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# INTEGRATED MODELLING OF CRACK PROPAGATION IN AA2024-T3 FSW BUTT JOINTS CONSIDERING THE RESIDUAL STRESSES FROM THE MANUFACTURING PROCESS

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## ABSTRACT

This paper deals with numerical and experimental investigation on the influence of residual stresses on fatigue crack growth in AA2024-T3 friction stir welded butt joints. An integrated FEM-DBEM procedure for the simulation of crack propagation is proposed and discussed. A numerical FEM model of the welding process of precipitation hardenable AA2024-T3 aluminum alloy is employed in order to predict the induced residual stress field. The reliability of the FEM simulations with respect to the induced residual stresses is assessed comparing numerical outcomes with experimental data obtained by means of the contour method. The computed stress field is transferred to a DBEM environment and superimposed to the stress field produced by a remote fatigue traction load applied on the friction stir welded cracked specimen. Numerical results are compared with experiments showing good agreement and highlighting the predictive capability of the proposed method. Furthermore, the influence of the residual stress distribution on crack growth is evidenced.

## INTRODUCTION

Friction stir welding (FSW) is an efficient solid state welding technique used for joining, for example, high strength aluminum alloys, as well as dissimilar materials, which are difficult to weld with traditional welding techniques [1]. In the FSW process, heat is generated by the friction between the tool shoulder and the work piece surfaces, and by the plastic deformation induced in the work piece. The material under the tool is heated up with consequent expansion; however it is partly constrained by the relatively colder material surrounding this region. Subsequently, the material starts yielding in compression and plastic deformation starts to develop. This fundamental mechanism of residual stresses evolution is very similar in all kinds of welding techniques whether are they fusion or solid state welding. Besides the thermal gradients due to non-uniform heat generation, mechanical

boundary conditions (i.e. clamping, contact conditions between work piece and the anvil, etc.) also plays a role for promoting plastic strains, hence residual stresses. Although the level of residual stresses resulting from the FSW process in aluminum alloys have shown to be lower as compared to traditional welding techniques [2], they still play a major role in fatigue and buckling behavior of FSW structures [3-5]. In order to understand and control the evolution of residual stresses, much work on FSW process modelling has been reported in literature. In thermo-mechanical modelling of FSW, the most convenient assumption is to neglect the material flow during welding. This results in semi-coupled thermo-mechanical models in a Lagrangian frame, where the thermal field is calculated prior to the mechanical field by separating the two analyses. Recently, Sonne et al. [6] presented a thermo-mechanical model applying the Myhr and Grong metallurgical model to study the effect of hardening laws and softening on residual stresses in AA2024-T3.

A deeper understanding of fatigue behavior of FSW assemblies is also required. In this sense some results have already been presented in the inherent literature. The slower crack propagation in the FSW material with respect to the base material was highlighted in [7,8] and related to microstructure, microhardness, and residual stresses. In this paper a numerical investigation on the influence of residual stresses, induced by the friction stir welding process, on fatigue crack growth in AA2024-T3 butt joints has been proposed. The computational approach is based on the coupled usage of finite element method (FEM) and dual boundary element method (DBEM). In particular, the thermo-mechanical model proposed by Sonne et al. in [6] is used to predict the residual stress state in the butt-welded aluminum plates. The computed residual stress field is then superimposed to the stress field produced by a remote fatigue traction load and crack propagation is simulated by the dual boundary element method (DBEM) in an automatic way. A two-parameter crack growth law is used for the crack propagation rate assessment [9]. The DBEM code BEASY and the FEM code ABAQUS are coupled in the aforementioned numerical approach by an in-house developed routine.

## THERMOMECHANICAL MODEL

The thermal model for the heat generation in FSW proposed by Schmidt and Hattel [10] is applied. The heat generation is expressed as a surface heat flux from the tool shoulder (without the tool probe) and is a function of the tool radius and the temperature dependent yield stress as follows

$$\frac{\dot{q}_{surface}}{A}(r, T) = \omega r \tau(T) = \left(\frac{2\pi n}{60}\right) r \frac{\sigma_{yield}}{\sqrt{3}} \quad (1)$$

where  $n$  is the number of tool revolutions per minute,  $r$  is the radial position originating from the tool center,  $\tau$  is the shear stress and  $\sigma_{yield}$  is the temperature dependent yield stress. The heat source is used as a boundary condition at the interface between the shoulder and the work piece for the closure of the heat conduction equation

$$\rho c_p \frac{\partial T}{\partial t} = \frac{\partial T}{\partial x} \left( k \frac{\partial T}{\partial x} \right) + \frac{\partial T}{\partial y} \left( k \frac{\partial T}{\partial y} \right) + \frac{\partial T}{\partial z} \left( k \frac{\partial T}{\partial z} \right) + \dot{Q}_{gen}''' \quad (2)$$

where  $\rho$  is the material density,  $c_p$  is the specific heat capacity,  $T$  is the temperature,  $k$  is the thermal conductivity and  $\dot{Q}_{gen}'''$  is the volumetric heat source term. In this model the latter is set to zero as all the heat generation is applied as a boundary condition in terms of a surface heat flux. For calculation of the transient as well as the residual stress field in the work piece, a standard mechanical model based on the solution of the three static force equilibrium equations is used, i.e.

$$\sigma_{ij,j} + p_j = 0 \quad (3)$$

being  $p_j$  the body force at any point within the plate and  $\sigma_{ij}$  the stress tensor. Hooke's law and linear decomposition of the strain tensor, as well as small strain theory, are applied together with the expression for the thermal strain [11]. The plastic strain evolution is based on the standard  $J_2$  flow theory with a temperature dependent von Mises yield surface. According to Sonne et al. [6] isotropic hardening behaviour is the most suitable for modelling the mechanical behaviour of AA2024-T3 when combined with a softening model. The microstructural evolution of the material during FSW is for Al-alloys in T3 condition expected to have a considerable effect on the residual stress distribution. In this study, the softening model developed by Myhr and Grong [12] is used to predict the phase transformation during welding as also proposed by Richards et al. in 2008 [13]. This model only account for the dissolution of GPB (Guinier-Preston-Bagaryatsky) zones in the HAZ with simple analytical equations that can be introduced in the FE code ABAQUS. The model relates the fraction of dissolved hardening precipitates  $X_d$  to the equivalent time of heat treatment,  $t_{eq} = t/t^*$  (where  $t$  is the period of time at a temperature  $T$  and  $t^*$  is the time for total precipitation dissolution at this temperature) as follows

$$\frac{f}{f_0} = 1 - X_d = 1 - t_{eq}^n = 1 - \sqrt[n]{t_{eq}} \quad (4)$$

$$t_{eq} = \sum_{i=1}^{N_{total}} \frac{\Delta t_i}{t_i^*} = \sum_{i=1}^{N_{total}} \frac{\Delta t_i}{t_{ref} \exp \left[ \frac{Q_{eff}}{R} \left( \frac{1}{T_i} - \frac{1}{T_{ref}} \right) \right]} \quad (5)$$

where  $t_{ref}$  is the time for total dissolution at the reference temperature  $T_{ref}$ ,  $R$  is the gas constant and  $Q_{eff}$  is the effective energy for precipitate dissolution. The fraction of hardening precipitates  $f/f_0$  then relates to the equivalent time  $t_{eq}$  via the fraction of dissolved precipitates  $X_d$  as shown in Eq. (4), where  $n$  is a material constant which is obtained experimentally. A

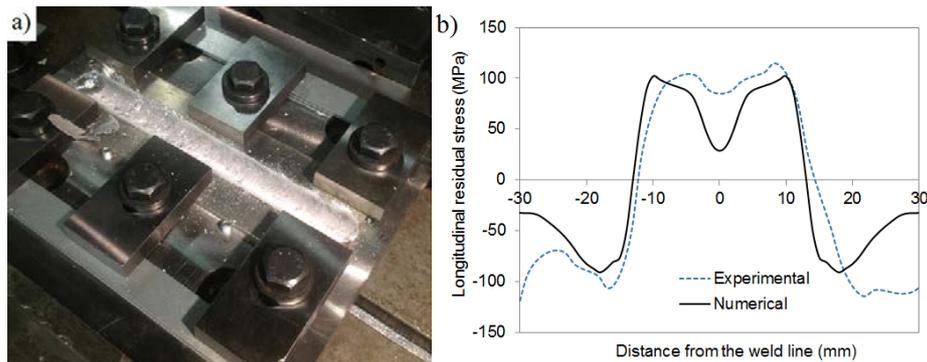
value of 0.5 is often used as indicated in the last part of Eq. (4). The yield stress is then predicted via a linear interpolation between original state and the fully dissolved state

$$\sigma_y = \left( \sigma_{y_{max}} - \sigma_{y_{min}} \right) \frac{f}{f_0} + \sigma_{y_{min}} \quad (6)$$

$\sigma_{max}$  being the yield stress of the material in the original T3 condition and  $\sigma_{min}$  the yield stress of the fully dissolved material. The yield stress at the instantaneous temperature is found by interpolation between the upper and lower bound yield stress curves in proportion to  $X_d$ . The upper and lower yield stress curves for AA2024-T3, is found in the original paper by Sonne et al. [6].

## WELDING SETUP AND MODEL VERIFICATION

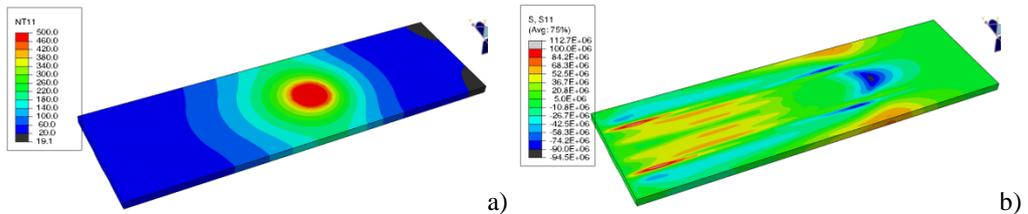
Rolled AA2024-T3 aluminium plates were joined by FSW using an AISI1040 quenched steel tool. The used tool consisted of a 20 mm diameter shoulder with a conical pin, characterized by the following dimensions: height of 3.80 mm, larger diameter of 6.20 mm and cone angle of 30°. The tilt angle and tool shoulder penetration were defined as 2° and 0.2 mm. The residual stress scenario used in the present analysis was obtained by the welding parameters of an angular tool velocity of 1400 rpm and a linear velocity of 70 mm/min. Residual stress analysis was performed adopting the contour method [14]. In Fig. 1a the welding setup is shown, while in Fig. 1b numerical and experimental residual stresses are compared. Please note that the validation of the temperature calculation is provided elsewhere [11].



**Fig. 1** a) Welding setup. b) Comparison of longitudinal residual stresses from experiment and simulation.

For the contour method in this particular case, the welded specimen was sectioned at mid-length and orthogonally to the weld line by a wire electrical discharge machining process. Out of plane displacements of the sectioned surfaces were recorded by means of a coordinate measuring machine. Experimental data were then imported, averaged and fit to a

unique smoothing surface in MATLAB. The measured and digitalized out-of-plane displacements were used, with reversed sign, as input nodal boundary conditions in an elastic FE model of the cut sample, initially assuming a block shaped geometry [14]. Additional constraints were imposed in order to prevent rigid body motion. From the simulation it is possible to get the transient temperature field during welding (Fig. 2a) and via the subsequent stress analysis the stress evolution in the welded plate is calculated (Fig. 2b). In Fig. 2b some lines in compression in front of the moving heat source and in tension behind the moving heat source are observed. These stresses are a result of the clamping conditions of the plate, where constraining prevents the plate in this area from thermal expansion (applied as spring elements).



**Fig. 2** a) Temperature field from the heat transfer analysis after 100 s of welding. b) The resulting longitudinal stress component from the stress analysis at the same time step.

For thermo-mechanical modelling it is very important to have a good description of the temperature fields in the heat transfer analysis before the subsequent mechanical analysis, as the temperatures are both the direct and indirect driving force for the stresses through thermal expansion and the metallurgical changes in the aluminium. When comparing the measured and simulated longitudinal residual stresses, the well-known M-shape is observed (see Fig. 1b), meaning that the stresses are lower close to the weld centreline than the outer shoulder radius of the tool. For this type of age-hardenable aluminium alloys, this characteristic shape of the cross sectional longitudinal residual stresses has been observed by several authors [16,17].

## CRACK PROPAGATION

The stress field computed using the FEM model was then transferred to a DBEM environment (BEASY) and superimposed to the stress field produced by a remote fatigue traction load applied on the friction stir welded specimen. A linear notch was created on the specimen to promote a preferred crack starting location. Simulation parameters were defined in order to reproduce the experimental test as described in [18,19]. The experimental tests were performed using an INSTRON 8500 fatigue machine at room temperature, according to the following testing parameters: frequency 10 Hz, maximum load 24 kN and stress ratio 0.1. The crack growth was monitored by crack gauges (Fig. 3a). The following two parameter model [9,18,19] was employed to describe the crack growth:

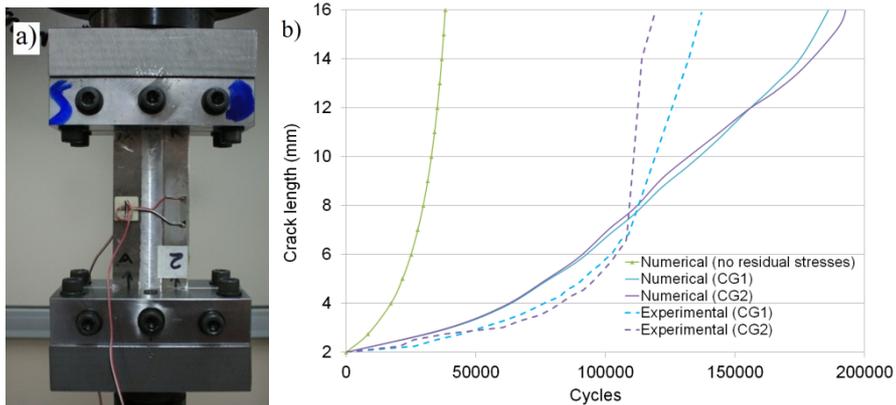
$$\frac{da}{dN} = A(\Delta K - \Delta K_{th}^*)^n (K_{max} - K_{max,th}^*)^m \quad (6)$$

being  $K_{min}$  and  $K_{max}$  the minimum and maximum Stress Intensity Factor (SIF);  $\Delta K = K_{max} - K_{min}$ ;  $\Delta K_{th}^*$  and  $K_{max,th}^*$  SIF thresholds;  $A$ ,  $n$ , and  $m$  material parameters (Table 1).

**Table 1** Parameters for the crack propagation model

$\Delta K_{th}^* (N/m^{3/2})$	$K_{max,th}^* (N/m^{3/2})$	$A (m^{1.5^{(n+m)+1}}/N^{n+m})$	$n$	$m$
1834121	3352014	6.745E-23	1.65	0.56

Residual stresses affect crack propagation since they change the effective value of the total SIF at the crack tip, with both the minimum and the maximum SIF values generally affected in the same way, so as to leave unchanged the parameter  $\Delta K$ . Consequently, the primary effect of residual stresses on crack growth rates is related to the  $K_{max}$  variations rather than to the  $\Delta K$  variations. Since residual stress effects manifest primarily through a variation in  $K_{max}$  levels, an arrest in crack growth can occur if these stresses are compressive and sufficiently high to make the overall  $K_{max}$  falling below  $K_{max,th}^*$ . The friction stir welding effects are reproduced by taking into account the residual stress influence on the driving parameters  $\Delta K$  and  $K_{max}$ . According to the used procedure, residual stresses, imported from the FEM code, are modelled by a distribution of tractions applied on the evolving crack faces.



**Fig. 3** a) Experimental setup for crack propagation test. b) Comparison between numerical and experimental crack advances vs. number of cycles.

The reported plot (Fig. 3b) clearly indicates the influence of residual stresses on the crack propagation rates: indeed, if the initial notch is far enough from of the weld line, the compressive stresses on the side of the weld slow down the crack growth. On the other hand,

faster crack propagation was predicted using the same model and simulation parameters, but neglecting residual stresses. The discrepancy between the numerical crack growth, calculated without allowing for residual stresses, and the experimental crack length, measured by the crack gauges, highlights the relevance of the process induced stresses on the in-service behaviour of friction stir welded assemblies in presence of dynamic loads.

## CONCLUSION

From the results and discussions presented above, following conclusions can be drawn:

- The developed thermomechanical model was shown to predict the longitudinal residual stresses compared to experimental measurements applying the contour method
- It is shown that the residual stresses significantly affects the fatigue behaviour of friction stir welded assemblies, where the compressive longitudinal stresses at the area of the initial notch slows down the crack propagation.
- The presented FEM-DBEM approach can be considered as a powerful tool for predicting the crack growth with the presence of residual stresses induced by the manufacturing processes.

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