. Finally either the measurement of radial arc pressure as an equivalent of the energy content of the arc, or deploying split anode calorimetry are the most appropriate methods, while the latter was found simpler to implement versus the former. Instead of a workpiece material which is normally used for welding applications, two water cooled copper anodes which are not melted by the welding arc are applied. As the welding torch is traversed across the interface between the two anodes the current and voltage flowing through each anode half is measured. As an additional benefit of split anode calorimetry the electrical arc power - achievable by multiplying the transient values of electrical current by the voltage correspondingly measured - and the calorimetric power or heat flux to the anodes can be measured. All aforementioned scientific approaches, however, require the involvement of partly complex numerical methods, since it appears impossible to directly derive the targeted information. This again may serve in part as a considerable source of error in the final output which can lead to significant variations while investigating welding arcs of similar appearance.

This study aims at deploying split anode calorimetry but pairing the investi-

gation of low current TIG arcs with the assessment of how the shielding gas nozzle design may affect the radial energy density of the arcs applied. Besides fundamental information on the mere distribution profile of low current TIG welding arcs and answering the question whether a regularly postulated Gaussian-like distribution applies, the involvement of different nozzle diameters was targeting at also deriving valuable information for the welding practitioner deploying TIG welding as an industrial joining process. It could be shown that phenomena were observed through the investigation which have not been reported so far in the literature, however were found to partly considerably affect the final output of the measurement and hence also influence the degree of significance of the results achieved.

In TIG welding arc investigation split anode calorimetry is found comparably favoured over other methods because of its reasonable experimental set up and deriving robust and meaningful output from the measurement. It was thus scientifically attempted to achieve quantitative explanations for the phenomena occurring; to provide future researchers with information seriously to be taken into account when intending to apply split anode calorimetry for TIG welding arc analyses.

Acknowledgements

I feel myself being blessed. Since having struck my first welding arc almost fourty years ago I can say: ‘*Welding is my life*’; being the main driving force behind this work. However, I couldn’t have made it to this point without a small number of remarkable human beings.

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‘Try me Lord
If you think there’s a way
That I can repay
What I’ve taken from you.’
Johnny Cash

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Introduction

Tungsten Inert Gas (TIG) Welding is broadly applied even in thermal joining of special and refractory metals to produce highest quality weld joints [1]. Invented in the 1940s and subjected to extensive scientific research it still is - similar to 'welding' as a whole - considered as a special process in codes and standards. This means that the quality of a weld joint cannot easily be predetermined by using specific parameters comparable to mechanical joining processes, but individually must be produced by the particular process applied. Consequently, the question *how* welding affects part structure and joint mechanical properties is of great interest [2] to the industry, thus, considered as one of the major driving forces in welding research [3]. Although TIG Welding to a broad extent is well understood, even nowadays some phenomena observed remain hardly explainable. For example, using the TIG welding arc to join workpieces of low wall thickness can lead to unstable arc - parent material interaction [4]. Increasing the weld current, in order to stabilise the process, often appears impracticable because of inducing further issues e.g. excessive penetration. Understanding these mechanisms, especially by comprehending the arc plasma properties, appears essential. Particularly the arc energy distribution and its impact to the workpiece is of great interest [3,5,6]. Aside from insight into arc generation [7–13], attempts were made not exclusively to consider the arc plasma but also its influence on forming the molten weld pool and its interplay with the parent material - especially introducing temperature dependent properties of the latter [14,15].

This work intends both to develop a generally improved understanding of the energy distribution of 'low current' [16] TIG arcs and to determine the effects of external factors. That is, how the design of shielding gas nozzles, deployed for protecting arc and anode surface from the surrounding atmosphere, influences the energy distribution of these low current arc plasmas. To achieve this, split anode calorimetry was applied, known as a robust methodology versus other techniques, due to providing reliable results in measuring electrical current- and calorimetric power or heat flux density. Therefore the study is essentially segmented in the following main chapters:

- 1. Literature Review**
- 2. Fundamental Investigation**
- 3. Derivation of Reference Values**
- 4. Investigation of Shielding Gas Nozzle Design Factors**
- 5. Discussion**
- 6. Future Work**

After discussing the results obtained through the different stages finally an outlook will be given for future work to focus on. It will be shown that the preconception of radial Gaussian energy distribution profiles in the arc, generally known from and published in the literature, appears not to basically hold for low current TIG arcs; particularly when using 50 amperes arc current. This is considered to be a fundamental finding of this investigation. Also it was found, that the experimental boundary conditions in applying the split anode calorimetry technique, as set up in this study, appears to bear a significant risk of delivering results with considerable noise in the output; impeding the mathematical approach applied in the study, theoretically requiring axial symmetry conditions in the arc plasma. The resulting noise in the calculations was found to hinder the development of an appropriate model thereby to achieve predictability of arc characteristics. However, the study will show that, among a number of different effects that can cause arc instabilities in industrial TIG welding arc application, also the non-Gaussian energy profile may add to these perturbations, especially when thermally joining light gauge materials.

To reasonably value the results achieved through the different stages of this study it is considered important to first reveal the genuine target of this investigation. The study was originally begun aiming at determining ‘*Fundamental Interaction Parameters of Low Current TIG Arcs*’. Due to different causes, however not further dealt with herein, this original target necessarily became adjusted along the course of the investigation to produce more ‘practicable’ results, suitable to the TIG welding users, to e.g. overcome specific issues occurring in the industrial environment.

In turn, it was decided especially to study the effects that different shielding gas designs or diameters may or may not have on the radial energy distribution of TIG arcs of less than 100 A. Apart from this, it became noticeable at a relatively early stage that the methodology applied could lead to some phenomena apparently not appropriately taken into account while considering the experiments and planning their conduction. For example, it was found that, applying a commercially available shielding gas nozzle, reported as uncomplicatedly applicable to regular (industrial) TIG welding applications, was leading to surface oxidation phenomena while using the split anode calorimeter produced, and apparently affecting the arc-anode interplay to a considerable degree. However, this was estimated at the initial stage due a broad variation of external parameters firstly to obtain a fundamental data matrix constantly to apply through the further course of the study. These information in mind the achieved results shall be discussed in an attempt of a more structured manner, finally to achieve a clearer understanding and also to provide an outlook to possible future work based on the findings obtained.

1 Literature Review

Welding arc phenomena are not trivial and extensive research was applied to understand their physics. This literature survey, particularly focusing on non-consumable welding arcs, for example used in Tungsten Inert Gas (TIG) Welding, also referred to as ‘Gas Tungsten Arc Welding’ (GTAW), shall reflect standard analysis techniques and the state of the art in welding arc investigation.

1.1 The Electric Arc - Fundamental Considerations

According to Llewellyn-Jones [17] the electric arc belongs to the group of electric discharges. Ayrton [18] states Davy [19] as the first one studying electric arcs around the year 1800. Further with Volta’s invention of the ‘galvanic pile’, electrical discharges both at atmospheric and vacuum conditions became subject of scientific research in the early beginnings of the 19th Century. An electric circuit or driving force is required for all arc phenomena to occur. Early applications of the electric arc were for street lighting [7, 19]. Figure 1.1 adopted from [19], schematically shows an electric arc typically deflected upwards by convection.

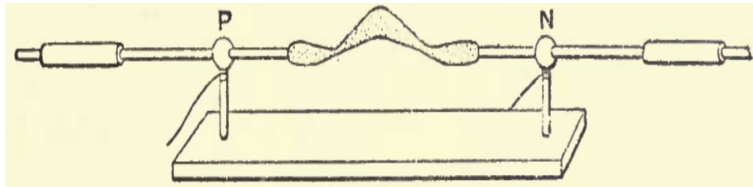


Figure 1.1: Schematic electric arc - sustained horizontally. After [19].

Akeman [7] investigated potential and fluid-flow of carbon arcs. He described the stationary arc as a self-consistent fluid flow, governed by two oppositional directed, but balanced, process stages termed ‘ionisation’ and ‘ion-recombination’. Detailed review of the virtually innumerable amount of systematic research on the arc is provided by [17]. According to Guile [9] the important regions adjacent to the electrodes show two principal characteristics:

- “High electrical and thermal fields”
- “Contraction – i.e. high current densities relative to those of the arc column”

Furthermore the arc plasma is found comprising three specific zones [20, 21] referred to as:

- i Cathode fall region
- ii Arc column region
- iii Anode fall region

Figure 1.2 (a), adopted from Hoyaux [8], schematically depicts the different arc regions as well as anode and cathode voltage drop. The arc is regularly more constricted adjacent to the cathode enabling to clearer visually distinguish the electric poles. Quantitatively proved by measurements Hoyaux, claims the linear voltage extrapolation close to anode and cathode, Figure 1.2 (b), as not fully reflecting the real electrical conditions.

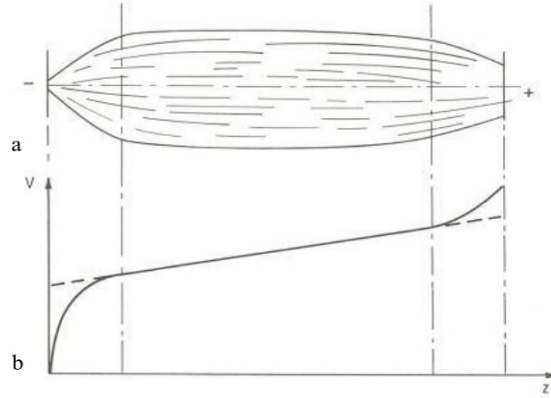


Figure 1.2: Arc regions (a) and corresponding voltage drops (b). Note that ‘V’ and ‘z’ are for voltage and longitudinal axis, respectively. After [8].

Schellhase [13] suggests balanced ionisation- and recombination-processes and the heat flux within the arc plasma fundamental in order to achieve and consistently maintain a stable arc. Figure 1.3 schematically shows generation, transport and recombination of charge carriers present in an arc.

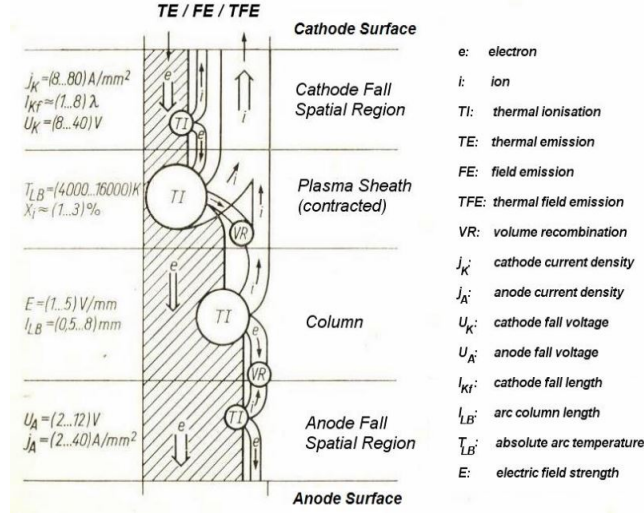


Figure 1.3: Schematic charge carrier generation and transport through the arc plasma, acc. to [13]

1.1.1 The Cathode Region

The electrically negative cathode supplies electrons to the arc plasma. Three different mechanisms have been found required for allowing electrons to emit from the cathode:

1. Thermionic Emission

Rising the temperature of an electrical conductor leads to an excitement of its atoms. Providing sufficiently high temperature, electrons, close to the surface and actually bound to the atoms, may be caused emitting from the metal. The electron density can be considered as a function of temperature and calculated applying the so-called Richardson-equation [13]:

$$j_K = A_R T^2 e^{\frac{e U_{aK}}{k T}} \quad (1.1)$$

j_K is the cathode current density, T represents the absolute temperature and e is the electron charge. U_{aK} is the cathode work function and A_R represents the Richardson constant:

$$4\pi e m_e k^2 / h^3 \quad (1.2)$$

where m_e is electron mass and respectively k and h are the BOLTZMANN- and PLANCK constant. Since the current density is heavily depending on the cathode surface temperature only refractory metals (e.g. tungsten) have been found capable of sustaining sufficiently high temperatures to enable thermionic emission *and* explaining the current densities as measured in e.g. gas shielded tungsten welding arcs [9]. Figure 1.4 graphically reveals material work function and temperature regimes required to enable thermionic emission.

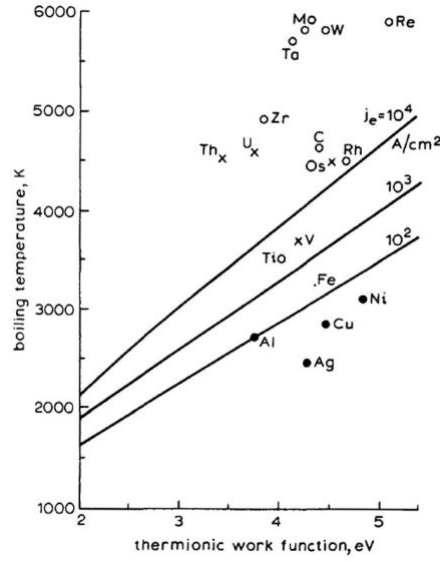


Figure 1.4: Material work functions and temperature regimes for initiating thermionic electron emission [9].

2. Field Emission

If the cathode temperature is low, also referred to as “*cold cathode*” [9] or “*non-thermionic*” [11]; electron emission can nonetheless be caused by increasing the field strength up to few 10^7 V/cm [13]. Non-thermionic electron emission is mainly affected by vapour generation on pure cathode surfaces as well as by oxide layers enabling the electron to tunnel the surface [11].

Equation 1.3 can be used to calculate the electron current density in $[A \cdot m^{-2}]$ [9, 13]:

$$J_e = \frac{6 \cdot 10^6 \left[\frac{\mu}{\phi} \right]^{\frac{1}{2}}}{\mu + \phi} E^2 e^{\frac{6.8 \cdot 10^9 \phi^{\frac{3}{2}}}{E}} \quad (1.3)$$

J_e is the electron current- and E the electric field intensity. Respectively μ and ϕ represent the Fermi energy level and the material dependent electronic work function of a surface. It needs mention that equation 1.3 does not take into account the ratio between the current carried by electrons and ions and thus, needs appropriate adjustment [9]. Schellhase [13] and Lowke and Tanaka [22] suggest that, when calculating the electron emission purely based on field effects, the amount of emitted electrons is insufficient to consistently maintain the arc discharge. Bollenrath *et al.* [23] whereas consider welding arc field emission effects especially important in alternating current (AC) gas tungsten arc welding, due to the cyclically varying polarity which causes the regime periodically changing from thermionic- to field emission and vice-versa.

3. Thermionic Field-Emission (TFE)

The boundary between thermionic- and field emission is considered fluent. Conn [24] suggests increased temperatures to also causing reduced field strengths, thereby assisting electrons easier to emit from the cathode. Involving temperature into field emission effects the cathodic electron density according to [13] can be calculated as:

$$j_c = \frac{4\pi e m_e k^2}{h^3} T^2 e^{\frac{eU_{cm} - \sqrt{e^3 E_{cm}}}{kT}} \quad (1.4)$$

e is the elementary electric charge and m_e is the electron mass. Respectively k and h are the BOLTZMANN and PLANCK constant. T is the absolute temperature, U_{cm} is for cathode material electron work function and E_{cm} represents the cathode field strength. Surface roughness, contaminants as well as material inhomogeneities, e.g. grain boundaries, may cause overlapping effects between thermionic and field emission to occur.

Guile [9] describes the theory of the cathode region as complex and to overcome this he suggests three specific areas to distinguish its spatial extension:

1. Contraction Zone

- (a) High Luminosity Zone
- (b) Space Charge Zone

High current density at the cathode and lower density in the arc column causes a field strength gradient, hereby the region close to the cathode to constrict; i.e. forming the *contraction zone*. Adjacent to this contraction zone ionisation and excitation processes cause the *high luminosity* zone to occur [9]. The *space charge zone* finally is suggested being a thin sheath covering the cathode [11]. It is of particular interest due to its high voltage gradient, capable of accelerating electrons and ions. Highly dynamic interactions in the arc plasma partly merge these regions to each other. That is, they should be not considered as precisely confined [9]. The space charge zone is believed leading to different visible arc shapes [11]. This is confirmed by Goldman [20] who systematically defined 5 (five) *fundamental* shapes in using argon shielded pure tungsten cathodes, see Figure 1.5.

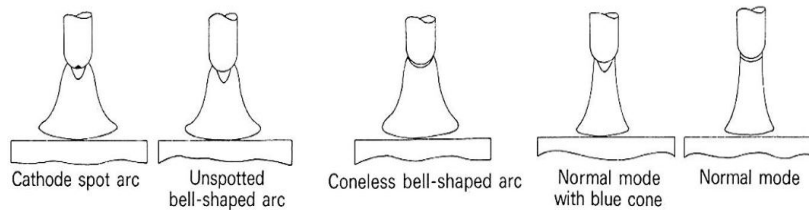


Figure 1.5: Schematic of fundamental visible arc shapes. Argon shielded pure tungsten electrode. After [20].

1.1.2 The Arc Column

Fanara [25] describes the arc column more expanded, Figure 1.6, compared to cathode- and anode zone. The column is characterised as a homogeneous

mixture of electrical charge carriers; i.e. electrons and ions. Electrical neutrality and uniform temperature distribution lead the arc column to show ‘local thermodynamic equilibrium’ (LTE) conditions [11].

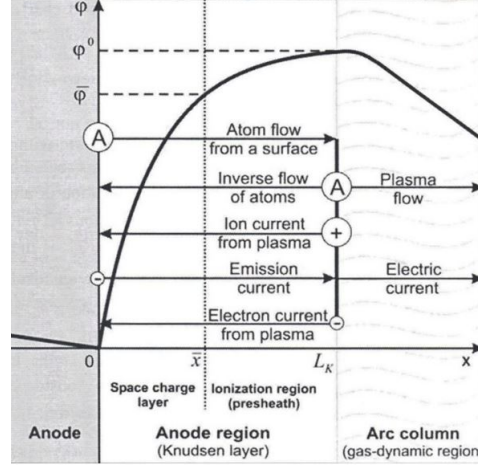


Figure 1.6: Schematic welding arc anode region. Note that φ represents the potential. After [21].

An electrical virtually neutral space charge layer of low thickness ($\approx 10^{-8}$ m) is considered providing the major voltage drop between the solid anode and the plasma. Ionisation and recombination processes take place in a ionisation region, also referred to as ‘*presheath*’. Both in common is termed Knudsen layer; defined as the anode region boundary that merges with the arc column [21]. On the one hand its spatial dimensions are suggested varying because being boundary condition dependent [25], but also essential, due to enabling continuous current flow to the arc plasma, thus maintaining stable arc conditions [9].

Apart from the current density of 10^6 to 10^8 Am^{-2} [9,25] the ambient pressure is found to significantly influence anode arc attachment. Two different anode modes can be defined connected to this [11]:

- Normal Anode Mode
- Anode Spot Mode

The normal anode mode is characterised by “bell-shape” symmetry, Figure 1.6 (a) and described relatively resistant to any anode motion [11].

The arc current flow is markedly constricted to single spots while in anode spot mode, Figure 1.7 (b), which makes the arc difficult to control. Hence, the anode spot mode is not recommended for welding.

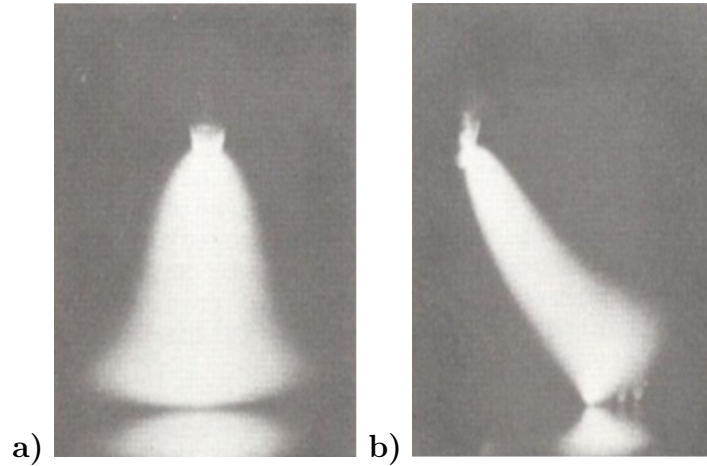


Figure 1.7: Gas Shielded Tungsten Arcs deployed under different ambient pressures leading to different arc modes. *a* - Normal anode mode and *b* anode spot mode. (After [10])

1.2 The Tungsten Inert Gas Arc

This work particularly deals with the TIG welding arc which has become important in the industry for achieving high-quality weld results and shall be elaborated in the following sections.

1.2.1 Arc Generation

Welding power sources consistently supplying electric energy to the welding circuit meet the requirement to continuously maintain ionisation and ion-recombination. H. M. Hobart and P.K. Devers claimed a patent in 1926 on applying an electric tungsten arc shielded by helium (Hobart) or argon (Devers) [26]. Figure 1.8 represents the schematic circuit diagram of a conventional rectifier power supply to produce both direct- (DC) and alternating current (AC) gas tungsten arcs.

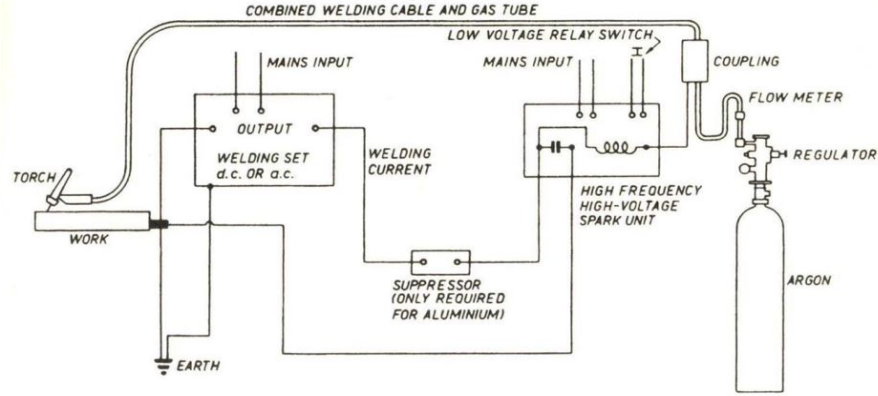


Figure 1.8: Schematic gas tungsten arc welding setup. After [27].

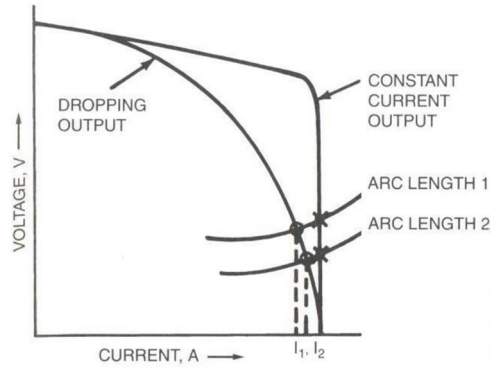


Figure 1.9: Schematic TIG power source output characteristic. After [26].

The electrical power source output characteristic shows either constant current or drooping voltage-current behaviour, Figure 1.9. TIG welding makes use of high melting tungsten electrodes and the electron emission can be improved by doping the tungsten with elements providing a lower work function. Particularly rare earth element oxides such as cerium-oxide (CeO_2), lanthanum-oxide (La_2O_3) or zirconium-oxide (ZrO_2) were found beneficial dopants. Table 1 provides overview over possible electrode material compositions.

AWS Classification ¹	Colour Code	Dopant	Dopant Oxide	Dopant Content [wt-%]
EWP	Green	-	-	-
EWCe-2	Orange	Cerium	CeO ₂	2
EWLa-1	Black	Lanthanum	La ₂ O ₃	1
EWLa-1.5	Gold	Lanthanum	La ₂ O ₃	1.5
Blue	Lanthanum	La ₂ O ₃	2	
Yellow	Thorium	ThO ₂	1	
Red	Thorium	ThO ₂	2	
Brown	Zirconium	ZrO ₂	0.25	
Grey	Not specified ²	-	-	

Table 1: TIG electrode dopants and colour codes. After [26].

Flemming [28] calculates the heat Q_c generated at the non-consumable cathode as:

$$Q_c = iu_c - A \quad (1.5)$$

and the heat produced at the anode as:

$$Q_a = iu_a + A \quad (1.6)$$

where respectively i , u_c and u_a represent current density and cathode- and anode fall voltage. ‘ A ’ represents a material work function constant.

Equations 1.5 and 1.6 show the heat generated at the tungsten electrode is lower with electrode negative polarity (DCEN) vs. positive polarity (DCEP) - the main reason for regularly applying the electrode as the cathode. Electrons emitted from the cathode flow to the workpiece and, in turn, positive

¹According to AWS A5.12.

²Manufacturer must identify type and nominal content of oxide or dopant additions.

ions are transferred from the workpiece to the cathode. Figure 1.10 schematically illustrates this behaviour simplified for both direct current (DC) and alternating current (AC).

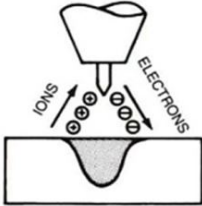
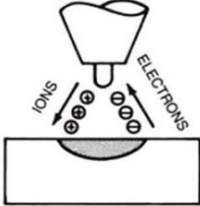
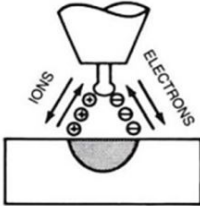
CURRENT TYPE	DCEN	DCEP	AC (BALANCED)
ELECTRODE POLARITY	NEGATIVE	POSITIVE	
ELECTRON AND ION FLOW			
PENETRATION CHARACTERISTICS			
OXIDE CLEANING ACTION	NO	YES	YES—ONCE EVERY HALF CYCLE
HEAT BALANCE IN THE ARC (APPROX.)	70% AT WORK END 30% AT ELECTRODE END	70% AT WORK END 30% AT ELECTRODE END	70% AT WORK END 30% AT ELECTRODE END
PENETRATION	DEEP; NARROW	SHALLOW; WIDE	MEDIUM
ELECTRODE CAPACITY	EXCELLENT e.g., 3.2 mm (1/8 in.) 400 A	POOR e.g., 6.4 mm (1/4 in.) 120 A	GOOD e.g., 3.2 mm (1/8 in.) 225 A

Figure 1.10: TIG electric current characteristics. After [26].

Cathode dimension and composition are stated amongst others to affect the voltage-current characteristic in TIG [10]. Figure 1.11 schematically plots the voltage drop produced from pure and thoriated tungsten electrodes.

1.3 TIG Arc Measurement Methods

The evaluation of results of any interaction between arc plasma and workpiece requires a thorough understanding of how power is produced within the arc and being transferred to the anode. Some meaningful technologies shall be dealt with hereafter to quantitatively describe these mechanisms.

1.3.1 Spectroscopy

Atomic spectroscopy, or, more accurately, optical emission spectrometry (OES), was actually developed for chemistry elemental analysis purposes but can also be used for arc analysis. Its fundamental principle is based on radiation emitted by energy carrying species, e.g. electrons, within the

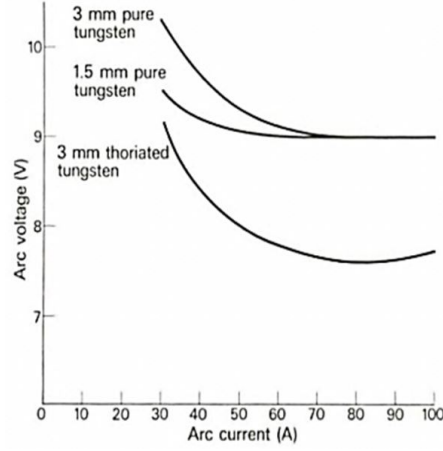


Figure 1.11: Voltage and current behaviour affected by electrode material composition and diameter. After [10].

arc plasma. Different elements, deliberately transported into an inductively coupled plasma, are ionised and the emitted wavelengths analysed. Basically electrons orbit the atomic nucleus at discrete distances equivalent to different energy levels, see Figure 1.12.

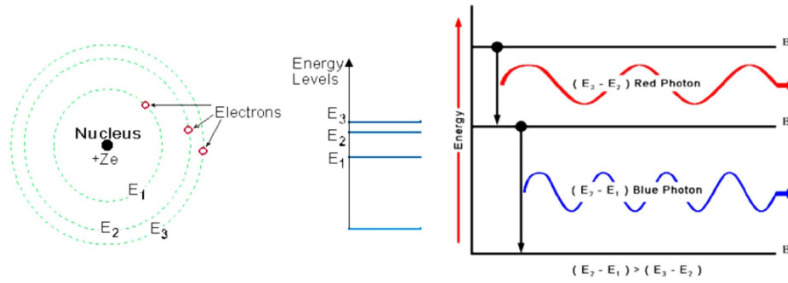


Figure 1.12: Schematic energy levels in an atom. After [29]

Although relatively stable bound to these orbitals, electrons can nonetheless be elevated from their original energy level, referred to as excitation. Hence, energy provided to the atom can raise an electron to some higher energy level, say E_n . This excited electron, however, strives back to its original, energetically more stable, ground level, say E_m . As a consequence, characteristic energy packages, equal to that energy amount absorbed before and

referred to as ‘*light quants*’, are emitted. The relation between the energy and the frequency or wavelength of the emitted light quant is fundamental and can be expressed:

$$E_m - E_n = h\nu = \frac{hc}{\lambda} \quad (1.7)$$

where h is PLANCK’s constant, c is the speed of light and λ is the specific wavelength of the light quant emitted.

Each species or particle excited within a scatter band caused by Heisenberg’s uncertainty principle, emits a characteristic wavelength [30], corresponding to the particle’s energy content and referred to as particle emission spectrum, see Figure 1.13. That is, the wavelength can be used to derive e.g. the temperature. Spectrometry is applied to determine light wavelengths radiated from the arc plasma to further assess on the plasma energy content. Different to methods directly affecting the plasma by interaction (e.g. Laser Scattering), OES may be considered as a *non-invasive* technique; i.e. the plasma is not perturbed by the method applied.

Behringer [30] explains a frequency of $2.418 \cdot 10^{14}$ Hz = $1.602 \cdot 10^{19}$ J = 1 eV, equals a temperature of $1.1604519 \cdot 10^4$ K [31]. Hence, it becomes possible to deduce the plasma temperature from its energy content [30]. Providing a welding arc as the light source; *Inductively Coupled Plasma-Optical Emission Spectrometry* (ICP-OES) [32] can be seen as an appropriate method applicable to welding arc spectrometry.

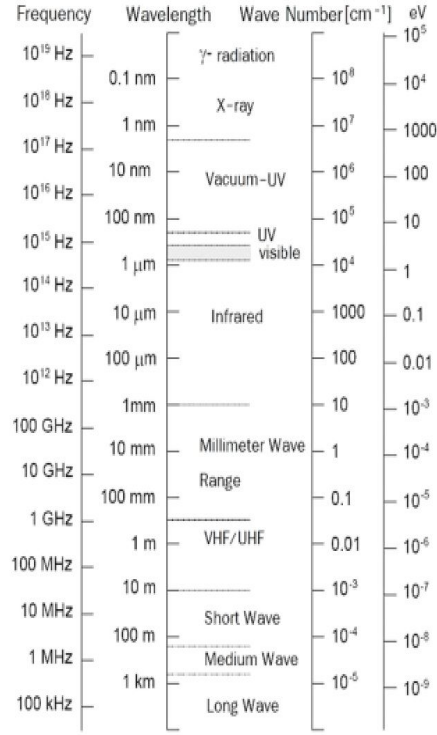


Figure 1.13: Relationship between frequency, wavelength and temperature (eV). After [30].

Figure 1.14 schematically depicts the experimental setup used by Ma *et al.* [33] for welding arc spectroscopy.

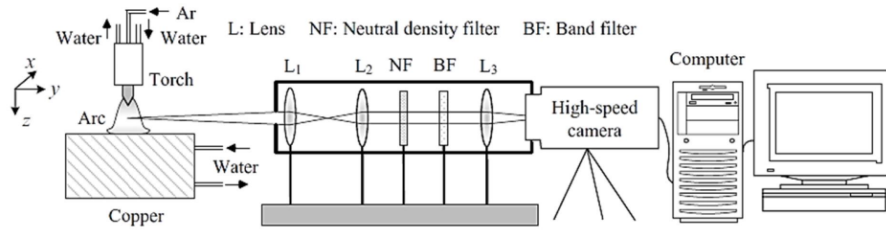


Figure 1.14: Schematic experimental setup used for spectroscopic analysis of energy density and temperature profile of pulsed gas tungsten arcs. After [33]

Numerous researchers used non-invasive spectrometry to investigate electric arcs. Ma *et al.* [33], Olsen [34, 35], Vilharino and Scotti [36], Yeates [37], Vervisch *et al.* [38], as well as Thornton [39], Cram *et al.* [40], Haddad and Farmer [41], Farmer *et al.* [42] and Degout and Catherinot [43] have focused on free-burning gas shielded tungsten arc plasmas.

Nick *et al.* [44] used a specially designed apparatus to cascade the argon arc between two anodes and two cathodes. Tanaka and Ushio [45, 46], and Tanaka *et al.* [47] used direct spectroscopy; i.e. laser scattering phenomena, to evaluate the arc temperature close to the anode.

Arc plasma was investigated using regular process conditions [33], [36–43, 45, 46, 48] and [45, 46], or deploying special designed chambers filled with shielding gas [34, 35] and [44]. Another important distinction in spectroscopic plasma investigation is drawn by the anode material being either water-cooled non-melting copper [33–36, 39, 41–43], or molten metal [37, 38, 42, 43].

Cram *et al.* [40] different to practical experimental investigations, developed a theoretical model mainly involving the spectroscopic research of Haddad and Farmer [41] and Farmer *et al.* [42].

Special assumptions are made on the arc plasma to derive the desired information from spectrometry and particular mathematical means are required. Figure 1.15 schematically shows that the actual radial point intensity of a certain unit plasma volume needs presumption as a line intensity in order to achieve the radial plasma energy density.

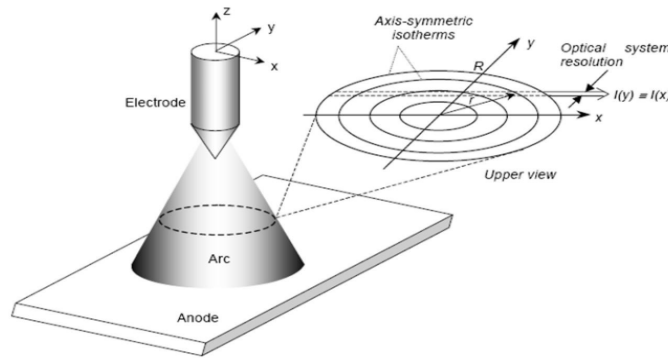


Figure 1.15: Schematic arc line intensity detected by the optical system. After [36].

Thornton [39] suggested to apply a mathematical conversion method termed ‘Abel-Inversion’, written as:

$$\varepsilon(r) = -\frac{1}{\pi} \int_r^R \frac{1}{\sqrt{(x^2 - r^2)}} \frac{dI(x)}{dx} dx \quad (1.8)$$

$\varepsilon(r)$ is the dimensionless normalised emission coefficient and r and R , respectively, represent the radii of interest as depicted in Figure 1.15. However, the mathematical approach proposed by [39], regularly needs appropriate adjustment depending on the experimental boundary conditions [36, 39, 41].

Olsen [34] maintained an arc between a tungsten cathode and a water-cooled copper anode, arranged in a vacuum tight argon filled chamber, Figure 1.16. Plasma temperatures between 10,000 and 25,000 K along with welding currents of 200 – 800 A have been determined choosing particular argon species wavelengths.

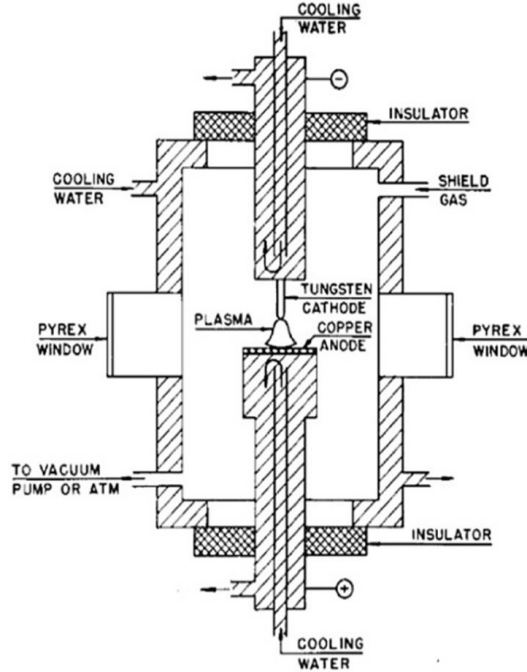


Figure 1.16: Experimental chamber for arc spectroscopy. After [34].

Haddad and Farmer [41] studied free-burning gas tungsten arcs at currents of 100 A and 200 A. Inconsistencies were found between experimental- and calculated results and explained due to variations in the plasma temperature distribution; while Farmer *et al.* [42] operated a free-burning 200 A gas tungsten arc upon a melting anode (AISI 304 stainless steel) to compare the results with a former work using water-cooled copper [41]. Chromium (Cr) spectral lines are suggested useful for metal vapour evaluation and Cr vapour was found slightly diffusing (≈ 1.0 mm) axially from the molten pool into the arc column. The authors suggest any further axial expansion suppressed by high velocity plasma jets streaming towards the anode. Metal vapour from the molten anode is underscored to neither significantly change the arc structure nor the arc temperature versus a water-cooled copper anode, see 1.17.

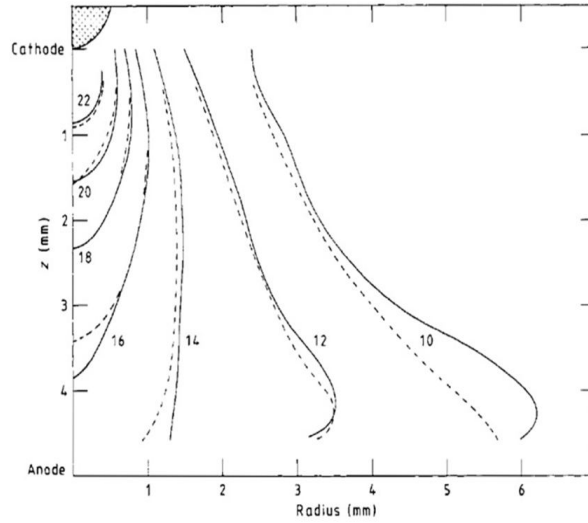


Figure 1.17: Differences in the temperature profile between water-cooled copper (broken lines) and stainless steel anode (solid lines) for a 200 ampere gas shielded tungsten arc of 5 mm length. Note: temperature scale in $K \times 10^3$. After [42].

In a more recent work metal vapour effects were spectroscopically studied by Tanaka and co-workers [48], who have analysed a free-burning helium shielded tungsten arc, also deploying an AISI 304 stainless steel anode. Although confirming Farmer and Haddad's [42] results that cathode jets appear

capable of sweeping metal vapour away from the weld pool, they nonetheless, and unlike Farmer and Haddad, could detect only iron (Fe) vapour close to the weld pool (anode). The plasma volume again was found enriched by “large” amounts of Cr- but also Manganese (Mn) vapour. Convective circulation is suggested to cause greater amounts of Cr-and Mn vapour diffusing into the arc plasma, mainly affected by *concentration gradient* for Mn (lower boiling point) and *potential gradient* for Cr (lower ionisation potential). If the diffusion forces exceed the cathode jet forces, metal vapour by convective circulation flow is transported into the plasma [48], Figure 1.18.

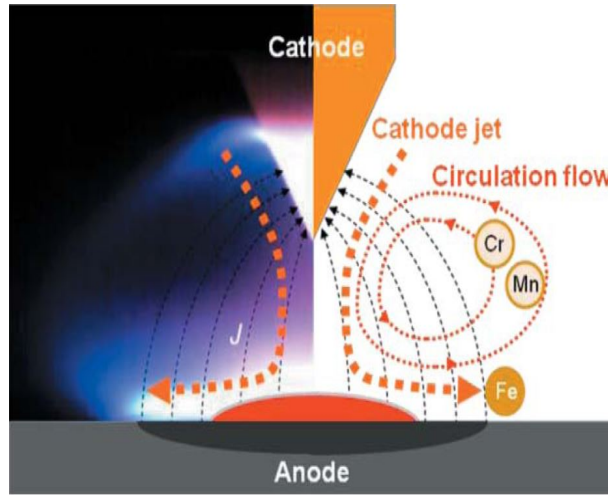


Figure 1.18: Convective circulation in gas shielded tungsten arc. After [48]

Vervisch *et al.* [38] used spectroscopy to study penetration depth variations in AISI 304 stainless steel TIG. Periodic spectral line fluctuations were found, caused by a rotating anode spot of increased temperature providing metal vapour to the plasma. The region of interest, i.e. ≤ 0.5 mm above the anode, is described difficult to analyse using spectroscopy. [38] suggest the weld pool considerably important due to producing metal vapour affecting the plasma - anode interaction.

Degout and Catherinot [43] assembled a water-cooled AISI 304 molten anode experimental setup to determine essential arc plasma parameters. They found a metal vapour layer above the anode whose thickness is proposed to be a function of current density.

Yeates [37], using a laser for melting an aluminium specimen, states that

plasma temperature and electron density are the most important variables in plasma diagnosis and suggests the ‘Line-to-Continuum’ technique preferable for calculating the electron temperature.

Vilharino and Scotti [36] found sufficiently high electron excitation, as well as the welding arc’s ultraviolet, visible and infrared spectral emission, beneficial for spectroscopic temperature evaluation. The authors investigated a low current (40A) free-burning gas tungsten arc maintained upon a water-cooled copper anode. The Fowler-Milne-technique, explained in-depth in Fowler [49], was used for converting the spectroscopic results into temperature. This technique has also been used and described suitably by other researchers [30, 33, 39]. Vilharino and Scotti [36] explain the Fowler-Milne-technique, however, as leading to calculation errors for arc temperatures $T \lesssim 10 \times 10^3$ K. Thus, it is assessed limited to only higher temperature regimes ($10 \times 10^3 \leq T \leq 25 \times 10^3$ K). Modifying the method and expanding the scale to lower current welding arcs the authors have overcome these limitations.

Linear instead of a Maxwellian plasma particle density distribution - actually presumed for an arc plasma - was revealed by [36]. The question whether particle collision is the governing physical mechanism, leading to negligible plasma temperature, i.e. particle density gradients and uniform temperature distribution; hence, leading to thermal equilibrium has raised special interest throughout spectroscopic arc research [33, 34, 36, 39–41, 44].

Thornton [39], involving the work by Cram *et al.* [40], claims local thermal equilibrium (LTE) for the arc centre but partial local thermal equilibrium (PLTE) only for the its boundaries. Special attention is required when transferring the obtained results to arcs differing in diameter or maintained at different ambient pressures.

Nick *et al.* [44] established *non*-LTE conditions in producing cascaded atmospheric pressure argon arcs and using special experimental equipment, Figure 1.19.

Haddad and Farmer [41] suggest that the assumption of a departure from LTE may cause, in part significant, discrepancies between experimental and theoretical results. The authors claim that presuming LTE for the particular type of (welding) arc investigated is invalid.

Ma *et al.* [33] however, presumed LTE for the sake of simplification and found a strong relationship between cathode surface- and plasma temperature. The plasma temperature decreases with increasing cathode surface

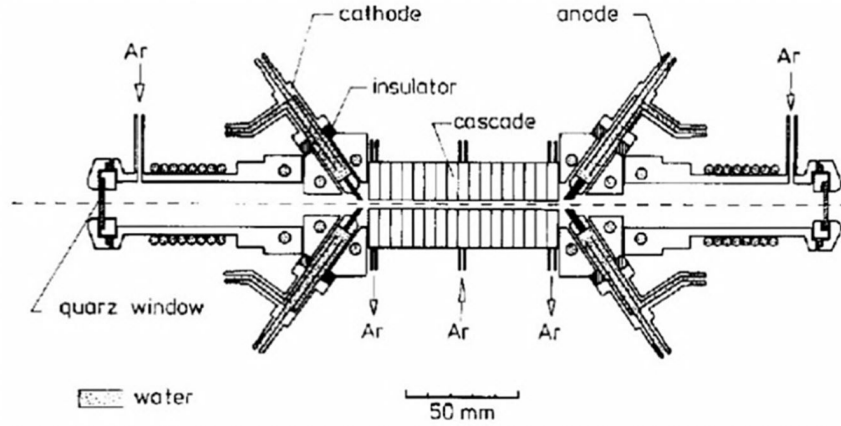


Figure 1.19: Experimental setup for spectroscopic argon shielded cascaded tungsten arc analysis. After [44].

temperature, due to an enlarged cathode area raising the arc current by increased thermionic emission.

Unlike Thornton [39], Vilharino and Scotti [36] could reveal only negligible temperature differences between experiment and theory (Fowler-Milne-technique). In contradiction to [39] the authors thus suggest LTE conditions to exist also in lower current plasmas and at the arc fringes.

An invasive method for investigating arc plasma behaviour is referred to as ‘*laser scattering*’ [45–47]. The field strength of a laser beam, focused on a specific cross sectional area of the arc, causes electrons to oscillate thereby emitting electromagnetic waves [46]. Two different phenomena can be described occurring related to the particles that cause scatter:

- Thomson scattering (Electron feature) – caused by free negative charge carriers (electrons) in the plasma.
- Rayleigh scattering (Ion-peak Rayleigh) – caused by interaction of the laser field with electrons attracted to and orbiting the atomic nucleus. Rayleigh scattering is related to heavy particles (atoms and ions).

Figure 1.20 depicts experimental setup for laser scattering, used by Tanaka and Ushio [46].

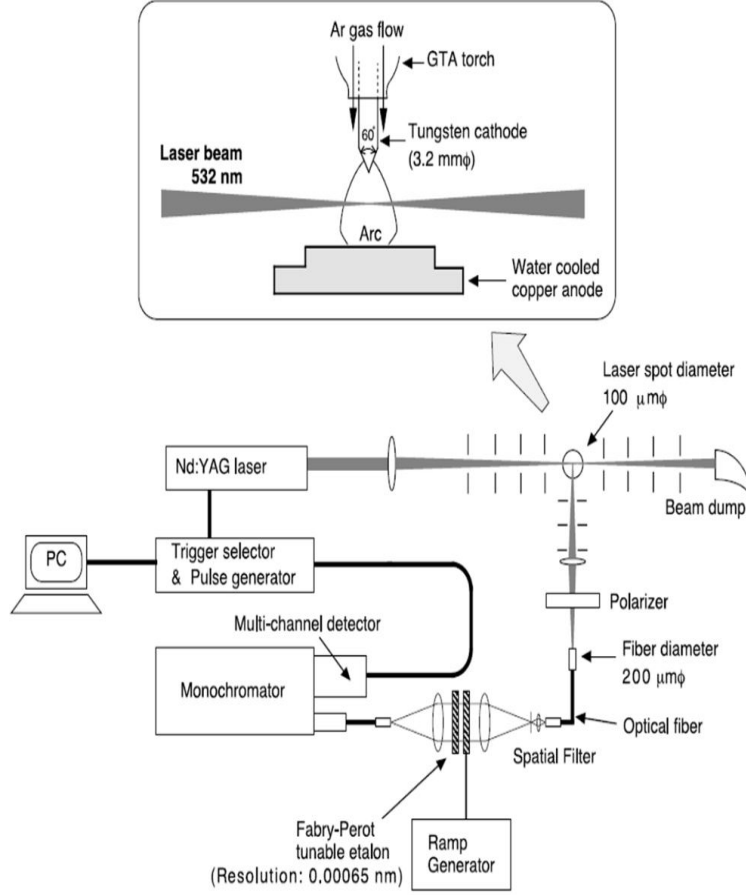


Figure 1.20: Schematic experimental setup for laser scattering of atmospheric argon shielded tungsten arc. [46]

Temperatures of $23 \times 10^3 \text{ K} \pm 5\%$ for the electron peak temperature, as $17 \times 10^3 \text{ K} \pm 10\%$ for the heavy particles were detected for a 150 A current arc. Ushio *et al.* emphasise the impact of the electrical current on heat transfer to the anode. For lower current arcs (50 A) the main fraction of heat transfer to the anode is suggested being carried by electrons; conduction and convection whereas prove but secondary. For higher arc currents (150 A) conduction increases by a factor of approx. 5. Independently of the investigated arc length (2.5 mm; 5.0 mm and 10.0 mm) constant average electron temperatures of $22 \times 10^3 \text{ K}$ were found close to the cathode. The electron temperature close to the anode however, was found decreasing, dependent on the arc length.

That is, 22×10^3 K; 16×10^3 K and 10×10^3 K for respectively 2.5 mm, 5.0 mm and 10.0 mm electrode to work distance, [50].

Quigley *et al.* [51] have accounted for dissimilarities present between non-melting copper anodes and real welding conditions; i.e. the effects of metal vapour in the arc. Deploying spectroscopy in connection to a vaporising iron anode they could, different e.g. to the results of Schoeck and Eckert [52], determine smaller amounts of arc power transferred to the anode. Attributed to metal vapour effects the authors suggest power transfer by convection, conduction and radiation from the shielding gas to play a rather secondary part. For an arc of 1.6 kW power (100 A / 16 V) they deduced the energy fractions according to Table 2.

Total arc power IV	1600 W	100 %
Workfunction I_ϕ	420 W ± 30 W	26.2%
Thomson effect I_{th}	60 W ± 25 W	3.8%
Anode fall IV_a	150 W ± 50 W	9.4%
Conduction H_{cd}	70 W	4.4%
Convection Q	33 W ± 15 W	2.1%
Radiation P_R	20 W	1.2%
Evaporation P_E	- 33 W	2.1%
Radiation from Pool P_a	- 10 W	0.6%
Total Power Transfer	710 W ± 120 W	44.4% ± 7.5%

Table 2: Total heat balance for a 1.6 kW atmospheric argon shielded tungsten cathode and mild steel anode arc [51].

Summary

Spectroscopy is a valuable means both with consumable electrode welding (MIG/MAG) [53, 54] but also especially in non-consumable electrode welding arc analysis. To achieve technically meaningful information from the captured spectral data it is, however, necessary to convert these e.g. into temperature profiles, which requires to take into consideration a number of different essential variables; e.g. the effects of metal vapour [55] and special mathematical approaches are applied to achieve this. While modified magnetohydrodynamic (MHD) equations are used to evaluate experimental spectroscopic findings in MIG/MAG processes [55]; the ‘Abel-Inversion’

is mentioned to serve among the most important ones when TIG arcs are concerned. Nonetheless, the literature reveals that the mathematics is often remaining discussable among the researchers. Specifically ‘adjusting’ the numerical means in accordance to the aimed objectives appears evidently required. Unlike MIG/MAG welding arcs, non-consumable electrode processes like TIG welding are regularly considered gas-, but not metal vapour dominated. The findings derived from research involving metal vapour, thereby pronouncing real welding environment conditions, [38,42,48] could show that the arc plasma is indeed affected by metal vapour interaction [38,48], but - as far as experimental conditions are applied as in the literature - this is found to have no significant effect on arc temperature distribution and shape [38].

Appropriate effort was expended to clarify the question whether local thermal equilibrium (LTE) conditions may be assumed for the arc plasma [54]. LTE would advantageously afford treating the plasma as a fluid, thereby to simplify mathematical processing. Various researchers [38–42], suggest that the arc is deviated from LTE, at least within particular regions, e.g. its fringes. Others again [33] suppose LTE for the whole plasma, either resulting from experimental outcome or for the sake of simplification.

Invasive *laser scattering* may be assessed more precise versus non-invasive spectrometry which, to a certain degree, is due to an improved understanding of the variables affecting the results. Inaccuracy caused by limited spatial and time resolution, deviation from thermal equilibrium conditions in the arc plasma [30], as well as the compulsory need to precisely identify the elements that produce the spectra of concern [36] may be stated limiting the abilities of spectroscopy in welding arc analysis. Tanaka and Ushio [45,46] thoroughly studied anode heat transfer mechanisms in TIG welding by applying invasive laser scattering for radial line scanning free-burning argon arcs. The authors reveal difficulties in properly treating the experimentally derived scattering signal which can be estimated as the sum Rayleigh scattering (representing the - lower - heavy particles temperature) and Thomson scattering (representing the - higher - electron temperature).

Also, the increase of the electron temperature caused by interaction with the laser beam is required to be taken into account; hence, to “extrapolate to the real electron temperature at zero laser energy”. However, and despite making use of different experimental boundary conditions versus this study, Gaussian radial temperature distribution for a 150 A argon arc plasma was postulated in applying this measurement principle [46]. In summary spectrometric methods and their output quality stringently appear to depend on the experimental conditions chosen.

1.3.2 Arc Power Measurement - Determination from Calorimetry

The Split Anode Technique Literature reveals a number of studies dealing with anode mechanisms in arc welding. An important method is referred to as ‘*split anode*’ technique. Because being essential also for this work it shall be dealt with in greater detail. Based on fundamental research of Nestor [56], the method uses a water-cooled copper body divided in two halves and split by a narrow gap. Figure 1.21 (a, b) adopted from Tanaka *et al.* [6] and Tsai [57] schematically shows the experimental setup and plane surface probe principle.

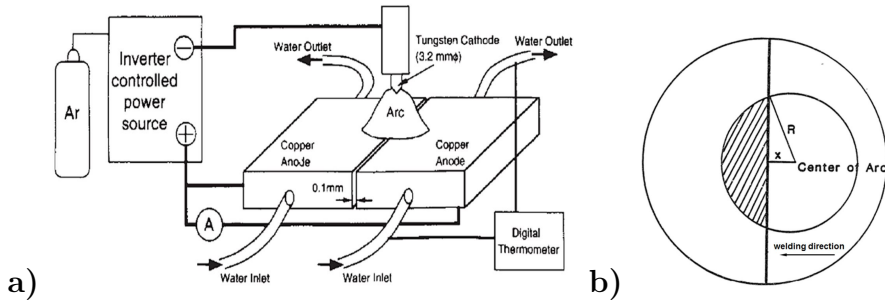


Figure 1.21: a - Schematic split anode experimental setup (a) [6], b - plane surface probe principle [57].

Figure 1.22 plots the real environment split anode calorimeter used by Tanaka *et al.* in [6].

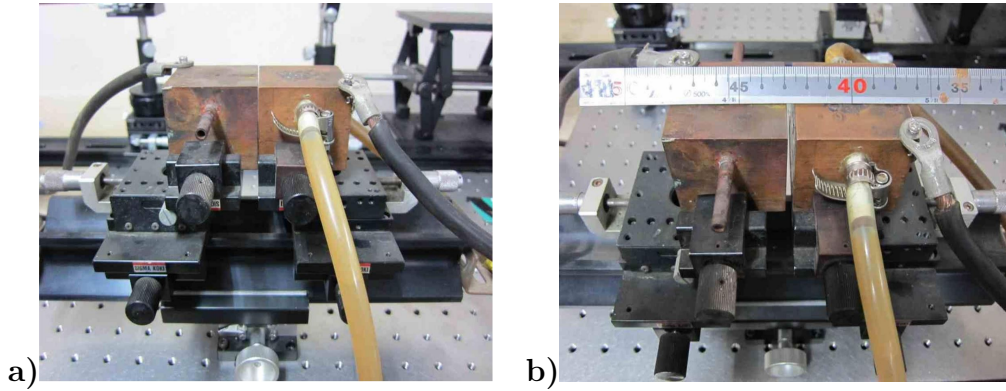


Figure 1.22: *a* - Split copper anode calorimeter utilised in [6]; *b* - calorimeter dimensions. (Photos friendly courtesy of *M. Tanaka*)

A gas tungsten welding arc is ignited upon one anode probe (hereafter referred to as '*probe 1*'), and consistently maintained at constant axial distance while traversed across the anode. Complying with the calorimeter principle, the difference (Δ_T) between the water inlet and outlet temperature is used to derive the amount of power transferred to the substrate. Providing sufficient low arc travel speeds, Δ_T , at any position becomes balanced, comparable e.g. to deploying a stationary arc. Power- and current density distribution at the anode surface may be estimated according to Figure 1.23 .

Electrons, condensing at the anode are commonly accepted as carrying the major fraction of energy through the arc [10,13,57,58]. Hence, *current density* is considered as proportionally affecting *power density* [58].

Consequently the shaded area, Figure 1.23 (a), is presumed as being equivalent to the fraction of current or power transferred to the right plane. According to Tsai [57] R and x respectively are describing the relationship between arc axis position and arc radius.

Apps and Milner [59], particularly considering heat flow by radiation and conduction, performed first extensive studies on argon shielded welding arc anode interaction. Continulative studies using the split anode technique differently configured were then accomplished by [6,50,57–62]. Further research was also performed on TIG anode mechanisms using single part copper- [63], or respectively steel- [51,57], as well as aluminium and titanium anodes [57]. Wilkinson and Milner [60] investigated monatomic (Ar; He) and diatomic

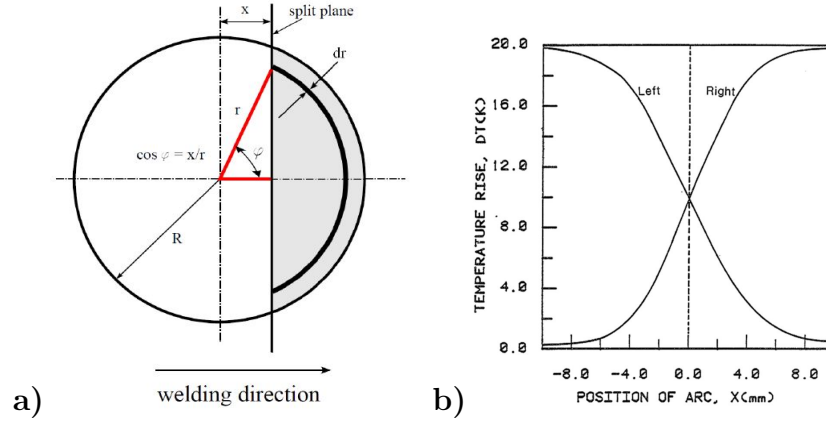


Figure 1.23: *a* Schematic split anode boundary conditions and *b* schematic temperature drop and rise as function of arc position. The shaded area in (a) represents the total integrated current supplied to ‘anode 2’. Note image (b) adopted from [57].

shielding gases (N_2 ; H_2) with a tungsten arc using a three-part split anode configuration, see Figure 1.24.

Wilkinson and Milner [60] suggested that the anode centre is heated only by the electron current stream while the annulus was assumed absorbing heat only from the plasma. The anode voltage drop and, as a consequence, the electron flux contribution to the anode were found depending on the shielding gas.

Nestor [56] may be considered as the first researcher who systematically studied heat- and current intensity distribution at the anode of high current (100 A / 200 A / 300 A) inert gas arcs therefore using the split anode technique.

The split anode calorimeter, assembling two water-cooled semi-circular copper parts separated ≈ 0.13 mm to one another, was mounted in a water cooled and vacuum tight brass chamber, see Figure 1.25. After evacuation shielding gas was introduced and various ambient pressures deployed. Nestor studied the effects of:

- Gas ambient pressure
- Cathode material and tip geometry
- Electrode separation (arc gap)

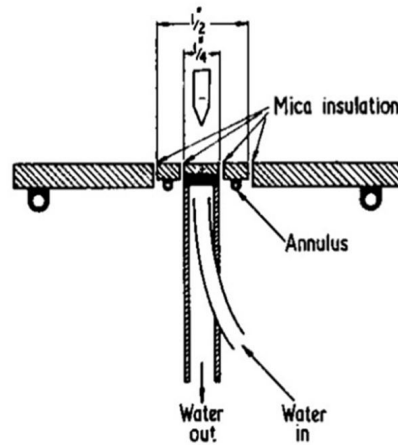


Figure 1.24: Schematic of the split anode used to determine electron- and plasma jet heating. After [60].

- Arc current
- Surrounding atmosphere composition
- Localised arc constriction

and additionally introduced an axial magnetic field through field coils mounted to the chamber, while maintaining the arc stable normal to the anode surface.

Nestor's work is estimated pioneering and both the physical principle - based on his novel measurement approach - and the results achieved from the broad variation of experiments are a main driving force for this own work, considering TIG arcs of lower current. Apart from that, Nestor has interestingly found a number of current- and power density results which are not normal distributed; e.g. with small electrode tip to workpiece distances.

Similar to Goldman [20] and referring to studies conducted by Olsen [34], Nestor defined two distinct arc attachment modes termed:

- Normal Mode (NM)
- Cathode Spot Mode (CSM)

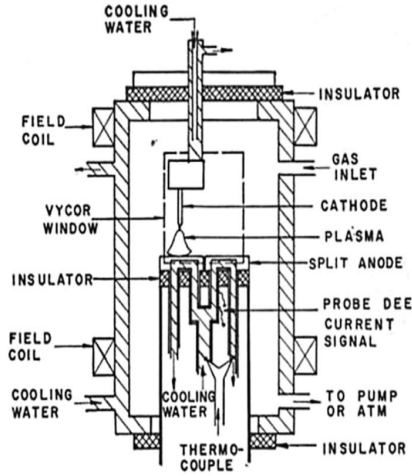


Figure 1.25: Split anode calorimeter assembly used by [56].

primarily affected by differences in cathode material, cathode-tip geometry and ambient pressure.

The power transferred to the anode was found similar for both modes with just negligible differences. CSM however showed reduced cathode contact areas vs. NM thus higher arc voltage, heat- and current intensity peak values are suggested resulting from CSM [58].

Arc plasma temperature- and current distribution were described difficult to determine and anode power transfer was observed decreasing with rising electrode to work distance.

Shielding gas heating intensity was found increasing with:

- Decreasing arc length
- Increasing arc current
- Change from NM to CSM

Argon shielded tungsten arcs at various current heights ($50 \text{ A} \leq I \leq 150 \text{ A}$) were studied by Schoeck and Eckert [61] and for 100 A weld current by Tsai and Eagar [64].

According to [61] hydrodynamic flow patterns (cathode jets) are the major difference between ‘high’- and ‘low’ intensity arcs. These jets, increasing convective heat transfer, are proposed to arise from variations of the pinch effect, a magnetically induced arc contraction along the cathode axis and connected to high current densities. Consequently higher weld currents induce stronger cathode jets. Varying current density along the arc axis results in field strength variations leading again to the arc’s bell shape.

Converti [65] indeed agrees on the importance of the Lorentz force (causing the ‘pinch effect’ and explained in greater detail in [10], controverts, however, Schoeck’s hypothesis of cathode jet generation. Instead of that Converti supposes the problem to be more complex and finally divides the Lorentz force into two vector components, depending on pressure and axial net force distribution, Figure 1.26 (a, b).

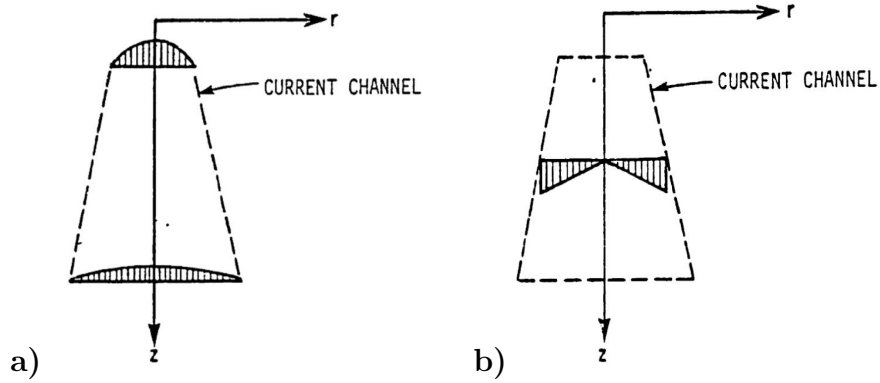


Figure 1.26: Schematic arc pressure- (a) and (b) axial net force distribution. After [65].

The one acting radially inwards induces the arc plasma to rotate. The axial Lorentz force component is considered essential in plasma jet generation and its net value can be calculated by:

$$F_z = \frac{\mu I^2}{4\pi} \ln \frac{R_2}{R_1} \quad (1.9)$$

Here μ is the magnetic permeability, I is the current flux through the arc and R_1 and R_2 are the arc radii at the inlet and outlet area of interest (cathode and anode). The interaction of both radial and axial Lorentz force component results in plasma streaming or jet effects.

Schoeck and Eckert [61] note the anode fall voltage to particularly affect the power transfer to the workpiece and the anode fall voltage is considered as a function of the anode sheath layer thickness. The fraction of electron thermal energy contribution to the anode was found decreasing with increasing current, explained by the reduced anode fall voltage, resulting from a reduced anode sheath layer thickness; i.e. electrons, accelerated along the anode fall, gain less energy vs. greater sheath layer thickness. Related to the arc radius, convective heat transfer was found to rise with increasing distance to the arc axis [61].

Ushio *et al.* [50] confirmed the results of [61] and [58] on dropping temperatures close to the anode along with increasing electrode to work distance. The increase in total arc power however, could not compensate the energy losses caused by a reduction of anode current density.

Ushio *et al.* [50] suggest arc length, at constant current, directly affecting the current density and thus the power input to the anode. That is, the higher the arc length, the lower the current or power density and in turn the lower the cross sectional anode weld penetration area, Figure 1.27 .

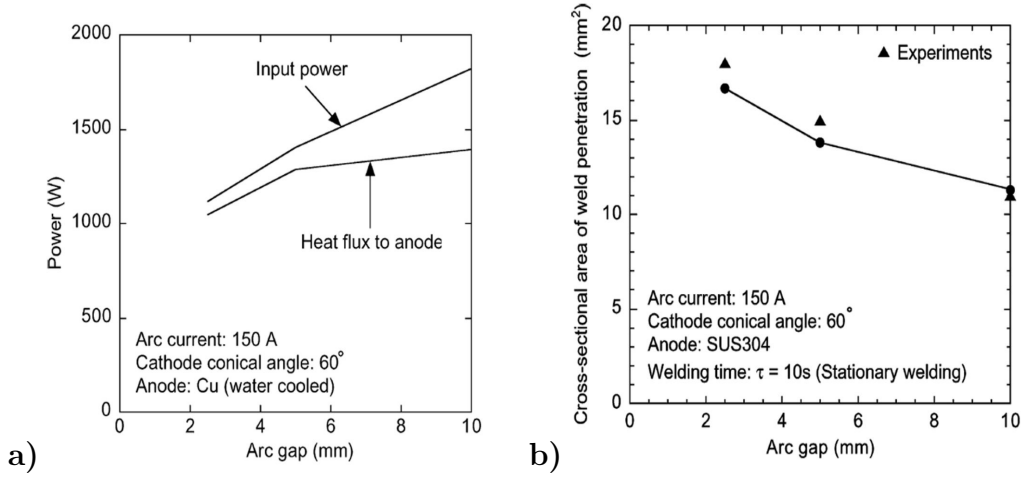


Figure 1.27: Power input and heat flux (a) to Cu-anode in relation to electrode to work distance and (b) relationship of stainless steel weld penetration cross section and varying electrode to work distance acc. to [50].

Based on the Abel transformation approach expression (1.10) is used to obtain the radial current or heat density [62]:

$$q(r) = \frac{1}{\pi} \int_x^R \frac{d^2Q}{dx^2} \frac{dx}{\sqrt{(x^2 - r^2)}} \quad (1.10)$$

q represents the heat flux fraction from the total heat transfer Q at a radial distance x from the cathode centre. R is the radial distance at which the flux becomes essentially zero; i.e. defines the arc fringe region.

Noise in the results obtained from equation 1.10 is considered caused by the second derivative in the integrand depending on the experimental data [62]. dQ/dx becomes zero when the cathode centre crosses the anode gap, hence, the error becomes greatest corresponding to the exceeding sensitivity to small changes when $dQ/dx \Rightarrow 0$. Thornton likewise states that high data output quality is required in connected to the Abel methodology and suggests this approach even as “*impracticable*” [39].

Vilharino and Fanara [66] likewise point out the difficulties in applying the Abel technique, stating that the double differentiation of the experimental data can yield misleading results. The authors thereby confirm the work of [62] and suggest a modified experimental split anode setup involving a non-insulated copper foil wrapped in between two foils of insulating material, Figure 1.28 hereby to either achieve single differentiation or to even entirely eliminate the Abel inversion. The modified split anode calorimeter has been referred to as ‘Anode Detector’ and was used to validate the results achieved from applying ‘Langmuir probes’.

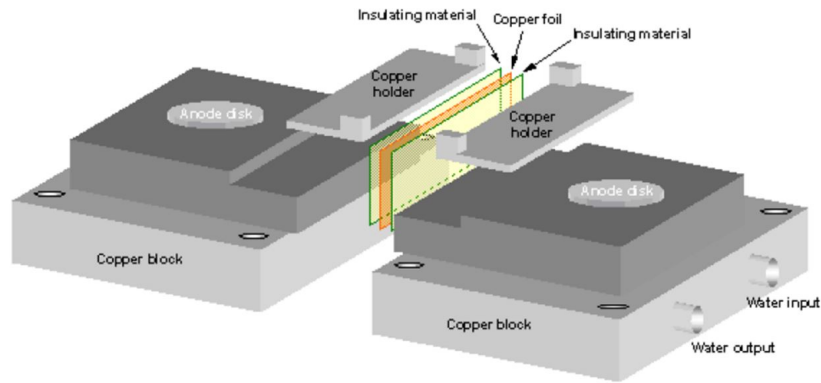


Figure 1.28: Modified split anode calorimeter for achieving higher result accuracy [66].

However, the arc was reported heavily deflected hence losing its axisymmetric shape while crossing the splitting plane. It needs caution in conjunction with the Abel Inversion approach, that all deviations from the presumed arc axial symmetry in extreme cases may lead to invalid theoretical predictions [62]. Thus achieving and maintaining arc symmetry throughout the measurements is compulsory. Vilarinho and Fanara [66] have attempted to calculate the amount of arc deflection as to develop appropriate means to overcome the latter. From the thermodynamic arc plasma properties they could deduce a theoretical relationship between arc deflection and weld travel speed, Figure 1.29 (a) leading the authors to raise the speed for traversing the arc across the anodes to 60 mm s^{-1} to achieve optimal arc stability. From Figure 1.29 (c), which, according to the authors depicts the arc path traces after deploying 10 mm s^{-1} , it remains difficult to understand how the TIG arc can stabilise applying even 60 mm s^{-1} travel speed, especially when using lower currents. Arc stabilisation, leading to axial symmetry conditions, however, is estimated decisive to achieve meaningful results and subsequently to apply the numerical approach, yielding hence higher accuracy in the measurement output, as claimed achievable. Unfortunately the work of [66] leaves this particular point; i.e. how a measurement could be obtained delivering stable raw data for further theoretical processing, rather unconsidered. However, and although literature dealing with these issues is rarely published, it shows that, especially in split anode calorimetry, arc displacement or wandering is yet known in TIG arc research; hence, means are considered to overcome these drawbacks to allow for greater experimental robustness and improvement in output quality.

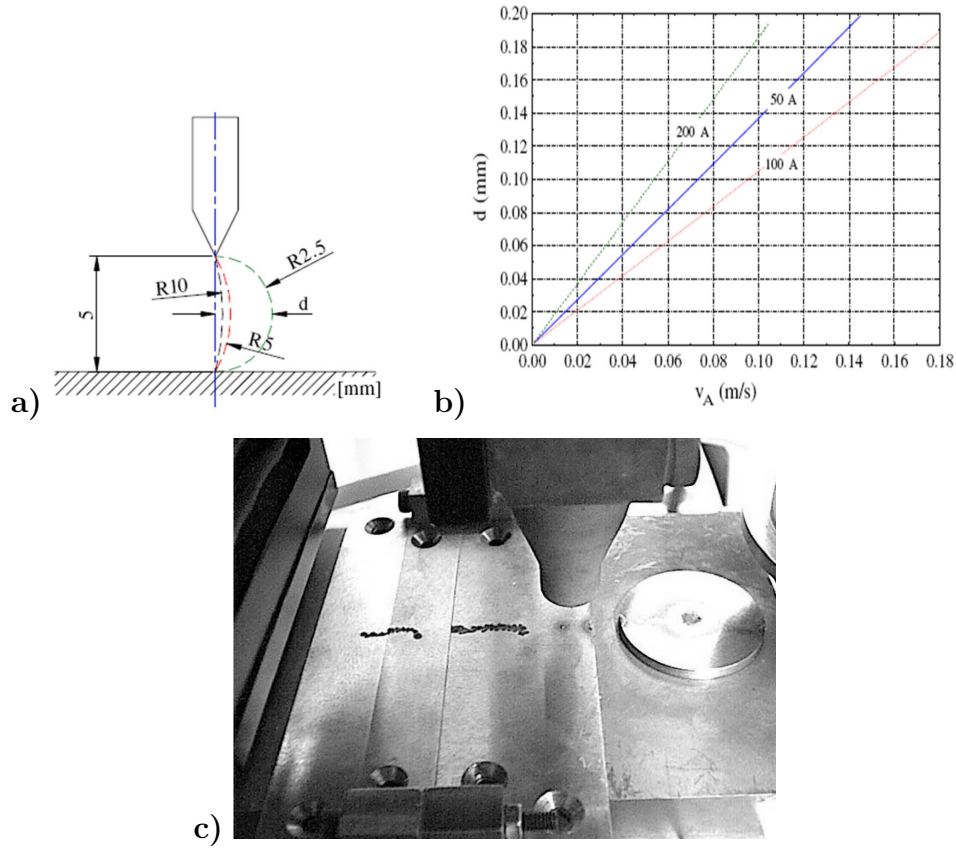


Figure 1.29: *a* - Arc displacement distribution for 5 mm electrode to work-piece distance; *b* - displacement d as a function of arc travel speed and arc current; *c* - arc path traces on copper anodes after deploying traverse speed of 10 mm s^{-1} . [66].

1.3.3 Methods Distinct to Split Anode Calorimetry

Constrictor Tube Technique Calorimeters unlike the split anode technique have been developed to investigate anode interaction mechanisms in deploying gas shielded tungsten arcs. Sanders *et al.* [63] studied anode spot phenomena using a special copper cascade anode arrangement, Figure 1.30.

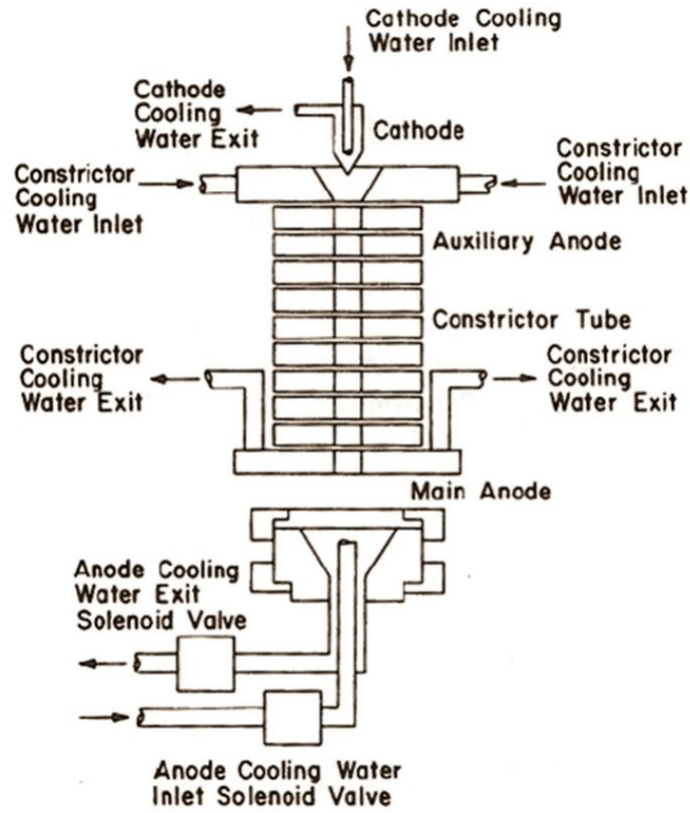


Figure 1.30: Constrictor tube calorimeter assembly. After [63].

Mounted in a spherical argon filled chamber the assembly is claimed providing benefits in observing the anode by diminishing the effects caused by strong cathode jets, regularly present in industrially used gas shielded tungsten arcs.

The authors distinguish the plasma structure adjacent to the anode into:

- Flow-affected zone (thickness ≈ 10 mm).
- Anode boundary layer (thickness $\approx 10^{-1}$ mm).
- Sheath - covering the anode surface (thickness $\approx 10^{-5}$ mm).

The anode region is proposed mainly caused by the flow-affected zone and the anode arc root is found significantly affected by the cathode jet velocities. 2

(two) different jet dominated arc roots were established referred to as *cathode jet dominated* (CJD), see Figure 1.31 (a) and *anode jet dominated* (AJD), Figure 1.31 (b). The authors assume *linear* plasma jets for the current range of $100 \text{ A} \leq I \leq 300 \text{ A}$ [63].

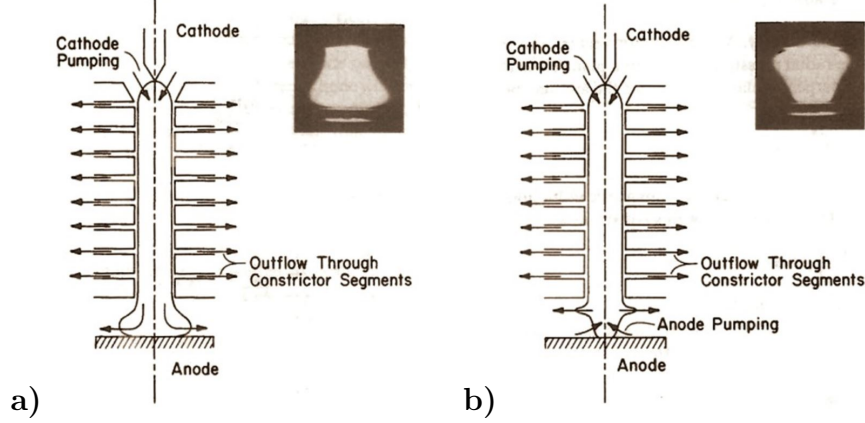


Figure 1.31: a - Cathode jet dominated (CJD); b - anode jet dominated (AJD) anode surface arc attachment. After [63].

Arc symmetry is found influenced mainly by arc length and cathode jet velocity. Rising current increases the cathode jet velocity V_{max} according to the expression [63]:

$$V_{max} \sim (jI)^{1/2} \quad (1.11)$$

Here j is the current density and I is the total arc current. Reducing either j or I or both leads to AJD arc roots connected to self-induced magneto-hydrodynamic (MHD) anode pumping effects. Adjusting electrode to work distance and weld current correspondingly results in stable anode jet dominated and symmetric arcs attached to the anode surface, Figure 1.31 (b).

Anode jet dominated arcs can be also prone, however, to result in asymmetric arc attachment at the anode (anode spot), see also Figure 1.32 (a), which can be observed even in higher numbers either quasi-static or stochastically wandering upon the anode surface. Figure 1.32 (b) draws a copper anode surface after welding and affected by several anode spots.

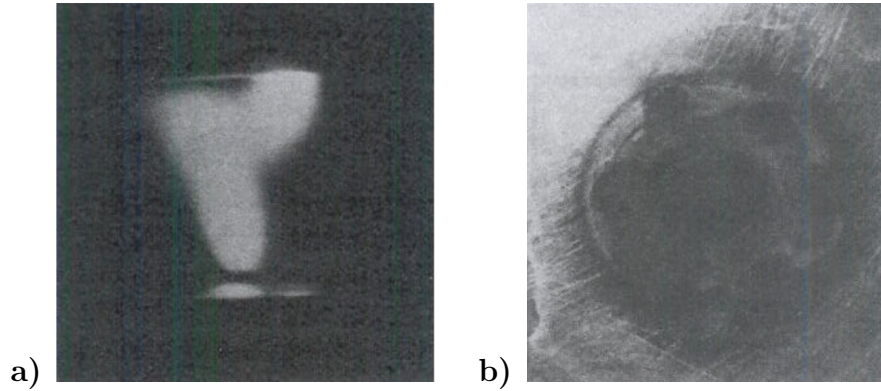


Figure 1.32: *a* - Atmospheric argon arc showing off-axis anode jet attachment; *b* - and several anode spots resulting from off-axis arc roots. After [63].

Electrostatic Probe Method (Langmuir Probe Technique) Another method for arc plasma diagnostics referred to as ‘Electrostatic- or Langmuir Probe Technique’ shall be dealt with hereafter.

Langmuir [67] and Mott-Smith and Langmuir [68] have developed this method to evaluate ion current relations in collision-less plasmas [25]. It was used, more advanced though, also to study welding arcs [25, 69, 70]. Advantages may be seen in its uncomplicated [25] and less time-consuming experimental setup [25]. However, significant differences in the experimental need attention when compared with spectrometric methods [70].

Unlike Gick *et al.* [69], Vilharino [70] revealed considerable deviations in the results achieved from both techniques. That is, arc temperatures predicted by optical emission spectroscopy were found 40% to 70% higher vs. those measured using electrostatic probes. These discrepancies are explained due to lack of fundamental theoretical understanding when employing electrostatic probes connected to high-pressure flowing plasma regimes. Also material based difficulties (probe material to plasma interaction) are described to play an important role [70]. Fanara [25] claims that an adequate theory is yet missing to satisfactorily explain all effects occurring in gas tungsten welding arc plasmas.

The method makes use of the physical behaviour of a biased, i.e. electrically charged electrode (probe) either a “disk, cylinder or sphere” [70] temporarily exposed to an arc plasma. Showing negative potential, electrons approaching

the probe, undergo repulsion from the probe surface. Positive ions whereas are attracted to the probe producing a ‘positive ion sheath’ surrounding the probe. Choosing the probe dimensions large vs. the ion sheath thickness allows to measure this positive ion current and to calculate the positive ion current density of the particular location within the arc plasma.

Charging the probe positive at gas pressures “not too high” in turn leads to a negative ‘electron sheath’ around the probe usable for measuring and calculating electron current or electron density, respectively [67]. Reliable evaluation of sheath thickness and sheath voltage distribution is required to understand the probe behaviour [25]. Gick *et al.* [69] employed moderate current (100 A) and mild steel anodes to obtain realistic conditions and an experimental setup schematically depicted in Figure 1.33 to analyse gas tungsten arcs.

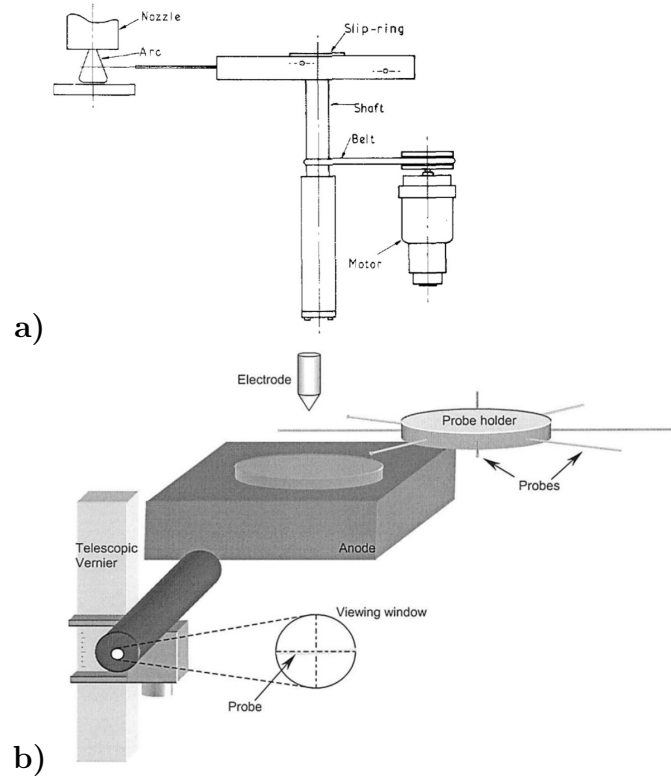


Figure 1.33: *a* - Electrostatic probe assembly as used by [69] and *b* - by Vilaronho [70].

The invasive character of the electrostatic probe method is one major difference to optical emission spectroscopy. Hence, special attention must be paid to not cause interference between plasma and probe thereby to perturb the plasma conditions. The probe material has to prove resistant to high thermal impact from the arc. Different diameter tungsten [25], molybdenum [69], or copper probes [70] are employed; using multiple cylindrical probes is found beneficial for welding arc investigations.

Appropriate experimental setup allows to accurately fix the probes e.g. to a rotating disk thereby exposing them repeatedly and short-periodically to the arc plasma, Figure 1.33 (b). A time of $\approx 1 \times 10^{-3}$ s to once enable the probe to cross the arc plasma was found to be sufficient in [69]. However, mechanical misalignment between probe and plasma cross section may become a critical issue employing this technique [70].

Figure 1.34 (a) adopted from [69] for a position of 4 mm below the cathode shows probe current and radial temperature distribution in relation to probe current after applying the Abel inversion technique for achieving the “true” radial temperature distribution at one specific point in the arc. Figure 1.34 (b) depicts the isothermal map in accord with the boundary conditions chosen in [69].

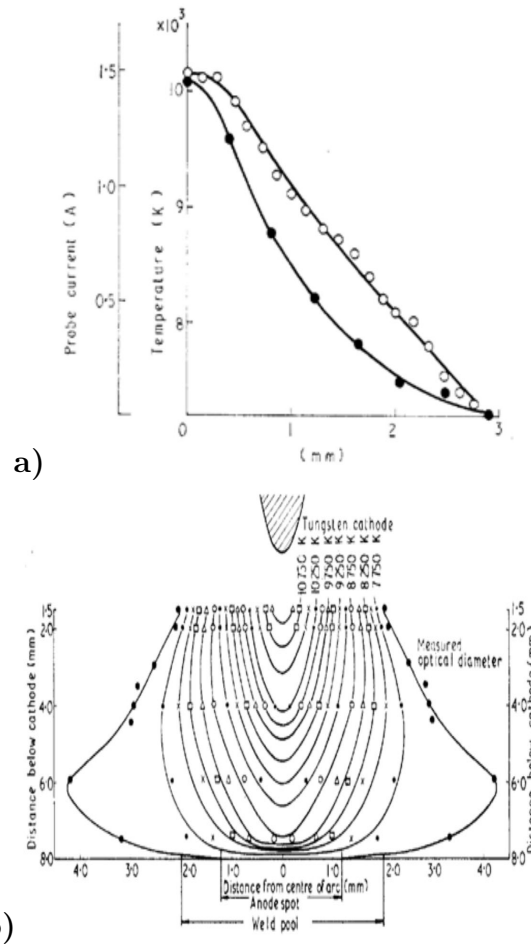


Figure 1.34: *a* - Probe current as a function of transverse probe position and radial temperature distribution; *b* - isothermal map of 100 A gas tungsten welding arc maintained upon steel anode [69].

Summary

Investigation of arc anode heat flow is based mainly either on the calorimetric *split anode technique* or the *electrostatic probe* method. Advanced since the beginning 1960's [56, 58], the split anode technique allows to evaluate the energy distribution of non-consumable welding arcs and its probable influence on e.g. weld bead generation. Although beneficial in handling the experimental setup this method is nonetheless prone to statistical uncer-

tainties. To overcome, a theoretical approach ('Abel Inversion') was found but requires appropriate adjustment depending on the experimental boundary conditions [36, 56, 58, 61, 62]. Also, due to utilising water cooled copper anodes the split anode technique neglects important factors, such as the formation of a liquid weld pool producing metal vapour and thereby affecting e.g. the arc plasma composition [51].

Other researchers e.g. [51] choosed methods unlike the split anode technique, deploying instead a mild steel anode and producing a liquid weld pool to represent more realistic welding conditions. Nonetheless, the split anode method could establish novel theories and improve the understanding of the physical interrelationship between welding arc and anode [57, 64].

The electrostatic probe technique, based mainly on fundamental research of Langmuir [67] and Mott-Smith and Langmuir [68], has dealt in the beginnings especially with e.g. mercury arcs. More recently advancements were made also for welding arc investigation [25, 69, 70]. The method is used to determine the temperature profile of a line section of the arc by additionally applying mathematical approaches, e.g. the Abel Inversion technique. As the electrostatic probe technique is invasive precise adjustment is substantial between the highly thermal resistant probe material and the arc plasma. To keep the interaction time between probe and arc low *multiple probe techniques* are applied for welding arc research. Compared with optical emission spectroscopy it is less time consuming and less complicated in its experimental setup. Regardless of this, however, it needs particular attention that the values achieved from invasive methods like the electrostatic probe technique can considerably differ from those values obtained by spectroscopy [66]. All remaining methods as previously described here were not found to prove consistent measurable scientific impact especially in non-consumable electrode arc studies.

1.4 Heat Source Distribution - Theoretical Considerations

To determine weld bead dimensions, the electrical process parameters must be related to the real weld bead shape. Different theoretical approaches are especially taking the heat source, represented by e.g. the welding arc, and its distribution into account. Amongst others numerical modelling has become

a remarkably powerful tool in describing physical phenomena in welding; for example to predict weld distortion or interior weld stresses resulting from the welding operation. Numerous scientific publications are available on this topic, but just a brief overview shall be given hereafter.

1.4.1 Temperature Field Models in Arc Welding

Linear heat conduction models are considered beneficial to calculate interaction effects between welding heat source and the solid parent material [71]. Research, accomplished by Rosenthal [72, 73] is estimated fundamental in heat conduction models applied to welding. Introducing electrical weld parameters and correlating these with work piece dimensions his calculations could firstly provide answers to numerous welding problems [74, 75]. Geometrically characterising the source of heat as '*point*-' , '*line*-' and '*plane*-' , Figure 1.35 Rosenthal applied Fourier's law of heat conduction, analytically describing an inhomogeneous heated body transferring heat energy from higher (source of heat) to lower heated material volume elements. His approach allowed for functional *analytical* solutions.

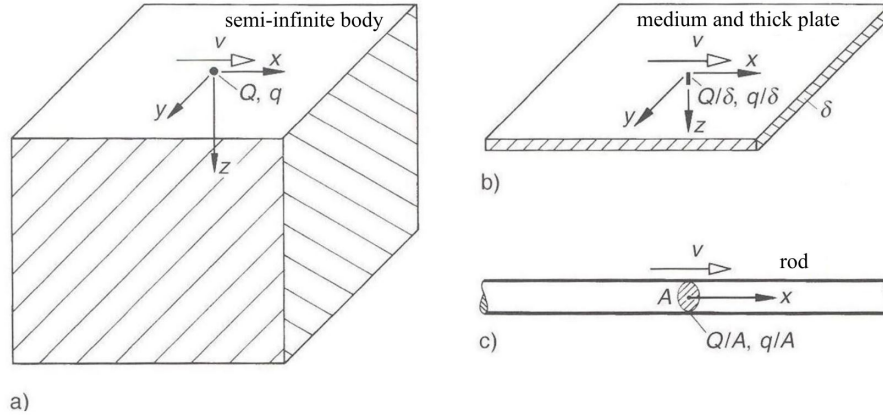


Figure 1.35: Schematic approach for base bodies (representing workpiece geometries) for analytical heat conduction calculation. *a* - Semi-infinite solid body subjected to stationary or moving surface point heat source (3-dimensional heat distribution); *b* - medium and thick solid body subjected to stationary or moving surface line heat source (2-dimensional heat distribution) and *c* - rod subjected to stationary or moving area heat source (bilateral 1-dimensional heat distribution). Adopted from Radaaj [71].

Rykalin has contributed a ground-breaking textbook on calculation of heat sources applied in welding operations [76]. Unlike Rosenthal, however, he has made use of a *numerical* mathematical approach [71].

Intensive studies on correlating experimental weld results to Rosenthal's point heat source have been conducted by Christensen *et al.* [77]. The authors created a dimensionless parameter to allow for comparability with the theoretical results. However, assuming the heat source temperature infinite the Rosenthal approach has proved inaccurate [78, 79].

The real welding process actually differs from 'concentrated'³ heat source characterisations. Chen [75] explains that Friedman [80] proposed an adequate modification thus by introducing the 'Gaussian distributed' heat source, Figure 1.36, where, d_H is the heat source diameter, q_m is the maximum heat flux and q_r represents the heat flux as a function of the radius r from the heat source centre.

³Note that the term "*concentrated*" is used in the literature in conjunction to heat flux to the workpiece.

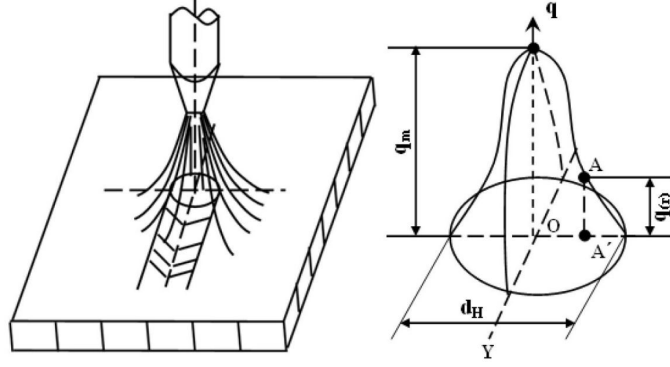


Figure 1.36: Schematic Gaussian distributed heat source characterisation. After [75].

According to [75] the heat flux can be computed by:

$$q(r) = q_{max} e^{(-3r^2/R^2)} = \frac{3Q}{\pi R^2} e^{(-3r^2/R^2)} \quad (1.12)$$

here, q_{max} is the maximum heat flux in the arc centre, Q represents the arc energy input and R the arc spot radius. The results attributed to e.g. TIG are estimated being sufficiently accurate [75], when obtained under “certain conditions” [74].

Goldak and Akhlaghi [78] state Pavelic *et al.* [79] as the first ones advancing Rosenthal’s point heat source approach to achieve a disc model of Gaussian surface heat flux distribution, Figure 1.37.

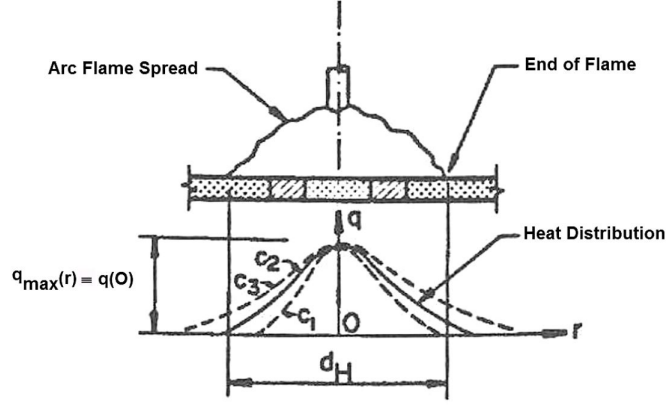


Figure 1.37: Schematic disc heat source model. After [79].

$$c_1 > c_2 > c_3$$

$$d_H \equiv \text{Hot Spot Diameter}$$

$$c \equiv \text{Concentration Coefficient}$$

First defining weld pool shape either experimentally (macro sectioning) or theoretically ([77]) and then predicting the heat conduction in the solid material, the surface heat flux $q(r)$ for a normal distributed circular heat source at the radius r can be calculated as:

$$q(r) = q(0) e^{-Cr^2} \quad (1.13)$$

here, $q(0)$ represents the maximum flux at the arc centre. To apply this expression and to obtain C the heat flux is assumed uniformly distributed in a circle of diameter $d = 2\sqrt{C}$. The value for C is thus a function of the heat source width, becoming larger with smaller heat source diameters or “more concentrated” heat sources, see also Figure 1.38 [81].

To predict the thermal effects on weld joint integrity Krutz and Segerlind in [82] proposed a Finite Element (FE) non-linear model as an advancement of the ‘disk’ heat source type. Involving a moving heat source coordinate system, schematically drawn in Figure 1.38, equation 1.13 becomes:

$$q(x, \xi) = \frac{3Q}{\pi c^2} e^{-3x^2/c^2} e^{-3\xi^2/c^2} \quad (1.14)$$

here, Q is the energy input rate and c is the characteristic flux distribution radius [81].

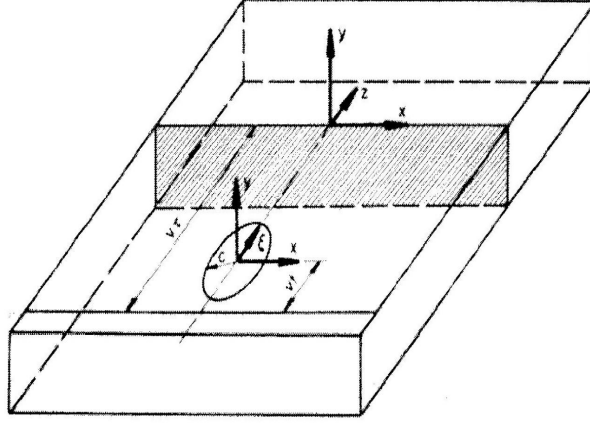


Figure 1.38: Schematic heat source model involving a moving coordinate system (x, y, ξ) . c is the characteristic radius of the heat flux distribution, ν is the welding velocity in $[\text{m s}^{-1}]$ and τ is the lag factor, defining heat source position at time $t = 0$. After [78].

Despite the fact that computer performance has significantly advanced, it has nonetheless remained difficult to properly predict complex weld pool interaction and fusion zone conditions [78]. In an attempt to overcome this Goldak *et al.* [81] developed the 'double-ellipsoid' volumetric heat source model, schematically depicted in Figure 1.39. For the first time, this model allowed adjusting the theoretical boundary conditions by defining the semi-axes (a_1 ; a_2 ; b and c in Figure 1.39 related to melting temperature isotherms; i.e. to the solid-liquid interface of the weld pool [78].

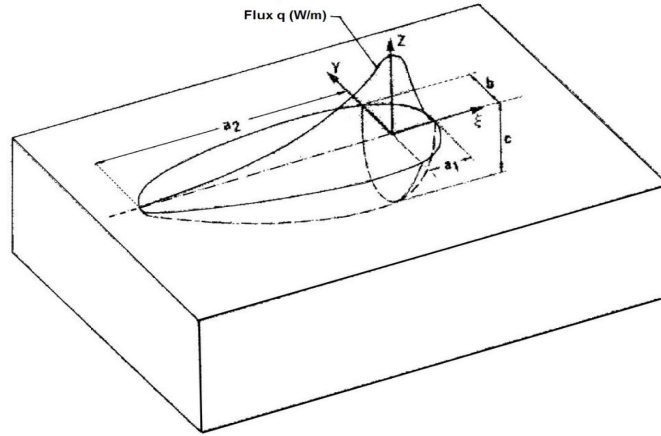


Figure 1.39: Schematic heat source model. After [78].

Ellipsoidal fusion zones are presumed in this numerical approach. In case of heat sources differing from the ellipsoidal shape, e.g. high energy beam processes, such as laser or electron beam, it is recommended to either apply metallographic means (cross sectioning) or theoretical methods, such as adapting the results of Christensen *et al.* [77] to correlate experimental to numerically modelled output [78].

Summary

To reduce the amount of experimental work, heat source modelling is important in quantitatively elaborating weld pool and weld fusion zone phenomena [78]. Rosenthal [72,73] has established an analytical approach to predict heating cycles and weld results. This analytical way is limited though because considerable simplifications are presumed, e.g. concentrating the heat source to a ‘point’, ‘line’ or ‘plane’. Also, temperature dependent material properties have not been taken into account [74]. Hence, such approaches have proved prone to - partly - significant errors in the calculated results. Advances, mainly established by Rykalin [76], use *numerical* methods instead to predict the thermal cycle, thereby allowing for broader application of the method without limitations to a special case, e.g. quasi-stationary weld conditions [74]. A Gaussian temperature distribution profile, [79, 80] both as a surface (disk) or, for including the weld pool volume, hemispher-

ical volumetric approach, is considered suitable hence; considered beneficial to more realistically cope with welding applications [71, 75, 78].

Heat source shape and heat flux prove decisive in the response of the material to join; i.e. they are known to crucially affect weld pool shape and stability, as internal (e.g. metallurgical microstructure) and external factors (e.g. part distortion). As shown, the numerical models taking this into appropriate account are sensitive with particular respect to heat source geometry and energy density distribution, which might have added to the fact that Gaussian distribution is prevalently found both in research literature and theoretical welding arc considerations; the latter to beneficially add in achieving theoretical results reasonably meshing with, and describing the real weld.

Increasing computer capacities, reducing calculation time and improving the comparability between model and experiment, have led to applying finite element methods (FEM) permitting the incorporation of dynamic parameters such as temperature dependent physical properties.

In this respect fundamental work has been performed by Goldak and co-workers [78, 81] who have established the so-called ‘*double ellipsoid*’ model. Taking into account the dynamic behaviour of a progressing volumetric heat source (weld pool) of finite temperature it allows fine adjustment of the model to provide close predictions of the fusion zone and weld temperatures; limited though when using heat sources different to an ellipsoidal shape, e.g. high energy sources such as laser- or electron beam. However, to overcome these deviations both metallographic means (e.g macro sectioning) as well as theoretical adaptations such as dimensionless parameters (established e.g. by [77]) are recommended to achieve meaningful output [78].

More recent numerical models deal with the incorporation of particularly heat source (weld pool) convection effects, which play an important role in describing weld phenomena [83]. These models make use of dimensionless parameters such as *Marangoni*- or *Prandtl* number as well as apply the so-called *dimensionless power*. Such numerical approaches are expected to gain greater future impact since improving the predictability on effects caused by the interaction between weld heat source and solid material.

Conclusive literature review remarks

The literature, critically reviewed, shows a considerable amount of effort expended to evolve technological means that should make it feasible to quantify meaningful data from investigating the arc, as well as its interaction with the material to join. It also clearly reveals the difficulties in both the aforementioned development and the practical application of methods used to analyse arc phenomena, as also shows the physical limitations yet to overcome.

Predictability of results applying TIG welding is - if even - hardly achievable. Despite the significant endeavour that was devoted to advance welding processes to improve the degree of result repetitiveness [84], the response of the material joined by using non-consumable electrode arcs like TIG, is still affected by complex interaction of a high number of variables. A major task to settle in this conjunction certainly is that the results obtained from measurements, e.g. by using non-invasive methods like optical emission spectroscopy or invasive methods like the electrostatic probe technique, have technical limitations that also limit the degree of certainty of the results achieved which regularly is attempted to overcome by in part complex numerical means transmuting raw data into meaningful assessment.

In a broader context the literature reviewed showed the ‘split anode technique’ as developed and firstly applied in the late 1950’s by Nestor [56], as a robust and reliable means versus other methods, to obtain meaningful data from the experiments to conduct. Although some particular specifics have to be carefully taken into consideration; i.e. a ‘perfect’ method to experimentally study appears is not yet available in research, split anode calorimetry was nonetheless decided being used to conduct the experimental work. Additionally to this, the literature showed that low current tungsten cathode arcs, if even, were relatively rarely subject of decided plasma investigations. It was consequently part of this PhD topic to pair both - split anode calorimetry and low current TIG arcs - along the course of this study to derive meaningful information.

2 Fundamental Investigations

This section particularly reflects on data and information published in the research paper referred to as: ‘*Investigation of Low Current Gas Tungsten Arc Welding Using Split Anode Calorimetry*’, authored by S Egerland, P. Colegrove, and S Stewart; published in Science and Technology of Welding and Joining, Vol. 22 (1), 2017, pp. 71-78. [85]. It reveals the fundamental approach to determine reference factors for constant application throughout the whole study; i.e.:

- Incremental width
- Anode thickness
- Surface condition
- Cathode chemical composition
- Cathode geometry

As earlier mentioned, split anode calorimetry was used by a considerable number of researchers for non consumable electrode arc welding investigations [6, 50, 56–58, 86, 87]; however, prevailingly for higher current arcs. The aim of this study was to apply split anode calorimetry to investigate and determine the physical behaviour of TIG arcs with currents of ≤ 100 A; considered, hence, as ‘low current arcs’ [16]. Figure 2.1 schematically plots the calorimeter used in this study and which will be depicted in greater depth and detail in Appendix A.1.

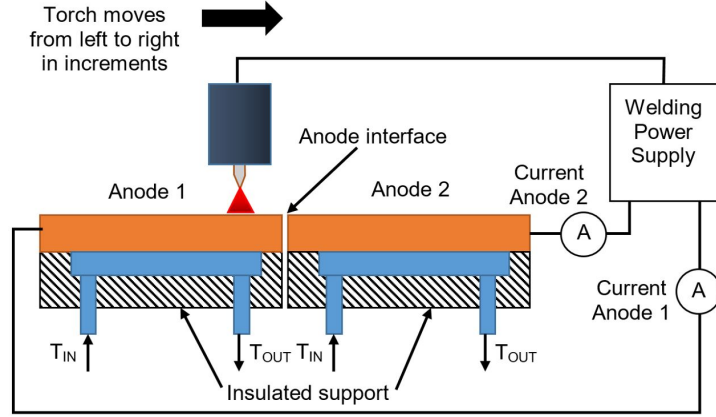


Figure 2.1: Schematic diagram of the split anode calorimeter.

The calorimeter's two non-melting copper anodes replace the workpiece material, normally deployed in real welding applications. As the welding torch is traversed in increments across the interface or splitting plane between the two anodes, the current, flowing through each anode, is measured. Figure 2.2 plots the current flowing into the two anodes as the torch traverses the interface. A plot of the temperature difference, ΔT and the corresponding calorimetric power, Q_{cal} has a very similar shape. This again may be multiplied by the arc voltage to determine the electrical power into each anode.

Each anode is water cooled, and the difference, ΔT , between inlet (T_{IN}) and outlet flow temperature (T_{OUT}) allows the power, Q_{cal} , being dissipated in each anode to be determined from equation 2.1:

$$Q_{cal} = \dot{m}c_p\Delta T \quad (2.1)$$

where $\dot{m}$ is the mass flow rate of water and c_p is the specific heat. Figure 1.23 (a) in top view schematically shows how the arc plasma, assumed axisymmetric, is split between the two anodes, while 2.2 from a real measurement plots the electrical current flux into both anodes as the arc is approaching the split plane interface.

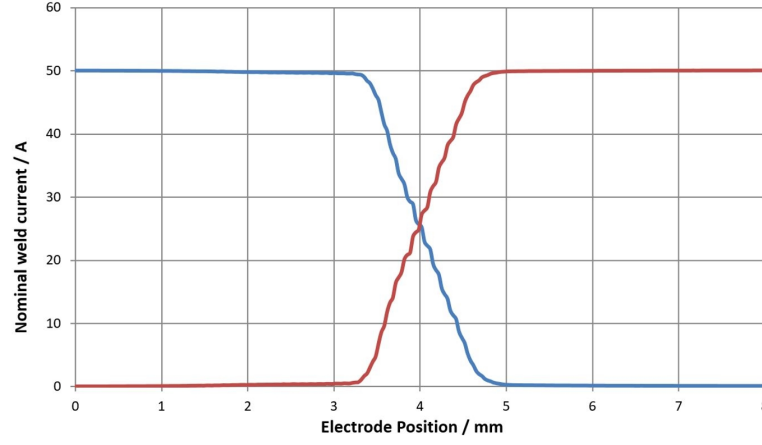


Figure 2.2: Typical plot of the current flux flowing into the two anodes.

Figure 2.3 (a) draws the approach developed by Nestor [56, 58] and Nestor and Olsen [86]. R indicates the outer arc boundary beyond which the current density is assumed to be zero; hereby also defining the calculation regime. The radial current or heat distribution is then computed for any distance r , from the arc axis to obtain a distribution profile similar to Figure 2.3 (b).

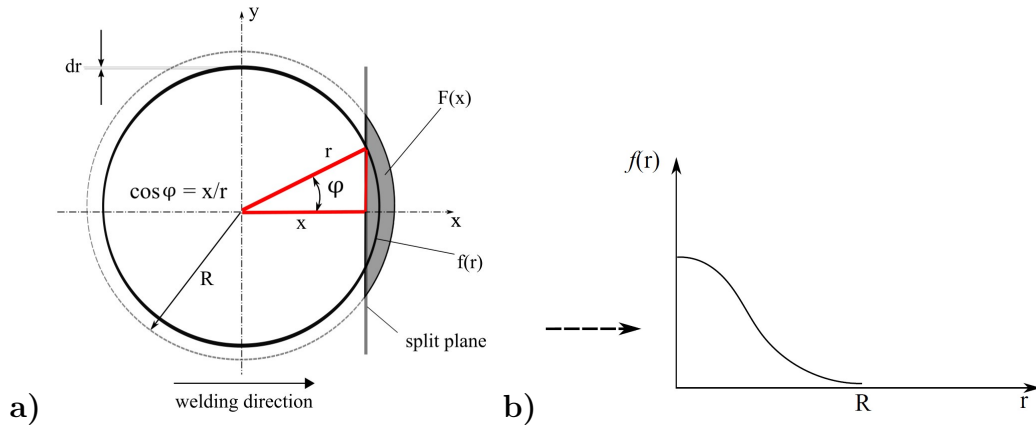


Figure 2.3: a - Schematic split anode current distribution (note that the shaded area represents the total current or heat $F(x)$ supplied to anode 2); b - radial density distribution function $f(r)$.

In conjunction to Figure 2.3 (a) we can write for the total current measured:

$$F(x) = \int_x^R f(r) l(r) dr \quad (2.2)$$

Here $l(r)$ is the arc length of the circle segment at radius r , defined as:

$$l(r) = r2\varphi \quad (2.3)$$

And where:

$$\varphi = \cos^{-1} \left(\frac{x}{r} \right) \quad (2.4)$$

Which leads to:

$$F(x) = \int_x^R f(r) r 2\cos^{-1} \left(\frac{x}{r} \right) dr \quad (2.5)$$

Differentiation with respect to x , transforms 2.5 into an ‘*Abel Integration Equation*’ and its solution can be written as the inverse mapping of 2.5 [56] expressed by equation 2.6:

$$f(r) = \left(\frac{1}{\pi} \right) \int_r^R F''(x) / (x^2 - r^2)^{1/2} dx \quad (2.6)$$

Where:

- r is the particular radius where the density is being determined;
- $F''(x)$ is the second derivative of the variable whose density needs to be determined, i.e. either the current or the power. This data is provided as a function of the position, x ;
- $f(r)$ is the corresponding density of this variable as a function of the distance from the electrode centre;
- R is the arc radius or the starting point for the integration.

One of the advantages of the technique is there are two separate methods for calculating the power density (electrical power and calorimetric power) which can be used to validate each other. The technique was originally developed by Nestor [56, 58, 86] and was subsequently implemented by Schoeck and Eckert and [61], Tsai [57], Tsai and Eagar [64], Lu and Kou [88], Tanaka *et al.* [6, 46] and Ushio *et al.* [50], who demonstrated that it provides reliable data for GTAW arcs.

The aforementioned studies demonstrated Gaussian-like current density profiles under most conditions [6, 47, 50, 52, 60, 62]. There are however a few exceptions. In particular Nestor [56] for high current arcs found minima in the heat and current distribution along the arc centreline when varying electrode to work piece distance (ETWD) and shielding gases. Decreasing ETWD was shown to produce minima versus longer arcs; whereas constricting the arc, using a water cooled orifice, was observed to reduce the density minima and led to more Gaussian type current and heat distribution. Lu and Kou [88] found distributions generally steeper than Gaussian by developing a different statistical approach referred to as “non-parametric minimization”. The authors state that considering current and power densities of welding arcs Gaussian distributed can be a “poor approximation”. Nevertheless all researchers agreed that the major factors influencing density distribution were arc length or arc voltage [6, 56, 57, 64], current level [6, 45, 50, 56–58, 60–62, 64, 88], anode voltage fall [61], ambient gas pressure [56], electrode diameter, geometry and type [6, 50, 56–58, 64, 88], and shielding gas composition [6, 50, 56–58, 64].

None of the previous split anode studies have, however, investigated combinatory effects of low currents, step size used for the data acquisition, thickness of the anode, effects of anode surface oxidation or tungsten electrode geometry and type. Therefore this study seeks to understand how each of the above factors influence the current and power density distributions measured with the calorimeter.

2.1 Experimental

Figure 2.1 schematically shows the design of the calorimeter whose base was constructed of temperature resistant glass fibre reinforced polyamide. A cooling channel was machined into the calorimeter body, being mechanically joined to an insulating sheet of Synthetic Resin Bonded Paper (SRBP) and

rigidly fixed to a metallic gantry equipped with hardware for connecting the data acquisition system and thermocouples. The calorimeter was equipped with 2 precisely machined (80 mm x 62 mm) oxygen-free copper anodes which had thicknesses of either 3, 7, or 12 mm. The gap between the anodes was set using heat and electrical resistant polyimide film (DuPont™ KAPTON™ HN '3 mil') which consistently provided a uniform gap width of 75 μm . Prior to every experiment the anodes were prepared by manual longitudinal wet-grinding with 500-grit abrasive paper. Subsequently 'Scotch-Brite™' scouring pads were used for longitudinal polishing, achieving a smooth surface structure and to remove any oxides that formed during previous experiments.

A GTAW power source (Fronius Magic Wave 2200) equipped with a cooling unit (Fronius FK 2200) and water cooled torch (Fronius TTW 3000a F/F++/UD/4m) was used for the experiments. To achieve sound gas shielding a large internal diameter nozzle ($\varnothing$ 19.0 mm) with a gas lens was used. Most experiments used a 2% Ce(IV)-oxide (CeO₂) doped electrode type (WC20) which had a diameter of 2.4 mm with a vertex angle of 36°. The electrode tip to work piece distance (ETWD) was kept constant at 3.0 mm throughout all experiments. The shielding gas was argon (II acc. to ISO 14175 - purity 99.996%) with 7.0 Lmin⁻¹ flow-rate.

Since very accurate positioning of the torch relative to the anodes was desired; an 'IAI RCA 2 SA3C' high precision linear actuator was used with a repeat accuracy of 0.05 mm. A 20 channel multiplexer data acquisition system (Type: Agilent 34790a) was employed. The equipment operated in the following way:

- The manipulator positioned the torch.
- A delay time of 10 seconds was applied before any data collection occurred. This was done to allow the system to stabilise – particularly the water inlet and outlet temperatures.
- The data logger was then triggered and 5 measurements were taken with a delay of 1 second between each measurement.
- A second delay of 0.5 seconds was applied before the manipulator moved the torch to the next position. Increment sizes of 0.1, 0.2 and 0.5 mm were investigated in this study.

The experimental matrix is shown in Table 3. The effects of increment; anode thickness; anode surface condition; electrode tip geometry and electrode type were investigated.

Target subject	Anode thickness	Increment	Current	Remarks	ETWD	
denotation	t	s	I	-		
unit	mm	mm	A	-	mm	
s	7.0	0.1	50	-	3.0	
		0.2				
		0.5				
t	3.0	0.2	50	-		
	7.0		75			
	12.0		100			
Surface	3.0	0.2	50	Oxidised		
Electrode geometry	12.0	0.2	50	Truncated tip		
Electrode composition	12.0	0.2	50	Pure W vs. W+ CeO ₂ (WC20)		

Table 3: Parameters used for split anode reference experiments.

Figure 2.4 (a) shows the calorimeter setup and Figure 2.4 (b) depicts the truncated electrode design. Two measurements were conducted for each condition. Due to the small increments and number of samples collected, each measurement took about 22 minutes; approaching 45 minutes for the smallest increment size.

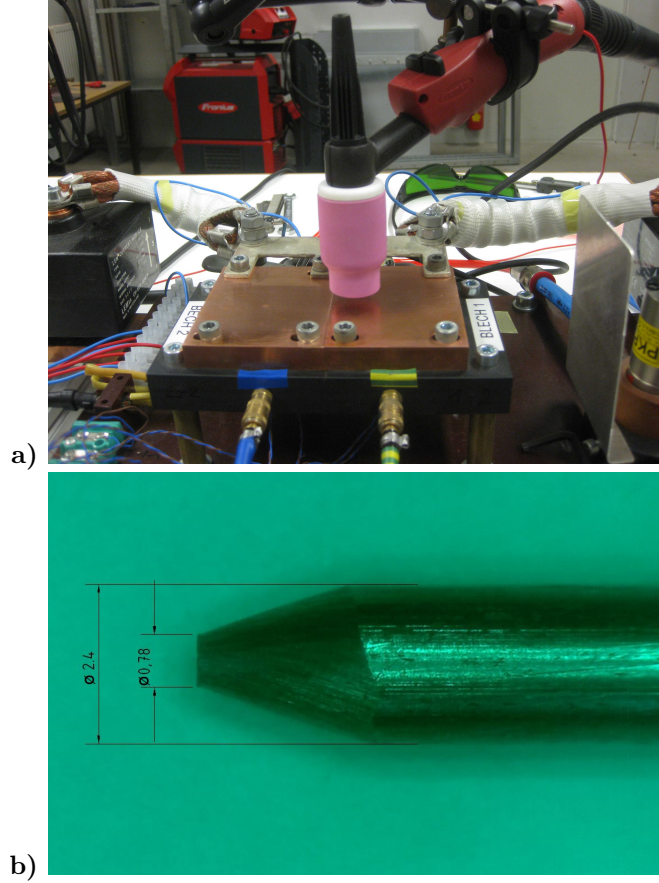


Figure 2.4: *a* - Calorimeter experimental setup; *b* truncated electrode tip.

The derivative of the measured current data $F(x)$ was determined numerically with Lagrange's five-point formula according to equation 2.7:

$$F'_2(x) = \frac{1}{12\Delta l} (f_0 - 8f_1 + 8f_3 - f_4) \quad (2.7)$$

Δl is the increment size, and f_i is the raw data at a particular position, i . The five point formula was used to help smooth out noise. Additional smoothing was applied by calculating the average of four values of the derivative $F'_i(x)$. This procedure (with smoothing) was applied twice to obtain the second derivative.

2.2 Results and Discussion

2.2.1 Anode Characteristics

Figure 2.5 shows the raw data of the current vs. position for the different increment sizes for 7 mm anode thickness. In addition it shows the second derivative of this data.

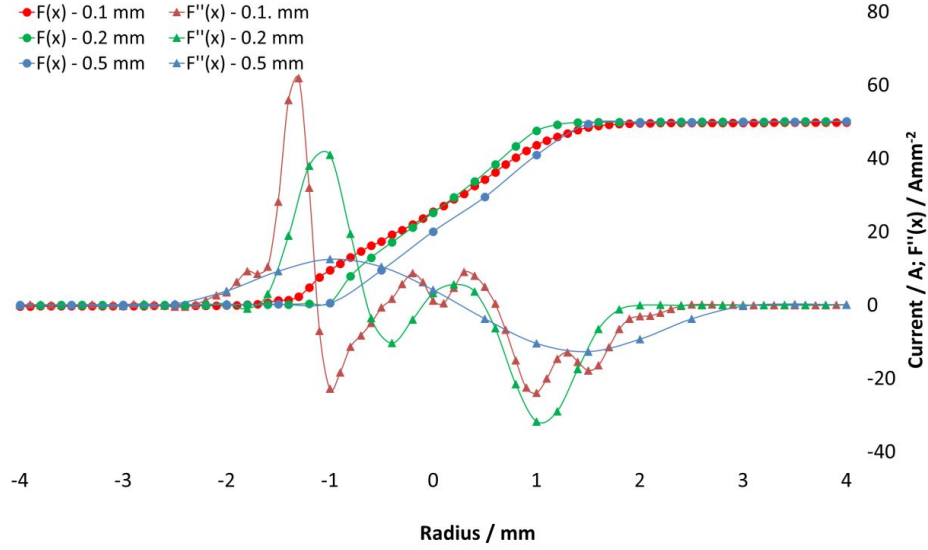


Figure 2.5: Plots showing the effect of the manipulator increment: a - Raw data for anode 1 showing current and second derviative of current versus position.

Although the raw data was similar for the three increment sizes, there is clearly a large difference in the second derivative which affected the calculation of the current density from the Abel inversion in Figure 2.6.

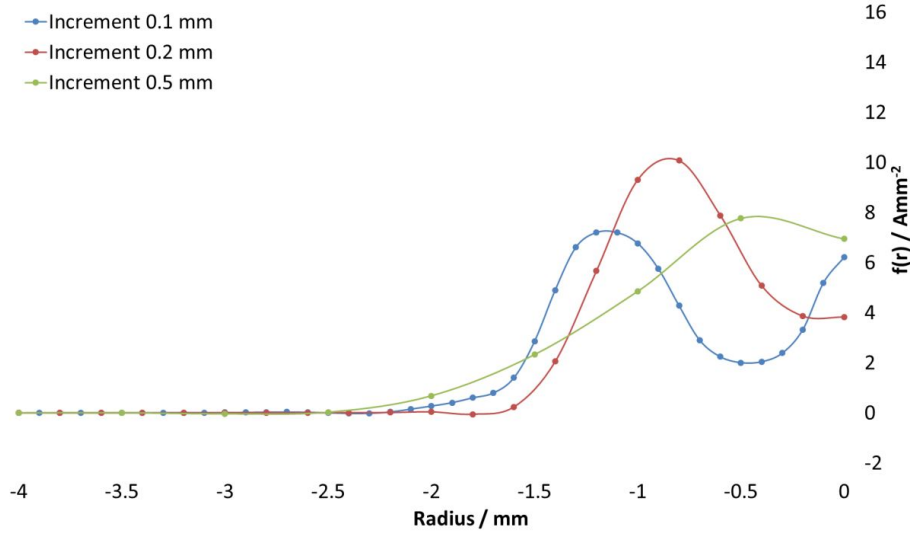


Figure 2.6: Current density distribution corresponding to 2.5 for 50 A nominal arc current and an anode thickness of 7 mm.

When using the large step size the power intensity approached a Gaussian curve, while the smaller step sizes indicated that the maximum occurred away from the centreline. Therefore the increment size has a significant effect on the profile for this current level. The effect of the anode thickness on the current density for 100 ampere nominal currents is shown in Figure 2.7. The values for the 7 mm and 12 mm thickness anodes were very similar while the values for the 3 mm were significantly different. Overheating effects were observed with the 3 mm anode and 100 ampere nominal current due to the poorer thermal dissipation with the thinner material. Therefore the temperature of the anode appears to have had a significant effect on the current intensity profile. At lower currents all anode thicknesses gave very similar results. Another important observation was the minima observed in the arc centre for the 50 A measurements in Figure 2.6 was not observed – the intensity plots are much closer to a Gaussian profile.

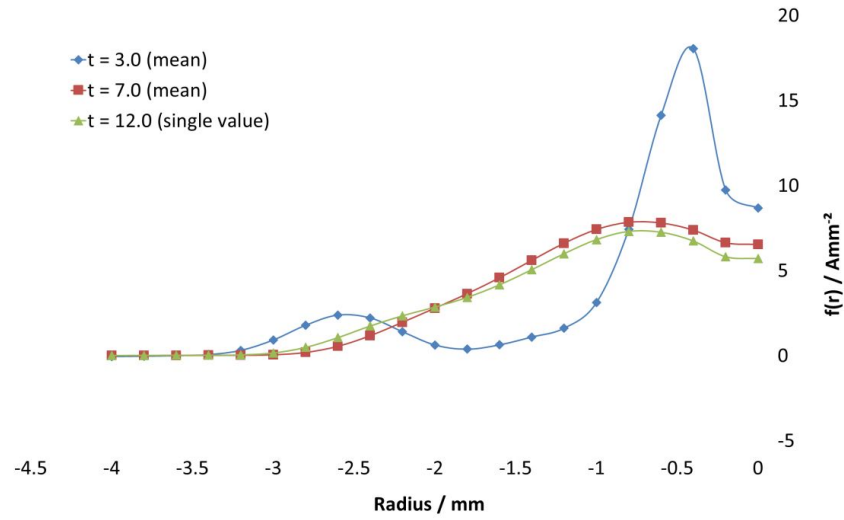


Figure 2.7: Current density distribution as a function of the anode thickness for a nominal arc current of 100 A.

A plot showing the effect of the anode surface condition is shown in Figure 2.8. This demonstrates how the presence of the surface oxide result in a narrower arc with a higher intensity in the centre.

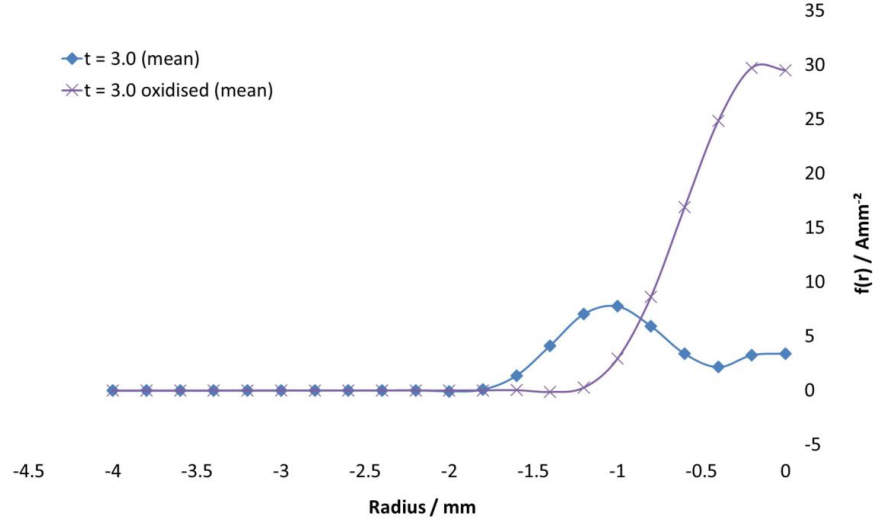


Figure 2.8: Current density distribution as a function of the anode surface condition for a nominal arc current of 50 A.

2.2.2 Effects of Welding Parameters

A plot of the current density as a function of the nominal arc current is shown in Figure 2.9 and the corresponding plots of the power densities which are calculated from the welding power input and the calorimeter power input are shown in Figure 2.10. Collectively these figures illustrate how increasing the current widens the arc while the current and power densities remain quite similar. In addition, the shape of the power density from the weld power input and the calorimeter were very similar providing confidence in both measurements and adding validity to the minima observed in the centre of the 50 ampere arc. Process efficiency defined as the ratio of calorimetric power over electrical power was calculated 0.81 ($I = 50$ A); 0.82 ($I = 75$ A) and 0.8 ($I = 100$ A) which is similar to values reported by Stenbacka [89].

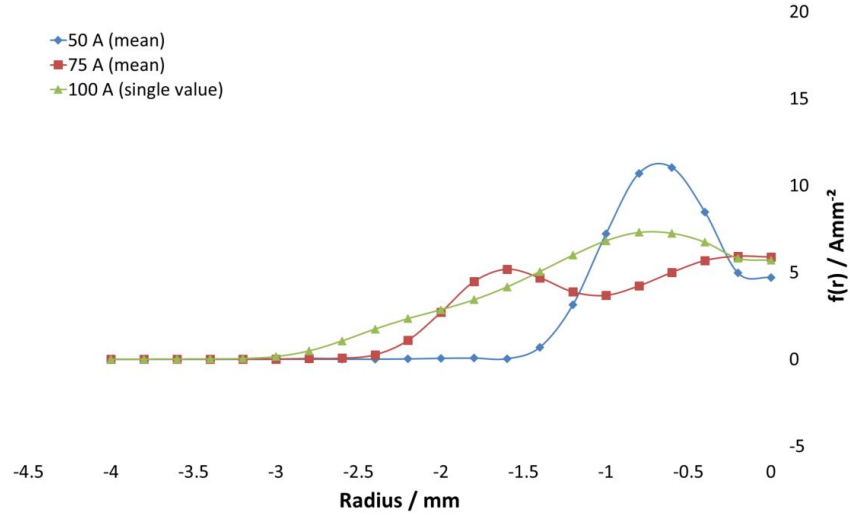


Figure 2.9: Effect of arc current on radial current density distribution.

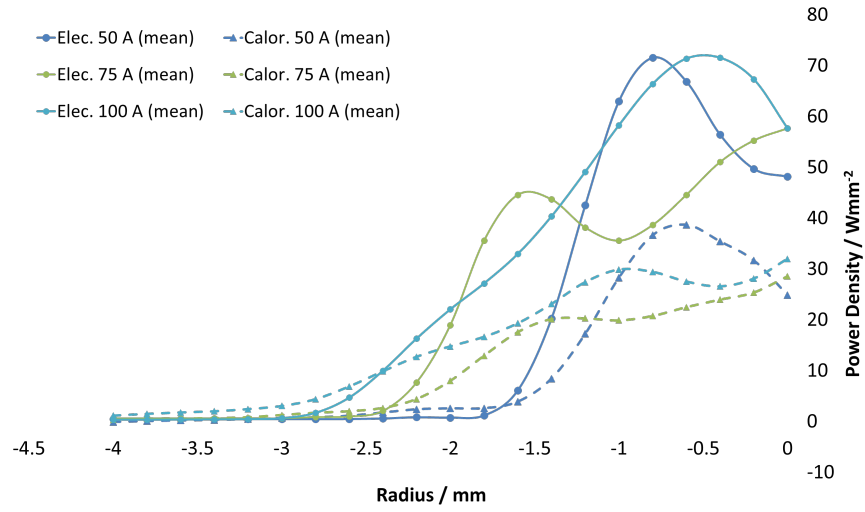


Figure 2.10: Effect of arc current on power density distribution from power supply (elec.) and calorimeter (calor.).

Finally, the results for the three electrode types are shown in Figure 2.11. Like the oxidised anode surface condition this also has a significant impact

on the current distribution, with the current density being concentrated at the centre of the arc for the truncated cone and being fairly wide and flat for pure tungsten. Additionally we could observe cathode tip changes after welding. For example Figure 2.12 shows the electrode tip after applying 50 ampere weld current for $\approx 1.32 \times 10^3$ seconds.

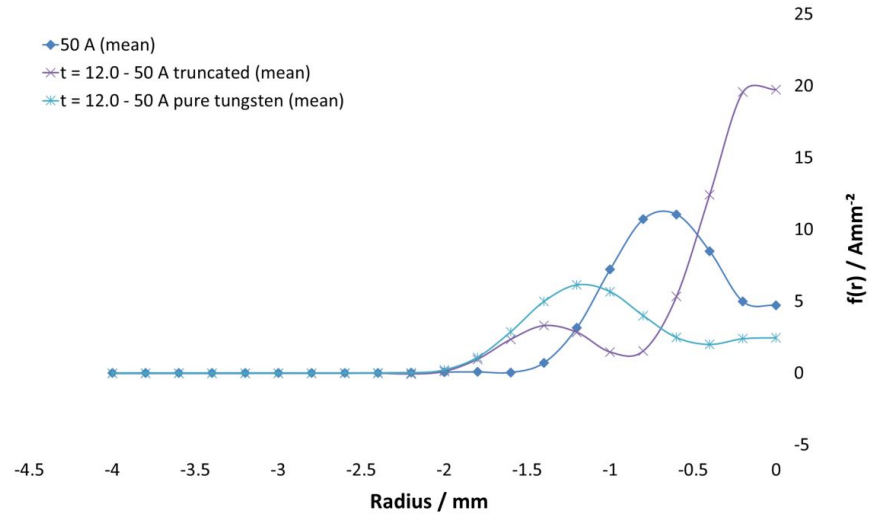


Figure 2.11: Effect of cathode chemical composition and tip geometry on radial current density distribution.

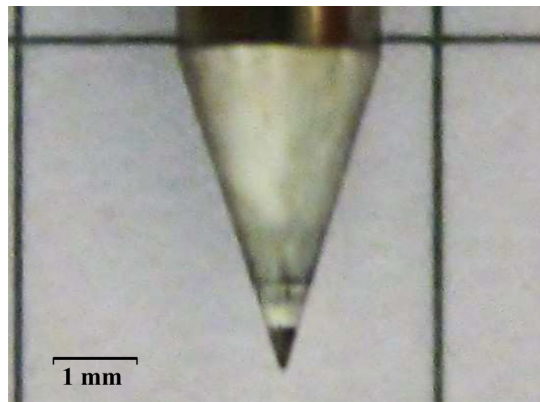


Figure 2.12: Typical cathode tip appearance after arc application.

The results on the increment impact in Figure 2.5 and Figure 2.6 demonstrate that large increment sizes (0.5 mm) lead to a current density profile that approaches a Gaussian shape and thereby confirms results achieved by Tanaka *et al.* [6], Tsai [57], Tsai and Eagar [64], Ushio *et al.* [50]. However, the current density with smaller increments is clearly different and demonstrates that this is an important parameter when undertaking split anode measurements. The increment size needs to be sufficiently small to capture the detailed shape of the density distribution being investigated. However, the smaller the increment size, the more susceptible the data is to noise in the measurements due to the calculation of the second derivative. This is evident in the data for the 0.1 mm increment and its corresponding derivative in Figure 4(a). While second derivatives for the 0.2 and 0.5 mm increments are symmetric about the anode centreline, the data for the 0.1 mm anode is not, suggesting that it is more likely to be subject to noise or other effects. This is why the 0.2 mm increment was used for all the subsequent studies. The minimum in the current and power densities around the centre of the arc with low currents is an interesting finding. Heberlein and Pfender [90], spectroscopically investigating the anode boundary layer of a 100 ampere atmospheric pressure argon arc, found the electron density maxima shifted ≈ 1.5 mm away from the cathode centre. Tanaka *et al.* [6] provided results which, in part, also showed that there was a current density maximum close to, but not exactly at the arc centre – although plotting a line of best fit that used a Gaussian-like profile. Allum [91], conducting gas tungsten arc pressure measurements using varying currents, revealed pressure maxima not corresponding with, but away from the axis of the arc. Lu *et al.* [88] have numerically modelled a non-Gaussian distribution, showing a distinct heat flux drop at, but a density peak shifted ≈ 1 mm away from the arc centre. Therefore the results of this study support findings which have been reported elsewhere, although their significance was not highlighted.

The results revealed in this investigation showed the current and power density maximum away from the arc axis disappeared with increasing current, simultaneously joined by widening the anode spot. The latter supports the work of Tanaka and Ushio [46] who stated that the arc current level significantly influences the heat transfer conditions via the anode boundary layer. Compared with the 50 A arc, doubling the nominal current led to a more Gaussian-like distribution profile. Although Lu *et al.* [88] calculated heat flux minima at the arc centre, their simulation nonetheless showed the peaks

even for their 200 A arc model, which would contradict the suggested maxima dissipation with increasing current. However, the authors assumed a cathode diameter; electrode vertex angle; ETWD and gas flow-rate for their model, significantly varying from our experimental conditions.

The oxidised anode surface results confirmed the results from Fihey and Simoneau [4] who proposed that elements with high electron affinity (e.g. oxygen) form negative ions which constricts the anode spot and increases penetration depth. The authors [4], however, varied the amount of oxygen in the shielding gas and stated higher arc voltage along with rising the oxygen content. Although applying oxidised anode surfaces an arc voltage increase was not found in our investigations. Nonetheless, the calculations conducted in this study showed an arc plasma constriction and increased current density towards the arc axis; which would affect the penetration profile in regular TIG (melting anodes) as claimed by Majetich and Yeo [92]. According to the results from Richardson [93], consistent anode surface conditions were found essential for achieving reproducible measurements. The findings on cathode tip truncation contradict the work from Bernáth *et al.* [94] who have not found measurable variations in the plasma shape of gas tungsten arcs when applying pointed or tapered cathode. However, it is suggested that my results are confirmed by Sadek *et al.* [95] and Key [96], who stated differences between pointed and truncated electrode tips. Also Ushio *et al.* [97] deduced differences by blunting the electrode tip and interpreted these due to a variation of the current density near to the cathode which changed the induced plasma flow impedance. This, in turn, changed the arc force acting on the anode surface. Therefore it is suggested that truncating the electrode may lead to changes in the arc to work piece interaction and leading to differences in the real weld penetration profile, which would confirm findings by Key [96] and Kou [98].

Ushio *et al.* [97] have observed variations in the dopant concentration level near the tip of ThO_2 , La_2O_3 , and Y_2O_3 doped tungsten electrodes but not CeO_2 . Cerium-oxide doped electrodes were used for this study. The electrode tip is affected by the highest temperature [97], hence, dopant depletion in this region may explain the minima found in the current and power density distributions. The work function may gradually change across the tip corresponding to the CeO_2 concentration. In turn, this may change the current density and explain the observed minima at the arc centre.

2.3 Summary

From these initial fundamental investigations conducted the following summary can be drawn:

- i. The split anode calorimeter used is suitable for obtaining reliable data from low current GTAW arc measurements.
- ii. The increment size for traversing the arc across the anodes has a significant impact on the calculated current density distributions.
- iii. The smallest increment (0.1 mm) was subjected to greater noise when calculating the second derivative but showed a very distinct final density plot with a significant density maximum away from the arc axis.
- iv. The largest increment (0.5 mm) was found to approach a Gaussian density profile, which has been reported by other researchers.
- v. Increasing the weld current widens the anode spot diameter, causes the density peak away from the arc centre to disappear and results in a Gaussian-like distribution profile.
- vi. Oxidised anode surfaces resulted in arc constriction with significantly increased current density close to the arc centre.
- vii. Truncated cathode tips led to a higher current density close to the arc axis.
- viii. Pure tungsten showed the flattest current density profile vs. CeO_2 -doped tungsten cathodes.
- ix. Electrode dopant depletion may explain the minima found in the arc centre current and power density distribution.

3 Derivation of Reference Values

This section shall particularly consider the results obtained from studying the application of a reference shielding gas nozzle type of large diameter ($\varnothing$ 30 mm) and published in the research paper referred to as: ‘*Arc instabilities during split anode calorimetry with the TIG welding process*’, authored by S Egerland, P. Colegrove, and S Stewart; published in ‘Welding in the World’, Vol. 62, (4), 2018, pp. 831-845 [99].

As a consequence from discussing the results provided in the previous chapter, it was decided to involve the effects that the shielding gas nozzle design; i.e. especially the nozzle diameter, may have on the energy distribution of low current TIG arcs. The latter should lead to an improved understanding of how shielding gas nozzles may influence practical welding applications in the industrial field. Finally to achieve an investigation stage making it feasible quantitatively to compare values derived from the experiments, it was considered to carry out an experimental series particularly applying a so-called ‘reference nozzle’. This prototype nozzle was produced under the requirement to be sufficiently large in diameter, thus, assuring to produce safe laminar gas flow, as also not to interact with thereby not detrimentally to affect the arc in its spatial appearance. It was also decided to establish constant experimental boundary conditions. The anode thickness was assigned to be 12 mm, thereby to secure that surface melting, found along the preliminary stage of the study e.g. with deploying 3 mm anode thickness, cannot occur even with applying the higher arc current of 100 A.

3.1 Experimental

Table 4 plots the experimental parameters deployed in this investigation. While with the initial experiments three different currents were applied, table 4 shows that finally two arc currents were chosen; i.e. 50 A and 100 A. This was due to the differences known from practical applications and showing that arc stability can significantly vary between both values, see Figure 3.1.

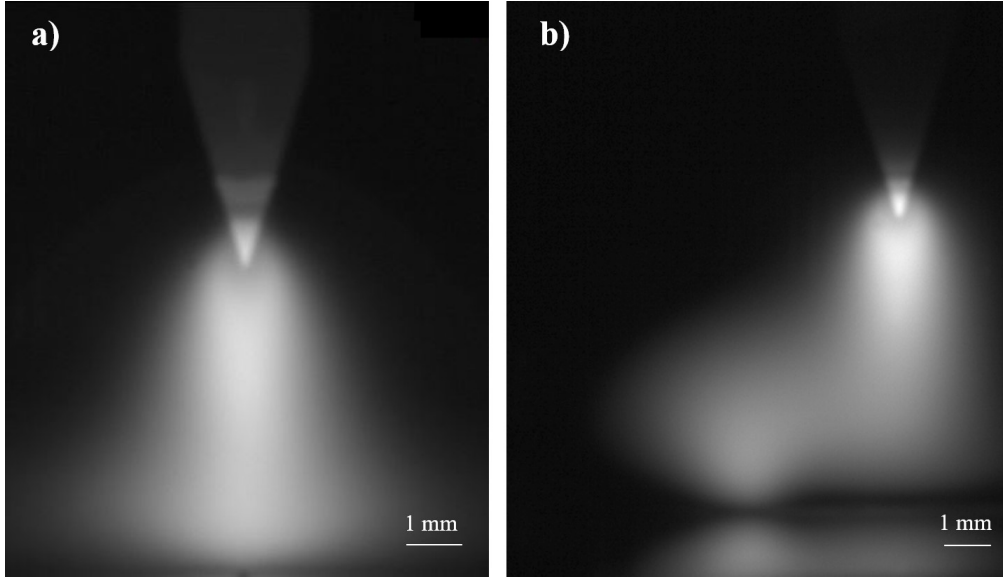


Figure 3.1: *a* - symmetrically shaped arc with $I = 100$ A; *b* - arc deflection with $I = 50$ A.

The table also reveals that the electrode tip to workpiece distance (ETWD), which was found to play an important part in the energy distribution of a TIG arc by other researchers [6, 52, 56], was taken into appropriate account in this experimental series. As aforementioned a $\varnothing$ 30 mm shielding gas prototype nozzle, depicted in Figure 3.2 (a, b), was produced to be applied, made from austenitic stainless steel X8 CrNiS 18-9 according to EN 10088-2:2005 ($\sim$ AISI 303). A double mesh gas diffuser was used to prevent the experiments from gas flow induced arc disturbance as to obtain laminar gas flow.

In this investigation, the calorimeter was additionally equipped with a metal-oxide semiconductor (CMOS) camera (Type: ‘IDS UI-1226LE’) to observe arc stability and to correlate camera images with the output from the calorimeter. The camera was coupled and synchronised with the linear manipulator traverse path and captured two images per 0.2 mm increment; i.e. the camera was triggered twice to take the first image at the beginning of the increment and the second one at its end. As with the previous investigation, the cathode was a 2% CeO_2 doped (WC20 type) electrode, longitudinally ground using a stationary wet diamond wheel tungsten grinder (Type: ‘Pro Point

Plus') to precisely obtain a 36° included angle.

The electrode extension from the shielding gas nozzle exit was constantly kept at 6.0 mm and argon of 99.996% purity was used as the shielding gas. A digital 'SMC PFM 725' type gas flowmeter with adjustable flow rate of $0.0083 - 0.42 \text{ Ls}^{-1}$ and display accuracy of $\pm 3\%$ (full scale reading) was used to assure precise gas flow conditions.

Parameter	Unit	Value
I	A	50/100
ETWD	mm	3.0/6.0
α (vertex angle)	$^\circ$	36
Composition	-	WCeO ₂
Cathode diameter	mm	2.4
Gas flow rate	Ls^{-1}	0.3
Gas flow velocity	ms^{-1}	0.42

Table 4: Experimental matrix.

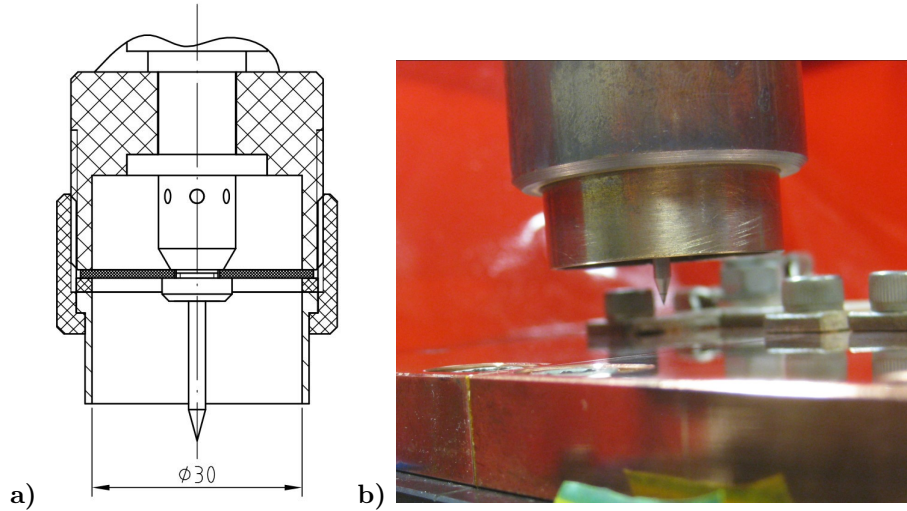


Figure 3.2: *a* - schematic reference shielding gas nozzle design; nozzle dimensions assembled with tungsten cathode and gas mesh diffuser; *b* - gas nozzle mounted to welding torch.

Three weld sequences were conducted per variable to understand the vari-

ability of the measurements. Unlike the initial investigation, where Microsoft Excel[™] was used to perform the calculations; MATLAB[™] was used here to process the data and a digital polynomial smoothing filter (Savitzky-Golay filter) was applied to initially smooth the raw data values. The calculation approach remained the same as explained in the previous chapter of this thesis. The outer arc radius, R , was 5.0 mm. The welding arc was assumed centred relative to the split plane as soon as both anode halves were receiving similar amounts of electrical current conducted through the arc.

3.2 Results and Discussion

3.2.1 Current density distribution

After applying the computational approach described in the previous chapter, the arithmetic mean from all three trial sequences was calculated and plotted as a function of the arc radius; then, in general, these results were subjected to cubic spline smoothing additionally to smooth the curves in Figure 3.3 (a, b) for ‘*anode 1*’ and ‘*anode 2*’.

Figure 3.3 (a) plots the results for 50 A arc current and 3.0mm ETWD, while Figure 3.3 (b) reveals that doubling the ETWD to 6.0 mm decreases the density distribution at the arc centre versus 3.0 mm ETWD and broadens the distribution of the current while shifting the density maximum away from the arc axis.

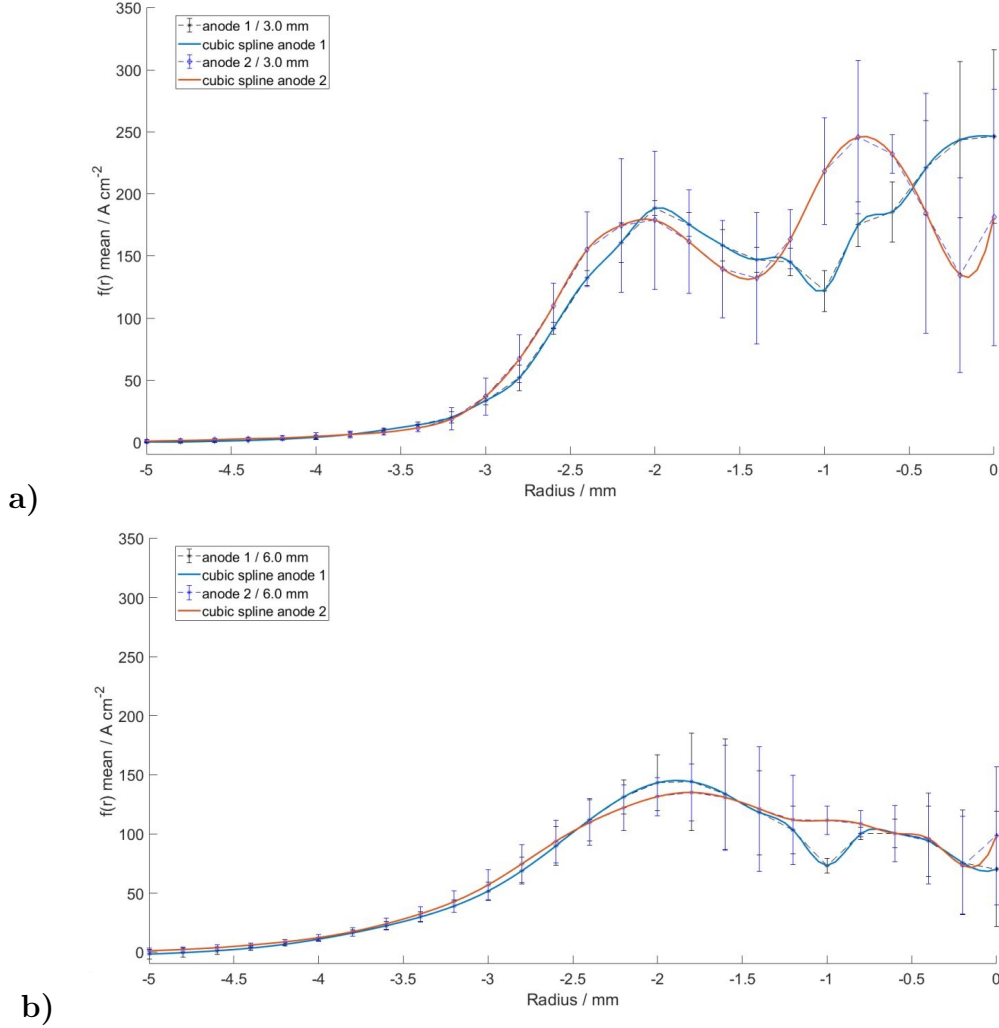


Figure 3.3: *a* - Current density distribution as a function of arc radius and 50 A arc current and 3.0 mm ETWD; *b* - current density distribution for 50 A and 6.0 mm ETWD. Error bars indicate standard deviation.

Figure 3.4 (a), for 3.0 mm ETWD and 100 A current, shows, similar to the 50A / 3.0 mm ETWD sequence, the current density rises again towards the arc centre. Doubling the ETWD to 6.0 mm while maintaining the current constant, Figure 3.4 (b), leads to a flatter current density fairly uniform across the arc radius and, compared to 3.0 mm ETWD, shows lower density close to the arc axis.

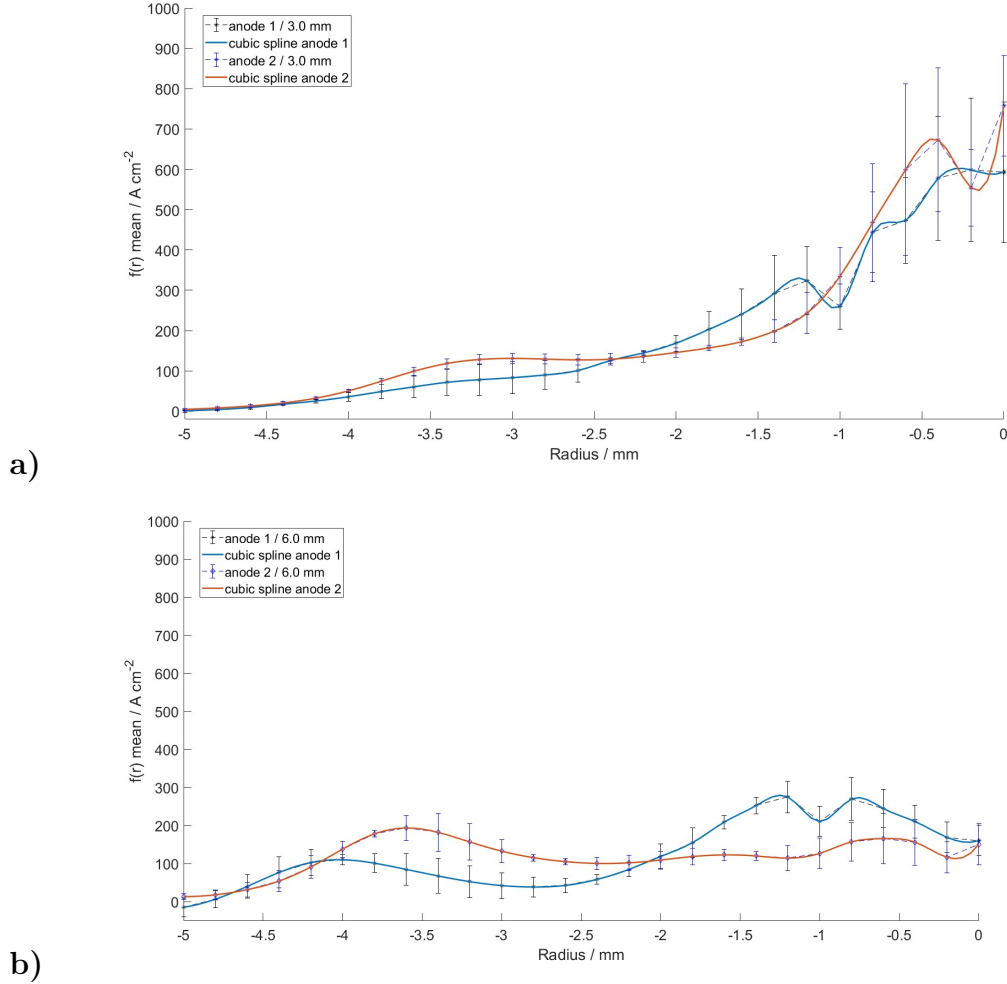


Figure 3.4: *a* - Current density distribution as a function of arc radius and 100 A welding current and 3.0 mm ETWD; *b* - current density distribution for 100 A and 6.0 mm ETWD. Error bars indicate standard deviation.

As an exception Figure 3.4 (b) interestingly shows no significant current displacement for ‘*anode 1*’ and ‘*anode 2*’ at the split plane while 50 A nominal arc current paired with 3.0 mm and 6.0 mm ETWD reveals considerable noise among the results. Except Figure 3.3 (a), showing considerable variation in the curves between the ‘*anode 1*’ and ‘*anode 2*’, the remaining graphs are nonetheless suggested appearing similar for both anodes.

As far as concerning the ‘arc length’ the findings are well correlating with measurements from Tanaka *et al.* [6], who have investigated 100A TIG arcs along with ETWD variation; as well as with results from Nestor [56] and Schoeck [52]. It is noteworthy that Nestor in his work used an arc current of 200 A as well as different cathode composition, cathode diameter, and, due to conducting the trials in an argon filled chamber, in general was using no gas nozzle. Schoeck has investigated argon shielded TIG arcs within a similar current envelope (50 A; 100 A; 150 A) but was using a pure tungsten cathode of bigger diameter ($\varnothing$ 3.2 mm), different vertex angle (45°), and a smaller gas nozzle diameter ($\varnothing$ 15 mm). All researchers found an increasing arc length radially spreads the energy density distribution and vice versa to raise the current density around the arc axis when ETWD is decreased. This phenomenon was also detected in this study during the experiments with lower current (50 A). Hence, it is supposed to be a natural behaviour of any high intensity TIG arc ($I \gtrsim 30\text{A}$) which would confirm results from Schoeck [52] who has suggested that the arc cross section is influenced by the “cathode jet”; i.e. a gas flow, induced by electromagnetic forces, is highest at the cathode and decreasing towards the anode. The arc plasma is less or even “negligibly” compressed by the Lorentz force at the anode which expands the anode spot cross section; in turn, the current density decreases.

Figure 3.5 (a, b) shows the mean values from both anodes and for all welding sequences conducted; i.e. the single curves for both anodes depicted in Figure 3.3 (a, b) and 3.4 (a, b) were arithmetically averaged and plotted to the negative x-axis.

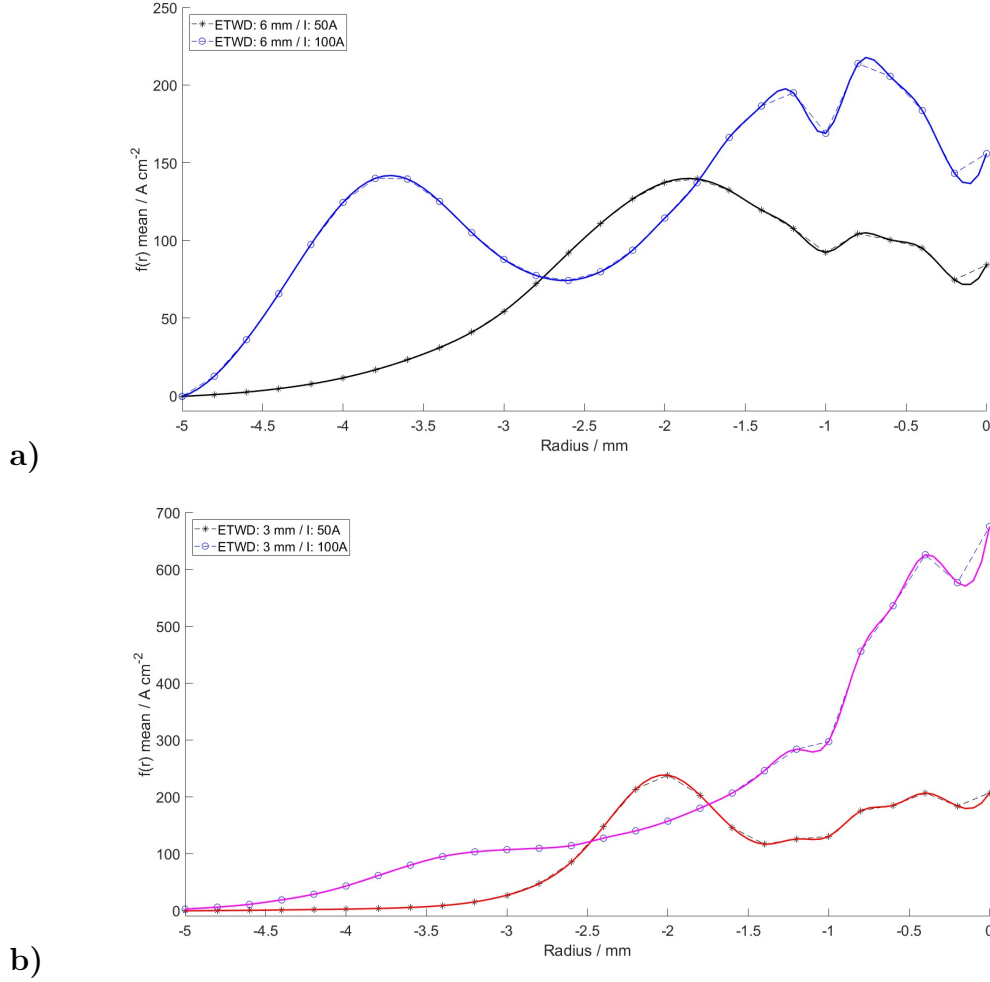


Figure 3.5: *a* – Curves of ‘*anode 1*’ and ETWD = 6.0 mm weld sequences; *b* - curves of ‘*anode 2*’ and ETWD = 3.0 mm weld sequences. Single curve values (Figures 3.3 (a, b) and 3.4 (a, b) averaged and plotted to negative x-axis.

Figure 3.5 (a, b) also shows that decreasing the ETWD while maintaining the welding current constant gives rise to an increase of the current density in general, which confirms findings from Schoeck [52] and Schoeck and Eckert [61]. However, unlike these researchers, who were stating Gaussian-like distributions, a density maximum was found in this study shifted away from the arc axis, which basically confirms the results from the previous inves-

tigation [85]. An increase of the welding current and keeping the ETWD constant leads to a current increase towards the arc axis, thereby confirming results from Ushio *et al.* [50] and Tanaka *et al.* [6]. However, and this is considered as an essential finding from all welding sequences conducted, there was no ‘arc current versus electrode tip to work distance’ pairing found in these experiments, resulting in repeatable arc stability and symmetry conditions. Consequently it is supposed unlikely that merely higher arc currents, causing for example higher cathode jet velocities hence entirely assure arc symmetry under the conditions provided by the present experimental setup – or, even when applying split anode calorimetry in general, at least as far as concerning the given current range applied. Except Figure 3.4 (b), all experiments were found to produce a distinct displacement in the current density distribution for the split interface region; i.e. the last current values calculated for ‘*anode 1*’ (shortly before the arc is crossing the split interface) are considerably different to the first values derived for ‘*anode 2*’. These differences can be explained by small deflections in the arc as it crosses the split plane which are described in the next section.

3.3 Arc Behaviour

Figure 3.6 shows images while applying 50 A and 3.0 mm ETWD and correspondingly Figure 3.7 shows the anode surface oxidation which is regularly shaped and does not indicate any instability at the interface. From these observations arc symmetry appears possible when using low ETWD, even when applying lower welding currents, which finally allows to use the Abel Inversion, which requires ‘axial symmetry’ to properly being applied. From section *a* through *c* in Figure 3.6 the arc is approaching and crossing the split interface; i.e. section *a*, is 0.45 mm before and *b* is close to the centre (0.04 mm), while *c* shows the arc after crossing the split plane (0.55 mm). Correspondingly sections (*a'*) through (*c'*) depict modified image resolutions (reduced grey colour depth: 16 colours 4 bits per pixel ‘BPP’.)

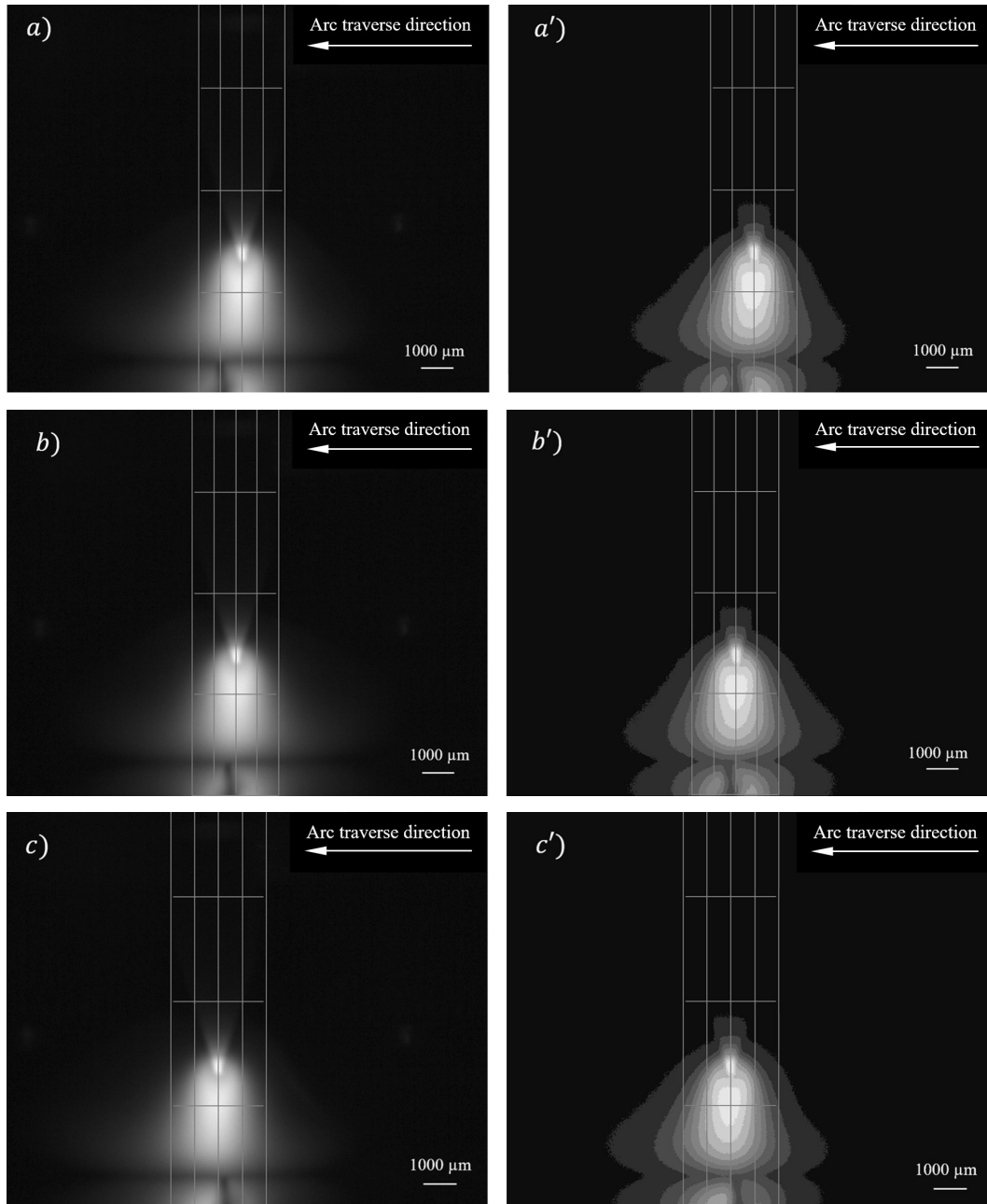


Figure 3.6: High definition camera reference arc application images virtually revealing axial symmetry while arc traversing the split plane. $I = 50\text{A}$; ETWD = 3.0 mm.

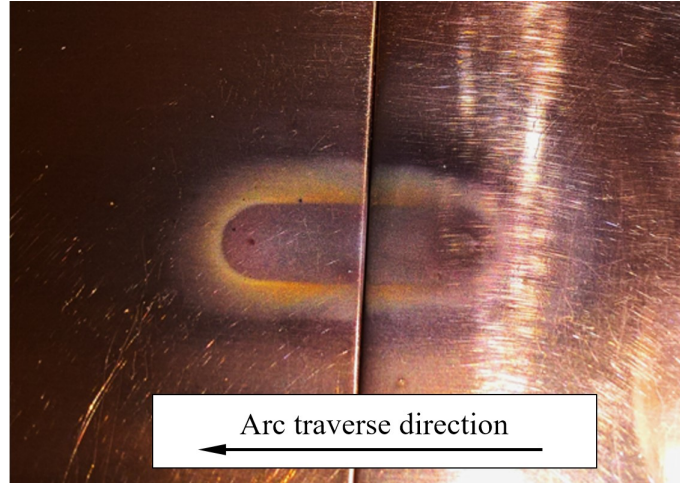


Figure 3.7: Symmetric and uniform anode surface oxidation of welding sequence plotted in Fig. 10. Reference nozzle; $I = 50\text{A}$; $\text{ETWD} = 3.0\text{ mm}$.

For direct comparison Figure 3.8 as an excerpt plots the application of the same parameters and clearly reveals notable deflection of the arc both prior to reaching the split interface and also remaining after crossing the split plane. This suggests that even low ETWD – in practical TIG welding applications well-known to rather stabilise the arc – does not mandatorily assure consistent arc symmetry when applying split anode calorimetry as given at least in this investigation. This supposedly leads to the noise in the calculated output as indicated by the error bars especially in Figure 3.3 (a). As plotted in 3.6 the arc is approaching and crossing the split interface from section a through c ; i.e. section a , is 0.48 mm before; b is close to the centre (0.07 mm) and c shows the arc after crossing the split plane (0.52 mm). Sections (a') through (c') depict appropriately modified image resolutions processed and labelled similarly to Figure 3.6.

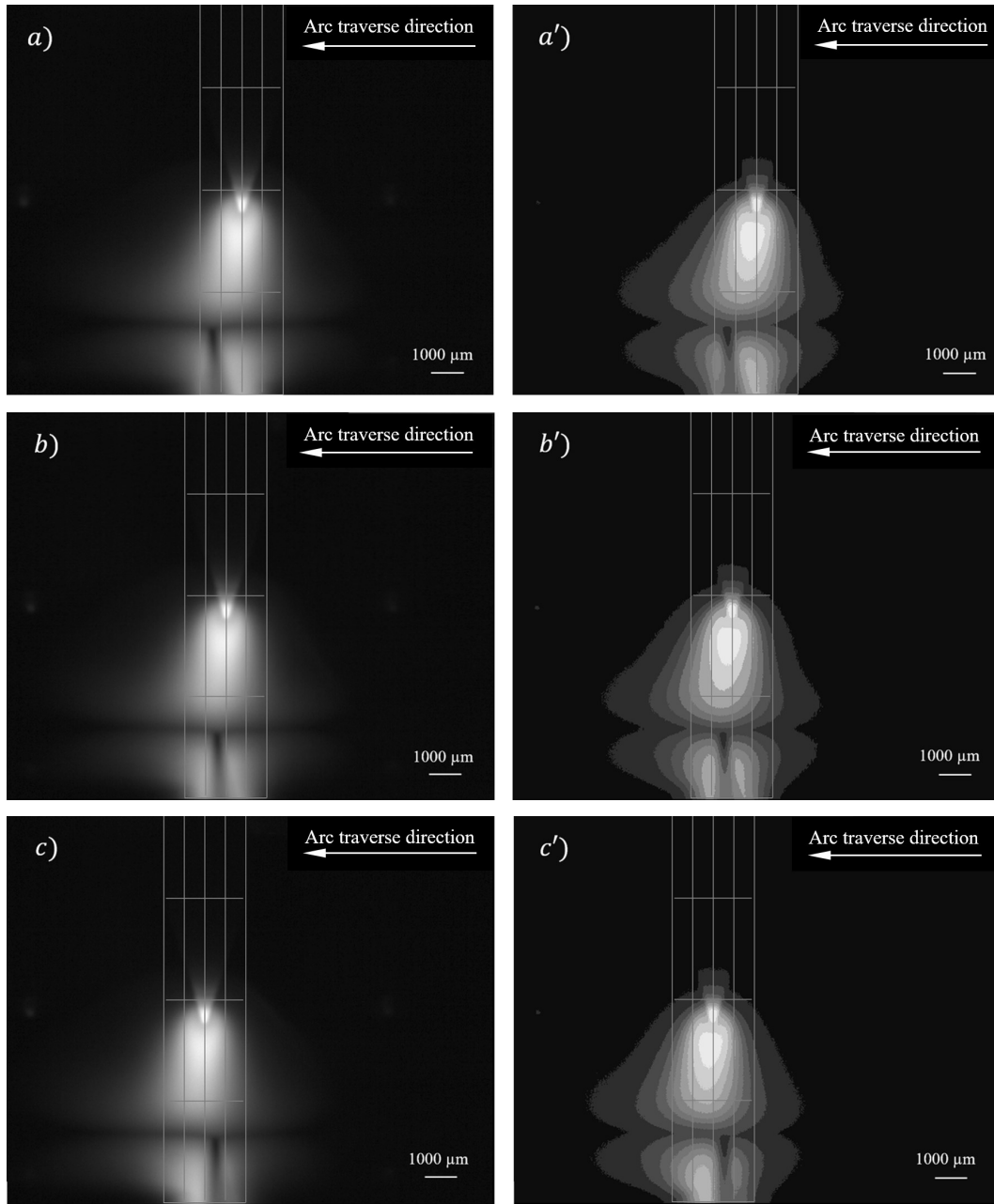


Figure 3.8: High definition camera reference weld images revealing axial *asymmetry* while arc traversing the split plane. $I = 50\text{A}$; ETWD = 3.0 mm.

Figure 3.9 (a - c) shows for 100 A and 3.0 mm ETWD stable arc symmetry both while the arc approaches and traverses the split interface. The welding voltage, compared to the 50 A trials, increased by 1.5 V in average (12.3 V vs. 10.8 V) along with doubling the weld current. An increased amount of current per unit time is transferred across the arc gap, leading to both an increase in the current density around the arc centre and the heat transferred to the anodes. The arc is approaching and crossing the split interface from section *a* through *c* ; i.e. section *a*, is 0.65 mm before; *b* is close to the centre (0.05 mm) and finally *c* shows the arc after crossing the split plane (0.55 mm). Sections (*a'*) through (*c'*) depict correspondingly the appropriate modified image resolutions, similarly labelled to Figure 3.6.

By analysing the modified photographic images in Figure 3.3 (*b'*) and Figure 3.9 (*b'*) the virtual arc core region diameter (note arbitrarily defined as the brightest column region in the grey colour reduced photographs) could be found approximately doubled while raising the weld current from 50 A to 100 A and maintaining the ETWD constant. This also confirms results derived by Schoeck [52] who suggested that an argon shielded TIG arc must become wider to appropriately accommodate any current increase and an arc gap remaining unchanged. Schoeck in the same work particularly focuses on the importance of the cathode jet which supposedly increases with increasing weld current and, in turn, stabilises the current conducting anode spot, which might explain the reduced noise among the measurements compared to the 50 A/3.0 mm ETWD weld series.

Tanaka *et al.* [6] postulated a Gaussian density profile for 100 A weld current. However, although the current density rises more uniformly towards the arc axis with this current versus the 50 A series; this was not found in these experiments. Nonetheless the arc centre current peak values calculated from these experiments were in good agreement with the findings from Tanaka *et al.* [6]; regardless of their use of a $\varnothing$ 3.2 mm lanthanum-oxide (2% La_2O_3) doped cathode type, different ETWD (5.0 mm), and different cathode vertex angles (30°; 45°; 60°).

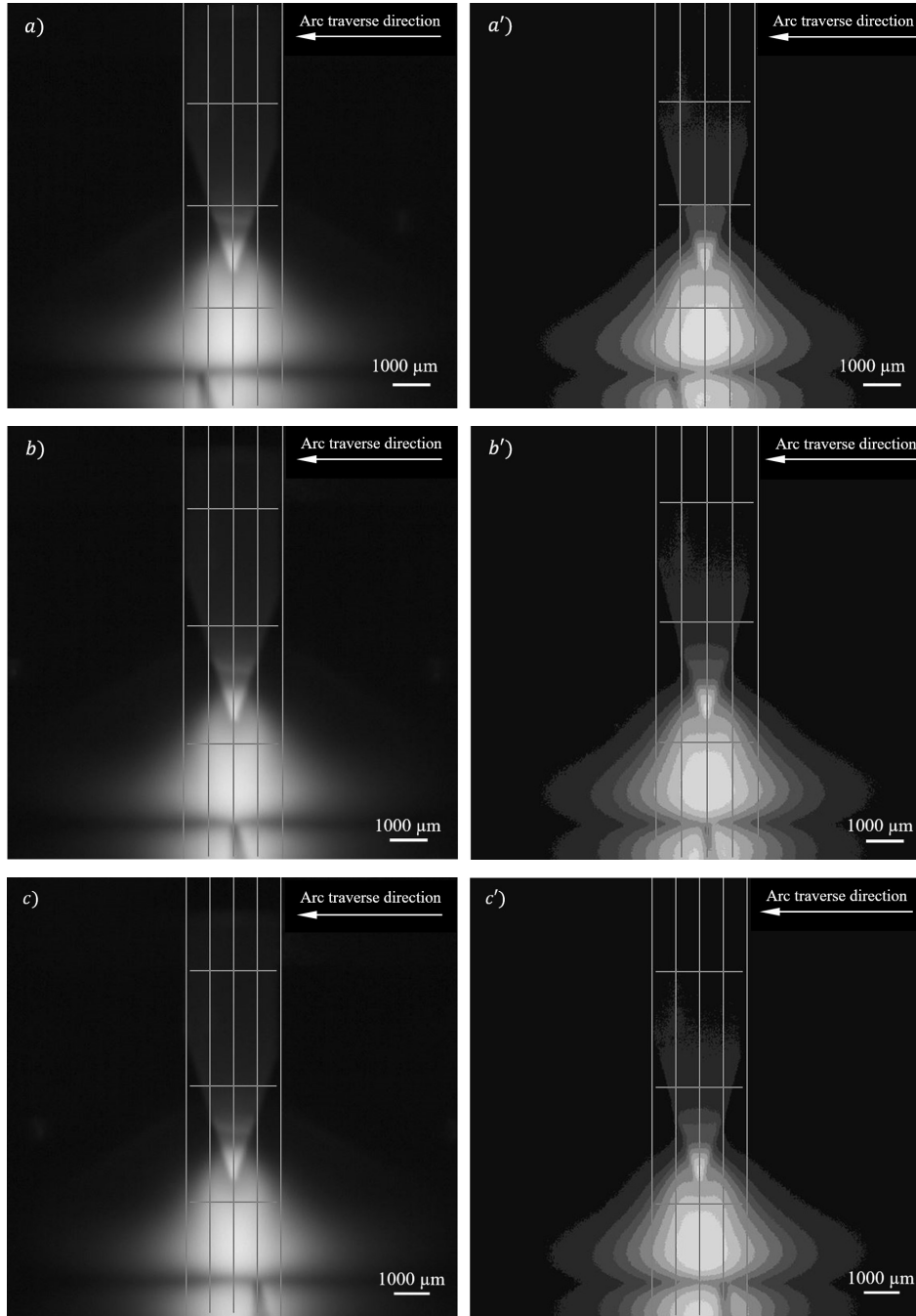


Figure 3.9: High definition camera reference weld images revealing axial symmetry while arc traversing the split plane. $I = 100$ A; ETWD = 3.0 mm.

Figure 3.10 shows high definition camera photographs of different arc positions of one of the ‘100 A / 6.0 mm ETWD’ arc application sequences. No real distinction is virtually identifiable between the real camera images (*a* through *c*); however, the corresponding colour depth reduced images (*a'* through *c'*) show that, in this particular case, the arc is already diverted prior to the interface centre and obviously full axial symmetry was not achieved until the arc had passed the split plane. Comparing these images with the previous sections in Figure 3.9 and averaging both the arc core and the entire visible arc region one may evaluate similar dimensions with the arc core both for 3.0 mm and 6.0 mm ETWD, but a greater overall arc diameter for the latter. According to Schoeck [52] the current conducting region is smaller than the region that transfers heat to the anodes and although Schoeck defines the anode surface temperature independently of the current height applied uniformly with 1000 °C the arc may become deflected due to varying temperature gradients towards the split interface (heat accumulation). It is suggested in [52] that anode surface temperature effects are negligible versus the temperature gradient present in the arc; however, a temperature accumulation *before* the anode interface, caused by the spatial, hence, thermal separation of both anode halves, might explain both the arc deviation prior to the split interface. It furthermore might however also explain the consecutive arc stabilisation after crossing the interface; providing the temperature field is balanced again by heat conduction in the second anode.

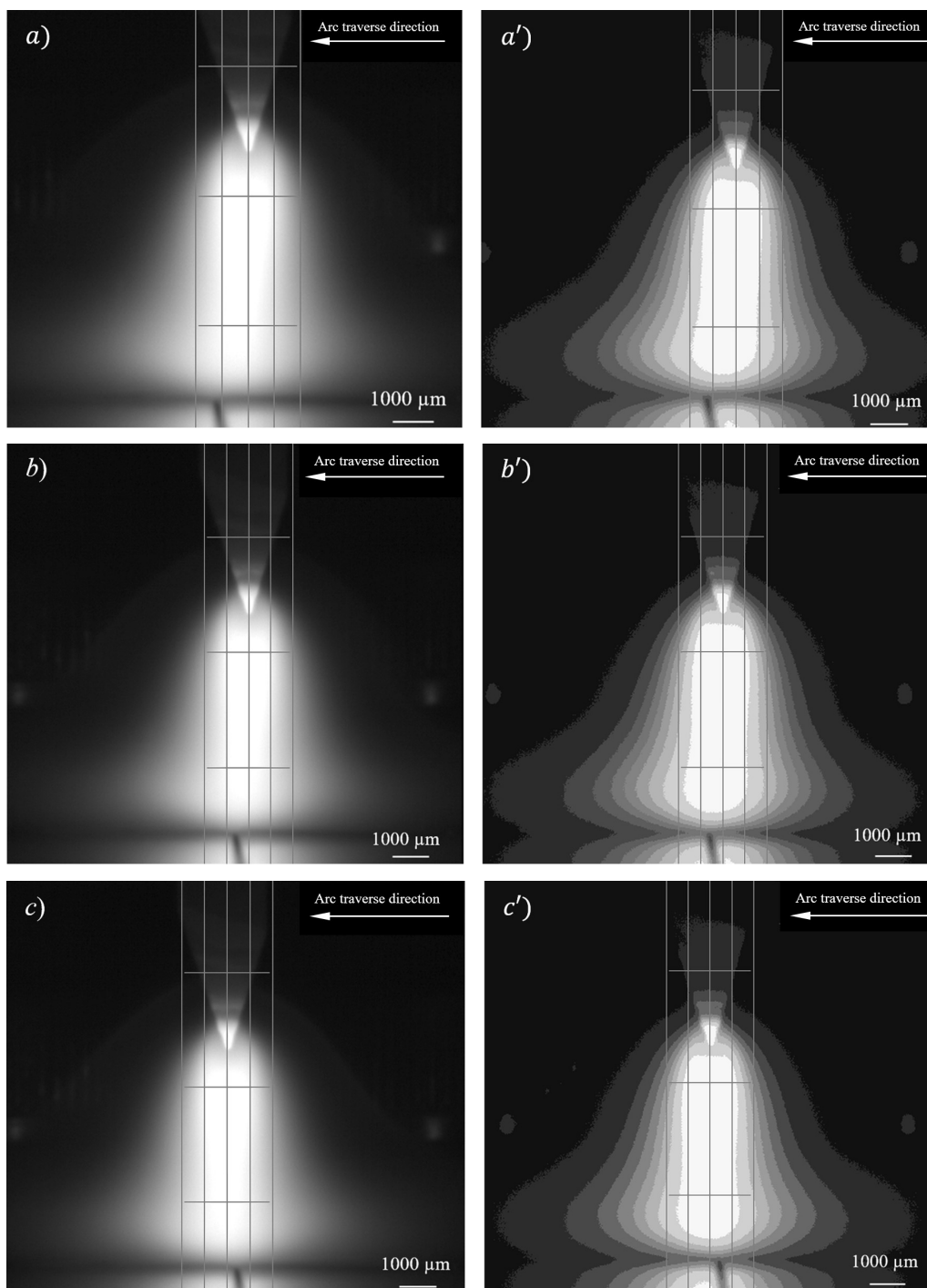


Figure 3.10: High definition camera reference weld images. $I = 100$ A; ETWD = 6.0 mm.

Finally to compare the previous results with a sequence applying 6.0 mm ETWD and 50 A welding current; Figure 3.11 virtually reveals high arc stability, hence, axial symmetry both prior to and after the arc has crossed the split interface. The arc is approaching and crossing the split interface from sections *a* through *c*; i.e. section *a* is 0.90 mm before; *b* is as close as 0.1 mm to the centre, and *c* shows the arc 0.90 mm after crossing the split plane. Sections *a'* through *c'* again depict the appropriately modified image resolutions being labelled similar to 3.6.

Jet velocities have been reported to be between $\approx 180 \text{ ms}^{-1}$ [100] and 130 ms^{-1} [52] for a 100 A argon arc plasma and to increase with the current which should help to stabilise the arc. The results here however indicate that the 50 A condition was more symmetric in this particular instance. Repetitive trials could indicate that the process is susceptible to random instabilities; so a trial, that is repeated multiple times, may demonstrate symmetric behaviour during one test and non-symmetric behaviour during the next. This may be due to the surface condition of the anodes among other things. Hence, the symmetric behaviour in Figure 3.11 and non-symmetric behaviour in Figure 3.10 appear as not reflecting the process conditions, but rather the random nature of this particular experiment. The next section shall be finally used to raise some considerations on the discontinuities observed.

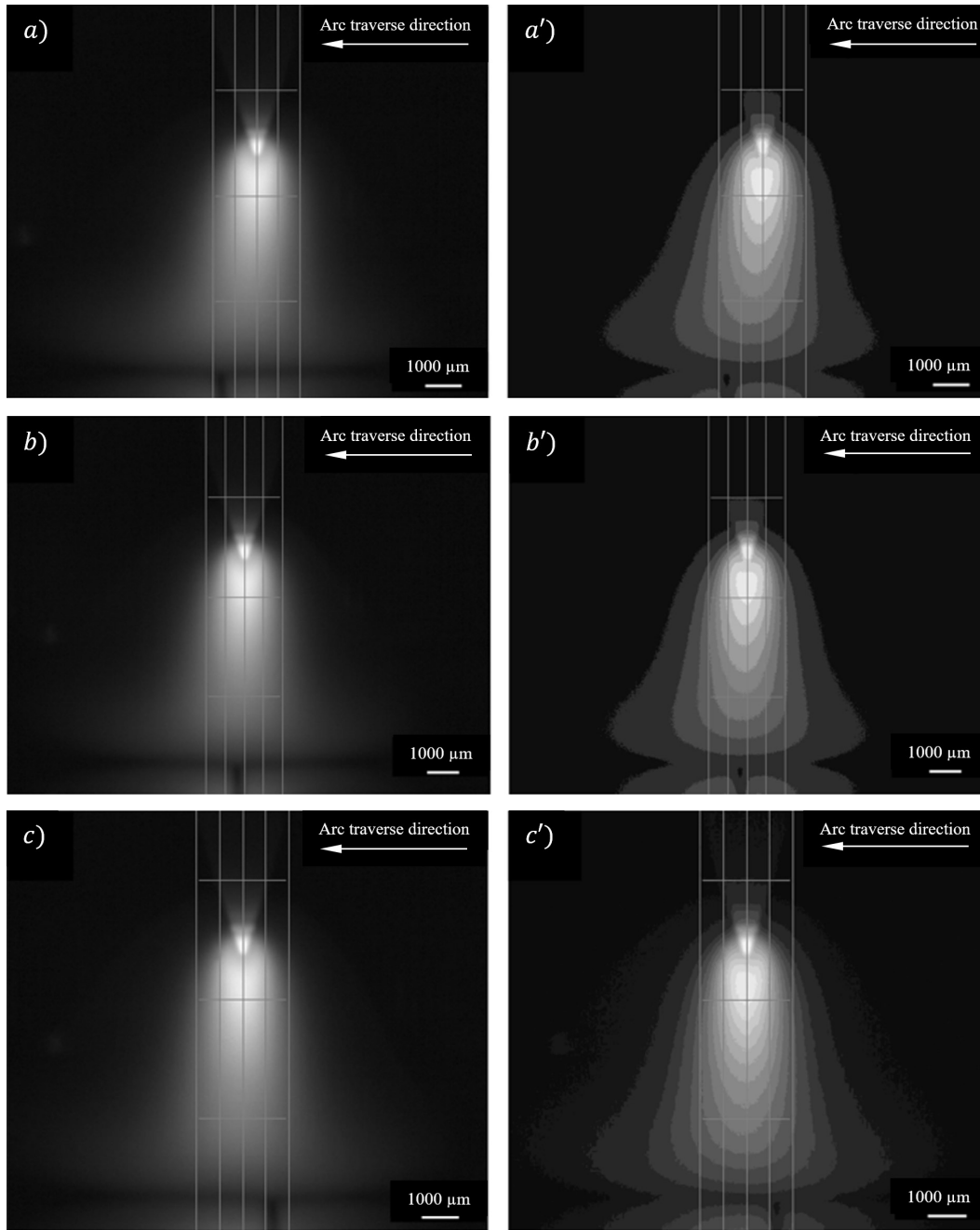


Figure 3.11: High definition camera reference weld images revealing proper axial symmetry while arc traversing the split plane. $I = 50$ A; ETWD = 6.0 mm.

3.4 Oxidation

Although water cooled anodes have been applied in this study, various surface oxidation patterns were observed after using the reference nozzle. This is suggested in accordance to the statements from Schoeck [52] or Schoeck and Eckert [61] who assumed anode surface temperatures of 1000 °C or 927 °C respectively. According to [101] copper oxides of different chemical composition are formed at temperatures (t) of $\approx 125\text{ °C} \leq t \leq 1025\text{ °C}$. Cuprous oxide (copper (I) oxide or Cu_2O) is a red coloured firmly adhering oxide layer formed at temperatures of $\approx 125\text{ °C}$, while first scaling processes take place at temperatures of $\approx 250\text{ °C}$. Temperatures $\geq 260\text{ °C}$ lead to increased production of cupric oxide (copper (II) oxide or CuO), a black coloured crystalline oxide layer. Cu_2O is a semiconductor whose precise structure and thermoelectric properties are, according to Korzhavyi and Johansson [102], not yet fully understood. Young and Schwartz [103] have investigated Cu_2O single crystals in a temperature range of 300 K through 500 K to quantify the oxide’s thermoelectric power and electrical conductivity and found the latter increasing with a rise in temperature and furthermore refer to O’Keefe and Moore [104] who have revealed that the oxide conductivity at temperatures greater than 500 K depends on the heat of the reaction of $\text{CuO} \rightleftharpoons \text{Cu}_2\text{O}$. O’Keefe and Moore [104] particularly emphasise that any “interpretation of electrical properties above 500 K is complicated by the fact that Cu_2O is thermodynamically unstable in air at temperatures below 1000 K.”. Dark blue and even black coloured oxide layers were found in these experiments after arc application; hence, it is supposed that surface temperatures similar to those stated by Schoeck [52] and Schoeck and Eckert [61] have occurred during the experiments, i.e. the anode surface temperatures prove sufficiently high to allow for a partial reaction between oxygen and copper thereby forming different thickness oxide layers. It could however not be clarified whether these oxides are instantaneously formed; i.e. while the arc is interacting with the anode surface, thereby finally affecting measurement output stability, or may have formed after the arc has already passed a specific position upon the anode. For example, Haessler *et al.* [105], though using higher welding currents in their investigation, have found that a deflected TIG arc can cause complex vortexes which lead to shielding gas contamination with air from the ambient atmosphere surrounding the shielded zone. The prototype gas nozzle was designed to derive reference data for comparison with values from experiments applying smaller diameter nozzles. The gas flow rate

was chosen, according to its large diameter arbitrarily but, as derived from preliminary trials, under the consideration to maintain it constant also with those smaller nozzles yet to deploy. By equipping the reference part with a double grid mesh gas diffuser, the nozzle was assumed capable of producing laminar gas flow throughout the experiments, safely to protect anodes and arc interaction zone from contamination with oxygen. However, some fundamental uncertainties were remaining in dependence to arc current height and ETWD:

1. Anode surface temperature - considered to mainly affect oxidation reactions.
2. Cathode jet velocity - considered to mainly affect gas flow stability.
3. Degree of arc deflection - considered to induce or affect vortex phenomena.

This has finally not allowed to determine the actual shielding gas flow conditions while applying the prototype gas nozzle. From the different colouration observed, however, it is assumed that, through the welding sequence, the shielding gas is contaminated with oxygen and the oxide layers found are the consequence thus of the interaction of primarily oxygen with the copper anode surface raised to higher temperatures.

Figure 3.12 (a, b), representatively shows for 100 A welding current and 6.0 mm ETWD significant variation in the appearance of oxides found after arc application completion. In particular, Figure 3.12 (a) reveals imbalanced conditions, especially around the split interface, i.e. the arc was retained in the split plane vicinity which obviously led to arc deflection (note the ovality of the oxide layer at ‘*anode 1*’) and heat accumulation before the arc was crossing the split interface between both anodes. Dependent on the actual, i.e. contingently affected surface condition, the anode spot may move around while traversing the copper anodes, which from the camera images could - in part - occur for the spatial x-axis direction. Hence, although sufficient delay time was applied before triggering the data acquisition system, to allow the arc to stabilise before the manipulator moves the cathode forward incrementally, the arc nonetheless appears dynamically to detrimentally interact with the anode surface along the traverse path. Providing that - within the boundaries of the systematic error - anode preparation quality

(i.e. dimensional accuracy, surface preparation quality) was high in the experiments carried out; arc symmetry appears to remain unpredictable due to random effects, which can be considered as a further essential outcome from this investigation.

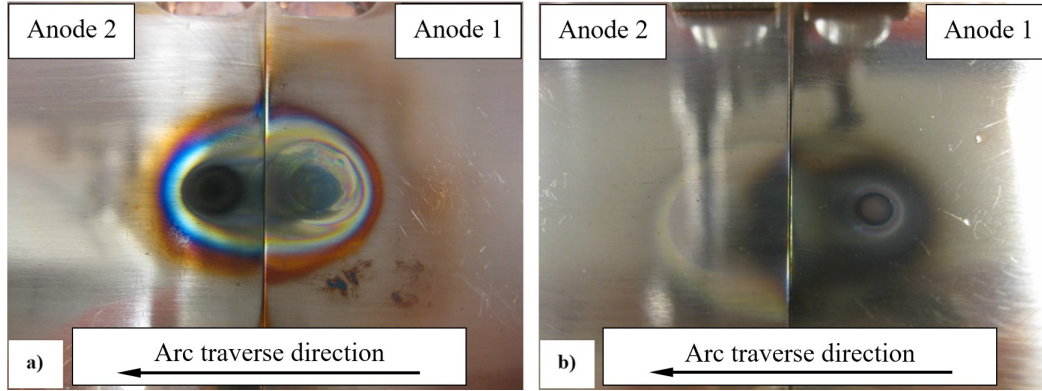


Figure 3.12: *a* - Surface oxidation left by the arc after traversing the anode indicating plasma accumulation and arc asymmetry; *b* - reduced oxide traces with obviously axially symmetric anode spot, no heat accumulation, and smooth transition at the split interface. Welding conditions throughout: $I = 100$ A; ETWD = 6.0 mm.

Notably, from Figure 3.5 (a, b) the results achieved in this investigation did not show Gaussian-like current density distributions, with neither 50 A nor 100 A arc current applied. As discussed earlier in section 2 of this thesis, it was found that a cuprous surface oxide layer, once over-welded, can considerably influence the current density (narrower arc and density increase towards the arc centre), and the experimental outcome at this stage basically confirms the findings derived from the previous investigation. The previous study, however, was conducted using anode surfaces with higher roughness grade (wet 500 grit abrasive paper and dry scouring pad grinding was applied prior to welding), as well as a smaller shielding gas nozzle diameter (ϕ 19.0 mm) and lower gas flow rates (≈ 0.17 Ls⁻¹). Also, no photographic images were taken, which makes it difficult directly to compare both experimental series because of the different conditions applied in both investigations. It appears interesting, however, that the cuprous oxide layer, once formed, seems to stabilise the arc - and thus presumably arc symmetry - but obviously causes random effects while being generated upon the anode surface;

understanding that this is occurring while the arc stabilises along the adjusted delay time. That suggests that the oxidation may indeed cause arc instabilities but as such is not the general root cause for the non-Gaussian current density profiles. To the best knowledge of the author of this thesis the aforementioned phenomena; i.e. arc instability or oxidation effects and their impact on the measured data, have not been reported before in the existing literature dealing with split anode calorimetry used to analyse TIG arcs. This finally leads to some fundamental suggestions derived from the study; i.e. either:

1. The TIG arc energy density is Gaussian distributed as long as applying means such as Nestor [56,58]; i.e. sealed chambers filled with argon but not using gas nozzles of any kind; or,
2. Using a gas nozzle to shield the TIG arc, which in fact would represent real industrial environment conditions, affects the measurement output, perhaps leading to non-Gaussian density profiles. However, in turn, it is assumed that this behaviour might have been detected also in previous investigations which were using an approach similar to the one dealt within this thesis and as such should have been taken appropriately into account or published by other researchers.

Subsequently to this investigation stage it was decided to conduct further analyses to carry out by varying gas nozzle diameter, volumetric flow rate, or gas flow velocity; possibly to determine factors stabilising arc and axial symmetry while traversing the arc across the split interface. However, although different in distribution, the results aforementioned in the previous section basically prove similar to the preliminary stage investigation as dealt with in chapter 2 of this thesis. Viz. these showed current and power density maxima shifted away from the arc centre. In general, Gaussian-like density distribution was also not found as a result from the experiments conducted and analysed, which contradicts results from other researchers, e.g. Tsai [57], Ushio *et al.* [50], Tanaka *et al.* [6], Tsai and Eagar [64], Schoeck [52], and Schoeck and Eckert [61].

3.5 Summary

From the experiments conducted through this investigation stage, the main findings can be summarised as follows:

- i. Split anode calorimetry and a prototype gas nozzle of 30 mm in diameter were used to investigate TIG arc current density distribution when applying 50 A and 100 A welding current.
- ii. Despite high anode design accuracy and preparation quality, arc deflections were found especially close to the split plane which distorted the output and are considered to underlie random effects and to significantly influence axial arc symmetry.
- iii. Although higher arc currents and lower ETWD apparently stabilise the arc, thereby confirming results from other researchers, no experimental condition was found in our study that would reliably lead to arc stability and remain consistent throughout each trial series.
- iv. Oxide layers were found after testing which are assumed to arise from shielding gas contamination with oxygen from the surrounding atmosphere due to turbulent flow and, as indicated by other researchers, presumably caused by arc plasma induced vortexes.
- v. Similar phenomena have not been reported from previous investigations which might be either based on the fact that a sealed chamber filled with argon but no gas nozzle was used, or these phenomena have not been taken into account as yet, i.e. similarly as to the approach of our study.
- vi. It remains unclear from this investigation how far anode surface oxidation is quantitatively affecting arc symmetry; however, adverse random effects on axial symmetry are thought to occur by complex interaction between anode surface temperature and degree of anode oxidation.
- vii. Gaussian-like current density distributions were not found in this work, hence, confirming the results from a the fundamental investigation stage of this study.

4 Investigation of Shielding Gas Nozzle Design Factors

This section particularly deals with the results obtained from studying the effects of varying shielding gas nozzle design, especially, however, the gas nozzle diameter to understand whether this may measurably affect the energy distribution in the arc. The results hereafter discussed were presented at the Joint Intermediate Meeting of the International Institute of Welding (IIW) Technical Commissions C-I; C-IV; C-XII, and SG-212 held on the 4th of March, 2019, in Greifswald, Germany. The corresponding research paper connected to the verbal presentation was referred to as: *‘Influence of shielding gas nozzle design on power density distribution in low current TIG welding arcs’*, authored by S Egerland, P. Colegrove, and S Stewart; and was submitted to the IIW for being published in ‘Welding in the World’. It needs particular mention that this experimental series, despite its scientifically oriented character, was decided to conduct because of the fact that the study’s outcome should finally also provide benefit to the industry.

In the previous chapter a large diameter shielding gas nozzle was used, designed to produce laminar gas shielding to allow for an arc unaffected by turbulent flow likely to cause arc fluctuations and hence, negatively influence the radial energy density distribution. Nonetheless arc instabilities were found which, to a certain degree, have led to unpredictable results in the arc current density distribution. So far different suggestions have been discussed on the root causes for the phenomena observed, however, as consistent arc stability was achieved through the measurement, in particular while the arc was crossing the split plane interface area, it was found that Gaussian energy density distribution is not apparent in the arc. If this happens in a real welding application, e.g. where the TIG cathode is centred at the split gap between the two sheets, the off axis maxima in the radial energy may cause the arc instabilities sometimes observed during these welding conditions. Although gas nozzle effects are particularly known from Plasma Arc Welding (PAW) where applying a nozzle is used to constrict the arc and to improve its axial stability [26], there is little research literature on the impact of nozzle design in TIG welding. Considerably more investigations were conducted in MIG/MAG welding; for example Ramsey *et al.* [106] have numerically modelled and experimentally validated the effects caused by different shielding gas nozzle diameters to mitigate side draughts as occur in

practical on-site MIG/MAG welding applications. Although their investigation was for Ar/CO₂ blends (80Ar/20CO₂) and the shielding gas nozzle design is considerably different to TIG shielding nozzles, they could find substantial differences in the nozzle exit velocities which increased as the nozzle diameter decreased. Unlike other studies, Ramsey *et al.* have involved the arc plasma by simulating a constant temperature field which could lead to proper correlation between the numerical model and the experiment.

Campbell *et al.* [107] in a numerical and experimental investigation also dealing with MIG/MAG welding, and mainly based upon the work of Ramsey [106], have varied the gas flow conditions by changing the shielding gas nozzle geometry; i.e. they have used a “double helix insert”, a “restricted plate”, and an “internal plate” in the shielding gas nozzle to study the effects caused by these hardware inserts. Campbell *et al.* could show that the reduction of the shielding gas nozzle diameter and the inclusion of a circumferential internal plate as an additional constriction, could increase the nozzle exit velocity which, in turn, allowed a reduced gas volumetric flow rate, without degrading welding quality. Dreher *et al.* [108] reported on the numerical simulation of MIG/MAG shielding gas nozzle effects both including and devoid of the arc plasma which is stated to be of crucial importance. The numerical models showed significant turbulences to occur within and in front of the MIG/MAG shielding gas nozzle. The authors also refer to the so-called ‘Schlieren’ Technique, which makes use of density variations as present e.g. in the shielding gas surrounding the arc. This method was also used by Siewert *et al.* [109] who were analysing shielding gas flow conditions in MIG/MAG-, TIG-, and Plasma Arc Welding. Volumetric flow rate threshold values are provided in [109] for the TIG process; stating that “shielding gas flow rates of 30 L/min and more” cause “turbulences surrounding the arc”, however, unfortunately insufficient detail on the TIG experimental setup prevent comparison with the present study. For PAW the authors found indications that, despite turbulent flow due to high flow rates, this had no detrimental effect on weld bead protection.

4.1 Aim and objectives

The aim of this investigation stage was hence to understand the factors that affect the stability of low current TIG arcs especially when used with a split anode calorimeter. This shall be achieved with the following objectives:

- a. To understand the effects of shielding gas nozzle diameter variation; the energy distribution in the arc was studied, to determine whether this had an impact on arc stability.
- b. To use efficient visualisation techniques capable of clarifying the shielding gas flow behaviour and its effects on arc stability under different experimental conditions.
- c. To perform experiments in a shielding gas chamber, that provided a protective inert gas atmosphere to determine whether surface oxidation is the primary cause for the arc instabilities observed in the previous stages of this study.

4.2 Experimental

The experimental setup was similar to the methodology applied and described in the previous sections of this thesis; however, the welding torch, was equipped with different prototype shielding gas nozzles. As the ‘reference nozzle’ these were produced from austenitic stainless steel X8 CrNiS 18-9 according to EN 10088-2:2005 (~ AISI 303) and capable of also accommodating the mesh diffuser assumed to produce laminar shielding gas flow. The essential data, applied through the experimental series of this study stage, is depicted in Table 5.

Parameter	Unit	Value
Current	A	50/100
ETWD	mm	3.0/6.0
Vertex angle (α)	°	36
Cathode composition	-	WCe ₂ O
Cathode diameter	mm	2.4

Table 5: Essential experimental data.

Figure (4.1) shows the commercial (FRONIUS TTWA F/F++/UD/4m) TIG welding torch equipped with the 20 mm diameter prototype shielding gas nozzle and also reveals the mesh gas diffuser used to generate laminar gas flow conditions. This investigation was targeted at reducing the shielding

gas nozzle diameter versus the earlier study stage where a 30 mm diameter nozzle was applied.

To compare the different nozzle diameters either the volumetric gas flow rate (Set 1) or shielding gas flow velocity (Set 2) was maintained constant as shown in Table 6. This was done to determine how each affected the power distribution in the arc.

Quantity		Nozzle diameter			
		5	10	15	20
Set 1	$\dot{V}$ (Ls ⁻¹)	0.3	0.3	0.3	0.3
	ν (ms ⁻¹)	15.3	3.8	1.7	0.92
Set 2	$\dot{V}$ (Ls ⁻¹)	0.0083	0.030	0.075	0.13
	ν (ms ⁻¹)	0.42	0.42	0.42	0.42

Table 6: Experimental values depending on shielding gas nozzle diameter.

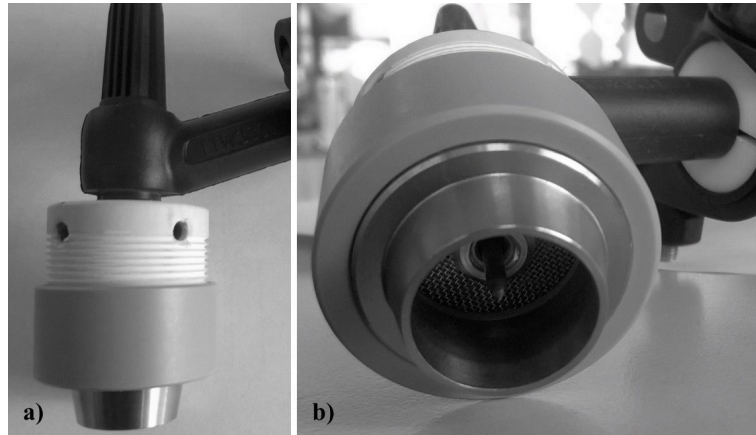


Figure 4.1: *a* - TIG welding torch assembled with 20 mm prototype gas nozzle; *b* - view into the nozzle, revealing gas mesh diffuser.

Four different nozzle designs, showing varying diameters of 5 mm; 10 mm; 15 mm, and 20 mm, were produced, as schematically depicted in Figure (4.2).

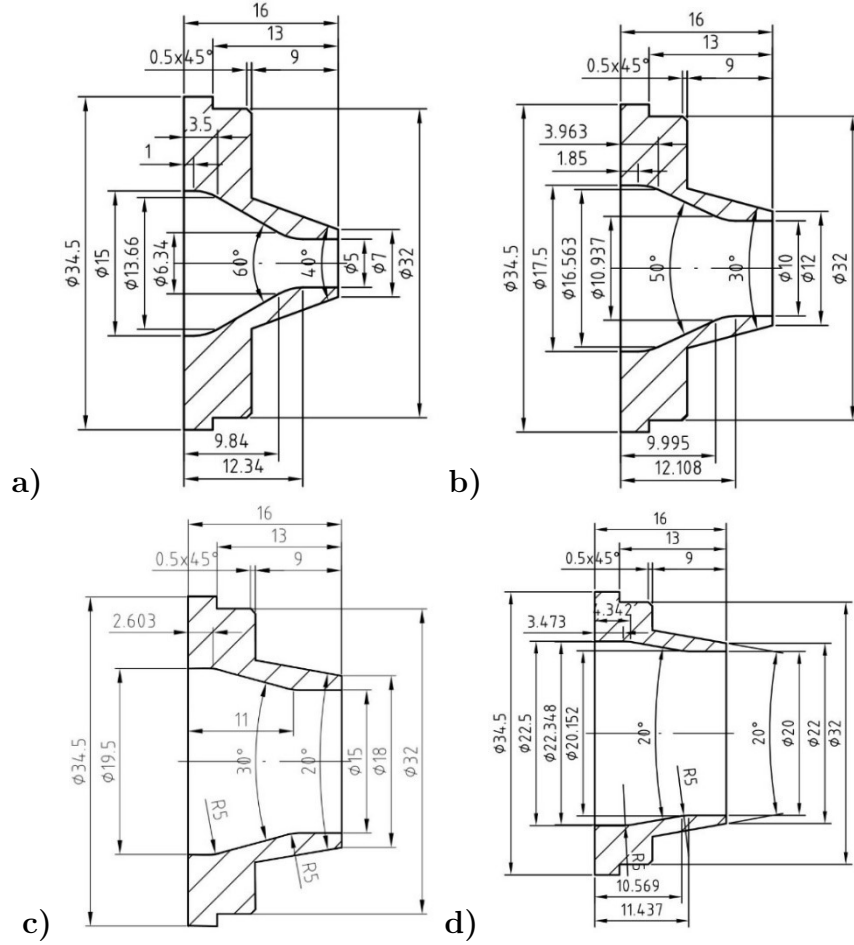


Figure 4.2: *a* - Prototype shielding gas nozzle diameter 5 mm; *b* - diameter 10 mm; *c* - diameter 15 mm, and *d* - diameter 20 mm. All dimensions in mm.

4.3 Calorimetric power density

The heat flux- or calorimetric power density was computed by averaging the 5 raw data values of the quantities previously described during each incremental measurement sequence and applying the following equation 4.1:

$$Q = \dot{V} \rho c_p \Delta T \quad (4.1)$$

With Q representing the heat transferred to the anodes and absorbed by the calorimeter coolant (water), ΔT as the difference between inlet and outlet temperature at the particular point in time, and $\dot{V}$ as the volumetric coolant flow rate. Using data from [110] the density ρ of the calorimeter coolant was calculated as 996.5 kgm^{-3} , while a specific heat (c_p) of $4182 \text{ Jkg}^{-1}\text{K}^{-1}$ was applied according to the data by [111]. As with the previous investigation stage MATLAB[™] was used to apply the Inverse Abel Transformation, developed by Nestor [56] and Nestor and Olsen [86] to obtain the current- or power density distribution as a function of the arc radius.

4.4 Shielding gas flow visualisation

It is considered important to understand whether volumetric gas flow rate variation would provide consistent laminar flow conditions. Gas flow visualisation trials in cold condition; i.e. without arc application, were conducted to assess the general applicability of the nozzle design; however, a simplified method compared to the ‘Schlieren’ technique was used which, although found beneficial by other researchers [108, 109] in welding gas flow investigations, requires an extensive experimental setup [109]. Efficient shielding gas flow visualisation could be achieved whereas in this study by applying a commercially available atomiser, Type ‘JOYETEC CUBIS[™]’. Using this device, a liquid 50% propylene glycol and 50% glycerine solution was vaporised and subsequently added to the regular shielding gas flow. The welding torch, equipped with the different prototype nozzles, was clamped into a gantry manipulator which allowed spatial adjustment of all parts relatively to one another. The visualisation of the vapour was achieved by using two focusable red laser line modules, Type ‘LFL650-5-12(9x20)90-F250’, of 650 nm wavelength. Either a base plate was used in appropriate distance to the cathode tip to simulate the anode surface, or the tests were carried out devoid of the base plate to understand the shielding gas free flow behaviour; i.e. to represent the flow without workpiece surface interaction. Figure 4.3 reveals in part the experimental setup deployed for the gas flow condition visualisation trial series as depicted in Table 6.

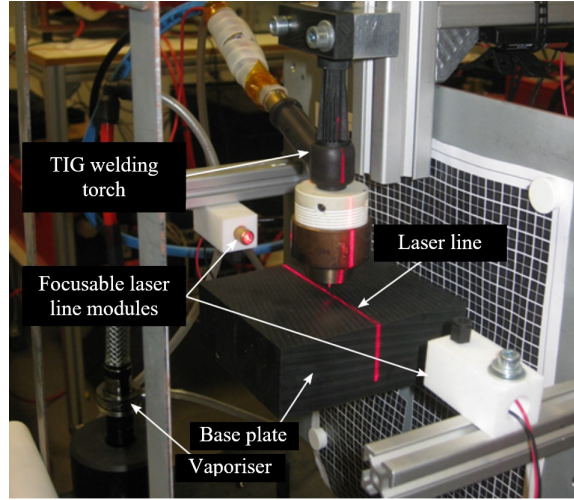


Figure 4.3: Shielding gas flow visualisation experimental setup.

Stimulated by Norrish [112] it was attempted to reveal whether the gas mesh diffuser used in the experimental setup was in general even capable of improving the gas flow to achieve laminar shielding gas flow. Figure 4.4 (a) visualises turbulences in deploying the $\varnothing$ 30 mm reference nozzle devoid of using the gas mesh diffuser, while Figure 4.4 (b) shows the application of the diffuser and how it causes the gas flow to become homogeneously laminar. The mesh diffuser has a significant positive impact and enables the generation of laminar flow conditions.

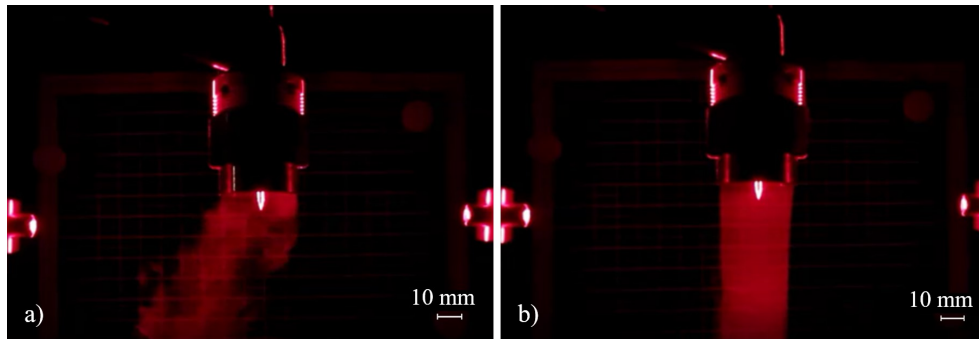


Figure 4.4: *a* Turbulent gas flow deploying the $\varnothing$ 30 mm shielding gas nozzle without mesh diffuser; *b* mesh diffuser installed and causing laminar flow conditions. Gas flow rate = 0.3 L s^{-1} .

4.5 Shielding gas chamber tests

In addition to the aforementioned experimental setup and to appropriately evaluate the anode surface oxidation phenomena observed, a random sample investigation, using a chamber as shown in Figure 4.5, to completely protect the anodes, was conducted. The primary target in deploying this chamber was to understand whether tests could be achieved without surface oxidation, and to understand whether this affected the quantitative output.

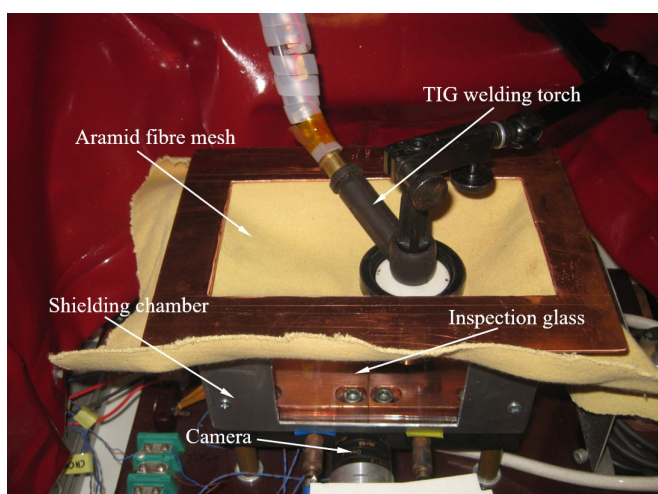


Figure 4.5: Shielding gas chamber for random sample test conduction.

The small aluminium chamber which was also including inspection glass, was produced precisely to fit to the calorimeter polyimide block. Covered by a temperature resistant aramid fibre mesh (KEVLAR™29) of 1.44 gcm^{-3} density and 550°C decomposition temperature [113], it allowed movement of the torch without mechanical resistance caused by the fibre mesh, but nonetheless, due to the mesh's permeability to the shielding gas, reliably to achieve ambient shielding gas pressure conditions inside the chamber. The random check was performed using the 30 mm diameter nozzle as also applied in the previous stage of this study and 50 A welding current along with 3.0 and 6.0 mm ETWD. The oxygen content within the chamber was accurately checked before arc ignition, using a PBI-Dansensor 'OXI-3' Type Portable Oxygen Indicator which assured that a maximum relative O_2 value of 0.3% was obtained during the test.

4.6 Results and Discussion

The following sections describe and discuss the results calculated from the measurement raw data according to the experimental data in Table 5 and Table 6. They primarily deal with the power transferred to the calorimeter; hence, the radial *heat flux* density distribution of the arc applied, and compare with the reference results dealt with in the ‘reference nozzle’ experiments to demonstrate the effects caused by the shielding gas nozzle diameter variation. Unlike the previous study stage, however, the first and second derivative of the radial power distribution were not smoothed in this investigation, which shall be announced already, may lead to greater noise in the calculation output, but nonetheless was carried out because of aiming at roughly understanding in how far smoothing the first and second derivative of the raw data ($F(x)$) may shape the results.

4.6.1 Nozzle diameter 15 mm

Figure 4.6 (a, b) compares the mean values of ‘anode 1’ computed from three arc application sequences and deploying the 15 mm diameter nozzle. Figure 4.6 (a) plots the 50 A results for 3.0 mm ETWD and reveals for the constant flow velocity and very low flow rates, an extreme density maximum (notice the y-axis scale), and a distinct density maximum for the constant flow rate, away from the arc axis. Interestingly neither of these maxima can be found in the $\varnothing$ 30 mm reference nozzle results (blue line) which show a relatively flat heat input profile across the arc radius. Figure 4.6 (a) also shows a collapse of the density around the split plane area when deploying constant flow velocity. The comparison for 50 A, however, applying 6.0 mm ETWD, is depicted in Figure 4.6 (b). Density maxima can be found at ≈ 1.5 mm away from the arc centre along with severe density collapses close to the split plane area both with constant flow rate and flow velocity.

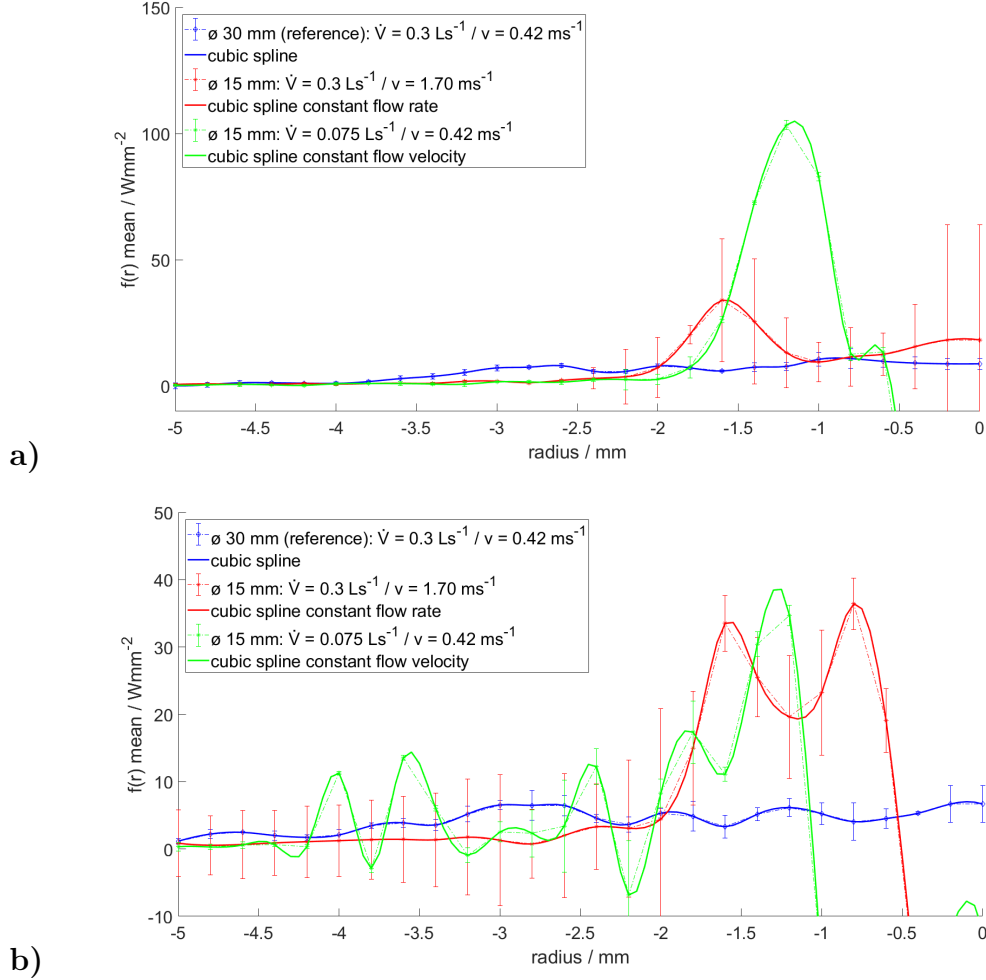


Figure 4.6: Calorimetric power density comparison of 50 A arc current, using 15 mm diameter nozzle and maintaining either volumetric flow rate or flow velocity constant. *a* - ETWD = 3.0 mm; *b* - ETWD = 6.0. Error bars represent standard deviation.

The 100 A results for 3.0 mm ETWD indicate flatter curves for both conditions being distributed similar to the reference nozzle curve, Figure 4.7 (a). This is comparable to the results achieved in the earlier stages of the study, where in general a more Gaussian density profile was found with no distinct electric current density extrema. Figure 4.7 (b) for 6.0 mm arc gap notably indicates the output differences compared with applying 50 A (Figure 4.6

(b)) and shows how doubling the arc current, even with higher ETWD, can stabilise the conditions. Significant heat flux density fluctuation could be observed with increasing ETWD and low volumetric shielding gas flow rate, Figure 4.6 (b), apart from the measurement collapsing phenomena while applying 50 A welding current.

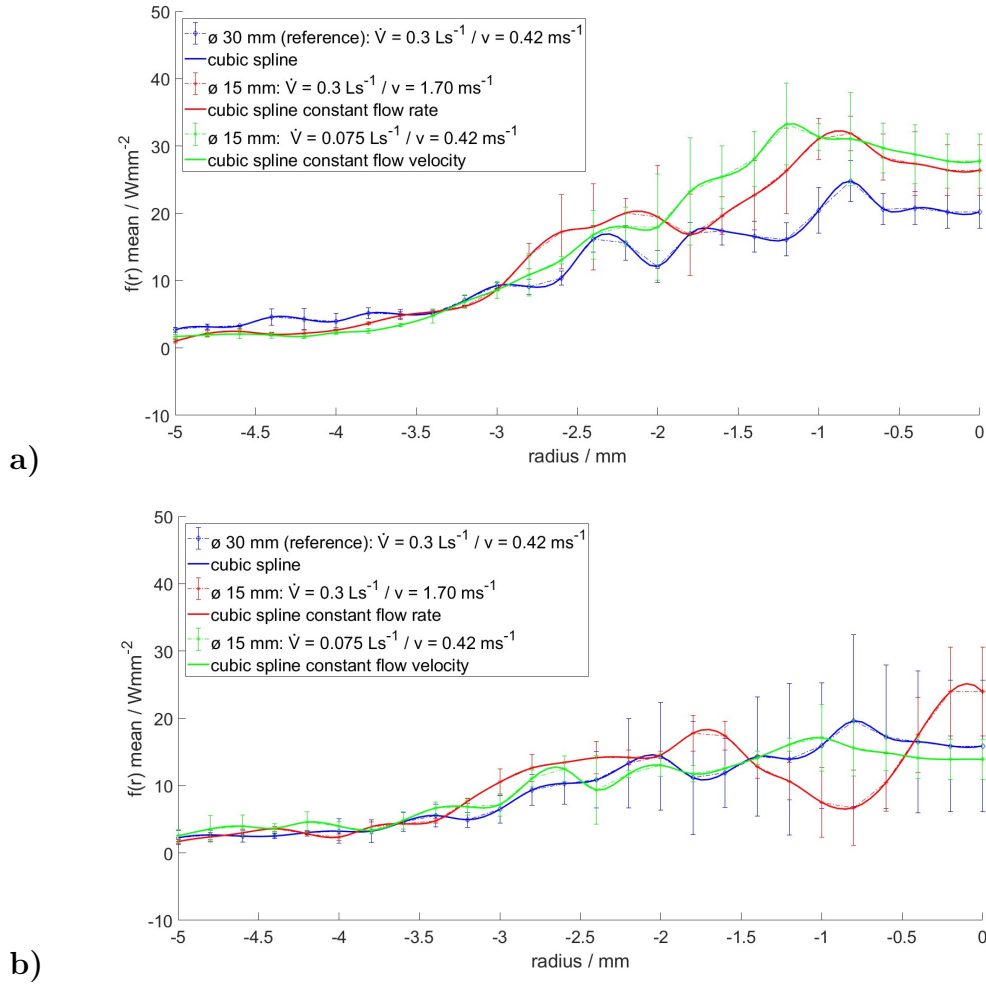


Figure 4.7: Calorimetric power density comparison of 100 A arc current, using 15 mm diameter nozzle and maintaining either volumetric flow rate or flow velocity constant. *a* - ETWD = 3.0 mm; *b* - ETWD = 6.0 mm. Error bars represent standard deviation.

4.7 Nozzle diameter 20 mm

Figure 4.8 (a, b) plots the 50 A arc application sequences while applying the 20 mm diameter nozzle.

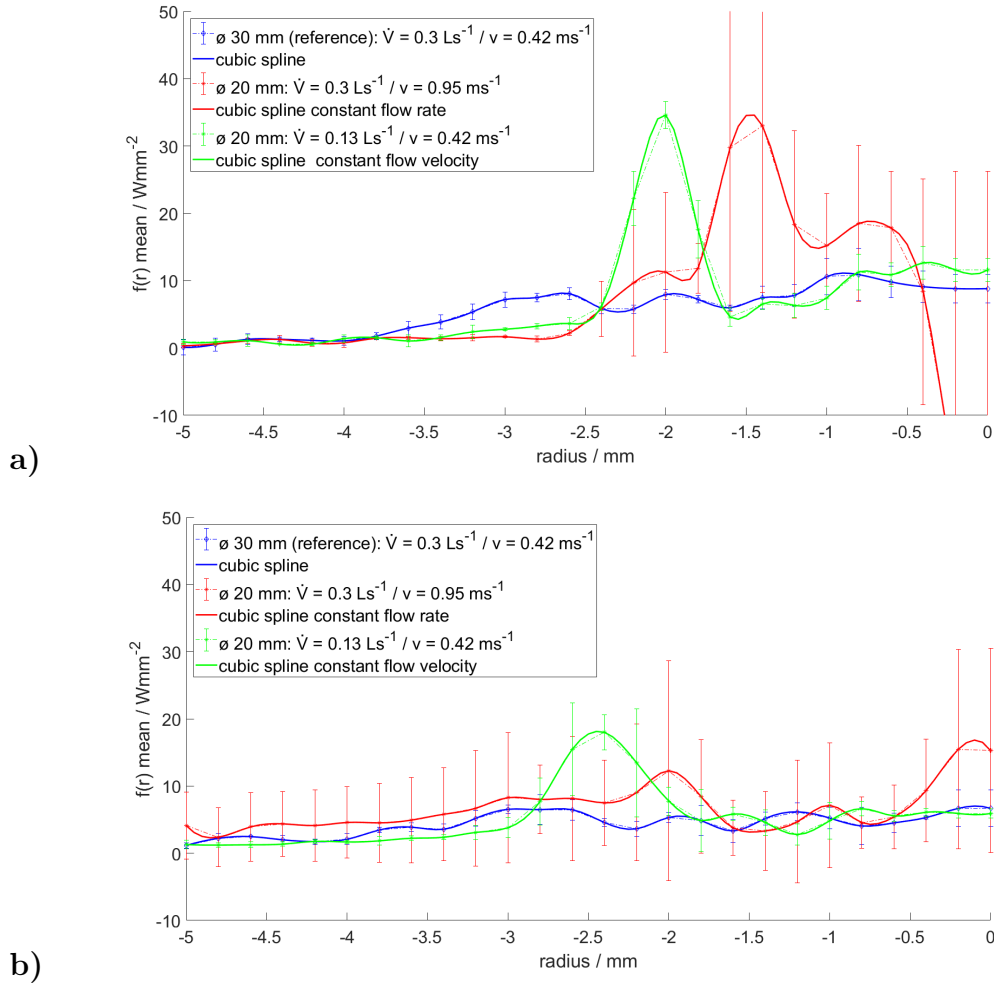
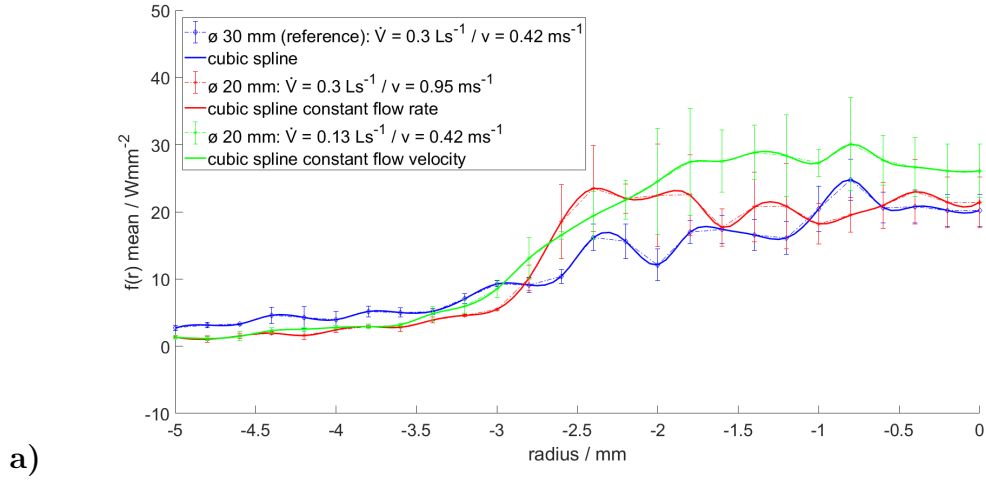
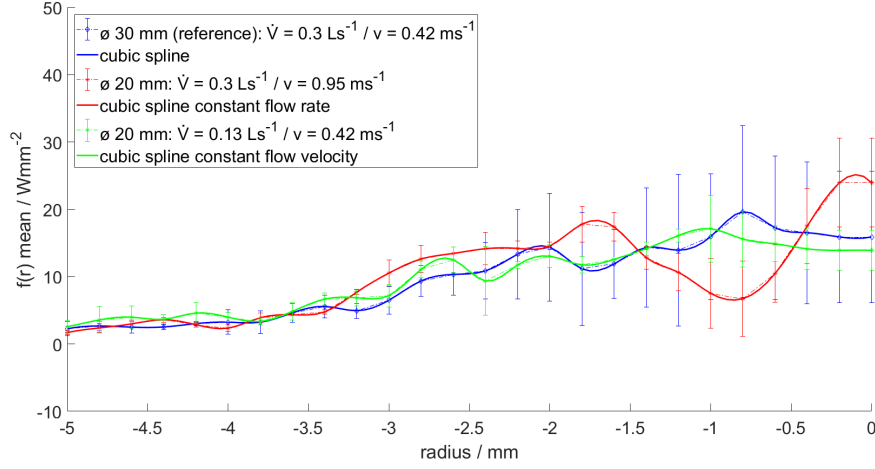


Figure 4.8: Calorimetric power density comparison of 50 A arc current, using 20 mm diameter nozzle and maintaining either volumetric flow rate or flow velocity constant. *a* - ETWD = 3.0 mm; *b* - ETWD = 6.0 mm. Error bars represent standard deviation.

Figure 4.8 (a) reveals the density collapsing with applying constant shielding gas flow rate which, however, is not the case with constant flow velocity. Surprisingly the reverse was found; a relatively stable (notice the low noise in the results) density peak can be observed at a radial distance of ≈ 2 mm away from the arc centre, which is comparable to the current density measurements earlier dealt with in this thesis. This peak can also be identified in Figure 4.8 (b), depicting the results for 6.0 mm ETWD, however, in a lower intensity versus the smaller arc gap. It is to be emphasised that a density maximum cannot be found whereas deploying the reference nozzle type (blue line in Figure 4.8 a, b).

Figure 4.9 (a) and (b) reveal the findings for the 100 A arc application and show a much higher stability compared with the 50 A welding current results. Collapsing around the split plane area is entirely missing and the heat flux density distribution appears alike the $\varnothing 15$ mm nozzle type with increasing values towards the arc centre; thereby showing also similarity versus the results found earlier in this study for the current density distribution.





b)

Figure 4.9: Calorimetric power density comparison of 100 A arc current, using 20 mm diameter nozzle and maintaining either volumetric flow rate or flow velocity constant. *a* - ETWD = 3.0 mm; *b* - ETWD = 6.0 mm. Error bars represent standard deviation.

4.8 Shielding chamber results

Figure 4.10 (a) compares the power density distribution deploying 3.0 mm ETWD along with 50 A welding current and both shielding chamber and the 30 mm reference gas nozzle used previously. Figure 4.10 (b) plots the heat input distribution when doubling the ETWD to 6.0 mm. A density maximum away from the arc axis can be found for the lower ETWD (3.0 mm) fairly similar to the reference nozzle results. For the 6.0 mm arc gap this density maximum is less while overall there is a wider distribution of the arc power and an increase in noise towards the arc axis, which can be found with both reference nozzle and shielding chamber. It also can be seen that the noise in the measurement output increases for the larger ETWD and that no clear indication of a density maximum away from the arc axis occurs with the higher cathode distance.

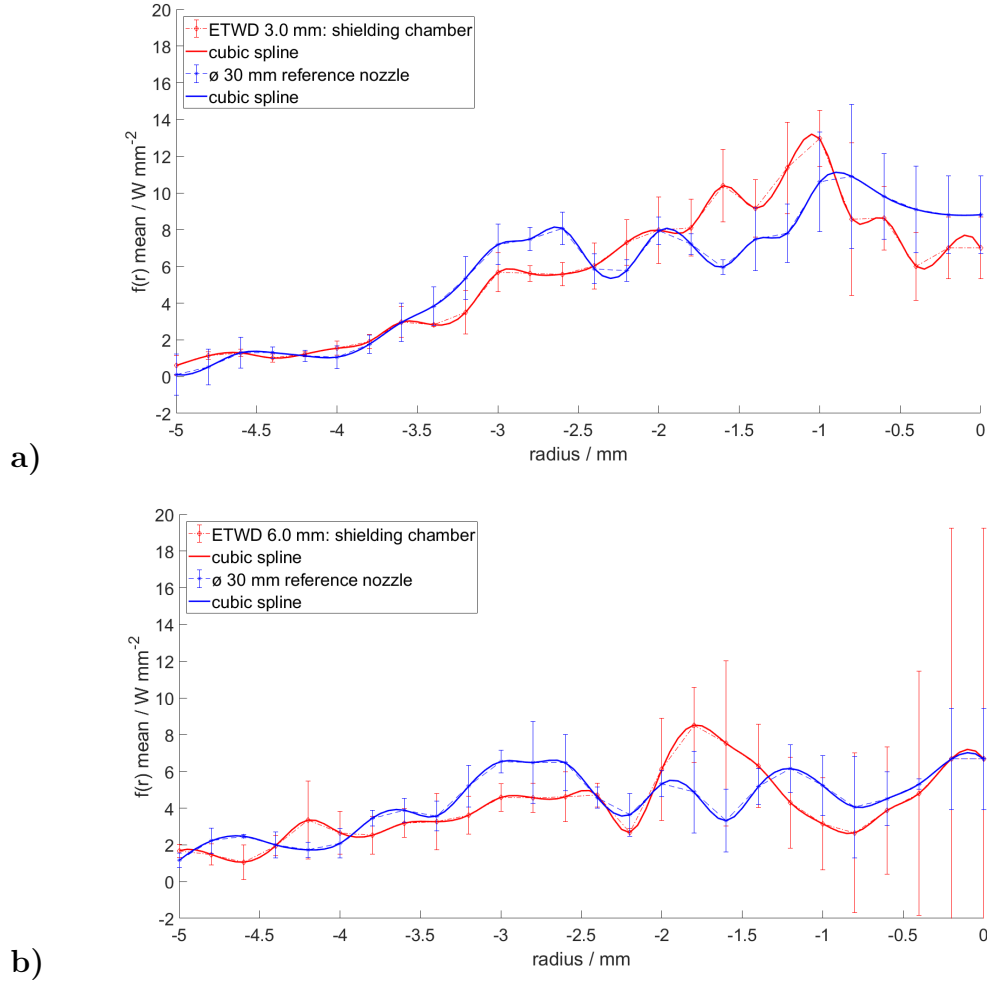


Figure 4.10: *a* Calorimetric power (red) and current density (blue) distribution applying the 30 mm diameter reference gas nozzle along with 50 A current and 3.0 mm ETWD; *b* for 50 A current and 6.0 mm ETWD.

Figure 4.11 (a) depicts oxidation on the copper anodes after the experiment which is considerably less than that observed in the previous experiments Figure 4.11 (b).

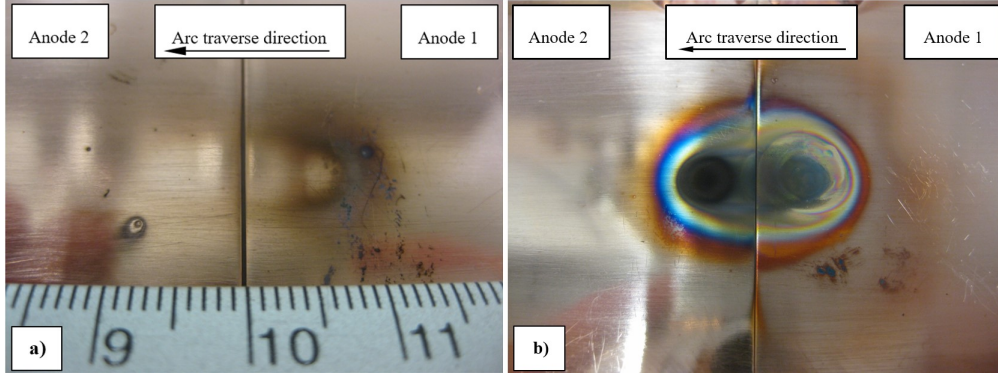


Figure 4.11: *a* Anode surface oxidation after arc application in shielding chamber; *b* anode surface oxidation exemplarily as occurring in previous experiments.

Unlike the application of the 15 mm and 20 mm diameter nozzle, and similar to the previous experimental outcome, the shielded conditions led to high arc stability when traversing the arc across the split plane.

4.9 Shielding gas flow

Varying anode surface oxidation degrees could be observed throughout all measurement sequences, similar to the previous tests conducted, although a double grid mesh diffuser, Figure 4.1 (b), was part of the prototype gas nozzles applied to produce laminar flow. Theoretical considerations, undertaken to identify the primary reasons for this surface oxidation [112], were also involving the case that the prototype gas nozzle may cause turbulent flow finally leading to the involvement of atmospheric gases thus surface oxidation. Figure 4.12 (a) for the 5 mm, and (b) for 10 mm shielding gas nozzle diameter shows for 3 mm ETWD turbulent flow conditions when applied to the base plate representing the split calorimeter anodes. Figure 4.12 (c) again for the 5 mm diameter nozzle and constant flow velocity of 0.42 ms^{-1} ; i.e. significantly reduced gas flow rates, reveals turbulent flow, which is initiated within

the nozzle itself. Figure 4.12 (d) for the 10 mm diameter nozzle depicts a transition between laminar and turbulent flow behaviour while maintaining the flow velocity constant.

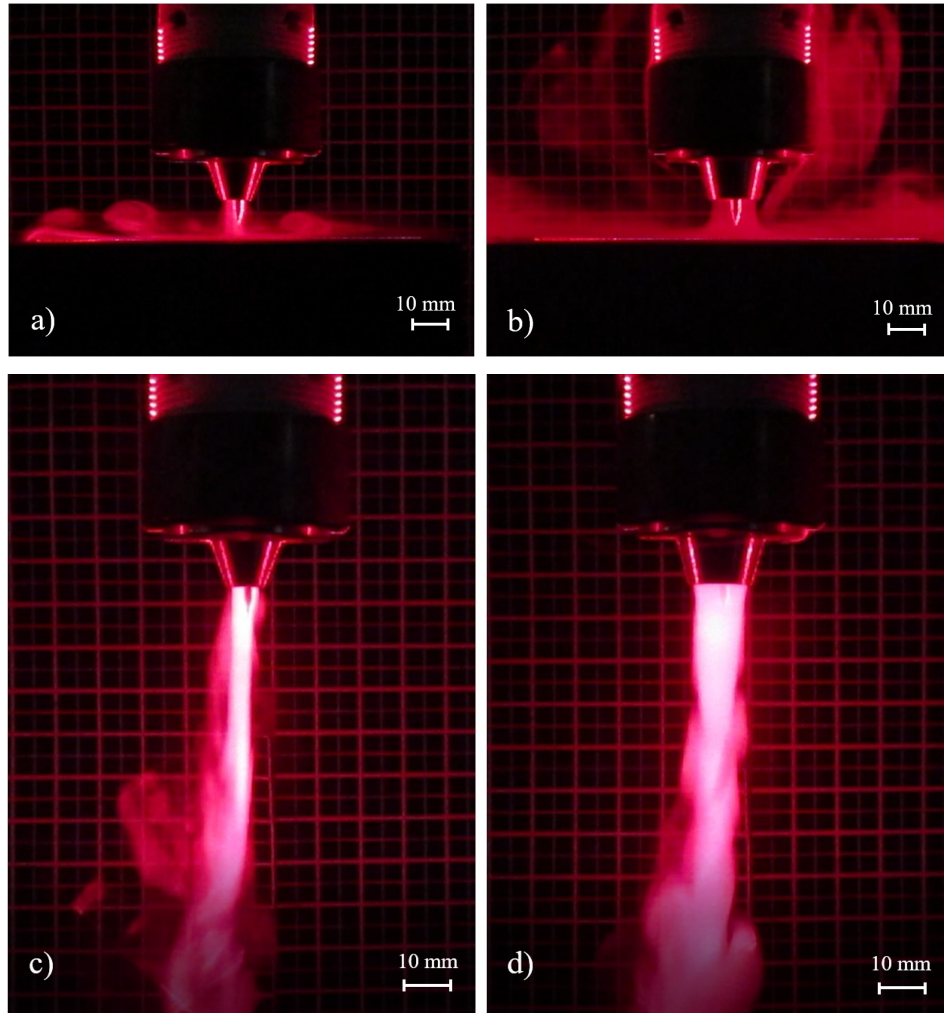


Figure 4.12: *a* turbulent flow pattern applying 0.3 L s^{-1} flow rate and 3 mm ETWD with $\varnothing 5 \text{ mm}$ shielding gas nozzle; *b* similar behaviour for $\varnothing 10 \text{ mm}$ nozzle and maintaining ETWD and flow rate constant; *c* turbulent flow with 5 mm diameter and constant flow velocity; however, applied in devoid of base plate; *d* 10 mm diameter nozzle applied in devoid of base plate and maintaining flow velocity constant at 0.42 ms^{-1} producing transient flow behaviour.

Figure 4.13 (a) plots the flow pattern for 0.3 L s^{-1} volumetric flow rate, nozzle diameter of 15 mm and 3 mm ETWD; Figure 4.13 (b) for the same nozzle diameter visualises for the reduced flow rate, maintaining the flow velocity constant; while Figure 4.13 (c) and (d) depict the 20 mm diameter nozzle conditions.

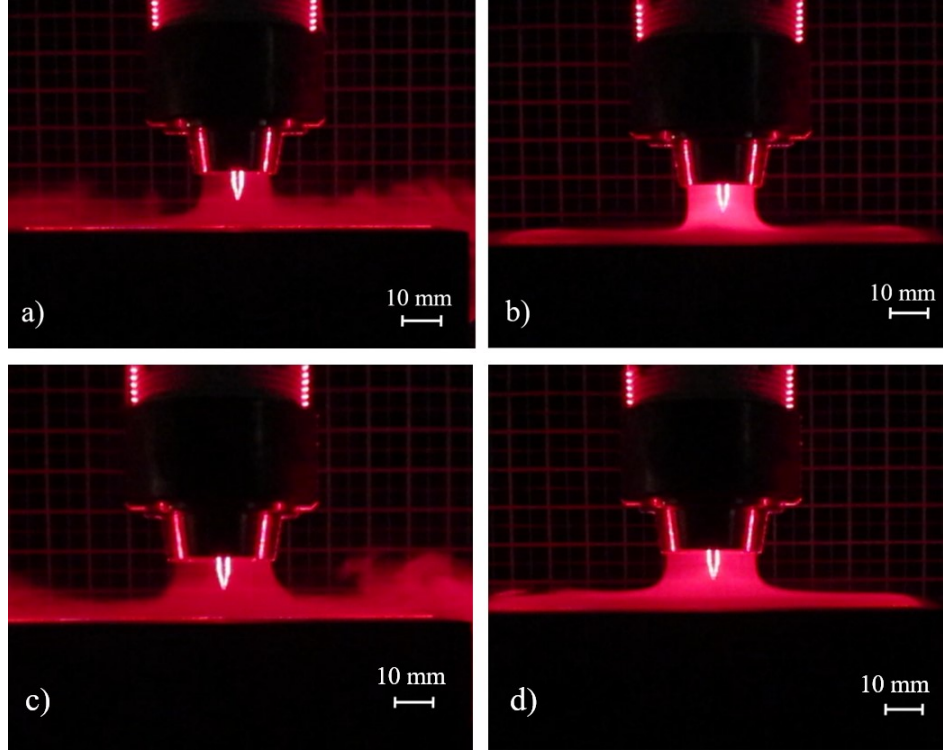


Figure 4.13: *a* Gas flow visualisation trials deploying 15 mm diameter shielding gas nozzle and maintaining 0.3 L s^{-1} gas flow rate constant; *b* 15 mm nozzle maintaining flow velocity at 0.42 ms^{-1} ; *c* 20 mm diameter nozzle at 0.3 L s^{-1} gas flow rate; *d* 20 mm nozzle at 0.42 ms^{-1} flow velocity. Note: ETWD = 6.0 mm throughout.

Table 7 summarises the results, highlighting how laminar flow is obtained with the larger nozzle diameter. Turbulent flow was found throughout for the 5 mm nozzle type; except a laminar behaviour when deploying 0.3 L s^{-1} gas flow rate, and in part, when using the diameter 10 mm nozzle type and maintaining the flow velocity constant. In this conjunction it shall be noted

that ‘transitional’ describes a condition categorised visually because of being not clearly identifiable.

Nozzle diameter	ETWD	Flow condition	Oxidation ⁽³⁾
[mm]	[mm]	[—]	
5	3	turbulent	not applicable
5	6	turbulent	not applicable
5	— ⁽¹⁾	turbulent / transitional	not applicable
10	3	turbulent / laminar	not applicable
10	6	turbulent / laminar	not applicable
10	— ⁽²⁾	laminar	not applicable
15	3	laminar	Surface oxides
15	6	laminar	Surface oxides
15	— ⁽²⁾	laminar	Surface oxides
20	3	laminar	Surface oxides
20	6	laminar	Surface oxides
20	— ⁽²⁾	laminar	Surface oxides

Table 7: Gas flow visualisation results for all prototype shielding gas nozzle types deployed. ⁽¹⁾ Application devoid of base plate. ⁽²⁾ Gas flow velocity = $0.42 \text{ ms}^{-1} = \text{const.}$ ⁽³⁾ Arc application experiment.

4.10 Reynolds number

It was suggested in the previous stage of the investigation [99] that cathode jet variation may affect or cause surface oxidation, hence, detrimentally influencing measurement or calculated output. It is remaining unclear whether the copper oxide found on the surface is either produced while the arc is applied to the anodes or after the arc interaction with the anode material; virtually, as a high temperature reaction of the anode surface subsequent to the arc application. Finding surface oxide layers also in applying the smaller shielding gas nozzle diameters, it appears consequently essential to understand the primary factors for surface oxidation; i.e. how can the anode oxidise although - theoretically - laminar shielding gas flow should have protected the arc interaction zone. Munson *et al.* [114] point out that categorising fluid flow precisely as “laminar, transitional, or turbulent” is not trivial because the final flow conditions are sensitively influenced by a number of different variables; it was nonetheless decided to take the Reynolds

Number, expressing the ratio of the inertial over the viscous forces of a fluid, into greater account in this study. To assess and compare the experimental results dealt with in the foregoing sections, the dimensionless Reynolds Number (Re) was calculated corresponding to each shielding gas nozzle diameter applied through the experimental series using expression 4.2:

$$Re = \rho v_0 D \mu^{-1} \quad (4.2)$$

where ρ is the gas density and v_0 is the initial average gas velocity, considered here as equal to the gas flow velocity; D is the nozzle diameter, and μ represents the gas viscosity.

For circular cross sections it is found that for $Re \lesssim 2.1 \times 10^3$ the flow is laminar-, while values between $\approx 2.1 \times 10^3 \lesssim Re \lesssim 4 \times 10^3$ cause transitional flow, and numbers of $\gtrsim 4 \times 10^3$ reflect turbulent flow behaviour [114]. Johnson *et al.* [115], visualising shielding gas flow using particle image velocimetry (PIV), attempted to factor the influence of nozzle design while applying commercial MIG/MAG shielding gas nozzles. To a rough approach it is suggested that the nozzle design used in this study can be approximated similar to the geometry described in [115]. Table 8 depicts the essential data used for calculating the Reynolds Number.

Parameter	Unit	Value
Density ρ	kgm ⁻³	1.781
Initial velocity v_0	ms ⁻¹	1.7/0.95/0.42
Nozzle diameter D	m	0.015/0.020
Gas viscosity μ	kgm ⁻¹ s ⁻¹	0.000021

Table 8: Essential argon and shielding gas nozzle data at ambient temperature.

Table 9 shows that the Reynolds numbers calculated correlate well with the conditions found in the gas flow visualisation experiments previously dealt with.

Nozzle diameter	Flow velocity	Re
[m]	[ms ⁻¹]	[—]
0.005	15.3	6.488
0.005	0.42	— ⁽¹⁾
0.010	3.8	3.223
0.010	0.42	— ⁽²⁾
0.015	1.7	2.163
0.015	0.42	0.534
0.020	0.95	1.611
0.020	0.42	0.712
0.030	0.42	1.069

Table 9: Reynolds number (Re) calculated for shielding gas nozzle diameters and gas flow conditions used; including $\varnothing$ 30 mm reference nozzle type. Note ⁽¹⁾ after experimental failure using constant volumetric flow rate not further considered and ⁽²⁾ reference shielding gas nozzle diameter as used in previous experiments.

4.11 Discussion

Gas flow impact on arc stability and anode oxidation

Although Table 9 shows laminar gas flow conditions actually to occur with the 15 mm and 20 mm shielding gas nozzle diameter, severe anode surface oxidation was found with these nozzle types, qualitatively increasing with the 15 mm diameter nozzle. According to Munson *et al.* [114] it appears difficult to assess clear values to distinguish fluid flow conditions. One of the reasons for the surface oxidation phenomena observed may be due to the fact that the aforementioned calculations are based on ambient temperature conditions and that the high temperature arc atmosphere may cause considerable deviations from these conditions. This is more thoroughly being dealt with in Appendix B. Turbulent shielding gas motion may thus realistically occur while applying the smaller diameter nozzles even though laminar flow should theoretically prevail. Comparing these findings with the results derived in the previous investigation experiments, which measured the radial current density distribution, a significant increase of noise could be found in the measured calorimetric power density. The mechanisms, causing the in-

stabilities may directly relate to the methodology deployed; i.e. split anode calorimetry, as long as not being applied under inert shielding gas conditions protecting the whole anode area, which is considered as a fundamental outcome of this study. It is also supposed that the previous suggestions, as particularly dealt with in the section 3 of this thesis, are reasonably proved insofar as that the partially dramatic noise found was initiated by oxidation processes through arc plasma interaction with the copper anode surface. This prevented stable process conditions, required, however, to apply the numerical approach used.

From the results obtained it is further supposed that the nozzle design or diameter respectively, considerably impacts the measurement output. Slowly traversing low current arcs across the water cooled anodes leads to continuous interaction with the surrounding atmosphere, amplified by small nozzle diameter and split plane interface, which resulted in different temperature conditions for the two anodes. Hence, consequently deviations from arc symmetry are caused through the measurement sequence, finally with drastic impact on the calculated results. As anode oxidation is regularly not reported as a technical issue to overcome in industrial TIG welding applications, even with 15 mm or 20 mm nozzle diameters, the aforementioned study leads to an essential question. Is split anode calorimetry, as experimentally set up in this study, capable of revealing data, reflecting TIG welding arcs as industrially; i.e. practically applied? It is a question apparently remaining unsolved in this investigation; however, using the shielding chamber was helpful in that it could be shown that – under the given specific experimental boundary conditions – the radial energy density of low current TIG arcs is not necessarily Gaussian distributed and there are similar results between an arc that is well shielded locally and one that uses a chamber for protection. Also, arc stability improved while increasing the weld current along with changing the radial energy density profile, to become more Gaussian distributed. This might imply that the studies conducted by other researchers [56,58] that have applied high currents under inert ambient atmospheres have shown normal distributed energy density profiles both with respect to current and heat flux.

Heat input density distribution

The 15 mm diameter nozzle application showed for 50 A current and 3 mm ETWD a heat input density distribution that severely collapsed around the anode split plane with constant flow velocity, see Figure 4.6 (a), which was, however, not the case with adjusting a constant gas flow rate. Density maxima were found shifted away from the arc axis and more pronounced compared with the reference nozzle results. Hence, for the lower ETWD and 50 A, a constant gas flow rate or increased flow velocity, leads to greater arc stability and density maxima away from the arc axis. The behaviour stated by Tsai [57] and Tanaka and Ushio [45], who suggested that the arc length has essential effects on anode heat distribution, was confirmed in this study by doubling the electrode to work distance, which, however, was also connected to arc instabilities as already reported in the previous chapters of this thesis. Hence, increasing the ETWD with the lowest applied current (50 A) causes irregular output in the heat input calculations and the smaller nozzle diameter could not beneficially improve this behaviour.

The wider heat density distribution radius found versus the current density is in agreement with the results derived by Schoeck [52], who, although applying different cathode diameters, described, that the “measurable heat transfer” is “only partly determined by the current”. Also Tanaka *et al.* [6] reported that increased arc lengths reduce the heat input density by widening its distribution radius. Comparable to the current density as found in this study, doubling the electrical current is leading to a more pronounced power increase towards the arc centre, as well as to less radial noise in the measurement, shown in Figure 4.7 (a). This confirms the findings of Tanaka and Ushio [45], Tsai [57], Tsai and Eagar [64], and Wu and Gao [116] who were reporting that “arc length” is primarily controlling the heat input to the anode; however the electron flux is in charge of controlling its magnitude.

Applying the 20 mm diameter nozzle led to heat input behaviour in the opposite direction versus the 15 mm diameter nozzle. In using the latter a density peak was derived by increasing the flow velocity, see Figure 4.6 (a), however, for the 20 mm nozzle, Figure 4.8 (a), a density maximum was found by increasing the flow rate. Since pronounced density maxima were not observed in deploying the 30 mm diameter reference nozzle type it consequently is suggested that the shielding gas nozzle diameter affects the radial heat input distribution, especially with regards to density maxima shifted

away from the arc centre. It remains difficult to explain why both nozzle types are showing converse behaviour with respect to flow rate and flow velocity. In addition to these results, it was most interesting that the shielding gas chamber trials conducted could lead to similar findings with respect to relatively clear off-axis density maxima, especially applying 3 mm ETWD, see Figure 4.10 (a). Finding these peaks displaced ≈ 1.5 mm from the arc axis and rather less pronounced with the 6 mm ETWD trial series leads to suggesting that low current arcs by applying the experimental conditions as in this study are not mandatory Gaussian energy density distributed. Significantly reduced surface oxidation was found in the shielding chamber tests along with a considerably improved arc stability while traversing the arc across the split interface. This suggests that surface oxidation, produced while the arc is interacting with the anode surface, is decisively affecting the measured data and in turn has essential impact on the calculated radial energy density distribution. Comparing the present findings with the previous results on current density the heat input calculations are found more susceptible to noise which appears coherent, given the increased number of variables required to compute the calorimetric power input to the anodes, as well as the anode material properties which finally remain unpredictable in response through the test sequence.

The wider distribution of arc power applying 6.0 mm ETWD also observed in the shielding chamber tests confirms the findings by Tanaka *et al.* [6] and Schoeck [52] as already discussed previously. In general Gaussian-like heat input density distribution was not produced along the investigations with 50 A arcs; neither by deploying a reference shielding gas nozzle, the smaller diameter nozzles, nor the shielding chamber. In conjunction to this non-Gaussian distribution, it is noteworthy that Heberlein and Pfender [90], who used spectroscopy to determine “the radial distributions of electron temperature and electron density” of a 100 A non-consumable tungsten cathode arc, also found radial density maxima in between 1 and 2 mm from the arc axis. Besides this Heberlein and Pfender also report of facing experimental issues similar to our study; i.e. although “always an off-axis peak” was observed; its “radial location”, as well as “the width of the distribution” was found varying “from run to run”. Even though the primary reason for this shifted peak remained unsolved in [90] it may be correlated with the radial temperature distribution in the arc plasma when considering further the work by Vilarinho and Scotti [117]. These researchers have investigated TIG arcs

of different currents and numerically simulated a temperature profile which shows ≈ 9000 K at approximately 2 mm away from the arc centre while applying a 40 A current arc. This temperature again can be correlated to the first ionisation stage [118] of argon, which in turn may lead to an increased electron-, i.e. current density, hence, representing an off-axis peak as also measured in our experiments. Poloskov *et al.* [119] who numerically modelled the structure of 100 A TIG arcs, also found radial current and heat flow density off-axis peaks. While stating the current- and heat input density rather Gaussian distributed as long as numerically treating the arc as merely sustained upon a flat workpiece, the authors calculate explicit off-axis maxima at ≈ 1.5 mm and 2 mm radially displaced from the arc centre in case of assuming a “crater” in the weld pool. The distinction from normal distribution was further attributed to the “point of the smallest distance from the tip of the electrode to the surface of the welded component”. It remains questionable whether this condition might be correlated to a split interface separating two anodes, thus explaining the off-axis peak found in this study.

4.12 Summary

From the experiments conducted and the theoretical considerations dealt with in this study, the following conclusions can be drawn:

- i. Given the experimental conditions applied, the shielding gas nozzle diameter does appear to have an influence on the anodic calorimetric power input.
- ii. The instabilities found are caused by oxidation processes occurring through the arc application sequence and are more pronounced with the calorimetric power than with the current density distribution calculated in previous studies.
- iii. Smaller shielding gas nozzles increase oxidation assumed due to poorer shielding and may cause turbulent shielding gas flow around the arc plasma.
- iv. Protecting the whole anode area by using a shielding gas chamber led to highly stable TIG arc condition even with 50 A current and surface oxidation considerably less than with the smaller nozzle diameters.

- v. The off-axis peak remains with the results from the shielding chamber, which leads to suggest that lower current TIG arcs have non Gaussian-like energy density distributions and show off-axis maxima possibly adding or inducing arc-anode instabilities in practical welding applications; e.g. in welding light gauge butt joint configurations.

5 Discussion

5.1 Comparative considerations

Although impressive research effort both theoretical and experimental was devoted through the past decades to achieve an improved understanding, even nowadays welding arcs can be estimated as not fully comprehended. Quite basically this is also the reason that ‘welding’ in general is still considered as a ‘special process’ in the manufacturing field conversely for example to mechanical joining methods, like screwing or riveting. The welding arc is assessed not to be a trivial physical subject easily to grasp in the entirety of its variations. Especially this is the case for consumable electrode arcs as applied e.g. in MIG/MAG welding, because of heavily being affected by not only weld pool interaction, but also by metal vapours produced by the weld pool and the wire electrode vaporised to a degree which requires particular attention in the theoretical considerations [55, 120]. However, also non-consumable electrode welding arcs, as deployed in TIG welding, partly show unforeseeable phenomena, capable of adversely affecting the weld result. Gaining insight into the mechanisms causing these effects by applying scientific; i.e. quantitative means, may yield an expanded amount of knowledge to make the welding arc, or its results achieved from the interaction with the material to join, calculable and more predictable in an engineering sense. This may justify also the work still expended around the world along with the vast amount of running investigations in welding research and, shall also serve as the driving force behind this study.

Fundamental work was conducted by early researchers on especially higher current TIG arcs [39, 56–58, 60, 64, 86] with highly valuable output to the user in an industrial environment; however, especially the research conducted in investigating lower current TIG arcs is rather limited and, hence, questions

remain, why some unexpected and hardly understandable phenomena occur in welding. It is the arc plasma that produces and transfers the energy required to melt and subsequently join the parent material chosen, thus it is of essential interest to understand how the energy is distributed in the arc.

This led to different suitable and applicable approaches, as dealt with in the literature review section of this thesis 1. All these methods could be found to have their specific advantages but also limitations with respect to accuracy and reasonability of the results achieved. Among the methods used to analyse the energy density of particularly TIG arcs, spectroscopy certainly can be agreed to play a special, since important role. However, to achieve valuable results from applying spectroscopic methods it requires, amongst a partly extensive experimental setup, a considerable amount of expertise correctly to choose and finally interpret those spectral lines estimated meaningfully to reveal the information buried in the different regions of the arc plasma. Consequently the spectroscopy mathematical approach, regularly based on a partly extensive amount of simplifications in the models and connected to derive quantitative output, is not trivial, which can add error to the results. This is suggested also the reason for significant deviations to some extent found among the different studies in the literature.

Applying instead for example more robust but, compared to spectroscopy, which is a non-invasive approach, methods directly affecting the arc, like the electrostatic probe technique, also may be joined by particular drawbacks [25, 70]. The “uncertainties” found in deploying these means can be “often substantially higher with respect to the relatively well established optical methods” [25]. Mainly due to the effects caused by the measurement itself on the subject investigated, the mathematical approach expended must hence take into account or compensate for the experimental error by the hardware setup applied. Given that additionally to this electrostatic probe measurements require special equipment to conduct the experiment makes this method special in characterisation and output.

It is accepted among the experts, that what can be measured as the ‘weld result’ is always is a complex response of the base material and its temperature dependent properties to the application of the arc plasma. However, and as already mentioned, incorporating the molten weld pool in the theoretical considerations leads metal vapour to play a major part, which extends the degree of intricacy while conducting both experiment and analysis to a significant extent. Hence, decoupling arc and weld pool by using appropriate

means, e.g. water cooled copper anodes, as in split anode calorimetry, allows to study the arc plasma undisturbed by the influences caused by the weld pool. This again may lead to stable and appropriate measurement output of adequate accuracy, at least as far as within the range of specific boundary conditions chosen for the experiment. In turn, split anode calorimetry, not only by explicitly following the literature but also from the mere logical train of thoughts, is suggested justifiable as the appropriate method to achieve the target and collect meaningful information on the radial energy density distribution of low current TIG arcs in this study.

As found over the course of the investigation phenomena can occur which were not reported by any means in the literature. These phenomena, considered to in part drastically affect the value of the results revealed in the study, are thus estimated to be a significant outcome of this investigation because of future allowing other researchers to take them into appropriate account while using split anode calorimetry e.g. to study low current tungsten inert gas shielded arcs. With that in mind, the discussion hereafter intends to appropriately assess those sources of error that may influence the significance of the concluding results.

5.2 Discussion of sources of error

5.2.1 Consideration of anode thickness and anode material

The initial stage of this study dealt with testing different anode thicknesses and could prove that, within the electrical current range chosen, only an anode thickness of 3 mm was found adversely affected especially in applying the maximum current of 100 A. Further investigation could reveal that no considerable differences in the measurement behaviour between 7 mm and 12 mm thick anodes exist which has led to the decision of using 12 mm thickness, to - though unexpected - safely exclude any negative effects by anode surface melting as found in part with the 3 mm thick anodes. This should allow for stable process and measurement conditions in any phase of traversing the arc across the anodes. Also, to the greatest extent, no detailed information on suitable anode thicknesses was derivable from literature. It unfortunately remained unclear for example, how the split anode calorimeter depicted in Figure 1.22, was configured with respect to anode thickness and parent material applied.

However, as proved through the final stage of this study; although theoretically no significant distinction in the material behaviour should be expected, it nonetheless is important to notice the partially dramatic noise in the calculated output while considering the calorimetric power or heat flux to the material. This is suggested due to the additional parameters to take into account when measuring and calculating the calorimetric power; i.e. conversely to the current density, supposed mainly to be affected by the electrical properties, mainly electrical conductivity, of the anode material - and where thicker anodes may prove rather even beneficial because of not showing temperature dependent electrical resistance variation. Measuring the calorimetric power input as such is also different to the output of the electrical power calculated by multiplying the instantaneous current measured, by the corresponding voltage value, as depicted in Figure 2.10, showing different radial distributions for electrical and calorimetric power density. This confirms the finding by other researchers who have assessed the heat flux density different to the current density.

Considering that transport of heat over the cross section of the anode is a function of the spatial dimensions, anode thickness is suggested to play an important part and thicker anodes may increase noise in the measurements or calculations, respectively. Hence, lower anode thicknesses may prove beneficial when calorimetric power is concerned, especially when using low arc currents like in this study. Finally, as also surface oxidation processes were observed through the experiments, the chemical composition of the anode material was not explicitly taken into account for being assessed as an additional factor adding to this surface oxidation which in turn might have significantly influenced arc stability or arc symmetry, especially while the arc was approaching and crossing the anode interface. ‘Oxygen-free’ (Cu-OF) copper was applied as the anode material in this study; however, although this material grade is referred to as “free” of oxygen it just achieves a relative purity of 99.95% and thus contains a certain amount of impurities [121], suggested to affect surface reactions while applying the arc. Regardless of the fact that the aforementioned are presumed generally to play an important part when applying split anode calorimetry for TIG arc investigations, to the best knowledge of the author, they have not been taken into appropriate account in the scientific literature.

Finally, the steep and, since randomly caused and affected, incalculable temperature gradient around the split interface between ‘anode 1’ and ‘anode 2’ is estimated as a residual fundamental uncertainty throughout the whole experimental series conducted. Although the arc is approaching the split plane incrementally with low travel speed and with pause times considered sufficient to symmetrically stabilise upon both anodes, there is nonetheless a spatial separation between both anode halves consistently secured by electrically insulating polyimide tape remaining. Hence, adverse combinatory effects of ETWD, electrical current height and surface conditions may lead to the results as found through the experiment carried out in this study. Anode thickness may play an additional part in these phenomena since the dimensional configuration, as aforementioned, is suggested to play a major part, especially while measuring the heat flux captured by the calorimeter.

5.2.2 Considerations on anode surface effects

In close conjunction to the aforementioned subsection, varying anode surface conditions may prove as a further source of error detrimentally affecting measurement and calculation results. Welding arcs, especially, however, non-consumable electrode arcs like applied in TIG welding, are regularly considered axisymmetrically shaped, with clear minima and maxima assignable. However, this - theoretical - condition is considered only achievable by providing an environment that allows the arc to fully stabilise after ignition and subsequently to be sustained unaffected by exterior or interior factors. Providing that water cooled copper base plates permit to achieve these stable or stationary conditions, would positively emphasise split anode calorimetry to be an appropriate choice for analysing low current TIG arcs. However, as was found to a high degree through the experimental series conducted, instabilities occurred while the arc was traversed across the anodes. These instabilities were remaining difficult to explain with respect to their root causes, until the shielding chamber tests were finally carried out as dealt with in section 4 of this thesis.

Surface oxidation processes took place while arc interaction with the anode surface, even with a shielding gas nozzle type supposed large enough in diameter to presume safe laminar gas flow conditions, hence, to allow for axisymmetrical arc behaviour or shape. It also needs particular attention that involving a high resolution CMOS camera, applied in this investigation along

with the beginning of using the reference nozzle type, may indeed provide beneficial visual information as could be proved in section 3 of this thesis; however, this technical means is considered as not fully capable of sufficiently resolving and capturing the *instantaneous* phases of the arc - anode interaction. Hence, although it could be shown that asymmetry occurred in the arc, especially while approaching the split anode interface, hereby providing a reasonable explanation for the asymmetry between ‘*anode 1*’ and ‘*anode 2*’ of the calorimeter, the primary reasons for the oxidation occurring through the arc application sequence remained unrevealed in this study. It is also assumed complicating in this conjunction, that the anode surface temperature through arc application was not really quantifiable and the literature on the generation of cuprous oxides varies from $\gtrsim 125\text{ }^{\circ}\text{C}$ to $\gtrsim 1025\text{ }^{\circ}\text{C}$ [101]. Assuming further that anode surface temperatures are achieved as high as $\approx 2073\text{ K}$ [122] it appears considerable to a high degree that, in direct connection to the instantaneous shielding gas flow conditions around the arc, intensive chemical reactions between the copper anode and ambient atmospheric gases, e.g. oxygen, detrimentally affect arc symmetry, required however to properly apply the mathematical approach to gain information on the arc’s radial energy density distribution. Apart from this, the degree of surface roughness as applied in the experiments can be estimated as another factor difficult to define. Although it was attempted that consistently high anode surface qualities with qualitatively lowest roughness degrees were produced by the procedure e.g. explained in section 3, it obviously cannot be obviated that small differences, e.g. caused by micro voids, low depth scratch marks, or indentations, might have caused arc asymmetry behaviour, especially with conducting the lower current (50 A) experimental series. With that in mind, i.e. the sensitivity of arc versus anode interaction to achieve axial symmetry - and also considering the low travel speeds applied in this study in order to achieve and maintain highest arc stability - it remains difficult to comprehend e.g. methods that were deploying arc travel speeds of 60 mm s^{-1} to accurately derive information on the arc’s radial energy distribution [66].

5.2.3 Considerations on shielding gas nozzle design

Investigating the influence of the shielding gas nozzle design in TIG welding was determined as an essential part of this whole study, which led to the production of differently sized prototype nozzles. As such, after appropriate

literature research, but also from own experience, the part designs or diameters tested, were not anticipated to yield detrimental results as partially revealed from the investigation. However, it nonetheless needs appropriate consideration that, to achieve a high degree of comparability, shielding gas flow rates were to be defined to maintain constant throughout the experimental series. These constants were basically derived from the reference nozzle prototype test series and might to a certain degree remain questionable in direct conjunction to especially deploying the smaller nozzle diameters and maybe a further practical (industrial) application of these nozzle designs.

The prototype reference nozzle showed a diameter of 30 mm, as aforementioned, intended to achieve stable laminar gas flow devoid of any external influences detrimentally affecting or interacting with the arc plasma. The initial experiments in this study were conducted applying a commercially available (internal diameter 19 mm) nozzle type and 7 Lmin^{-1} ($\approx 0.12 \text{ Ls}^{-1}$) volumetric gas flow rate (adjusted as based on personal experience). This flow rate again led to an average gas flow velocity of $\approx 0.42 \text{ ms}^{-1}$. Consequently the reference nozzle (internal diameter 30 mm) was used along with setting the volumetric flow rate to 18 Lmin^{-1} (0.3 Ls^{-1}) thereby also achieving an average gas flow velocity of $\approx 0.42 \text{ ms}^{-1}$. Hence, these data then finally served as the reference values maintained constant over all experimental series conducted.

It is understandable that, keeping the previously stated input values constant, may lead to either extraordinary low (flow rate) or high (flow velocity) output values while applying particularly the 5 mm and 10 mm diameter nozzles and that this does not really prove an application oriented approach; however, fundamentally this study was accomplished from a mere scientific perspective and just in secondary instance for deriving ‘industrially’ applicable welding parameters. Hence, the aforementioned approach is suggested justifiable with respect to the research character of this investigation. Thus it is also supposed that the results from the shielding gas nozzle experiments prove beneficial and contribute knowledge because of revealing phenomena which have not been reported in the literature dealing with split anode calorimetry used for TIG arc analyses. That is. Although surface oxidation is not reported from the industrial TIG welding application ground, even by applying shielding gas nozzles of smaller diameter, it was found consistently to occur through this investigation.

It is obvious that these oxidation processes can significantly affect the arc

application stability and hence the experimental outcome as a whole, probably even negatively affecting the real conditions in the arc plasma. However, it is nonetheless considered valuable that these hardly comprehensible phenomena have further led to a closer investigation of the shielding gas nozzle flow behaviour by involving theoretical (Reynolds Number) and experimental considerations (gas flow visualisation tests). As dealt with in the final stage of this study 4, the experimental results achieved fairly correlated with the calculated Reynolds Number values; however, as both of which is actually connected to “cold flow conditions” [115], significant differences could be revealed to the real arc application (‘hot’) conditions.

From this some interesting questions can be raised; e.g.:

- Is split anode calorimetry, as a scientific investigation approach, in general even safely applicable when pairing regular shielding gas nozzles and low current TIG arcs?
- Do the raw data, experimentally derived, factually represent the internal arc structure or grant access to the information targeted at? More clearly; is a low current TIG arc, as produced in the experiments and slowly traversed across the anodes, identical in its radial energy distribution, to a low current TIG arc as industrially applied?

Both questions in the context of this thesis appear to remain unanswerable; however, as was shown in the random sample experiments, if TIG welding gas nozzles would be available producing a shielding quality as comparable to a shielding chamber and providing conditions that allow the elimination of surface phenomena detrimentally affecting the result, both queries could be answered positively. Hence, split anode calorimetry would prove a reliable method to investigate non-consumable electrode arcs, and the calculations would lead to results representing the arc structure as a transient function depending only on the actual position of the arc relative to the split plane.

Understandably this does not necessarily imply further prediction on how low current TIG arcs, as applied in this study, may finally practically interact with a specific parent material to join, e.g. while increasing the arc travel speed versus the conditions applied in the split anode experiment. However, it is nonetheless suggested as a valuable outcome of this study that all these intricate questions have found their way to appropriate consideration, which,

as shown in the literature, was apparently not the case so far. It is also supposed to be a fundamental finding of this investigation that - by applying a shielding chamber - radial energy distributions were revealed distinct from the Gaussian-like appearance as regularly published in the literature; at least as far as applying low electrode tip to workpiece distance and conditions as deployed in the experiments. Hence, this should help future research to appropriately take these results into account when split anode calorimetry is intended to study low current inert gas shielded tungsten arcs.

5.2.4 Considerations on the numerical approach

As comparable to e.g. spectroscopy and electrostatic probe technique a considerable limitation of the split anode calorimetry method certainly lies in the fact that the energy density distribution of an arc can only indirectly be derived; i.e. applying appropriate mathematical means becomes mandatory. In case of split anode calorimetry it is the Inverse Abel Transformation 2.6 which allows to calculate the radial distribution of energy in the arc plasma from the total current values $F(x)$ measured as raw data from the experiment. However, as described by Tsai [57], Eberhart and Seban [62], Thornton [39], and Vilarinho and Fanara [66], calculating the second derivative of $F(x)$ makes this approach highly sensitive in the achievable results and, hence, may lead to considerable noise in the calculation output; as also found in part in the results dealt with in the previous sections of this thesis. It is thus surprising that Gaussian distribution of TIG arcs is regularly reported in the literature while applying split anode calorimetry; for both current- and heat flux density. In the conjunction to the aforementioned subsection, achieving Gaussian-like distributions requires the arc plasma to be treated axial symmetrically shaped [90]. A distinction becomes thus obvious between the results as regularly shown in the literature and the findings obtained in this investigation. As a matter of fact, as shown through the different investigation stages, to a significant extent, the arc was found deviated or unstable especially around the split plane interface, hence often detrimentally affected through the experimental series. Besides the essential question whether the Inverse Abel Transformation can generally be applied, as done however along this investigation, it unfortunately remains unclear how smooth Gaussian-like distributions could be achieved in other studies. Among the different options one may assume, applying appropriate smooth-

ing measures to eliminate the noise in the calculations, as reported e.g. by Tsai [57] and Tsai and Eagar [64] appears as the logical consequence to derive Gaussian-like distribution profiles. In the same context one may in general raise the question why Gaussian-like distribution profiles are regularly discussed when radial current- or heat flux distribution of TIG welding arcs are concerned. This question unfortunately remains unsolved in this study but it is assumably that normal distributed density allow further numerical modelling to be applied.

To compute the first and second derivative of $F(x)$, this study made use of ‘Langrange’s five-point formula’ 2.7 and it hence may require special attention whether this particular approach, as was decided to apply at the very initial stage of this investigation, may also add to the numerical oscillations partly visible in the calculation output. This becomes even more discussable when a rise in the degree of complexity is concerned, as in calculating the heat flux to the anodes, and, as previously assumed 5.2.1, thermodynamic properties of the anode material appear to achieve greater impact on the accuracy of the results. As this subject was considered highly important for the scientific significance of this study it was thoroughly discussed [123] in order to find out whether the mathematical approach of using ‘Langrange’s five-point formula’ appropriately serves or maybe would require reasonable replacement.

The mathematical variation discussed with, and proposed by Choquet [123], was suggesting to use in lieu of the ‘Five-point formula’, a Taylor expansion as regularly applied in Computational Fluid Dynamics (CFD) to describe diffusion problems where uniform discretisation or “isotropic transport of the information” is concerned. This discussed alternative option, obviously doubtless meaningful in a mere mathematical sense, finally leads to the point that allows to eliminate the calculation of the first derivative of $F(x)$, to just being left straightforward to compute the second derivative of the total current captured from the measurement. This was proposed by Choquet in order to also broadly eliminate the partly massive fluctuations as shown in the plotted results.

Although not further dealt with in this thesis it nonetheless needs appropriate mention in the context of this final discussion, that the ‘Taylor expansion’ approach for this expression ??, as suggested by [123], was tested through calculations and indeed led to smooth Gaussian-like distributed profiles, even with results, that were previously showing some considerable amount of nu-

merical oscillation. However, it was unfortunately found that the calculations carried out this way show similar distribution with energy densities appearing too high, see Figure in Appendix of this thesis. Hence, it was considered as smoothing out merely too much of the information actually buried in the arc or raw data. Further given the fact that, depending on the quality degree of arc stability, especially close to the split interface between the anode halves, the calculation output could also achieve reasonable results while applying ‘Lagrange’s five-point formula’; the alternative ‘Taylor polynom’ approach was finally disregarded by sticking to the ‘Lagrange’ approach. Consequently, to achieve the most appropriate information on radial energy distribution in low current inert gas shielded tungsten arcs.

It needs particular emphasis in conjunction to the latter, that the final random test, applying a shielding chamber which was proved capable of leading to highly stable arc behaviour by beneficially counteracting undesired anode surface phenomena like oxidation, could enable a much smoother calculation output versus the shielding gas nozzles tested. This is also estimated to be a fundamental finding of this investigation and suggests that the ‘Lagrange five-point formula’, as applied in this study, properly allows to achieve meaningful results as long as the experimental environment is reliably set up to yield stable and uniform boundary conditions.

6 Conclusions

The theoretical and experimental considerations undertaken in this study allow to draw the following conclusions:

- i. Split anode calorimetry is considered as a suitable scientific method to analyse low current TIG arc plasmas, despite irregularities experimentally occurring through this investigation. However, a lower current threshold value may exist to consequently achieve reasonable and meaningful results from applying this method.
- ii. Special attention must be paid to accurate preparation of the whole methodological setup which includes the calorimeter anodes but also all peripheral parts applied like the tungsten cathode or the hardware and consumables used.

- iii. The shielding gas nozzle affects the radial current- and heat flux distribution in low current TIG arcs as produced by applying the conditions chosen in this study.
- iv. Shielding gas nozzle diameters, actually proven applicable in industrial welding, behave different with respect to their shielding- or anode protection properties in split anode calorimetry.
- v. Surface oxidation processes observed along with the experimental stages of this study are considered detrimentally influencing the whole measurement.
- vi. The predicted flow behaviour determined by the Reynolds Number calculated for the nozzle diameters finally applied in the experiments agrees well with the practical flow conditions as was shown in gas visualisation tests as long “cold” conditions were concerned.
- vii. Differences in the shielding behaviour when applying the arc are attributed to unpredictable and difficult to determine arc behaviour along with slowly traversing the arc across the anodes. Hence, turbulent flow appears to arise through the split anode experiment.
- viii. Current density measurements could show a more uniform distribution versus calorimetric power or heat flux which is assumed to be caused by the thermodynamical properties of the anode material applied as well as by the spatial dimensions of the anodes used.
- ix. The arc deviation visually captured by involving CMOS photography appears being caused by an intricate combination of anode surface effects and different surface temperature gradients between ‘anode 1’ and ‘anode 2’, separated by the split interface.
- x. The numerical approach of applying the Inverse Abel Transformation was found leading to significantly stabilised output, as soon as both shielding and thus stability conditions could be improved; this was proved by the application of a shielding chamber avoiding detrimental surface effects through the experiments.

- xi. Non-Gaussian distribution profiles with density maxima away from the arc axis were found especially with the 50 A arc application in case of 3 mm electrode tip to workpiece distance. While the latter suggests an arc stabilisation while crossing the split plane interface; the former, as particularly proved by applying the shielding chamber, showed that low current TIG arcs are not necessarily normal distributed in their radial energy density.
- xii. The energy density maxima found shifted approximately 2 mm away from the arc axis might be attributed to the first ionisation stage of argon which is described to initiate at $\approx 9,000$ K. Hence, a greater amount of electrons is supposed at this radial distance from the arc centre leading to the higher current density observed in the experiments.
- xiii. 100 A arcs particularly showed generally a more Gaussian-like distribution profile especially with 3 mm electrode tip to workpiece distance; i.e the current and power density was rising towards the arc centre.

7 Future work

As shown in the final discussion chapter of this thesis a row of questions is remaining that may affect the significance of this study. Hence, to lead these questions to appropriate answers future work may involve as follows:

1. Advanced 3-dimensional arc shape investigation, as e.g. proposed and applied by Nomura and co-workers [124–126], in combination with split anode calorimetry, may beneficially derive detailed information of the real structure of the arc while approaching and crossing especially the split plane interface. This might also lead to a clarification on the reasonability of applying the Inverse Abel Transformation approach to reveal the radial current or power density distribution.
2. In-depth investigation of the anode surface oxidation phenomena including determination of the actual anode surface temperature while arc-anode interaction, may lead to prove whether or not the hypothesis

as postulated in this study; i.e. that anode oxidation occurs *through* the arc interaction phase but not *after* the arc has passed a particular position along its path across the anodes, is correct.

3. An investigation involving a pre-heating of ‘anode 2’ may lead to a clearer understanding whether the hypothesis postulated in this study; i.e. that the different surface temperatures especially close to the split interface between both anodes lead to severe arc deviation and hence perturbation of the arc axial symmetry, proves correct.
4. A thorough assessment of the question whether the numerical methods, based on the assumption of axial symmetry of the arc and generally applied in split anode calorimetry, may require an appropriate revision or adjustment to compensate for the arc symmetry perturbations observed to a broad extent in the investigation, can beneficially add value to generally judge the significance of split anode calorimetry in welding arc investigations as a whole.

A Calorimeter Block Drawing

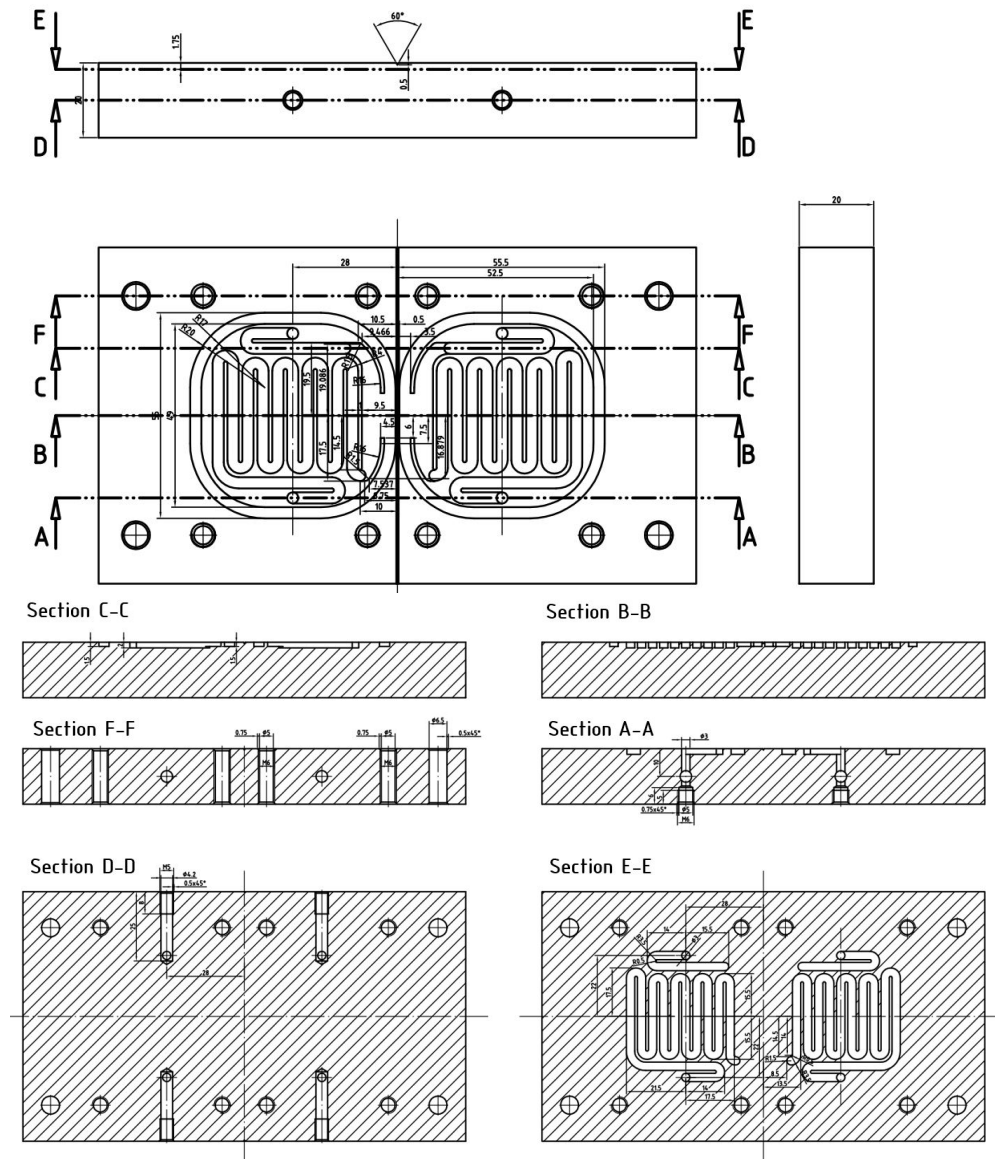


Figure A.1: Schematic calorimeter drawings

B Influence of shielding gas flow conditions

The available literature on Reynolds Number (Re) calculation in welding rarely involves temperature effects on the real values partially listed in 8 and thus the variation of density and volume as a function of gas temperature is rarely appreciated. Ramsey *et al.* [106], despite studying Metal Active Gas (MAG) Welding, have assessed the latter to be an essential factor for properly correlating numerical simulation with experimentally measured heat flow. Referring to an earlier study from Jönsson *et al.* [115], they have postulated the arc temperature for a 200 A argon shielded MIG arc to be $\approx 24,000$ K and have transferred this value to their numerical simulation software. The arc temperatures consequently computed from that ($\approx 32,000$ K) were described as “higher than that determined by Jönsson *et al.*” but were finally explained due to the shielding gas blend used, containing 80% Ar + 20% CO₂. Neither the arc temperatures as stated by [106] nor those found by [115] are confirmed by Murphy [127] who identified electron temperatures of $\approx 13,000$ K for a MAG arc of 326 A welding current, and $\approx 17,000$ K for an argon shielded TIG arc of 150 A current and 5 mm electrode tip to work distance, sustaining the latter, however, upon a stainless steel anode. Glickstein [128] stated cathode boundary layer temperatures of $\approx 11,000$ K for an argon shielded TIG arc of 100 A current which is well correlating with values derived by Gick *et al.* [69].

Regardless of the partial differences in magnitude, these values emphasise that the shielding gas is subjected to a considerable temperature rise while being consumed in the arc and, along with this temperature increase, strong deviations may be suggested from ambient temperature argon properties. This, in turn, is correspondingly supposed to also affect the Reynolds Number. Metcalfe *et al.* [129] listed values from 5,000 – 25,000 K while investigating the heat flow to the workpiece deploying Plasma Arc Welding. In a more recent work Murphy and Arundelli [130] have produced substantial work by calculating transport coefficients for argon in a range from ambient temperature through 25,000 K, well correlating with the results from [129]. Unfortunately the numerical values are not depicted in [130] but just graphically plotted, which consequently deteriorates an accurate Reynolds Number calculation. Vilarinho and Scotti [131], who numerically simulated lower current TIG arcs, could, however, make use of the numerical quantities from [130].

While considering the inner arc column as in partial local thermal equilibrium (PLTE) the approach used herein is to commence the Reynolds Number

calculation within the boundary region in closest vicinity to the arc centre. Figure B.1 schematically plots the radial temperature profile of a 40 A TIG arc using the values computed by [131] and shows the temperature significantly decreasing towards the shielding gas nozzle interior wall.

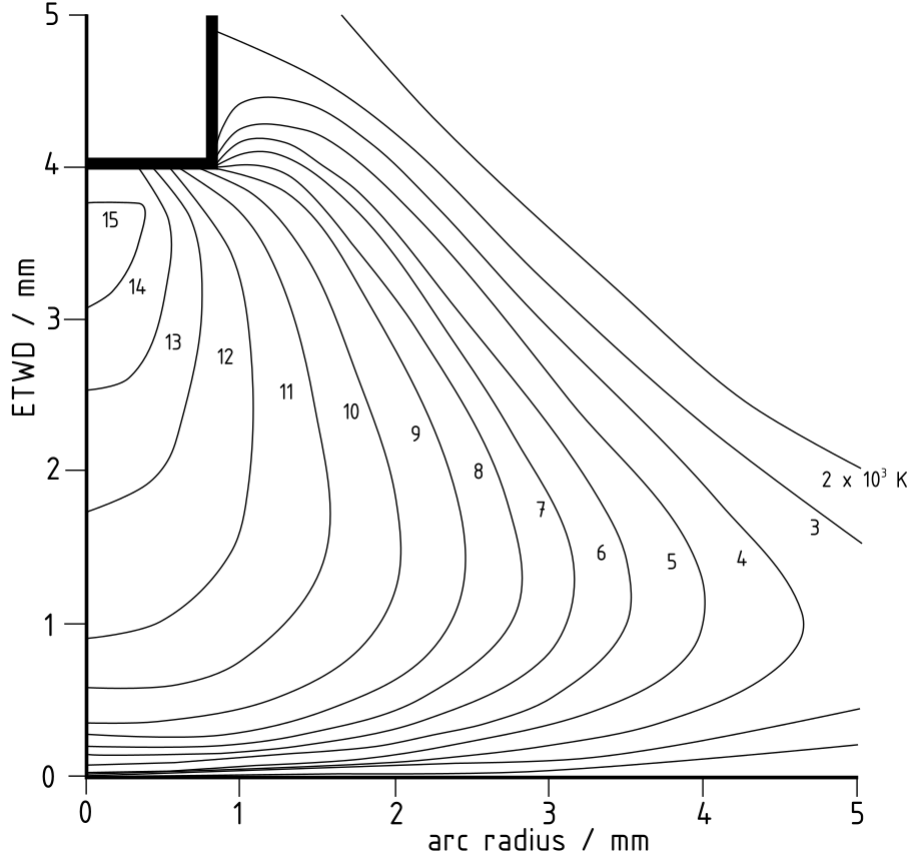


Figure B.1: Schematic temperature profile of a 40 A welding current TIG arc (After [131]).

Considering further that the shielding gas density varies from the arc centre towards the gas nozzle inner walls, the density is suggested to differ just negligibly from room temperature conditions when close to the nozzle wall. Vilarinho and Scotti have adopted four different welding currents. While their 40 A setup is close to our 50 A adjustment, the 100 A setup is equal to the experimental conditions applied in this study. The Reynolds Number,

amongst others, is a function of the fluid density, hence a Re gradient can be assumed, possibly leading to instabilities in the flow field.

The maximum flow velocities in [131] were respectively 98.7 ms^{-1} for 40 A and 172.75 ms^{-1} for 100 A arc current. It needs particular mention in this conjunction that these values were achieved considering the “arc length” (i.e. ETWD) representing ‘ D ’ in equation 4.2. This, however, would result in an actual value ‘ D ’ equal to zero, which again is considered unreasonable in a numerical sense. Though herein not discussing this further; by all means the approach applied in [131] is different to the assumption used in this study, applying the shielding gas nozzle diameter to represent ‘ D ’.

Chang *et al.* [132] applied numerical and analytical means to solve axisymmetric Maxwell-, and Navier-Stokes equations to calculate argon arc plasma velocity fields. The results were compared with ‘long’ arcs of ‘very high’ currents ($520 \text{ A} \leq I \leq 2160 \text{ A}$) and ‘shorter’ arcs of lower currents (200 A). Although leaving the shielding gas density differences at higher temperatures unconsidered and also 100 A was the lowest current value investigated by [132] they indicated significantly lower velocities compared to [131]. Argon arcs of 150 A current, studied by Tanaka and Lowke [15], showed maximum jet velocities of $\approx 200 \text{ ms}^{-1}$, noticeably decreasing in the radial direction, however.

Shielding gas density variation for higher temperatures was appropriately taken into account by Campbell *et al.* [133] to calculate arc pressure and plasma jet velocities. For an argon arc of 2 mm ETWD the authors computed a maximum velocity of $\approx 140 \text{ ms}^{-1}$. Though applying such a small arc gap, radial velocities are found for even 6 mm arc radius, revealing a drastic decrease of the cathode jet velocity and plotting a value of $\approx 20 \text{ ms}^{-1}$. Chang *et al.* [132] for the 100 A argon plasma showed jet velocities approaching to zero at a radial distance from the arc centre at $\approx 3.5 \text{ mm}$. According to [118] the ionisation of argon initiates at $\approx 9000 \text{ K}$; hence, providing the results of Vilarinho and Scotti, non-LTE conditions may be assumed beyond a radius of $\approx 2 \text{ mm}$ from the arc centre, see Figure B.1.

To obtain the Reynolds Number while assuming the temperature profile depicted in Figure B.1; the jet velocities for an arc radius of 2 mm ($T \approx 9000$ K), 3 mm ($T \approx 7000$ K), 5 mm ($T \approx 3000$ K) were calculated, as well as for the gas nozzle wall which is supposed to be at ambient temperature, by using expression (B.1) given in [133]:

$$\nu = \left(\frac{\mu_p I J}{2\pi \rho_g} \right)^{1/2} \quad (\text{B.1})$$

ν represents the plasma jet velocity, μ_p is the initial velocity of the gas. I and J respectively are the welding current and the current density (derived and calculated from the corresponding measurements) and ρ_g is the shielding gas density as a function of temperature. Table (10) shows the values computed for 50 A ($\dot{V} = \text{const.}$) and 3.0 mm ETWD and based on the foregoing assumptions.

Temperature	Gas density	Gas viscosity	Initial velocity	<i>Re</i>
[K x 10 ³]	[kgm ⁻³]	[Kgm ⁻¹ s ⁻¹]	[ms ⁻¹]	[-]
ø 20 mm nozzle				
9.273	0.054	269 x 10 ⁻⁴	86.37	346.76
7.000	0.070	229 x 10 ⁻⁴	29.55	180.66
3.000	0.162	113 x 10 ⁻⁴	1.36	38.99
0.293	1.781	210 x 10 ⁻⁴	0.42	712
ø 15 mm nozzle				
9.273	0.054	269 x 10 ⁻⁴	86.37	260.07
7.000	0.070	229 x 10 ⁻⁴	29.55	135.49
3.000	0.162	113 x 10 ⁻⁴	1.36	29.25
0.293	1.781	210 x 10 ⁻⁴	0.42	534.3

Table 10: Reynolds number (Re) calculated as a function of shielding gas temperature and density for applied shielding gas nozzle diameters of 20 mm and 15 mm.

It can be fairly seen that the Reynolds Numbers calculated are substantially below the values regularly indicating turbulent flow behaviour. However, in this conjunction one may generally question whether using the shielding

gas nozzle *diameter* appears reasonable; simply because of the relation of gas density and gas temperature. Hence, applying the shielding gas nozzle diameter as the variable ‘ D ’ in expression (4.2), may cause errors applying those values reflecting *real* gas temperature and density.

Taking this into account Re was calculated with reference to the arc diameter of interest; i.e. respectively $D = 4$ mm, 6 mm, and 10 mm for comparing the results with the output for the shielding gas nozzle diameter. Table (11) depicts the values in relation to the arc radii applied.

Temperature [K x 10 ³]	Gas density [kgm ⁻³]	Gas viscosity [Kgm ⁻¹ s ⁻¹]	Initial velocity [ms ⁻¹]	Re [-]
Arc diameter 4 mm				
9.273	0.054	269 x 10 ⁻⁴	86.37	69.35
7.000	0.070	229 x 10 ⁻⁴	29.55	36.13
3.000	0.162	113 x 10 ⁻⁴	1.36	7.80
Arc diameter 6 mm				
9.273	0.054	269 x 10 ⁻⁴	86.37	104.02
7.000	0.070	229 x 10 ⁻⁴	29.55	54.20
3.000	0.162	113 x 10 ⁻⁴	1.36	11.70
Arc diameter 10 mm				
9.273	0.054	269 x 10 ⁻⁴	86.37	173.38
7.000	0.070	229 x 10 ⁻⁴	29.55	90.33
3.000	0.162	113 x 10 ⁻⁴	1.36	19.50

Table 11: Reynolds number (Re) calculated for $D = 4$ mm, 6 mm and 10 mm as a function of shielding gas temperature and density.

The Re values in Table (11) prove to be even less, compared to the values calculated under presuming the regular shielding gas nozzle diameters, Table (10). It is also found that the jet velocities are in well agreement with the values stated by Metcalfe *et al.* [129], and particularly for those values above 3 mm arc radius and applying 50 A welding current, they approve the results of Chang *et al.* [132]. It is hence suggested that turbulent flow is not caused within the arc plasma while deploying the experimental conditions chosen in this investigation and thus that shielding gas flow turbulences while applying the arc is – at least theoretically – not the root cause for the anode surface oxidation found in our experiments.

Nonetheless, it was shown that using parameters as applied in ‘regular’ Reynolds Number calculations may lead to output discrepancies. Particular attention should be expended hence in precisely defining the numerical variables used in the Reynolds Number expression when applying this approach especially in arc welding. Considering these results it is hence suggested that the hypothesis raised in [99], supposing that mere ‘jet velocity’ variations cause oxidation while the arc interacts with the anode surface can be discarded. However, although the results depicted in 11 show Reynolds Numbers by far too low to indicate turbulent flow it nonetheless needs consideration that these values rather reflect stationary but not real and certainly more dynamic conditions. Hence, it still remains unknown whether the high temperature anode surface may cause jet perturbations and unpredictable motion of the shielding gas while interacting with the surface. The oxidation found can in turn be either supposed resulting *after* the arc was applied to the anodes, affected by temperature heights as postulated by Schoeck [52] or Antoniou [122], or *while* the arc is interacting with the surface. Consequently the arc instabilities observed around the split plane area especially with 50 A current, may also be caused by reaction processes connected to surface oxidation.

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