Estimation of The IMC Layer Thickness of Friction-Stir-Welded Aluminum/Copper Lap Joints By Using Temperature Simulation

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Research Article

Keywords: friction stir welding, dissimilar materials, temperature simulation, IMC layer estimation, aluminum/copper lap joints

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Estimation of the IMC layer thickness of friction-stir-welded aluminum/copper lap joints by using temperature simulation

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Abstract Many studies demonstrated the suitability of Friction Stir Welding (FSW) for joining dissimilar materials. Especially the combination of aluminum and copper is of high interest for many applications. Intermetallic compounds (IMC) forming during FSW due to interdiffusion and the thickness of the IMC layers strongly influence the joint properties, e.g. the joint strength or the thermal and electrical conductivity. Therefore, it is important to predict the IMC layer thickness to tailor the joint properties to the individual application. For this purpose, a thermal-pseudo-mechanical model was built to simulate the temperature field during FSW of aluminum EN AW-1050 and copper CW008A in lap joint configuration. The simulated temperatures as well as the heat inputs corresponded well with experimental data for a wide range of parameter settings. In order to estimate the IMC layer thickness, the simulated temperatures close to the interface of the materials were used. Two approaches for calculating the layer thicknesses were compared. In the first approach, the thickness is calculated based on the peak temperature lasting for several seconds at the interfacial area. This approach was applied for constant feed rates, while the second approach also accounted for the cooling slope and could therefore be used for different feed rates.

Keywords friction stir welding · dissimilar materials · temperature simulation · IMC layer estimation · aluminum/copper lap joints

1 Introduction

The modern automotive design and construction is facing several challenges. One key development is the transition to non-fuel consuming technologies. Here, often aluminum and copper alloys are combined to provide a high efficiency of electrical powertrains. The suitability of Friction Stir Welding (FSW) for joining dissimilar material combinations has been proven and reviewed in many studies [1,2,3]. FSW is a solid-state welding technology producing joints with a minimum of intermetallic compounds (IMCs), which may have a deleterious influence on the joint strength. Modified FSW processes, such as ultrasound-enhanced FSW [4,5], can further limit the amount of occurring IMCs. Detailed reviews on FSW of aluminum/copper joints regarding the dependencies between the welding parameters, the welding conditions, the mechanical properties and the metallurgical structure of the joints are given by [6], [7], and [8].

The formation of IMCs and their layer thickness depend on the restrictions of the experimental set-up: heat
accumulation due to thermal effects is primarily influenced by the joint configuration, the parameter setting and the tool offset to the interfacial area of the materials.

For friction-stir-welded joints of aluminum 5754-H114 and pure copper, increased amounts of the proposed IMCs Al$_2$Cu and Al$_4$Cu$_9$ were reported by [12] with a raising heat input. The authors concluded that the mechanical properties of the joints were determined by the grain size and the IMCs. [10] measured the IMC formation during FSW of lap joints of aluminum 1060 and commercially pure copper. They reported reduced amounts of IMCs when the feed rate increased or the rotational speed decreased. The IMCs could be detected near the interface region of the workpieces. Insufficient joint strengths presumably occurred at low and high heat inputs. During FSW of lap joints of aluminum A5083 and commercially pure copper, [11] observed reduced tensile strengths due to increased amounts of IMCs. Additionally, the occurrence of micro cracks was reasoned by excessive heat inputs. Several studies detected continuous IMC layers at the interface of the materials resulting from interdiffusion. For butt joints of aluminum 1060 and commercially pure copper, the IMC layer thickness was about 1 μm [12,13]. [14] and [15] observed similar thicknesses for butt joints of aluminum 5A02 with copper T2 and aluminum AA110-H14 with commercially pure copper, respectively. Very thin IMC layers led to increased joint strengths due to excellent metallurgical bonding. Varying the feed rates resulted in different IMC layer thicknesses and tensile strengths for dissimilar joints of aluminum and copper [16,17]. Both studies reported decreasing IMC layer thicknesses with increasing feed rates. For aluminum AA 6061-T6 and pure copper, the maximum joint strength was achieved by moderate feed rates [16]. A decent tensile strength was caused by thin layers of Al$_2$Cu and Al$_4$Cu$_9$ for joints of aluminum AA 1050 and oxygen free copper [17]. The dependency between the welding temperature and the IMC layer thicknesses was analyzed for lap joints of aluminum EN AW-1050 and copper CW008A by [13]. The welding temperature and the joint strength increased with increasing rotational speeds. An Arrhenius law describes the correlation between the thicknesses of the IMC layers (all below 1 μm) and the welding temperatures very well.

Regarding the electrical efficiency of friction-stir-welded aluminum/copper joints, the influence of the heat input (and the IMC formation) is not clear. When increasing the heat input, higher electrical resistances were measured for butt joints of aluminum AA5754 and copper CW11000 [19]. This was presumed to result from the IMC formation. In contrast, no significant influence of the heat input on the electrical resistance was observed by [20] during FSW of lap joints of aluminum ASTM 6060 T5 and copper ASTM B110. The authors concluded that the formed IMCs had no deleterious effect on the electrical conductivity of the joints.

The thickness of the IMC layer is crucial for the mechanical properties of any dissimilar material joint produced by FSW and should be limited according to [21]. The authors concluded, that in general a thickness of about 1 μm is the best solution. Increasing the heat input during FSW was found to result in increased IMC layer thicknesses as well. A dependency between the welding parameters, the joining mechanism (mechanical interlocking or substance-to-substance bond by IMC formation) and the final (mechanical) joint properties was also observed for other dissimilar material combinations, e.g. aluminum/steel [22,23,24] or aluminum/titanium [25,26,27].

In order to limit or even control the IMC layer thicknesses for aluminum/copper joints, several approaches have been reported. Offsetting the probe of the FSW tool to the interface of butt joints of aluminum 1060 and commercially pure copper led to thin layers of about 200 nm [28]. Another approach is to use a temperature control during welding as suggested by [29]. This results in constant layer thicknesses over the entire weld seam [30], which can also be controlled for complex geometries [31,32]. Although temperature control in general enhances the process robustness of welding dissimilar aluminum/copper joints, the IMC layer thickness also depends on the feed rate [33].

The simulation of FSW is a cost-efficient solution to identify suitable welding conditions (e.g. the welding temperature and feed rate) resulting in the desired IMC layer thickness and the required joint properties (e.g. joint strength, thermal/electrical conductivity). Several approaches on the simulation of FSW of dissimilar materials have already been published. The plasma-assisted FSW of butt joints of aluminum AA1100 with pure copper was modeled by [34]. For heat generation, a sticking/sliding condition was used, while the weld region between aluminum and copper was modeled by a functionally graded material approach. The peak temperature of the process could be predicted with a deviation of 5-12% to the experiments. A coupled Eulerian-Lagrangian method was used by [35] to simulate FSW of a duplex stainless steel joined with a copper alloy. The heat input was calculated by a modified Coulomb friction law. The simulation approximated the experimental data very well in terms of temperature and loads. Cooling assisted FSW of aluminum/copper butt joints (AA6061-T6 and electrolytic touch pitch Cu) was numerically modeled and simulated by a heat transfer
Objectives and approach. In order to improve the understanding of the formation of IMCs, simulating the temperature field during FSW of dissimilar material joints is essential. This allows to determine the relation between the welding parameters (especially the rotational speed and the feed rate) and the temperature profile at the interfacial area of the welding partners. Based on the temperature profile, the IMC layer thickness can be estimated.

In the following, a thermal model with a thermal-pseudo-mechanical (TPM) heat source based on [38] and [39] is discussed. Since mechanical material data often has to be approximated for thermo-mechanical models resulting in increasing complexity and calculation time, the TPM approach is more suitable for only determining the temperature field during FSW, which is the aim of this study. For this purpose, the following procedure was pursued:

1. Lap joints of commercially pure aluminum (EN AW-1050) and commercially pure copper (CW008A) were produced with varying parameter settings. The temperature was measured during FSW.

2. A model to simulate the temperature profile during FSW was established:
   - The model geometry had to reflect the actual experimental set-up as close as possible to guarantee comparable boundary conditions.
   - The heat source was applied via the TPM approach according to [38] and [39].
   - The temperature dependent shear yield stress at elevated temperatures, which is required for the TPM heat source was derived based on [40] with experimental data from [41].
   - The simulated and experimental temperature profiles were compared and the model quality was evaluated.

3. The IMC layer thicknesses were estimated via two different approaches:
   - First, only the peak temperatures near the interfacial area of the workpieces were used to estimate the IMC layer thicknesses via the approach of [41].
   - Second, an iterative approach as presented by [42] was applied using the simulated peak temperatures as well as the temperatures of the cooling slope at every time-step.

   - The results of both approaches were compared with available data of [31] and [33].

2 Experimental set-up

In order to validate the simulated temperature data, experiments were performed on a CNC milling machine (Heller MCH250). The tool shoulder had a diameter of 14 mm. The threaded probe had three equally distributed flats and a diameter of 5 mm, conically narrowing to 3.6 mm at the tip. FSW was carried out with a tilt angle of 2° and in position-controlled mode to ensure a constant distance between the probe tip and the interfacial area of the work pieces, while a shoulder plunge depth of 0.1 mm was maintained during welding. The sheets of the commercially pure aluminum alloy EN AW-1050 were dimensioned 245 x 100 x 4 mm and positioned on the advancing side (AS), as shown in Fig. 1. The sheets of the commercially pure copper alloy CW008A were dimensioned at 245 x 100 x 2 mm and were on the retreating side (RS). The set-up was identical to [18], [30] and [31]. The sheets overlapped by 40 mm. An aluminum shim (245 x 60 x 4 mm) was positioned between the clamping device and the copper sheets and a copper shim (245 x 60 x 2 mm) between the aluminum sheets and the backing plate (steel, 596 x 396 x 36 mm) for backing.

In order to evaluate the simulated temperatures, experiments were carried out with varying rotational speeds n (RPM), feed rates v and probe-tip-to-interface distances d (see Table 1).

Thermocouples of the type K and a diameter 0.5 mm were inserted into the sheets to measure the temperature during FSW (see Fig. 2). Drill holes on the face...
Table 1: Parameter settings of the experiments

| exp. no. | rotational speed $n$ in min$^{-1}$ | feed rate $v$ in mm/min | probe-tip-to-interface distance $d$ in mm |
|----------|----------------------------------|------------------------|-----------------------------------------|
| 1        | 1800                             | 300                    | 0.3                                     |
| 2        | 1800                             | 100                    | 0.3                                     |
| 3        | 2800                             | 300                    | 0.2                                     |
| 4        | 2800                             | 500                    | 0.2                                     |
| 5        | 800                              | 300                    | 0.2                                     |
| 6        | 1800                             | 500                    | 0.2                                     |
| 7        | 800                              | 100                    | 0.2                                     |
| 8        | 2800                             | 100                    | 0.2                                     |
| 9        | 800                              | 500                    | 0.2                                     |

Heat source Since FSW of dissimilar materials requires high heat inputs, almost steady state conditions at the interface of the workpiece and the FSW tool can be assumed. A TPM model for steady state conditions was introduced by [38] and [39]. Here, the surface heat flux $q_{\text{total}}$ at the tool-workpiece interface can be calculated by:

$$q_{\text{total}} = \omega \cdot r \cdot \tau_{\text{yield}}(T)$$

(1)

In this equation, $\omega$ is the angular rotational speed, $r$ is the radial coordinate on the tool surface and $\tau_{\text{yield}}(T)$ is the temperature dependent shear yield stress for the spatial temperature distribution $T = T(x, y, z)$ at the tool-workpiece interface. The heat input at the tool-workpiece interface is asymmetric due to the asymmetrical velocity profile of the FSW process. A superposition of the feed and the rotational motion during FSW results in the effective velocity at every location on the tool-workpiece interface:

$$v_{\text{eff}}(r, \varphi) = \sqrt{(-2\pi n r \sin(\varphi) + v)^2 + (2\pi n r \cos(\varphi))^2}$$

(2)

Combining Eq. 1 and Eq. 2 leads to the final formulation of the heat source on the tool-workpiece interface, which was used in the model:

$$q_{\text{total}}(r, \varphi) = \frac{\sqrt{(-2\pi n r \sin(\varphi) + v)^2 + (2\pi n r \cos(\varphi))^2} \cdot \tau_{\text{yield}}(T)}}{}$$

(3)

4 Material data

Temperature dependent yield stress The temperature dependent yield stress is crucial for the TPM model. However, experimental data often is not available for the strain rates $\dot{\varepsilon}$ of FSW of up to $10^3$ s$^{-1}$ [43,44]. Thus, the approach of [40] was used to derive temperature dependent yield stresses at elevated strain rates. It is
Fig. 3: Geometries included in the TPM model based on the components of the experimental set-up

based on experimental data of the yield stress $\sigma_{\text{yield,0}}(T)$, while the strain rate dependency is incorporated by the power-law:

$$\sigma_{\text{yield}}(\dot{\varepsilon}, T) = \sigma_{\text{yield,0}}(T) \left( \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right)^{\frac{1}{k}}$$  \hspace{1cm} (4)

Here, $k$ is the power-law exponent and $\dot{\varepsilon}_0$ is the reference strain rate. After calculating a characteristic point $(\sigma_{\text{yield,C}}, \dot{\varepsilon}_C)$, where all isotherms of the $\sigma-\dot{\varepsilon}$ plane end, the temperature dependent power law exponent $k(T)$ can be redefined as:

$$k(T) = \frac{\log \left( \frac{\dot{\varepsilon}_C}{\dot{\varepsilon}_0} \right)}{\log \left( \frac{\sigma_{\text{yield,C}}}{\sigma_{\text{yield,0}}(T)} \right)}$$  \hspace{1cm} (5)

For this study, the basic experimental data was taken from [41] for aluminum EN AW-1050 (see Table 2). The characteristic yield stress was calculated to be $\sigma_{\text{yield,C}} = 188.07$ MPa and the characteristic strain rate to be $\dot{\varepsilon}_C = 7.43 \times 10^5$ s$^{-1}$. The power-law exponents were updated stepwise for every temperature level and the range between two temperatures. The initial yield stress $\sigma_{\text{yield,0}}(T)$ was calculated for each temperature at a reference strain rate of $\dot{\varepsilon}_0 = 0.1$ s$^{-1}$.

Since no data existed for the temperatures between 500°C and the melting temperature of EN AW-1050 of 650°C, the yield stress within this temperature range was described by:

$$\sigma_{\text{yield,T>500°C}}(T) = \sigma_{\text{yield,1000,500°C}} \cdot \frac{(1 - (T/650°C)^{-4})}{(1 - (500°C/650°C)^{-4})}$$  \hspace{1cm} (6)

Fig. 4 shows the calculated temperature dependent shear yield stress

$$\tau_{\text{yield}}(T) = \sigma_{\text{yield}}(T)/\sqrt{3}$$  \hspace{1cm} (7)

Table 2: Calculated power-law exponents for temperatures given by data from [41]

| Temperature | Exponent | Temperature | Exponent |
|-------------|----------|-------------|----------|
| 20          | 34.56    | 360         | 8.66     |
| 120         | 23.84    | 400         | 7.33     |
| 240         | 16.64    | 450         | 6.18     |
| 300         | 11.46    | 480         | 6.19     |
| 350         | 8.99     | 500         | 5.38     |

for the strain rate of $10^3$ s$^{-1}$ compared to the reference data. At 650°C the heat input is zero as $\tau_{\text{yield}}$ approaches 0, which reflects the self-stabilizing effect of FSW.

Fig. 4: Calculated temperature dependent shear yield stress
Material properties and thermal conditions The TPM model was used to analyze the thermal conditions during FSW of aluminum/copper lap joints. The thermal properties of both materials are highly temperature dependent. Therefore, temperature dependent values for elemental aluminum, copper and iron (for clamping devices and FSW tool) were implemented, as the respective chemical compositions especially of commercially pure aluminum and copper were similar. The thermal conductivities were taken from [45], the specific heat capacities from [46], and the densities were extracted from [47].

Convective heat fluxes were assumed to occur at the large surfaces (e.g. clamping devices, workpiece surfaces) to the ambiance. Thermal radiation was inserted with an emissivity of $0.2$ for aluminum, $0.224$ for copper and $0.6$ for the backing plate, the clamping devices and the FSW tool. The thermal contact conditions had the most significant impact on the temperature distribution and were the key values for calibration. For this purpose, areas with an increased heat transmission were assumed to exist (see Fig. 5):

- Circular areas at the aluminum/copper (diameter: $8$ mm) and the copper/backing plate interface (diameter: $10$ mm) underneath the axial tool pressing the components on each other.
- A rectangular area at the aluminum/copper interface (width: $4$ mm), where the workpieces are "bonded" due to interdiffusion processes after tool transition: since the IMCs form after the probe transition, the overlapping area of the rectangle and the circular area of the tool pressure were dominated by this kind of bonding and consequently values from the rectangular area were used here.

The thermal contacts were modeled using a constricted conductance $h_c$ and a gap conductance $h_g$. The respective values are given in Table [3]. It was assumed that the thermal conductivity is enhanced by the "bonded" weld seam. Therefore, the thermal conductance at the interdiffusional area was set almost five times higher than at the unwelded aluminum/copper interface.

5 Results and discussion

Temperature simulation The described TPM model was calibrated to accurately describe the temperature distribution of aluminum/copper lap joints within the given parameter range. Hereby, the aluminum sided peak temperatures were focused. On the aluminum side, the FSW process is executed and the heat within the process zone is conducted to the interfacial area of the workpieces. Fig. [6] shows the experimental temperatures compared with the simulated temperatures. The data were evaluated at corresponding positions for the central parameter setting of experiment no. 1 ($n = 1800 \text{ min}^{-1}$, $v = 300 \text{ mm/min}$, $d = 0.3 \text{ mm}$). Regarding the heating slope as well as the peak temperature on the aluminum side, the experimental and the simulated data showed a good agreement. The difference between the cooling slopes first increased with time and then decreased. Similar observations could be made for the experimental and simulated temperatures on the copper side. The peak temperatures differed by $18$ K compared to $0.5$ K for aluminum.

![Fig. 6: Comparison of experimental and simulated temperatures for experiment no. 1 (n = 1800 min\(^{-1}\), v = 300 mm/min, d = 0.3 mm)](image-url)

Model reliability In order to quantify the model quality, the differences between the experimental and sim-
Table 3: Assumed constriction and gap conductances for different interfacial areas

| interfacial area                        | h_c in W/(m²K) | h_g in W/(m²K) |
|----------------------------------------|----------------|----------------|
| aluminum-copper                        | 8000           | 4000           |
| copper-backing plate                   | 1500           | 750            |
| aluminum-clamping devices              | 3000           | 1500           |
| interdiffusion area (rectangle)        | 39000          | 19500          |
| aluminum-copper (circular area)        | 20000          | 10000          |
| copper-backing plate (circular area)   | 5000           | 2500           |

The calculated values of the peak temperatures for aluminum and copper as well as the values of the heat inputs were analyzed. The model reliability was evaluated based on [48]. The error between simulated and experimental peak temperatures for aluminum (ERR_{Al,i}) and for copper (ERR_{Cu,i}) was calculated for each experiment i as follows:

\[
ERR_{Al,i} = 1 - \frac{T_{\text{peak,Al,exp},i}}{T_{\text{peak,Al,sim},i}}
\]

(8)

\[
ERR_{Cu,i} = 1 - \frac{T_{\text{peak,Cu,exp},i}}{T_{\text{peak,Cu,sim},i}}
\]

(9)

The error between the simulated and experimental heat input (ERR_{P,i}) was calculated similarly:

\[
ERR_{P,i} = 1 - \frac{P_{\text{total,sim},i}}{P_{\text{total,exp},i}}
\]

(10)

The total experimental heat input was derived from torque measurements during FSW with \(P_{\text{total,exp}} = 2\pi \cdot M \cdot n\). The reliability for each parameter setting i was then calculated by:

\[
R_{S,i} = (1 - |ERR_{Al,i}|) \cdot (1 - |ERR_{Cu,i}|) \cdot (1 - |ERR_{P,i}|)
\]

(11)

The single values of the overall reliabilities \(R_{S,i}\) ranged from 0.74 to 0.85 for the parameter settings, which agrees well with [44]. The averaged overall model reliability was 0.79. Hence, on average, each averaged error value is less than 8%. This shows the high robustness of the presented approach especially when considering the broad range of parameter settings.

Estimation of the interdiffusion layer thickness The main focus of this study was to use the TPM model to determine the thickness of the IMC layer of aluminum/copper lap joints. Table 4 lists the layer temperatures \(T_{\text{layer}}\) exported from the simulation model at a distance of 0.001 mm from the interfacial area within the aluminum sheet. The data were extracted along the weld line for all parameter settings. These temperatures were also applied to the interfacial area due to the high thermal conductivity of aluminum.

Table 4: Layer temperatures for all parameter settings exported from simulations

| exp. no | peak layer temperatures \(T_{\text{layer}}\) in °C |
|---------|-----------------------------------------------|
| 1       | 510.50                                        |
| 2       | 520.38                                        |
| 3       | 544.22                                        |
| 4       | 537.95                                        |
| 5       | 438.20                                        |
| 6       | 508.22                                        |
| 7       | 455.21                                        |
| 8       | 552.62                                        |
| 9       | 424.16                                        |

The calculated temperatures agreed well with the preset and controlled welding temperatures of [31] and [33]. Both studies were based on the same experimental set-up, while [31] used a constant feed rate of 300 mm/min and [33] varied the feed rate (see Table 5).

[18] estimated the IMC layer thickness \(x_{\text{layer}}\) of friction stir welded dissimilar lap joints based on thermal activation for the Arrhenius equation:

\[
x_{\text{layer}} = \sqrt{2t \cdot D(T)} = \sqrt{2t \cdot \left(D_0 \cdot \exp\left(-\frac{E_{\text{act}}}{k_B T}\right)\right)}
\]

(12)

Rearranging Eq. 12 allows for a linear fit for the layer thickness \(x_{\text{layer}}\) after temperature controlled FSW in the Arrhenius plot [31] via:

\[
ln(x_{\text{layer}}) = \frac{1}{2}ln(2t_{\text{eq}}D_0) - \frac{E_{\text{act}}}{2k_B T_{\text{weld}}}
\]

(13)

Here, \(t_{\text{eq}}\) was the equivalent time interval in the order of some seconds, \(D_0\) the prefactor of diffusion, \(k_B\) the...
Boltzmann constant, $E_{\text{act}} = 0.84 \pm 0.05 \text{eV}$ the activation energy derived from the measurements of the layer thicknesses and $T_{\text{weld}}$ the preset and controlled welding temperature ranging from 410°C to 570°C. The term $t_\text{eq} \cdot D_0$ was determined to be $1.57 \cdot 10^{-4} \text{cm}^2/\text{s}$. As mentioned, [31] carried out all experiments at a constant feed rate of 300 mm/min.

As shown in Fig. 7, a good match can be found with the observations of [31] and with [33] at a feed rate of 300 mm/min, when Eq. 13 is applied to the calculated layer temperatures of experiments 1, 3 and 5. The inverse temperature was used to show the linear behavior of the temperature dependent layer thickness.

Two aspects have to be pointed out, when comparing the calculated layer thicknesses of this study with the measured ones of the experimental studies:

- The calculated layer thicknesses are slightly higher than the measured thicknesses of the experimental studies [31, 33]. Therefore, the prefactor of diffusion and the activation energy may need to be updated to be used for layer temperatures exported from the described simulation.
- The key values for calculating the layer thicknesses (equivalent time, prefactor of diffusion and activation energy) of Eq. 13 have been derived from an analysis of layers, which were welded at a constant feed rate of 300 mm/min. Hence, especially the value of the equivalent time interval may not be suitable for other feed rates.

Another approach to estimate the IMC layer thicknesses was introduced by [42] taking into account the time-temperature dependency of the diffusion based layer growth. It can be used, if the peak temperatures as well as the cooling slopes are known. Applying Eq. 12 for two different temperatures ($T_1 > T_{II}$) results in schematic curves as shown in Fig. 8.

During the constant time interval $\Delta t$ the temperature $T_1$ leads to the final layer thickness $x_{II,\text{final}}$, which equals the initial layer thickness $x_{II,\text{initial}}$ for the temperature $T_{II}$. Therefore, the self-limiting effect of diffusion processes can be considered by calculating the theoretical time interval $t_{II,\text{theor}}$ that would cause $x_{II,\text{initial}}$ if only the temperature $T_{II}$ occurred:

$$t_{II,\text{theor}} = \frac{x_{II,\text{initial}}^2}{2 \cdot D(T_{II})}$$ (14)

The final layer thickness $x_{II,\text{final}}$ at the temperature $T_{II}$ after the time interval $\Delta t$ can then be estimated by:

$$x_{II,\text{final}} = \sqrt{2 \cdot D(T_{II} \cdot (t_{II,\text{theor}} + \Delta t))}$$ (15)

---

**Table 5: Parameter settings and layer thicknesses of [31] and [33]**

| no. | controlled welding temperature $T$ in °C | inverse welding temperature $1/T$ in 1000/K | feed rate $v$ in mm/min | layer thickness $x_{layer}$ in nm |
|-----|----------------------------------------|------------------------------------------|------------------------|---------------------------------|
| C1* | 410                                    | 1.46                                     | 300                    | 145±31                          |
| C2* | 430                                    | 1.42                                     | 300                    | 177±29                          |
| C3* | 465                                    | 1.35                                     | 300                    | 236±44                          |
| C4* | 500                                    | 1.29                                     | 300                    | 316±37                          |
| C5* | 535                                    | 1.24                                     | 300                    | 339±50                          |
| C6* | 570                                    | 1.19                                     | 100                    | 601±60                          |
| C7* | 480                                    | 1.33                                     | 200                    | 452±93                          |
| C8* | 480                                    | 1.33                                     | 300                    | 245±43                          |
| C9* | 480                                    | 1.33                                     | 500                    | 254±40                          |
| C10**| 480                                   | 1.33                                    | 800                    | 152±34                          |

Data from: [31]; **[33]**
A given temperature profile then enables an iterative calculation of the layer thickness by:

\[ x_{\text{layer}, \, n+1} = \sqrt{x_{\text{layer}, \, n}^2 + 2 \cdot D(T_{n+1}) \cdot \Delta t} \quad (16) \]

Here, \( n + 1 \) is the number of time periods and of the respective temperatures of the cooling slopes. The calculation starts with the peak temperature at \( n = 0 \), where the cumulative layer thickness \( x_{\text{layer}, \, n=0} \) is set to 0. \( D(T_{n+1}) \) is the diffusion coefficient for the temperature \( T_{n+1} \). The prefactor of diffusion was set to \( 10^{-4} \text{ cm}^2/\text{s} \) and the activation energy to 0.79 eV, which is within the range of [31]. The calculation ended, when the temperature was lower than 200°C. At this temperature it was assured that the layer growth is less than 1 nm per time interval.

Fig. 9 shows the layer thicknesses calculated by the iterative approach. It can be seen, that different feed rates and layer temperatures lead to different thicknesses, which corresponds well to the observations of [31] and [33]. The calculated layer thicknesses for a feed rate of 300 mm/min are very close to the measured ones of [31]. The data points at the layer temperatures of this study were connected by dashed lines for the three different feed rates. Since no parameter setting led to a layer temperature of 480°C, the iterative approach could not be applied to this temperature. The intersections of the dashed lines with the standard deviations of [33] show the suitability of the iterative approach for different feed rates, if the cooling slope at the interface can be applied. The estimated layer thicknesses at feed rates of 100 mm/min and 500 mm/min have to be verified. However, the calculated values of about 100 – 750 nm are plausible when compared to the discussed studies.

Key features of the approach The discussed approach has demonstrated its suitability to simulate temperatures of friction-stir-welded dissimilar joints and to estimate the IMC layer thicknesses. The key features of the approach are as follows:

1. Material data. The simulation model is driven by the temperature dependent material properties. Especially the heat input relies on the temperature and strain rate dependent flow stress. The model can easily be adjusted to changing material properties, if needed.

2. Thermal contact conditions. Several thermal contact areas were defined in this study to represent an increased heat conductance during FSW. This allows for a gradual adjustment of the overall thermal contact conditions, meeting the actual process conditions more precisely.

3. Model for more complex welding tasks. The TPM model allows a fast estimation of the welding temperature with a calculation time of 60 – 180 s. It could also be designed to predict temperatures (and as a consequence layer thicknesses) for areas, where heat accumulation due to geometrical issues (e.g. design of the sheets) occurs. Therefore, the heat accumulation could be reduced by re-adjusting the parameter setting based on simulation results.

4. Estimation of the IMC layer thickness. The described approach allows for an estimation of the IMC layer thicknesses based on the essential welding parameters: the rotational speed, the feed rate and the probe-tip-to-interface distance. Hence, the layer thicknesses and consequently the properties of friction-stir-welded aluminum/copper lap joints can be tai-
6 Conclusion and outlook

Welding of dissimilar materials is of particular interest for several issues of modern production and product design. In this study, an approach was developed to simulate the temperature distribution near the interface of aluminum/copper lap joints during FSW. These temperatures were used to estimate the thicknesses of the IMC layers for several parameter settings. Summarizing the above results, the following conclusions can be drawn:

- Despite its simplicity, the chosen TPM model enables the calculation of the temperatures and the heat inputs during FSW. Therefore, the predicted temperatures near the interface area can be used for an estimation of the IMC layer thicknesses.
- The material data can be parameterized with little effort. This allows for an easy adaption to other process conditions, e.g. the strain rate or the welding parameters.
- An iterative calculation of the IMC layer thickness was performed by taking into account the peak layer temperature as well as the temperatures of the cooling slopes. This results in a diversification of the layer thickness distribution by means of the feed rate. Good agreement with other studies could be found.
- The combination of the TPM model with the iterative calculation approach can be fully automated, which leads to short calculation times for an estimation of the IMC layer thicknesses. Afterwards, basic joint properties can be derived or even tailored to individual needs. Future studies need to clarify, if increasing experimental data would also increase the range of predictable properties.

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7 Declarations

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7.2 Conflicts of interest/Competing interests

The authors have no conflicts of interest to declare that are relevant to the content of this article.

7.3 Availability of data and material

In our opinion all relevant data is given to rebuilt the simulation model. All discussed data have been taken from published studies.

7.4 Code availability

Not applicable

7.5 Authors’ contributions

Not applicable

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