On the interaction between welding residual stresses: a numerical and experimental investigation

Gervasio Salerno^{1,2}, Chris Bennett¹, Wei Sun¹, Adib Becker¹,
Nunzio Palumbo³, Joe Kelleher⁴, and Shu Yan Zhang⁴

¹*Gas Turbine and Transmissions Research Centre, Faculty of Engineering, The University of Nottingham, University Park, Nottingham, NG7 2RD, UK*

²*Rolls-Royce plc, 8 Shop, Combe Field Road, Ansty, CV7 9JR, UK*

³*Rolls-Royce plc, Elton Road, Derby, DE24 8ED, UK*

⁴*ISIS Facility, Rutherford Appleton Laboratory, Harwell Oxford, Chilton, Didcot, OX11 0QX, UK*

Abstract

The interaction of weld induced residual stresses is an important issue for multi-pass and repair welding operations; effective prediction of the magnitude and location of peak residual stresses can lead to improved lifing predictions and greater understanding of the performance of components in service. However, the interaction of existing residual stress

field with new ones imposed by fusion welding processes has received little attention to date. This study presents a numerical and experimental investigation into the interaction of bead-on-plate welds in thick plates of IN718 with the aim of evaluating the effects of pre-existing weld residual stresses on the final global residual stress distribution. Sequentially coupled thermo-mechanical finite element (FE) models, which have been validated through temperature measurements, optical macrography and residual stress measurements, performed using the neutron diffraction technique, have been used to investigate the interactions. The results show good correlation between the experimental and model residual stress fields, demonstrating that the FE models are capable of predicting the redistribution of existing residual stress fields subjected to fusion welding processes and that this can be achieved with the use of parent material properties throughout, without considering any material property modifications which may occur due to microstructural changes in and around the weld region. It can also be seen from the results that, even with the relatively thick plates used in the study, a plane-stress state exists in the plates with a normal stress of approximately zero in all cases.

Keywords: Weld Modelling; Residual stress; Finite Element; Repair; Thermal; Mechanical

1 Introduction

Fusion welding is the most common, reliable and effective manufacturing process used to join components. The interest in developing effective numerical

methodologies to predict the effects induced by the process has resulted in a dedicated discipline, known as computational welding mechanics. Significant improvements have been made since the early studies of Rosenthal [1], who presented analytical solutions to evaluate the thermal field induced by fusion welding in the case of simple geometries and limiting hypotheses. At present, the most common approach adopted to investigate both the thermal and mechanical effects due to different fusion welding processes is based on sequentially coupled thermal-mechanical analyses, solved by the finite element (FE) method [2]. The methodology is very robust with a dedicated standard [3] and allows the prediction of both thermal and mechanical effects in terms of heat distribution, distortions and residual stresses which arise in the component being welded. It is quite common that parts subjected to a fusion welding process may have had previously experienced other manufacturing processes, such as thermal heat treatments and/or machining which can potentially create an existing residual stress field. This is also the case of weld repair, when the process is carried out to correct an anomaly created during the manufacturing phase or to repair damage caused by the operating conditions of the component. The questions of how and what is the extent of the interaction between the original residual stress field and the weld induced residual stress are not easy to answer. Very little attention has been paid to this topic in the literature. Little influence of the original weld stress on the repair residual stress characteristics was found by Dong et al. [4], though the original residual stresses were mapped into a 3D model from a 2D axisymmetric model, therefore neglecting some important

3D effects. Similar conclusions were drawn by Elcoate et al. [5] who found the predicted stresses in the proximity of the repair were relatively unaffected by the prior stress field. Deng and Kiyoshima [6] developed a numerical model to investigate the effects of a residual stress caused by a solution heat treatment in the case of laser beam welding of a SUS316 pipe, pointing out the effects on the final induced stress. The study showed that the fusion welding process controlled the local distribution of residual stresses, justifying the assumption of some authors who neglect the stress history of the component in the numerical simulations. In the case of weld repair, Dong et al [7] highlighted that common features in different repair scenarios are due to the high restraint conditions imposed by the material surrounding the repair. However, deviations can be attributed to a change in such conditions associated with specific weld repair applications. Salerno et al. [8] have proposed numerical methodologies which can be adopted to simulate weld repair by taking into account the initial stress field in the component, resulting in numerical results which appear to be qualitatively consistent with features highlighted by Dong et al. [7].

This work presents experimental and numerical investigations on the effects of weld passes in IN718 thick plates, with the aim of studying the effects of the interaction between weld induced residual stress fields. Bead-on-plate welds were experimentally analysed by means of thermocouple measurements, optical macrography and neutron diffraction measurements. Numerical models were implemented in both the general FE package ABAQUS and the specialized FE software Sysweld, evaluating the predictive capability of commercial FE codes of

the thermal and mechanical effects induced by the fusion welding process. The study provides relevant insights into the effects of the interactions between weld induced residual stresses, showing how the numerical FE models are capable of capturing the redistribution of the pre-existing stress fields in the component, correctly predicting important features of the final global stress distribution. Relevant conclusions are drawn on the question whether it is acceptable to take into account or neglect the pre-existing stress in a component subjected to a fusion welding process.

2 Experimental work

Bead-on-plate welds were produced using a focussed-arc TIG welding, carried out adopting the plasmatron apparatus at the University of Nottingham, supplied by Inocon Technologie. The focussed-arc TIG welding process operates on the principle of TIG. The difference is that the power density and the spot size of the arc are optimised for delivering energy densities similar to laser welding. A conventional TIG torch floods the area with a shielding gas to avoid oxidation upon solidification. Whereas the focussed arc TIG has a reduced gas exit nozzle, which uses the shielding gas to help shape the plasma, and increase the power density through a focussing effect [9].

Table 1: Chemical composition of IN718 (in wt%)

Ni	Cr	Nb	Mo	Ti	Al	Co	Mn	Si	Cu	Ta	C
54.3	10.5	3.96	4.05	1.24	0.786	0.421	0.268	0.499	2.51	3.26	0.0950

Table 2: Welding parameters adopted.

Set of parameters	Welding current (A)	Cur- age (V)	Volt- age (V)	Welding speed (mm s ⁻¹)	Shielding gas (Lit/min)
1	180		17.2	1.6	9
2	120		14.8	1.6	9

The welds were autogenous, i.e. no filler wire was adopted. The material used in this work was the IN718 alloy, with the chemical composition shown in Table 1, in the solution heat-treated state, supplied by Haynes International (Manchester, U.K.) in the form of 6.6 mm thick plates (120 x 160 mm). Two samples were produced as shown in Figs. 1 and 2.

Figure 1: Specimen A: top view.

Figure 2: Specimen B: top view.

In sample A, only a single weld pass was carried out, adopting the set of parameters 1 shown in Table 2. As the welding process was automatic, with the table moving and the torch head fixed, the plate was kept aligned with the torch using the clamping system shown in Fig. 3. In sample B, the plate was left cooling down to room temperature after the long pass. A second short pass was performed subsequently, adopting the set of parameters 2 (Table 2). In

this case, the clamping system could not be used because of the set up of the welding jig.

Figure 3: Experimental set up for welding trials.

As the torch head was fixed and the table was movable, after placing the

plate and aligning it with the torch head, the bars could not be positioned on the table which was therefore left free to distort during the second weld pass, ensuring the path was parallel to the first. The separation of the electrode tip to the workpiece was kept to 2.5 mm for all the weld passes performed. A nominally identical sample C was prepared with the same procedure carried out for specimen B, and used both for the preparation of the strain-free, d^0 , specimen, necessary for the neutron diffraction measurements, metallographic examination of the weld cross section and thermocouples measurements.

2.1 Thermocouple measurements and metallographic examination

In-situ temperature measurements were made in the weld heat-affected zone using K-type thermocouples at approximately the mid-length of each weld performed in sample C. Thermocouples were spot-welded on the top surface of the specimen to measure thermal histories at different locations from the centre of each weld pass, as shown in Fig. 4. In order to eliminate the electrical interference induced by the electric arc, low-noise thermocouples were used [10] in conjunction with a DBK48 Multifunction Isolated Signal Conditioning Module with three 8B47 Linearised Thermocouple Input Elements, supplied by Measurement Computing Corporation (MCC). After the initial long weld pass, the thermocouples were detached from the plate and reattached in proximity of the second pass, obtaining a set of three thermal histories for each bead-on-plate weld. The logged data were then transferred to a LabVIEW package for data

processing and analysis.

Figure 4: Thermocouple locations for the long and short pass.

Metallographic examination was conducted by water-jet cutting an area of material from around both the first and second welds of sample C. Specimens were prepared following the guidelines of Vander Voort [11]. After mounting them in resin, grinding was performed using SiC abrasive papers of 120, 240, 800 and 1200 grit size. Polishing was carried out with 6 and $1\mu\text{m}$ diamond abrasive paste with lubricant on the cloth path. Acid etching was performed using Beraha's tint etchant (100 mL HCL, 50 mL H₂O, 1 g potassium metabisulfite and 1 g ferric chloride) which was effective to highlight the weld fusion zones, subsequently examined by optical microscopy.

2.2 Neutron diffraction measurements

Neutron diffraction measurements were performed on the ENGIN-X facility at the ISIS neutron source at the Rutherford Appleton Laboratory, Didcot, Ox-

fordshire, UK [12]. Fig. 5 shows sample B mounted on the beamline.

Figure 5: Sample B mounted on the ENGIN-X beamline.

A $2 \times 2 \times 5 \text{ mm}^3$ gauge volume was defined both for the transverse and longitudinal orientation in order to ensure a good compromise between frequency of measurements, accuracy (using a sufficient measurement time per point) and spatial resolution. The counting time was typically 13.2 min per point, compatible with the planned number of measurements and the beam time allocated.

To obtain the strain-free lattice spacing, d_{hkl}^0 , the following methodologies are recommended in the literature [13]:

- measurement at a position (e.g. a far-field region) or time known to contain negligible stress.
- measurement on a stress-free powder or filings that are representative of the material being examined
- cutting stress-free reference cubes or combs from the sample

Figure 6: d^0 specimen cut from sample C (a) and d^0 specimen as seen from the top (b).

- application of force/moment balance.

As local changes in solute content of the parent alloy cause changes in the stress-free lattice parameter [14], the comb method was used in this work. This method allows the compositional and microstructural variation across the welds caused by the welding thermal cycles to be taken into account. A 3.5 mm thick slice of material was extracted from sample C (Fig. 6), to obtain the comb sample geometry, as shown in Fig. 7, using electrodischarge machining (EDM). As the plate was relatively thick, measurements were performed at two different locations across the thickness (indicated by red dots in Fig. 7), one close to the top surface (top path) and one to the bottom surface (bottom path). Also, a

Figure 7: Details of the d^0 specimen cross section. Red dots indicate the target of the beam.

higher number of measurements were conducted in the region in proximity of the two weld passes in order to better capture the interaction effects between the weld induced residual stresses. For each measurement location, the beam was pointed at the centre of the tooth, located at 1.4 mm from the top or bottom surfaces (red dots in Fig. 7), ensuring the gauge volume was entirely inside the material. In samples B, measurements were performed on the corresponding locations where the d_{hkl}^0 were extracted. In sample A measurements were carried out only in the locations on the left hand side of the weld by considering the symmetry. The Reitveld analysis was conducted to estimate the lattice parameters as a weighted average over all the diffracting grains [15] by using the General Structure Analysis System (GSAS), set of programs specifically designed to process and analyse diffraction data obtained with x-rays or neutrons [16]. The microstrain along direction i was computed as:

$$\epsilon_i = \frac{d_{hkl}^i - d_{hkl}^0}{d_{hkl}^0} \quad (1)$$

where d_{hkl}^0 is the strain-free inter-planar spacing and d_{hkl}^i are the corresponding spacings of interest, measured along three mutually orthogonal direction. It should be highlighted that the d_{hkl}^0 were extracted from sample C, nominally identical to sample B but they were also used to evaluate the microstrain in sample A. As the microstrain was determined along the principal directions of the samples, the following relationship was used to determine the stress along the i direction:

$$\sigma_{ij} = \frac{E}{1 + \nu} \left[\epsilon_{ij} + \frac{\nu}{1 - 2\nu} (\epsilon_{11} + \epsilon_{22} + \epsilon_{33}) \right] \quad (2)$$

The values of Young's modulus E and Poisson's ratio ν used for the calculations were 205 GPa and 0.29, respectively, as detailed by Dye et al. [17] for the same material.

3 Computational model

The bead-on-plate welds were simulated by means of sequentially coupled thermal-mechanical analyses. The approach is commonly adopted in literature for the simulation of different fusion welding processes [18–23]. It was used in this study as the deformations due to the welding process are not significant and the temperatures and displacements are not fully coupled, since displacements do not affect the temperature distribution. Therefore, the heat conduction problem

is first solved, independently from the mechanical one in order to obtain the thermal field due to the torch passes. The predicted thermal histories are then transferred as an input load in the mechanical step of the analyses. The flow chart in Fig. 8 shows the procedure for the FE analyses.

Figure 8: Flow chart of welding simulation procedure [20].

The thermal fields caused by the long and short weld pass were solved independently, by considering that sample B was left to cool down to room temperature after the first weld pass. Two mechanical models were then produced. In the first, only the entire thermal history (heating and cooling phases) due to the long pass was imposed. In the second, the thermal histories due to both the long and short weld pass were imposed. A third mechanical analysis was

also carried out, by only using the thermal history due to the short pass as an input, in order to evaluate the effects of the pre-existing stress on the final stress distribution.

Material properties were defined temperature dependent as in Figs. 9, 10 and 11 [24] except for the Poisson's ratio which is taken to be constant (0.33) as generally assumed for IN718 in studies of this kind. In the interest of simplicity, the welding metal was assumed to have the same material properties of the base metal. Although the approach is not formally correct as metallurgical changes in texture, grain size, recrystallization, and precipitate dissolution occur in the welded area, Tanner [25] proved that correcting the material properties for the melted and solidified material in the FE analysis has a negligible effect on the residual stress predictions in the case of IN718.

Figure 9: Thermal conductivity, κ , and density, ρ , for IN718 [24].

Figure 10: Specific heat capacity, C_p , and thermal expansion coefficient, α , for IN718 [24].

Figure 11: Young's modulus, E , and yield stress, σ_y , for IN718 [24]

Analyses were performed in both the commercial specialized FE code Sysweld and the general FE package ABAQUS/Standard (which uses implicit analysis [26]), in order to test the capability of both pieces of FE software to investigate the effects of the welding process, evaluating how the predictions compare between them and with experimental measurements.

3.1 FE Mesh

A view of the full mesh adopted for all the thermal and mechanical analyses in both FE codes is shown in Fig. 12. A fine mesh with a high element density, and a constant element volume of $1 \times 1 \times 0.5 \text{ mm}^3$ was designed in the proximity of the two weld passes.

Figure 12: Finite element mesh.

The area was identified by running preliminary thermal analyses for the long and short weld pass in a mesh with a constant element volume of 0.5 mm^3 , ensuring it was covering all the volume of material subject to a temperature rise. This reduced computational cost in the mechanical analyses, yet correctly simulated the heating process and captured the steepest temperature gradients close to both the torch passes and, therefore, the consequent thermal expansion of the material. The element volume was increased up to $3 \times 1 \times 0.5 \text{ mm}^3$ using two different transition rules, on both sides of the heat affected areas, as the only requirement in the weld far field is to replicate the stiffness of the component. The entire mesh contained 151200 hexahedral elements and

166738 nodes. Linear elements were used as it has been proven that a higher order elements do not yield significantly better results in structural welding simulations [27].

3.2 Thermal analyses

The governing equation for the transient heat transfer analysis in the fusion welding process is given by [28]:

$$\rho c \frac{\partial T}{\partial t}(x, y, z, t) = -\nabla \cdot \vec{q}(x, y, z, t) + Q(x, y, z, t) \quad (3)$$

where ρ is the density of the materials, C_p is the specific heat capacity, T is the current temperature, $\vec{q}$ is the heat flux vector, Q is the internal heat generation rate, x, y and z are the coordinates in the reference system, t is the time, and ∇ is the spatial gradient operator. The problem was solved using the full Newton-Raphson integration scheme in ABAQUS/Standard and the Broyden-Fletcher-Goldfarb-Shanno (BFGS) method in Sysweld, respectively. The integration time for the heating phases is chosen to be equivalent to one element travel length. This was then set to automatic for the cooling phases. To simulate the heat transfer due to fluid flow in the weld pool, the thermal conductivity was tripled for temperatures above the material solidus temperature (1260°C). The 3D double ellipsoid (Fig. 13) developed by Goldak et al. [29] was selected to simulate the heating process due to both the long and short weld pass.

Figure 13: Double ellipsoid power density distribution

The heat power is distributed as following:

$$Q_f(x, y, z, t) = f_f \cdot Q_0 \cdot e^{-z^2/a^2} e^{-y^2/b^2} e^{-(x-vt)^2/c_f^2} \quad (4a)$$

$$Q_r(x, y, z, t) = f_r \cdot Q_0 \cdot e^{(-z)^2/a^2} e^{-y^2/b^2} e^{-(x-vt)^2/c_r^2} \quad (4b)$$

where the subscripts f and r denote the front and rear regions of the ellipsoid respectively, f defines the fraction of the heat power deposited in either region (with $f_f + f_r = 2$). a , b and c are geometrical parameters of the heat source as shown in Fig. 13, which were selected based on the real weld cross sections, obtained with the metallographic analyses. Q_0 , v , t are the effective power input, welding velocity, and time, respectively. As the energy transferred from the front leading part of the weld pool is higher than rear part, f_f is assumed to be 1.4 and f_r 0.6. The process efficiency, η , was used as a calibration factor as presented in the study of Salerno et al. [30] and takes into account conductive heat losses due to the welding jig and clamping system which were not included in the FE model. The value was therefore assumed to be lower than the conventional efficiency of a TIG welding process, which is in the range of 0.73 - 0.82 [31]. A value of 0.54 was adopted for the weld parameters set 1 and 0.6 for the weld

parameters 2, which was obtained by arriving at the best fit both for the welding pool and thermal histories. The environment and initial temperatures of the plates were both set to 20°C. Heat losses through convection and radiation were simulated with the Newton and Stefan-Boltzmann laws, respectively, setting the convective and emissivity coefficients to 25 W/m^2 and 0.8 [32]. The above mentioned thermal boundary conditions were employed for all the free surfaces of the plates.

3.3 Mechanical analyses

The finite element mesh defined for the thermal analyses was used for the mechanical simulations, so that the predicted thermal field could be correctly transferred as an input load. As no solid-state phase transformation occurs in IN718, the total strain is decomposed as follows:

$$\varepsilon = \varepsilon^e + \varepsilon^p + \varepsilon^{th} \quad (5)$$

where the three components on the right hand side of Eq. 5 are the elastic, plastic and thermal strains, respectively. The elastic strain was modelled with the isotropic Hooke's law, while yielding was defined using the von Mises criterion. A rate-independent model with a linear isotropic hardening behaviour was assumed for the plastic material properties, setting the hardening coefficient to be 0.01 E [24]. The thermal strain is computed by means of the temperature-dependent mean thermal expansion coefficient, based on the temperature history imposed by the heating and cooling process. To simulate the mechanical

behaviour of the weld pool, different approaches were adopted in the two FE codes. In ABAQUS/Standard, the annealing function was used to model the effects of melting and re-solidification of the weld pool, setting the annealing temperature at the solidus temperature of IN718 (1260°C). The function ensures that the material which experiences temperatures at or above the selected annealing value, loses its hardening memory. The effect of prior work hardening is also removed by resetting the equivalent plastic strain to zero. When the temperature falls below the selected annealing value during cooling, the solidified weld pool can exhibit work hardening again [26]. In Sysweld the melting and re-solidification of the weld pool was simulated using the melt function. It works on the same principles of the annealing function of ABAQUS/Explicit. However, in this case the function resets to zero the entire strain history in the locations where the temperature rises above the selected melting value (i.e. the weld pool) [33] Although the methodologies are different, Salerno et al. [8] proved that the effect in terms of the global residual stress distribution is not significant.

The mechanical boundary conditions were applied to only constrain the rigid body motions, by considering that the clamping system was used to keep the plate in position and to ensure the torch head was running parallel to the sheet edges.

4 Results

4.1 Thermal analysis

The predicted weld pools from both the FE codes are highlighted in grey are compared with the real ones in Figs. 14 and 15 for set 1 and 2 of welding parameters, respectively, contoured by the isothermal curve at the solidus temperature of IN718 (1260°C). In the first case the material entirely melted across the thickness and the model were effectively calibrated in both the FE codes to correctly model this effect. In the second case the penetration of the bead is lower, due to the welding power being reduced. Again, the FE predictions show a good agreement with the real case, correctly predicting the real shape of the fusion zone and the penetration of the bead.

Thermal predictions were further validated by comparing the temperature histories in output from the FE models with the ones recorded by means of thermocouples. Figs. 16 and 17 show the comparison for set 1 and 2 of welding parameters, respectively. Small differences in the maximum temperature predicted from the FE codes at each location were expected, as already pointed out by Deshpande et al. [32]. However, trends are well correlated with the experimental results. The temperature gradients due to the torch approach in the heating phases are well predicted in both cases and good agreement is also found in the cooling phases for both sets of welding parameters, although in the case of set 1 the FE models predict a slightly faster cooling rate compared to the experimental case.

(a)

(b)

Figure 14: Real and predicted weld pool comparison for set 1 of welding parameters: Sysweld

(a) and ABAQUS (b)

(a)

(b)

Figure 15: Real and predicted weld pool comparison for set 2 of welding parameters: Sysweld

(a) and ABAQUS (b)

Figure 16: Thermal histories comparison for set 1 of welding parameters.

Figure 17: Thermal histories comparison for set 2 of welding parameters.

4.2 Mechanical analysis

For comparison with experimental results, the FE residual stress predictions were averaged over a volume of material equivalent to the gauge volume used for the neutron diffraction measurements. The error bars in the experimental measurements were obtained based on the uncertainty which arose from the Retveld method using the software GSAS, both for the free-strain lattice parameter d_{hkl}^0 and corresponding inter-planar distance d_{hkl}^i . Figs. 18, 19 and 20 show the comparison between the predicted and measured longitudinal, transverse and normal residual stresses for sample A along the bottom and top paths, respectively. Despite the different models adopted to simulate the mechanical

behaviour of the weld pool, the predictions of the FE codes match very well with each other. Also, the residual stresses along the top path are generally better correlated than the ones along the bottom path. If the attention is focused on the longitudinal stress, the FE models predicted the typical weld distribution with a tensile region in the location of the weld pass, which is balanced by a compressive stress in the area far from the weld, both on the bottom and top paths. In the case of the neutron diffraction measurements, a small compressive region is visible only in Fig. 18b and almost absent in Fig. 18a. The predicted trends of the transverse and normal stress also agree well with the measurements, though the experimental results in the case of the bottom path appear to be shifted by roughly 100 MPa (Figs. 19a and 20a) in comparison with the FE predictions.

The comparisons for sample B are shown in Figs. 21, 22 and 23, on the bottom and top paths. Trends are very well correlated and both the FE codes captured the stress magnitudes along the three directions. In this case, the experimental trend of the longitudinal stress shows a compressive stress area which balances the tensile stress area. The peak tensile stress appears in the region of the second weld pass, both on the bottom (Fig. 21a) and top path (Fig. 21b). The most interesting area is located between the two welds. Here, trends are again well matched but the FE predictions tend to predict a slightly compressive trough, which was only measured on the bottom path (Fig. 21a). On the top path, the neutron diffraction measurements show a trough in the area between the two welds, however, this does not reach compressive values.

The effects of the initial weld pass are completely hidden by the second one in terms of the transverse stresses along both the paths (Figs. 22a and 22b). The tensile stress tends to move in the location of the second weld pass and the magnitude appears to be almost of the same order of the longitudinal stress, consistent with the cases of localized fusion welding passes [7]. Although the plates were relatively thick, the FE models predicted a 2D stress state both for samples A and B, showing a zero normal stress as seen in Figs. 23a and 20b, consistent with the experimental measurements which highlight an oscillation of the stress around zero.

Figs. 24 and 25, show a comparison of FE predictions for the longitudinal and transverse distribution along bottom path (a) and top path (b), in the cases when the effects of the initial weld are taken into account or neglected. If the pre-existing stress is not simulated, the longitudinal stresses show no tensile peak in the location of the initial weld pass, and the stress is distributed as in the case of a theoretical welding distribution, though it is clearly not symmetrical as the short weld pass was not in the middle of the plate. In the case of the transverse stress, simulating or neglecting the initial weld pass does not appear to have a significant effect along the paths considered.

The same comparison is presented in Figs. 26 and 27 in terms of field distributions. It is evident that the global stress distribution is significantly different in the cases where the pre-existing stress is neglected or taken into account, both in the case of the longitudinal and transverse residual stress. However, though the short weld pass causes a stresses redistribution in the

plate, moving and redefining the tensile and compressive regions both for the longitudinal and transverse stress, the local stress distribution is controlled by the short weld pass itself. Therefore, local stress predictions around the short weld are very comparable in the cases where the pre-existing stress is accounted for or neglected.

(a)

(b)

Figure 18: Sample A: longitudinal residual stress along (a) bottom path and (b) top path

(a)

(b)

Figure 19: Sample A: transverse residual stress along (a) bottom path and (b) top path

(a)

(b)

Figure 20: Sample A: normal residual stress along (a) bottom path and (b) top path

(a)

(b)

Figure 21: Sample B: Longitudinal residual stress along (a) bottom path and (b) top path

(a)

(b)

Figure 22: Sample B: transverse residual stress along (a) bottom path and (b) top path

(a)

(b)

Figure 23: Sample B: normal residual stress along (a) bottom path and (b) top path

(a)

(b)

Figure 24: Effects of pre-existing stress on the longitudinal stress along (a) bottom path and (b) top path

(a)

(b)

Figure 25: Effects of pre-existing stress on the transverse stress along (a) bottom path and (b) top path

Figure 26: Transverse residual stress (a) neglecting and (b) considering preliminary residual stress from initial weld pass

Figure 27: Longitudinal residual stress (a) neglecting and (b) considering preliminary residual stress from initial weld pass

5 Discussion

Numerical predictions from the thermal analyses are well correlated with the experimental results, both in terms of fusion zone cross sections and thermal histories, in the locations where thermocouples were placed. As the thermal fields imposed into the IN718 plates were correctly captured, the thermal expansion of the material, imposed in the heating phases of both the welding processes (long

and short pass), and the shrinkage which occurs in the cooling phases, were correctly represented into the mechanical models. The residual stress measurements for sample A are generally well correlated with the numerical predictions for the top path, while on the bottom path, experimental trends appear not to be entirely consistent either with FE predictions or the theoretical weld stress distributions. The longitudinal stress shows a tensile region in the area of the weld, but there is no compression zone in the area around the heat affected zone to balance it. The transverse stress should reach a zero stress at the boundary of the sample but it does not. Also, the normal stress appears to have a non-zero value, not consistent with the small thickness of the plate, as all the other measurements show a normal stress oscillating around a zero value. The authors believe that the non-perfect correlation between FE predictions and experimental findings for measurements in sample A is an experimental error caused by to spurious microstrain arisen as a combinations of the two factors:

- changes in temperature;
- geometrical effects.

Measurements of the strain-free lattice spacings d_{hkl}^0 were taken during the day, immediately after the measurements in sample B, while measurements in sample A were taken over night. It has been pointed out by Withers [13] that a change in temperature of just 1 K gives a fractional change in lattice spacing (or apparent strain), of the order of 10^{-5} . As a result, a 10 K change in fluctuation in temperature when measuring the sample being investigated and the reference

spacing can be significant. Furthermore, a non-perfect alignment between the beamline and sample A might have occurred during the measurements, because the clamping system may not have been sufficiently tightened when the sample was mounted. Geometrical effects associated with partial filling of the gauge volume can give rise to anomalous shifts in measured diffraction angles, thus giving further spurious strains [34].

Thermal loads in pre-stressed structures can cause a stress-relief effect, resulting in a change in stresses in the thermally affected region, however, while a reduction in the tensile longitudinal stress of roughly 200 MPa is evident in the experimental data around the original weld location in specimen B, this change is not caused by stress-relief but is purely due to the mechanical redistribution of the stresses caused by the second weld pass, this can be confirmed by the fact that the FE model captures the reduction in stress at the original weld location, however, it includes no mechanism to for a stress relaxation or creep-type behaviour which would lead to the prediction of a stress-relief in the pre-stressed, thermally affected region.

Predictions from both the FE codes indicated that the effects of the initial weld are not significant in the location of the second weld pass. An explanation can be given as suggested by Deng and Kiyoshima and [6]: during the second weld pass, the fusion zone and its vicinity are heated up to a very high temperature and the material is softened enough so that the initial residual stresses are cancelled completely. After cooling, the stresses around the second weld pass are very similar to those induced by a simple weld pass.

The study also highlights the capability of modern commercial FE software to predict thermal and mechanical effects induced by a non-conventional fusion welding process (focussed-TIG) in IN718 thick plates, well correlated with experimental findings. As the FE code Sysweld is specifically designed and optimized to perform fusion welding simulations, the time to set up the FE models in this study and run the analyses was significantly reduced. However, comparable results were obtained using the general FE package ABAQUS, although the time to set up the FE models and carry out the sequentially coupled thermal-mechanical analyses would be significantly increased for a user who may be inexperienced in weld simulations.

6 Conclusions

- Experimental investigation of bead-on-plate welds in IN718 thick plates were conducted by means of weld macrography, thermocouples and neutron diffraction measurements with the aim to investigate the effects of the interaction between weld induced residual stresses. The second weld pass was shown to cause a redistribution of stresses, resulting in a reduction in the longitudinal peak stresses in the location of the first and also, the highest tensile transverse stresses to move to the location of the second weld pass. The magnitude of the transverse stress increased as a result of the weld repair. Although the plates are relatively thick, both the samples appear to be in a plane stress condition, showing a normal stress of

approximately zero.

- Good agreement was found between FE predictions and experimental measurements in terms of trends and magnitudes of the residual stress measurements and numerical predictions. The FE models predict the correct location of the peaks for the longitudinal tensile stress and capture the mechanical redistribution of the stress when the second weld pass is performed.
- Performing experimental tests to evaluate the thermal and mechanical material properties of a weld, obtaining a temperature dependent set of properties to be adopted in FE simulation is a complex task. In this study, a good matching between predicted and measured residual stresses was achieved by not considering any material property modification due to the different microstructures in the location of the initial weld. Realistic numerical predictions in the case of two weld passes were obtained despite the fact that in the FE models the material properties of the weld were assumed to be the same as the parent material.
- In the sample with two weld passes, the local residual stress is dominated by the second pass and the FE models predicted accurate results in that area, whether the initial weld was considered or neglected. It is therefore possible, for the case study investigated, to obtain a good local representation of the residual stress by simulating only the second weld pass.
- To answer the question whether it is acceptable or not to neglect the initial

stress in simulating the fusion welding process is simulated, the authors suggest considering the purpose of the numerical model. If the objective of the simulation is to evaluate the local stress distributions, a good representation of the stress might be obtained by neglecting the pre-existing stresses into the component. If the aim is to evaluate the global stress distributions in a complex geometry, including critical areas in the far weld region, and/or study the effect of different welding conditions (welding power, length pass, multiple bead deposition) it is strongly recommended that any pre-existing stresses in the component be considered, properly representing the actual pre-existing stress distributions into the FE model.

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