

Viewpoint Paper

Thermal modelling of friction stir welding

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Received 4 July 2007; revised 4 October 2007; accepted 8 October 2007

Available online 7 November 2007

Abstract—The objective of the present work is to present the basic elements of the thermal modelling of friction stir welding as well as to clarify some of the uncertainties in the literature regarding the different contributions to the heat generation. Some results from a new thermal pseudomechanical model in which the temperature-dependent yield stress of the weld material controls the heat generation are also presented.

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Keywords: Friction stir welding; FEM; Heat generation; High-temperature deformation; Aluminium alloys

Thermal modelling has since the late 1990s been a central part of the modelling of friction stir welding (FSW) in general [1–17]. One reason for this is that many of the properties of the final weld are a direct function of the thermal history of the workpiece. Furthermore, the FSW process itself is highly affected by the heat generation and heat flow. From a modelling viewpoint, thermal modelling of FSW can be considered the basis of all other models of the process, be these microstructural, computational fluid dynamics (CFD) or thermomechanical. In the FSW process the welding parameters are all chosen such that the softening of the workpiece material enables the mechanical deformation and material flow. However, unlike many other thermomechanical processes, the mechanisms of FSW are fully coupled, i.e. the heat generation is related to material flow and frictional/contact conditions and vice versa. Thus, in theory, a thermal model alone cannot predict the temperature distribution/history without prior knowledge of the heat generation, since the fundamental mechanisms of FSW are not part of a pure thermal model. For this reason, several analytical expressions have been given in the literature for the heat generation as a function of tool geometry and welding parameters, e.g. tool radius and rotational speed. These models have been further developed during the last decade, and they have now matured to such a state were it is appropriate to evaluate the use of such “analytical models” for the heat generation in FSW.

The main unknown parameters in these expressions are either the friction coefficient under the assumption of sliding and the material yield shear stress under the assumption of sticking. There are two options for obtaining these parameters: they can either be measured or assumed. Until now the latter strategy has been used; however, it might now be time to measure these key values experimentally. When doing this, one problem is that there are few (if any) suitable laboratory procedures, simply because the range of either slip rates (order of 1 m s^{-1}) or strain rates (order of 1000 s^{-1}) in combination with the wide temperature range (from room temperature to solidus temperature) calls for an enormous experimental setup. One could argue that the only way to evaluate FSW experimentally is to use the process itself. In practice, this is what has been done during experiments where tool forces and torque have been measured, from which the average friction coefficient and yield shear stress are estimated.

In the present work, a preliminary result from a new procedure for modelling the heat source in FSW is presented. The method is based on a high degree of knowledge regarding the plastic behaviour of the workpiece material at elevated temperature, i.e. that the yield stress dramatically decreases once the temperature approaches the solidus temperature, above which the material acts as a fluid. As a consequence, the material close to the tool/matrix interface will reduce its heat generation to negligible values if it exceeds the solidus temperature – reducing the temperature level – allowing the material to recover its strength. A self-stabilizing effect will thus establish at a temperature level below

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the solidus temperature. However, unlike material laws used in flow models, the strain rate dependence is not included in the model. The model can be considered a thermal pseudomechanical description in which the temperature-dependent yield stress is the driver for the heat source (see Schmidt and Hattel [18] for more details).

In all thermal models of FSW the main task is to solve the heat conduction equation with an appropriate set of initial and boundary conditions, i.e.

$$\rho c_p \dot{T} + u_i \rho c_p T_{,i} = (k T_{,i})_{,i} + \frac{Q_{\text{gen}}}{V}, \quad (1)$$

where $\frac{Q_{\text{gen}}}{V}$ is the volumetric heat source term (W m^{-3}) arising from plastic dissipation. However, in most pure thermal models of FSW, as in the present work, the heat generation from both frictional and plastic dissipation is modelled via a surface flux boundary condition at the tool/matrix interface. This is primarily because modelling the plastic dissipation as a volumetric source in Eq. (1) requires detailed information about the plastic strain rates and the deviatoric stresses, and hence calls for a full thermomechanical or CFD model. Having said that, it is obvious that the description of the heat source is the basis of any thermal model of FSW.

The generally adopted equation for the total heat generation is [13]:

$$\begin{aligned} Q_{\text{total}} &= \delta Q_{\text{sticking}} + (1 - \delta) Q_{\text{sliding}} \\ &= \frac{2}{3} \pi \omega [\delta \tau_{\text{yield}} + (1 - \delta) \mu p] \left[(R_{\text{shoulder}}^3 - R_{\text{probe}}^3) \right. \\ &\quad \left. (1 - \tan \alpha) + R_{\text{probe}}^3 + 3 R_{\text{probe}}^2 H \right], \end{aligned} \quad (2)$$

where δ is the contact state variable (dimensionless slip rate), τ_{yield} is the material yield stress at the welding temperature, μ is the friction coefficient, p is the uniform pressure at the contact interface, ω is the angular rotation speed, α is the cone angle, R_{shoulder} is the shoulder radius, R_{probe} is the probe radius and H_{probe} is the probe height.

However, when implementing this into a numerical model using a position-dependent surface flux (W m^{-2}), it is typically used in the following form [14]:

$$q_{\text{total}} = \dot{\gamma} \tau_{\text{friction}} + (\omega r - \dot{\gamma}) \tau_{\text{yield}} = \omega r [\delta \tau_{\text{yield}} + (1 - \delta) \mu p], \quad (3)$$

which in fact is the basis for deriving Eq. (2). Furthermore, when combining Eqs. (2) and (3) and assuming the simple tool geometry of a flat shoulder only, one obtains the following well-known expression for the heat generation (W m^{-2}) (see e.g. Ref. [14]):

$$q_{\text{total}} = \frac{3 Q_{\text{total}} r}{2 \pi R_{\text{shoulder}}^3}, \quad (4)$$

which can be applied as a radius-dependent surface flux in the model, under the assumption of a constant contact condition close to sliding or in cases of sticking where the shear layer is very thin.

From Eq. (4) it can be seen that Q_{total} can be considered an input parameter in the same manner as the friction coefficient, the pressure and the material's yield

shear stress in Eq. (2). However, it should be mentioned that having Q_{total} as one of the input parameters for the model could in some situations conflict with the very objective for thermal modelling of FSW [12]. This is especially the case when the model predicts temperatures for conditions not supported by measurements of the heat input Q_{total} . In these cases it is not straightforward to predict or simulate the effect of, for example, a change in welding or rotational speed, because the total heat generation Q_{total} is a function of these changes in parameters and hence can be considered an internal function of the FSW process, unlike other welding process, e.g. TIG, where the heat input is controlled externally.

This dilemma can be overcome by supporting the thermal model with a thermomechanical or CFD model which “includes” the underlying physics of the process, namely the material flow producing heat generation by plastic dissipation in the shear layer and frictional contact at the tool/workpiece. Such a procedure with a sequential coupling of thermal and “material flow and heat generation” models has been presented by Colegrove [19]. Fully coupled thermal-mechanical or CFD models have proven to be able to predict the heat generation as part of the solution [20–30]. Such simulations are highly demanding from both a computational and a human resource point of view, thus leaving pure thermal modelling of FSW as the best “simple” alternative (being non-comprehensive and CPU-efficient) for simulation of heat flow, bearing in mind the limitation of such models, which as mentioned earlier primarily lie in the evaluation of Q_{total} .

Having said that, it should be noted that the procedure most used for evaluating Q_{total} is to rely on experimental findings by simply performing the actual welds and measuring Q_{total} with a dynamometer, thereby accepting the inherent limitation of the resulting thermal model to predicting only temperatures for a known total heat generation.

Ref. [14] presented a proposal for a classification of different heat sources. One characteristic is how “detailed” the resolution of tool heat generation is. Three levels were evaluated: (i) including shoulder heat generation, without probe heat generation; (ii) including shoulder and probe heat generation, with probe material; and (iii) shoulder and probe heat generation but without probe material.

A second characteristic was whether the convective contribution due to the material flow in the shear layer was taken into account. Two extreme contact conditions were evaluated: full sliding and full sticking. In the case of sliding, the heat was applied as a surface flux, and in the case of sticking, the heat was applied as a volume flux in a shear layer. This shear layer was prescribed analytically assuming a uniform thickness with a linear velocity profile ramped between the tool velocity at the contact interface and the welding velocity outside the shear layer.

It was concluded that in order to analyse the temperature field in the volume under the tool, special attention should be paid to how the heat generation and the material “flows” around the tool probe. The main effect of the probe is to change the material properties to those of the tool. Another approach is to rotate the probe

and model the shear layer around it such that it resembles a flow model.

One new procedure is to couple the traditional analytical expression for the heat generation with a constraint based on experimental or phenomenological considerations. As earlier mentioned, a self-stabilizing effect will be established at a temperature level below the solidus temperature, hence this could be used as an average temperature constraint. In the thermal model of Tutum et al. [17] the heat generation in FSW of AA2024 is controlled using an optimization scheme such that an average temperature of 500 °C at the tool/workpiece interface is obtained.

This procedure has given promising results for temperature fields without prior knowledge regarding Q_{total} – in fact this is an output of the optimization analysis.

When suggesting alternative approaches for modelling the heat source as compared to those previously presented for thermal modelling of FSW, the assumptions under which the previous models were derived can be summarized as follows:

- for sliding: uniform pressure distribution and friction coefficient, independent of temperature and slip/strain rate;
- for sticking: uniform yield stress, independent of temperature and slip/strain rate.

Only under these assumptions can the heat generation be expressed analytically. None of these assumptions apply during the actual conditions in FSW; the assumption of independence of temperature in particular could be one of the reasons why the previous analytical expressions only capture to some extent the “real thermal physics” of the process. So, in essence, the limitation of the previous pure thermal models is that they do not account for the non-uniform thermomechanical conditions at the interface between tool and workpiece. A way to overcome this without doing full thermomechanical or CFD calculations is to use a novel “thermal pseudomechanical” formulation.

On the local scale, the heat generated due to frictional dissipation at the interface together with the heat generated in the shear layer due to plastic dissipation is for convenience treated as one combined heat generation, q_{total} . This heat generation induces a heat flow both into the workpiece and into the tool which is dependent on the welding conditions such as the material properties of the components and the controlling thermal boundaries (due to geometries such as tool flaps, tool shaft length, size of backing plate, thermal resistance between workpiece and backing plate, etc.).

To describe this, it is convenient to define the local thermal efficiency $\eta_{\text{workpiece}}^{\text{local}}$ as the ratio between the heat flow into the workpiece relative to the total local heat generation at a specific location under consideration, i.e.

$$\eta_{\text{workpiece}}^{\text{local}} = \frac{q_{\text{workpiece}}}{q_{\text{total}}}. \quad (5)$$

Similarly, on the global scale, the workpiece thermal efficiency of the process can be expressed by

$$\eta_{\text{workpiece}}^{\text{global}} = \frac{Q_{\text{workpiece}}}{Q_{\text{total}}}. \quad (6)$$

This parameter is often used in models where the tool is not enmeshed, and its value is most often estimated by inverse modelling. However, there is no obvious reason for concluding that the same relation holds for every segment at the interface, i.e. $\eta_{\text{workpiece}}^{\text{local}}$ will in general vary over the interface.

The distribution between the heat generated by frictional dissipation and plastic dissipation can also be evaluated both at a local scale and at a global scale, where the former refers to a specific location at the interface and the latter to the total heat generation.

The plastic dissipation work in the shear layer can be interpreted as a local “plastic surface heat flux” (W m^{-2}) given by [18]

$$q_{\text{plastic}} = \delta \omega r \tau_{\text{yield}}, \quad (7)$$

and the surface heat generation due to frictional dissipation is inherently a surface flux given by

$$q_{\text{friction}} = (1 - \delta) \omega r \tau_{\text{friction}}. \quad (8)$$

When evaluating the large-scale heat generation, meaning the total heat generation produced at the interface and in the surrounding shear layer, further assumptions could be made to enable an analytical expression. By assuming that the contact condition is uniform at the interface and also that τ_{contact} is independent of temperature and the slip rate/strain rate, the following global scale quantities hold:

$$\begin{aligned} Q_{\text{friction}} &= (1 - \delta) Q_{\text{total}} \\ Q_{\text{plastic}} &= \delta Q_{\text{total}}, \end{aligned} \quad (9)$$

which also are a consequence of Eq. (2).

When developing a thermal model of FSW (see Fig. 1a), a choice of reference frame must be made, i.e. whether the material follows the mesh (Lagrangian) or whether it flows through the mesh (Eulerian). A solution can be time dependent (transient) or time independent (steady-state). There has been a tendency towards using transient solutions with the Lagrangian reference frame and the steady-state solutions with the Eulerian reference frame; however, this does not necessarily have to be the case.

Figure 1b shows some schematic views of the implementation of a traditional radially dependent heat flux applied in the Eulerian reference frame as a “stationary heat source” with the material flowing through the mesh controlled by Eulerian inflow and outflow boundary conditions. In Figure 1c, the Lagrangian reference frame is shown with a heat source moving along the weld line, starting at one end of the workpiece and finishing at the other.

The Eulerian reference frame allows for a high mesh resolution near the heat generation, and it is often called a local model. A model using the Lagrangian reference frame often includes the whole geometry along the joint-line, thereby capturing both the starting and finishing periods. A lower mesh resolution is expected and a correspondingly less detailed heat source is normally used. Such a model captures the overall thermal response, and is often called a global model.

In order to characterize to what extent the geometrical components of the model are actually enmeshed, the

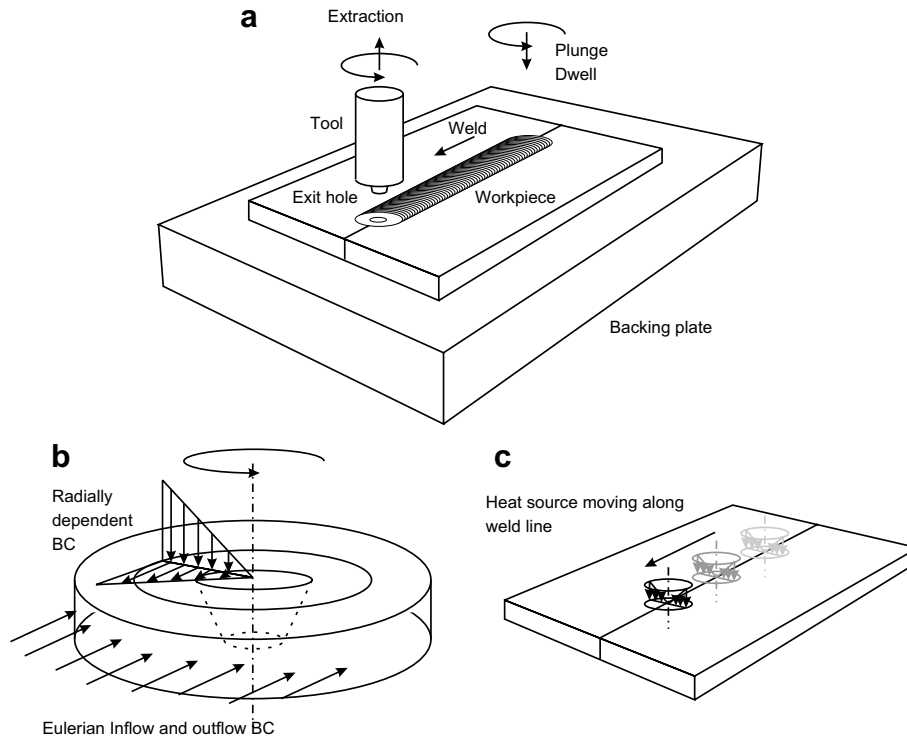


Figure 1. Schematic view of different reference frames. (a) Schematic view of FSW setup; (b) Eulerian reference frame used for FSW. Shown is the radially dependent heat flux, as well as a prescribed velocity field if the convective contributions of either the tool or shear layer flow are taken into account; (c) Lagrange reference frame used for modelling FSW with moving heat source.

term “enmeshed modelling level” is introduced. The enmeshed modelling levels have two directions, i.e. in-plane and out-of-plane. The in-plane level denotes whether the model is one-sided or includes the geometries on both sides of the joint line. In thermal modelling of FSW, the joint line is often treated as a line of symmetry, whereas in CFD/computational statistical mechanical (CSM) modelling of heat generation and material flow [19–30], the material flows across the joint line, hence making the problem non-symmetrical and requiring both sides to be enmeshed. When accounting for phenomena caused by the rotation of the tool, e.g. the convective contribution due to the material flow within the shear layer or tool shaft, the thermal model should include both sides of the geometries.

The enmeshed modelling level in the out-of-plane direction characterizes whether, for example, the tool, tool holder, machine head and clamping device in the upward direction are included and whether, for example, the backing plate and support table are included in the downward direction.

The enmeshed part of a model governs the memory requirements for solving the problem, hence a good modelling approach is to only enmesh those parts of the model that are “important” with a high mesh resolution, which in most cases of FSW is around the tool.

Including both the tool and backing plate together with the workpiece will in most cases exceed the memory capabilities of a normal PC. A choice has to be made between which components are included in the model and which are neglected (not enmeshed).

Figure 2 shows a schematic view of different enmeshed modelling levels starting with three levels in the out-of-plane direction including tool, workpiece and backing plate (see Fig. 2a), reducing down to one level, i.e. workpiece only (Fig. 2c). In the in-plane direction, Figure 2d shows an example of a one-sided model (corresponding to one level in the in-plane direction), whereas the previous three cases include both sides corresponding to two levels in the in-plane direction. The combination between the choice of reference frame and degree of enmeshed modelling level offers a huge

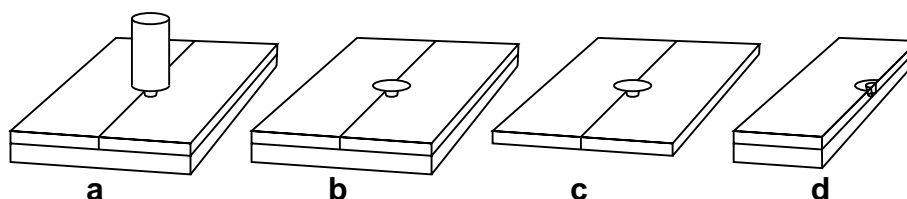


Figure 2. Schematic view of different enmeshed modelling levels. (a) Workpiece, backing plate and tool; (b) Workpiece and backing plate; (c) Workpiece; (d) One side of workpiece and backing plate.

range of possibilities, and the “correct” choice depends on the objective of each model. As an example, a residual stress model would normally need a temperature field coming from a Lagrangian transient thermal model [1,11,15]. The model presented in the following is a Eulerian, steady-state model with an enmeshed modelling level equivalent to that shown in Figure 2a.

As discussed earlier, the heat generation at a material segment depends on the material flow stress for the given temperature, strain and strain rate. The contact stress must be in equilibrium with the material yield shear stress of the underlying material during steady-state conditions. This also holds for contact conditions close to sliding, because for just the slightest degree of sticking, i.e. $\delta \sim 0 \ll 1$ (see Ref. [1] for definition), there will be plastic deformation at the actual interface, and hence equilibrium between the contact/frictional stress and the material response must prevail. For steady-state conditions, this leads to the following relationship:

$$\tau_{\text{friction}} = \tau_{\text{contact}} = \tau_{\text{yield}}, \quad (10)$$

which holds for every location at the actual interface.

In the thermal pseudomechanical model, the heat generation is described as a surface flux given by

$$q_{\text{total}} = \omega r \tau(T), \quad (11)$$

where $T = T(x, y, z)_{\text{interface}}$ is the non-uniform temperature at the contact interface. Comparing this expression with Eq. (3) underlines the fact that the entire heat generation is modelled as a surface flux. See Ref. [18] for more details on the model.

The temperature-dependent heat source given by Eq. (11) is implemented in a Eulerian model that, in addition to the workpiece, includes the tool and the backing plate. The thermal model is developed in Comsol 3.3 [31] and is based on an experimental weld of 7075 T6 instrumented with thermocouples. The welding conditions are given in Ref. [18], and the thermal properties are taken from Refs. [31,32]. The experimentally found values for the maximal yield stress of friction stir processed (FSP) Al 7075 [33] at strain rates of $\dot{\epsilon} = 10^{-3} \text{ s}^{-1}$ are used for the shear yield stress in Figure 3. A total of 100,000 degrees of freedom in the model are solved for.

Figure 4 shows the model results for the local heat generation $q = \omega r \tau(T)$ evaluated along the intersection

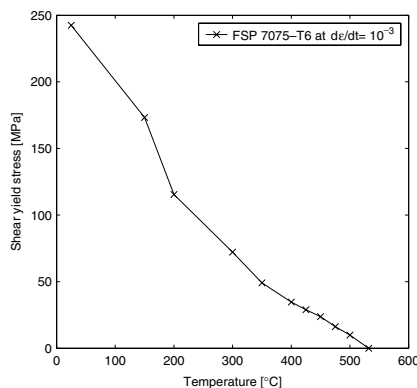


Figure 3. Shear yield stress derived from Ref. [32].

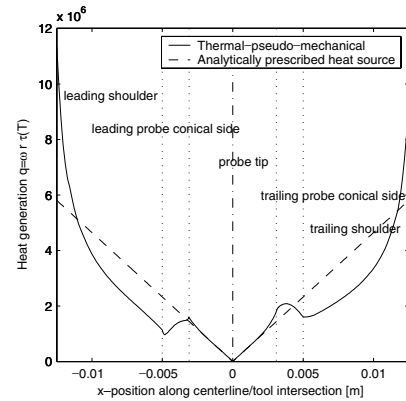


Figure 4. Local heat generation $q = \omega r \tau(T)$ evaluated along intersection between tool and centreline. Q_{total} in the analytically prescribed heat source model obtained from the thermal pseudomechanical model.

between the tool and the joint-line. The temperature-dependent heat generation denoted by (—) is the result of the thermal pseudomechanical model and this is compared with the analytically prescribed, linearly dependent heat generation denoted by (---); the latter was evaluated using the same total heat generation as used in the former. In the interval under the probe tip, the heat generation is close to linear since the temperature is nearly constant in this area. The exponentially increasing heat generation at both the leading and trailing shoulder regions is a combination of the ωr -term and the increase in yield shear stress due to the decrease in temperature for larger radii (here shown as x -values).

The total heat generated at the tool/matrix interface is found to be 1909 W (83% from the shoulder, 16% from the conical probe sides, 1% from the probe tip). Moreover, the global thermal efficiency is found to be 88%. Figure 5 shows a comparison between the experimentally measured temperature profiles and the modelling results. The good correlation between the profiles is obtained by adjusting only the heat transfer coefficients/contact resistances. Most important is the coupling between the workpiece and the backing plate.

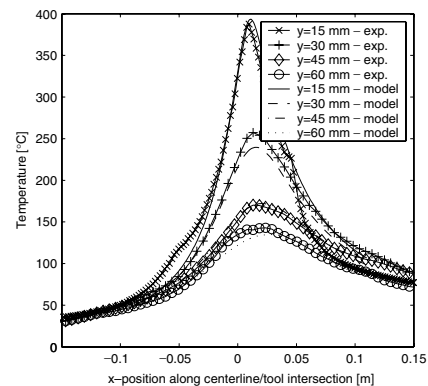


Figure 5. Comparison between the thermal pseudomechanical model and experimental results for the far-field temperature profiles at y -values of 15, 30, 45 and 60 mm.

To summarize, this is the first attempt to develop a thermal model where the total heat generation is not an input parameter, but is actually a result of the model itself.

The CPU-time required for the thermal pseudomechanical model is twice that of using an analytically prescribed heat generation owing to the increased non-linearity.

The good agreement between the analytically prescribed heat source model and the thermal pseudomechanical model in Figure 4 is only achievable when Q_{total} has been previously obtained from the latter before being inserted into the former.

In the past decade the FSW community has been presented with many types of models aimed at describing different aspects of FSW. In general, the complexity of these models has increased; however, the question is whether they give “enough” new information for the computational effort they require. The present model is a step back relative to fully coupled thermomechanical models, but it is an improvement over thermal models, in the sense that it captures the first-order effect arising from the self-controlling mechanism due to thermal softening. We need faster and simpler models that can be used as effective alternatives to more comprehensive models, e.g. CFD and fully coupled thermomechanical models. Together they constitute the basis for further understanding of FSW based on modelling. The critical need is for additional knowledge regarding the material response for different alloys, which should be an alternative to investigation of frictional behaviour.

In the present work the basic elements of the thermal modelling of FSW are briefly outlined, and it is pinpointed that the modelling of the heat generation between the workpiece and the tool is crucial for any thermal model of FSW. As a natural consequence of this, a new thermal pseudomechanical model in which the temperature yield stress of the weld material is the driver for the heat generation is proposed. The model shows very encouraging results as compared to existing more classical thermal models of FSW as well as compared to experimental measurement of temperatures. Thus the reliability and thereby the applicability of pure thermal models (which the proposed model in essence also belongs to) has been extended to a new level.

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