NRC Publications Archive Archives des publications du CNRC

Prediction and measurements of thermal residual stresses in AA2024-T3 friction stir welds as a function of welding parameters

Dubourg, L.; Doran, P.; Larose, S.; Gharghour, M. A.; Jahazi, M.

This publication could be one of several versions: author's original, accepted manuscript or the publisher's version. / La version de cette publication peut être l'une des suivantes : la version prépublication de l'auteur, la version acceptée du manuscrit ou la version de l'éditeur.

For the publisher's version, please access the DOI link below. / Pour consulter la version de l'éditeur, utilisez le lien DOI ci-dessous.

Publisher's version / Version de l'éditeur:

<https://doi.org/10.4028/www.scientific.net/MSF.638-642.1215>

Materials Science Forum, 638-642, pp. 1215-1220, 2010

NRC Publications Record / Notice d'Archives des publications de CNRC:

<https://nrc-publications.canada.ca/eng/view/object/?id=374d3f1c-2537-43a2-a89e-96dbac2753bc>

<https://publications-cnrc.canada.ca/fra/voir/objet/?id=374d3f1c-2537-43a2-a89e-96dbac2753bc>

Access and use of this website and the material on it are subject to the Terms and Conditions set forth at

<https://nrc-publications.canada.ca/eng/copyright>

READ THESE TERMS AND CONDITIONS CAREFULLY BEFORE USING THIS WEBSITE.

L'accès à ce site Web et l'utilisation de son contenu sont assujettis aux conditions présentées dans le site

<https://publications-cnrc.canada.ca/fra/droits>

LISEZ CES CONDITIONS ATTENTIVEMENT AVANT D'UTILISER CE SITE WEB.

Questions? Contact the NRC Publications Archive team at

PublicationsArchive-ArchivesPublications@nrc-cnrc.gc.ca. If you wish to email the authors directly, please see the first page of the publication for their contact information.

Vous avez des questions? Nous pouvons vous aider. Pour communiquer directement avec un auteur, consultez la première page de la revue dans laquelle son article a été publié afin de trouver ses coordonnées. Si vous n'arrivez pas à les repérer, communiquez avec nous à PublicationsArchive-ArchivesPublications@nrc-cnrc.gc.ca.

Prediction and Measurements of Thermal Residual Stresses in AA2024-T3 Friction Stir Welds as a Function of Welding Parameters

L. Dubourg^{a,1}, P. Doran^a, S. Larose^a, M. A. Gharghoury^b and M. Jahazi^a

^a Aerospace Manufacturing Technology Centre, National Research Council Canada, Montreal, Canada

^b Canadian Neutron Beam Centre, National Research Council Canada, Chalk River, Canada

¹laurent.dubourg@cnrc-nrc.gc.ca

Abstract. Friction Stir Welding (FSW) induces thermal residual stresses resulting in distortions in thin-walled structures. In order to understand and quantify this phenomenon, simulations and experiments of FSW on aluminium alloy (AA) 2024-T3 have been performed using different rotational and welding speeds. A sequentially coupled finite element (FE) model was used to study the residual stresses caused by the thermal cycling induced from FSW. The 3D FE model used temperature-dependent mechanical and thermophysical material properties. The predicted longitudinal stresses peaked at ~300 MPa and had a “W” profile with tensile stress peaks in the weld and compressive stresses outside the weld. In the FE model, the influence of process parameters on residual stress distribution was studied. The application of ‘hot’ welding conditions, i.e. low welding speed and high rotational speed, increased the residual stresses significantly, mainly in the transverse direction. Conversely, ‘cold’ welding conditions resulted in lower residual stresses. The magnitude and distribution of the residual stresses predicted by the FE model were validated by neutron diffraction. The results indicate a good agreement between the measured and predicted residual stresses in AA2024-T3.

1. Introduction

FSW is an emerging manufacturing technology increasingly used in the aerospace industry. The National Research Council Canada's Institute for Aerospace Research has undertaken a major initiative to manufacture a large scale aircraft structural element, e.g. a fuselage panel, using FSW. This multidisciplinary study included structural design, FSW process optimisation, mechanical property evaluation, non-destructive evaluation (NDE) using neutron diffraction and robotic processing [1, 2]. For the manufacture of aircraft fuselage panels that commonly involve riveting of stringers and frames onto a skin, FSW is a promising alternative for creating lighter and more aerodynamic structures at lower cost [3, 4]. However, stringer to skin joints pose a significant problem because of panel distortion [4, 5]. The aim of the present work was to predict and understand the influence of FSW parameters on the residual stresses. As a first step towards this objective, thermo-mechanical simulation using LS-DYNA software was performed to predict the residual stresses in 2.3-mm thick AA2024-T3 processed by FSW. Simulation results were compared with experimental measurements to validate the FE model. Additional simulations were conducted to evaluate the impact of different welding parameters on the residual stresses.

2. Experimental procedure

2.1 Experimental set-up. FSW trials were performed on 2.3-mm thick AA2024-T3 plates (300 x 140 mm²) using a MTS I-STIR machine operated in position control welding mode. The tool consisted of a cylindrical threaded probe (diameter of 6.3 mm) with a smooth concave shoulder (diameter of 12.7 mm) made of H13 tool steel. The influence of welding speed (2, 6 and 10 mm·s⁻¹) and rotational speed (500, 1000 and 1500 RPM) were investigated using a full factorial experiment design. All other key welding parameters were kept constant. For temperature recording, type K Thermo-Electric thermocouples (0.5 mm in diameter) were attached to the workpiece on the advancing side of the weld. Neutron diffraction was used to non-destructively scan the internal distribution of stresses after FSW [6] by means of intersecting incident and diffracted neutron

beams to form a small sampling volume that was fixed in space. The dimensions of the sampling volume were defined using neutron-absorbing cadmium masks. The component was translated with respect to the fixed sampling volume to scan a series of locations in the component. At each location, a diffraction peak was acquired, the mean scattering angle (2θ) was determined, and the crystal-lattice strain, ε , was calculated by comparison with the scattering angle ($2\theta_0$) for stress-free material, using equation (1). Strain was measured in the direction that bisected the incident and diffracted beams. The strain measurement direction was selected by orientating the component such that the bisector lay parallel to the direction of interest. The crystal-lattice strain was purely elastic, reflecting the stresses in crystallites, and was not directly sensitive to plastic strain. The uncertainty of the lattice strain measurements was typically about 10^{-4} . With three measurements of normal strain at each location, the corresponding normal residual stresses could be determined using a generalized Hooke's Law (equation (2)), where E is Young's modulus, ν is Poisson's ratio and the subscripts indicate the components of three mutually orthogonal components of strain and stress. In the present work, lattice strain measurements were performed using the L3 diffractometer of the Canadian Neutron Beam Centre. Lattice strains were measured in the longitudinal (parallel to the weld), normal (perpendicular to the Al plate surface), and transverse directions, at the middle length of the weld, which allowed the determination of the corresponding residual stresses using equation 2. A sampling volume of $2 \times 2 \times (2-20) \text{ mm}^3$ was used, depending on the component of strain being measured. Different stress-free scattering angles ($2\theta_0$), acquired from small coupons extracted from a component after all the measurements were done, were used for the weld and base plate materials. By this means, the changes in material microstructure due to the FSW process were considered.

$$\text{Equation 1: } \varepsilon = \frac{\sin \theta_0}{\sin \theta} - 1$$

$$\text{Equation 2: } \sigma_1 = \frac{E}{1+\nu} \left[\varepsilon_1 + \frac{\nu}{1-2\nu} (\varepsilon_1 + \varepsilon_2 + \varepsilon_3) \right]$$

2.2 Modelling. A sequentially coupled FE model was developed using LS-DYNA to study the thermal cycles and induced stresses that occurred during FSW. The 3D FE model (Fig. 1) takes into account the temperature-dependent mechanical and thermophysical properties of AA2024-T3. In a first step, the thermal history was predicted considering tool friction as a heat source, an approach that has been used successfully elsewhere [7-9]. The model was calibrated using an inverse method based on temperature measurements at various locations close to the weld centreline [7, 9]. The temperature distribution generated by the thermal model was then coupled to a mechanical model to predict the residual stresses. The dimensions of the Al weld plate and backing anvil are respectively $140 \times 100 \times 2.3 \text{ mm}^3$ and $140 \times 100 \times 12 \text{ mm}^3$, while the welding tool diameter was 12.7 mm. Symmetry with respect to the weld centreline was used to reduce the calculation time. Only half of the plate, backing and welding tool were thus included in the model, with symmetrical boundary conditions applied along the weld centreline. FSW simulation showed a significant stress gradient from the weld centreline up to the first 10 mm. The mesh was thus refined in the vicinity of the welding tool. The backing anvil and Al plate were modelled using solid elements and the welding tool was modelled using shell elements (Fig. 1.b). In total, 25,107 nodes and 134,975 tetrahedral elements were used. Heat flow was treated as a pure heat conduction problem, ignoring material flow around the FSW tool. Heating was modelled as a surface heat generation term and related to the basic welding process parameters via equation 3, where q is the surface heat flux ($\text{W}\cdot\text{m}^{-2}$), ω is the rotational speed of the tool ($\text{rad}\cdot\text{s}^{-1}$), M is the welding torque recorded by the FSW machine ($\text{N}\cdot\text{m}$), r is the radius of the tool shoulder and η is the process efficiency that takes into account the heat lost to the welding tool. The process efficiency coefficient was calibrated using an inverse method based on temperature measurements as a function of rotational speed (Table 1). The efficiency values are consistent with those reported in literature [8]. The process efficiency decreased with increasing rotational speed, due to the concomitant increase in tool temperature. All nodes had an initial temperature of 23°C (296 K). A heat source was positioned at the bottom surface of the welding tool and moved along the weld line at the welding speed. Convection was initially implemented but was excluded from the model since the results were not significantly

affected. Radiation heat loss was not included in the model. To model the heat transfer between the aluminium plate and the backing anvil, two different heat transfer coefficients were defined: H for the high-pressure contact under the FSW tool and H_0 for the low-pressure contact under the rest of the plate. H was larger than H_0 to account for the better contact between the Al plate and the backing anvil that results from the tool pressure. H and H_0 were also calibrated using the inverse method based on temperature measurements (see section 3.1). For the mechanical analysis, the FSW tool and the backing anvil were treated as rigid contact surfaces. To simulate the effect of the clamping device, all displacement and rotational degrees of freedom of the nodes were blocked along the longitudinal edge of the plate. After tool motion, 50 seconds were added to the simulation to consider cooling of the Al plate and the ensuing development of the residual stresses. The release phase from the clamping device was not simulated, i.e. the simulated and measured stresses reported correspond to those before release of the clamping pressure. This assumption reduced drastically the computing time and simplified the mechanical model. Moreover, the hypothesis that a reduction of the residual stresses before release resulted in a reduction of the residual stresses after release was presumed and precluded the necessity for modeling of the clamping release step.

Figure 1: a) mesh of aluminium plate and backing anvil, b) zoom-in of the FSW tool model

Equation 3: $q = \eta \frac{\omega \cdot M}{\pi \cdot r^2}$

Table 1: heat flux calculation

<i>Rot. speed</i> (RPM)	<i>Torque</i> (N·m)	<i>Nominal heat flux</i> (MW·m ⁻²)	<i>Efficiency</i> (---)	<i>Net heat flux</i> (MW·m ⁻²)
500	16	6.6	1.00	6.6
1000	13.5	11.2	0.85	9.5
1500	10.5	13.0	0.75	9.8

3. Results and discussion

3.1 Calibration, experimental results and validation with a single welding parameter. The calibration methodology for the model used an inverse method based on temperature measurements and involved an iterative process to minimize the difference between the simulated and measured temperatures as a function of time and distance from the weld centreline. Similar approaches were used in [7, 9]. The nominal parameters chosen for calibration were rotational and welding speeds of 1000 RPM and 6 mm·s⁻¹ (i.e. the centre of the process range). The calibration of the nominal case resulted in the following optimised parameters: process efficiency $\eta = 0.85$, high-pressure heat transfer coefficient $H = 15,000 \text{ W} \cdot \text{m}^{-2} \cdot \text{K}^{-1}$ and low-pressure heat transfer coefficient $H_0 = 400 \text{ W} \cdot \text{m}^{-2} \cdot \text{K}^{-1}$. The measured and predicted temperatures as a function of time and distance from the weld centreline that are compared in Fig. 2 indicate negligible temperature differences, confirming the relevance of the inverse method and the use of two heat transfer coefficients. However, the predicted temperature profile shows an unrealistic plateau of about 350°C in the weld (Fig. 2.b). This artifact is likely due to modeling the heat generated by the FSW tool as a constant heat flux, which results in a top hat heating profile. Realistically a Gaussian temperature profile is better than assuming that the material flow around the tool renders a homogeneous temperature.

Figure 2: Comparison of measured and predicted temperatures after model calibration with a rotational speed of 1000 RPM and a welding speed of 6 mm/s: a) temperature as a function of time, b) temperature as a function of distance

The measured and predicted longitudinal and transverse (i.e. parallel and perpendicular to the weld line) residual stresses after cooling are shown in Fig. 3 as a function of the distance from the weld centreline, determined normal to the weld line. For the longitudinal stresses (Fig. 3.a), the predicted and measured profiles are in agreement and show tensile stresses close to the yield strength of AA2024-T3 (345 MPa) in the weld area and compressive stresses in the rest of the Al plate. This tendency agrees with previous work [7, 8, 10-13]. However, the predicted tensile stresses in the weld are higher than the measured, while the measured compressive stresses (~ -35 MPa) are lower than the predicted (~ -10 MPa) in the base metal. Also the predicted stress gradient was sharper than that revealed by neutron diffraction. Finally, the predicted and measured trends in the base metal are somewhat different. The predicted stress was most negative close to the weld, becoming increasingly tensile further away from the weld, while the measured stress remained constant beyond a distance of ~ 20 mm from the weld centreline. These differences can be explained by the lack of blocked nodes close to the heating zone simulating the effects of the clamping device. In the present model, only nodes at the edge of the Al plate were blocked, facilitating the plate expansion along the longitudinal direction and reducing the residual longitudinal stress built-up. In the transverse direction, predicted and measured stresses were tensile in the base metal, similar to that reported in [7, 8, 11-13]. However, the predicted and measured profiles diverge greatly in the weld zone, where the measured stress was weakly compressive while the predicted stress was strongly tensile. This difference may be due to the presence of blocked nodes at the edge of the Al plate. The plate expansion along the transverse direction was thus prevented in the model, thereby increasing the predicted build-up of residual stresses. Nonetheless, the present model yields a stress profile in agreement with stress measurements, suggesting that the influence of material flow around the FSW tool is negligible and a top hat profile for the heat source is reasonable. However, improved conditions to effectively simulate the clamping conditions must be studied.

Figure 3: Comparison of the measured and predicted residual stresses with rotational speed of 1000 RPM and welding speed of 6 mm/s: a) longitudinal stresses, b) transverse stresses

3.2 Calibration, experimental results and validation with multi welding parameters. Besides the nominal case, the process efficiency η was calibrated for the rotational speeds of 500 and 1500 RPM using the inverse method based on temperature measurements, the results of which are given in Table 1. With the same low- and high-pressure heat transfer coefficients for the 9 cases studied, the modelled and experimental profiles were obtained (Fig. 4). In general, acceptable agreement is observed for the thermal results, confirming the relevance of using constant heat transfer coefficients for the 9 cases and one process efficiency per rotational speed. However, at a low welding speed ($2 \text{ mm}\cdot\text{s}^{-1}$) and high rotational speed (1500 RPM), i.e. high energy input (Fig. 4.c), the predicted temperatures are higher than that measured, which suggests that the modelled heat input is too high and/or the heat transfer coefficient is too low. Fig. 4 shows that the thermal profiles are very sensitive to the welding speed and less sensitive to the rotational speed. At low welding speed, more energy is transmitted to the Al plate resulting in a high temperature and vice versa. Indeed, the energy input introduced into the Al plate is inversely proportional to the welding speed, as shown in Eq. 4, where E is the energy input per unit volume ($\text{J}\cdot\text{m}^{-3}$), q is the surface heat flux ($\text{W}\cdot\text{m}^{-2}$) and v is the welding speed ($\text{m}\cdot\text{s}^{-1}$). By opposition, the temperatures in the Al plate are less sensitive to the rotational speed, which can be explained as follows. The rotational speed and torque varied in opposite ways (Table 1). However, the mathematical relation between the total heat input and rotational speed is not linear. For example, a 50% increase in the rotational speed (from 1000 to 1500 RPM) resulted in an increase of the heat flux input from 9.5 to 9.8 $\text{MW}\cdot\text{m}^{-2}$ (a 3% increase).

Figure 4: Measured and predicted temperature profiles for welding speeds from 2 to 10 mm/s and rotational speeds at 500 RPM (a), 1000 RPM (b) and 1500 RPM (c)

$$\text{Equation 4: } E = \frac{q}{v}$$

To understand the effect of the welding parameters on the residual stresses, simulations were performed for the 9 cases (Fig. 5). The stress was the highest for the highest rotational speed (1500 RPM) and the lowest welding speed ($2 \text{ mm}\cdot\text{s}^{-1}$), i.e. highest energy input. As with the temperature profiles, the welding speed had a higher impact on the residual stress than the rotational speed. The longitudinal stress clearly decreased with increasing welding speed, with the lowest values obtained for $10 \text{ mm}\cdot\text{s}^{-1}$. The impact of the rotational speed was not as clear. The effect of welding speed on the transition point between the tensile and compression area is visible in Fig. 5. At a low speed ($2 \text{ mm}\cdot\text{s}^{-1}$), a distance of 11.5 mm from the weld centerline is predicted. At $10 \text{ mm}\cdot\text{s}^{-1}$, this distance decreases to 7.0 mm. Finally, at low energy inputs, i.e. a rotational speed of 500 RPM (Fig. 5.a and 5.d) and welding speeds of 6 and $10 \text{ mm}\cdot\text{s}^{-1}$, the residual stresses are almost nil. This finding is clearly unrealistic, as indicated by the distortion of the welded plate after releasing from the clamp for these parameters. In this case, the inaccuracy of the model may be due to the top hat profile of the heat source. Indeed, the power flux input was not high enough to generate a plastic strain in the plate by expansion and to induce a residual stress build-up. The use of a heat source with a Gaussian profile instead of a top hat profile could solve this problem by locally increasing the temperature at the weld centreline.

Figure 5: Effects of FSW process parameters on predicted residual stresses: rotational speeds at a) and d) 500 RPM, b) and e) 1000 RPM, c) and f) 1500 RPM.

4. Conclusions

This study highlighted the following points. *i)* The difference between the predicted and measured temperatures was negligible. This confirmed the relevance of the inverse method for calibrating the thermal model and the usage of two different heat transfer coefficients between the Al plate and the backing anvil. The heat transfer coefficient under the FSW tool was higher than over the rest of the plate in order to consider the better contact due to the tool pressure. *ii)* The prediction of the longitudinal residual stress was in good agreement with the measurements performed by neutron diffraction validating the model assumptions: no modelling of the material flow around the FSW tool and a top hat profile for the heat source. However, improvements are needed in the mechanical analysis to take into account a more realistic clamping situation. *iii)* As compared to rotational speed, the welding speed has a greater influence on the temperature and residual stress.

References

- [1] L. Dubourg, A. Merati, M. Gallant, M. Jahazi, *Manufacturing of aircraft panels by friction stir lap welding of 7075-T6 stringers on 2024-T3 skin: process optimisation and mechanical properties*, in: 7th International Symposium on Friction Stir Welding, Awaji Island, Japan, May 20-22, 2008.
- [2] C. Mandache, L. Dubourg, A. Merati, M. Jahazi, *Materials Evaluation*, 66 (4) (2008) 382-386.
- [3] B. J. Dracup, W. J. Arbogast, *Friction stir welding as a rivet replacement technology*, in: Automated Fastening Conference & Exposition, Nashville, TN, USA, October, 1999.
- [4] A. Murphy, M. Price, P. Wang, *The Integration of Strength and Process Modeling of Friction-Stir-Welded Fuselage Panels*, in: 46th AIAA/ASME/ASCE/AHS/ASC Structures, Structural Dynamics, and Materials Conference, Austin, TX, USA, pp 1-15, April 18-21, 2005.
- [5] P. Michaleris, J. Dantzig, D. Tortorelli, *Welding Journal*, 78 (11) (1999) 361-s-366-s.
- [6] J. H. Root, J. Katsaras, J. F. Porter, B. W. Leitch, *Trans. ASME*, 124 (2002) 366-370.
- [7] Z. M. Hu, P. B. J. W. Brooks, *Residual Stresses in Friction Stir Welding*, in: 2004 ABAQUS Users' Conference, 2004.
- [8] Z. W. Feng, X.L.; David, S.A.; Sklad, P.S., *Science and Technology of Welding & Joining*, 12 (4) (2007) 348-356.
- [9] T. De Vuyst, L. D'Alvise, A. Simar, B. De Meester, S. Pierret, *Welding in the World*, 49 (3-4) (2005) 47-55.
- [10] H. W. Zhang, Z. Zhang, J. T. Chen, *Materials Science and Engineering A*, 403 (1-2) (2005) 340-348.
- [11] M. Prime, T. Gnaupel Herold, J. Baumann, R. Lederich, D. Bowden, R. Sebring, *Acta Materialia*, 54 (15) (2006) 4013-4021.
- [12] M. Peel, A. Steuwer, M. Preuss, P. J. Withers, *Acta Materialia*, 51 (16) (2003) 4791-4801.
- [13] M. H. Khandkar, J. Khan, A. P. Reynolds, M. A. Sutton, *Journal of Materials Processing Technology*, 174 (1-3) (2006) 195-203.