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The effects of turbulence on molten pool transport during melting and solidification processes in continuous conduction mode laser welding of Copper-Nickel dissimilar couple

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ABSTRACT

The melting and solidification stages of a continuous Copper-Nickel dissimilar metal conduction mode laser welding have been simulated numerically in this study. The heat, mass and momentum transports in molten metal pool have been analysed using both laminar and turbulent flow models separately for the same process parameters. The phase change aspects related to solidification and melting are accounted for by a modified enthalpy porosity technique while the turbulent transport is modelled by a high Reynolds number \( k - \varepsilon \) model. It has been observed that temperature fields obtained from both laminar and turbulent transport simulations are qualitatively similar to each other. The molecular thermal diffusivity of the molten metal mixture is found to be in the same order of magnitude as eddy thermal diffusivity, as a result of which the thermal field gets marginally affected by fluid turbulence. By contrast, eddy viscosity remains much greater than molecular viscosity, which leads to greater amount of momentum diffusion in the case of a turbulent molten metal pool, in comparison to that obtained from the corresponding laminar simulation. This is reflected in the reduction in maximum velocity magnitude in the turbulent simulation in comparison to the maximum velocity obtained from laminar simulation. In the case of species transport, the turbulent mass diffusivity is found to be about \( 10^7 - 10^8 \) times greater than molecular mass diffusivity. As a result, the species field in turbulent simulation shows characteristics of better mixing between two dissimilar molten metals than the species field obtained using the laminar transport model. The species distribution obtained from turbulent transport is shown to be in better agreement with experimental data reported in literature than the corresponding mass fraction distribution obtained from laminar simulation. It is also found that species distribution in the molten pool is principally determined by advective and diffusive transport during the melting stage and species transport by advection and eddy diffusion in turbulent pool increasingly weakens with decreasing temperature during the cooling following the laser melting stage.
## NOMENCLATURE

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| Co-ordinates   |
|----------------|--------------|
| $x$, $y$, $z$ | Co-ordinates fixed to the laser source |
1. INTRODUCTION

Modern Laser welding applications often require the joining of two dissimilar metal parts, the physical properties of which are significantly different. The thermo-solutal history of the molten pool transport determines the weld-pool morphology, microstructure, and segregation pattern [1]. Dissimilar metal welding has been extensively studied from the point of view of fusion characteristics and often these analyses are materials-specific [1,2]. Computational modelling of heat and mass transfer characteristics in the molten metal pool in welding applications has contributed significantly to the physical understanding of molten pool transport in similar metal welding applications [3-12]. Relatively few studies [13-15] addressed the modelling of dissimilar metal welding, despite its several industrial applications.

The scaling analysis and computational results [10,11,16] revealed that the maximum velocity obtained in the weld pool is in the order of 1.0m/s. For typical values of density and viscosity of molten metals (\(\rho \sim 7900\,\text{kg/m}^3, \mu \sim 0.00368\,\text{Pa.s}\)), a velocity scale \(U_s \sim 1.0\,\text{m/s}\) and a length scale equal to heat source radius \(r_q\) (\(r_q \sim 0.1\,\text{mm}\)) result in a Reynolds number in the order of 1000 (\(\text{Re} = \rho U_s r_q / \mu \sim 1000\)). Atthey [17] indicated that in molten metal pools, laminar to turbulence transition sets in when a Reynolds number \(\text{Re}_c = 2\text{Re}\) exceeds 600, which is supported by subsequent studies [18,19] where surface tension driven convection (i.e. Marangoni convection) is taken to be turbulent for \(\text{Re}\) in the order of 100. In addition to this Joshi et al. [11] reported perceptible surface oscillations in the molten pool for high power Gas Tungsten Arc Welding (GTAW) applications and indicated that such oscillations could be due to surface instabilities, which might eventually lead to turbulence. Moreover, the molten pool morphology obtained from experiments was found to be different from the prediction of numerical simulation using laminar fluid flow model [11]. It is therefore evident that the molten pool transport in weld pools is often likely to be turbulent for high power welding applications.

Mundra et al. [20] attempted to mimic the turbulent momentum transport in similar metal welding by artificially increasing the molten metal viscosity. Choo and Szekely [21] and Hong et al. [22] used two-equation models (i.e. \(k - \varepsilon\) or \(k - w\)) to address molten pool convection in stationary GTAW applications. However, \(k - \varepsilon\) or \(k - w\) equations are solved in a molten pool obtained from a conduction-based solution and were not solved in a coupled manner with other transport equations from the inception of molten pool. Moreover, the irregular morphology of the solid/liquid interface during welding would require wall treatment at phase boundaries. This
demands treatment of solid-liquid interface with regard to the implementation of wall conditions for $k - \varepsilon$ equations. Some of the above issues were addressed in the computational methodology proposed by the present author and his co-workers, where modelled $k - \varepsilon$ equations were solved in a coupled manner with other transport equations to address molten pool transport in arc welding and laser surface alloying operations [23-25].

Recently, Chakraborty and Chakraborty [26] extended the methodology proposed by Chakraborty et al. [24] to model turbulent transport in the molten pool during the melting stage in a typical dissimilar metal conduction mode laser welding. The thermo-solutal transport during the melting stage is likely to have significant influences on subsequent cooling characteristics and the species distribution in the cooled joint. In the present study, both heating and cooling cycles are simulated for a typical high power conduction mode laser welding of dissimilar metal couple where the effects of turbulent transport can be prominently realised. A binary metal couple based on Cu-Ni is taken for the study, as the physical properties of Copper and Nickel are significantly different and the phase diagram of Copper-Nickel does not show immiscible gaps and formation of intermetallic compounds [27].

In order to demonstrate the effects of turbulence in molten pool transport unsteady Reynolds Averaged Navier Stokes (RANS) simulations have been carried out for a continuous conduction mode laser welding of copper-nickel dissimilar couple. To demonstrate the contrasting features of turbulent convection, a simulation has been carried out corresponding to the process parameters used for the unsteady RANS simulation but without activating turbulence models. This simulation and the RANS simulation will henceforth be referred to as laminar and turbulent simulations respectively. The main objectives of this study are as follows:

(a) To model turbulent heat, mass and momentum transport in a copper-nickel dissimilar couple molten pool during both heating and cooling stages of the conduction mode laser welding process.

(b) To demonstrate the effects of turbulence on thermal history and species distribution of the welded joint.

The rest of the paper is organised as follows. In the next section, the necessary background for mathematical modelling will be provided along with the discussion on boundary conditions. The numerical implementation will be discussed in Section 3, followed by the presentation of the
results and subsequent discussion. The main findings are summarised and conclusions are drawn in the final section of the paper.

2. MATHEMATICAL BACKGROUND
A schematic diagram of a typical continuous conduction mode laser welding of copper-nickel dissimilar couple is shown in Fig. 1a. Two blocks of copper and nickel are kept side by side in a butt joint arrangement. According to Fig. 1a the laser torch moves along the interface separating copper and nickel blocks. Only a small part of laser power acts to increase temperature of the metal blocks, which is specified by efficiency $\eta$ following Phanikumar et al. [14,15] as:

$$q = \eta Q$$  \hspace{1cm} (1)

For the present study it is assumed that the laser torch is moving in the negative $x$ direction with a scanning speed $U_{\text{scan}}$. It is convenient to study the present problem in a moving co-ordinate system, which is carried out by the following co-ordinate transformation:

$$x = x' - (-U_{\text{scan}})t \quad ; \quad y = y' \quad \text{and} \quad z = z'$$  \hspace{1cm} (2)

where $(x', y', z')$ is the stationary co-ordinate system and $(x, y, z)$ is the moving co-ordinate system translating with the laser torch. In Eq. (2) the negative sign in front of $U_{\text{scan}}$ indicates that the laser torch is moving in the negative $x$ direction.

In order to model the melting and intermixing of two different metals, a locally homogeneous mixture model has been employed, where the mixture properties are determined by the relative proportion of the components. The following assumptions are invoked in the present mathematical model, which simplifies the analysis without losing the essential physics of the problem:

1. The welding is assumed to take place in conduction mode and no keyhole formation is considered.
2. The molten metal flows are assumed to incompressible and Newtonian in nature and thus the density change due to the temperature variation is accounted for by Boussinesq’s approximation.
3. The efficiency of absorption of heat from laser beam $\eta$ is assumed to be same for both Cu and Ni.
4. Laser power is assumed to be distributed in a Gaussian manner at the top surface.
5. The top surface of the welded joint is assumed to statistically flat [10,14,15].
The generalised form of the governing equations for turbulent molten pool transport in tensorial notation \((x_1 = x, x_2 = y, x_3 = z)\) can be written as:

\[
\frac{\partial}{\partial t} (\rho \bar{\phi}) + \frac{\partial}{\partial x_j} (\rho u_j \bar{\phi}) = \frac{\partial}{\partial x_j} \left( \Gamma \frac{\partial \bar{\phi}}{\partial x_j} \right) - \frac{\partial (\rho \cdot \bar{u}_j \bar{\phi}^t)}{\partial x_j} + S_{\phi} \tag{3}
\]

where \(\bar{\phi}\) represents the mean value and \(\phi^t\) represents the fluctuating component of a primitive variable \(\phi\), \(\Gamma\) is the diffusion coefficient associated with \(\phi\), and \(S_{\phi}\) is the source/sink term associated with the particular governing equation in question. The corresponding values of \(\phi\), \(f\), \(\Gamma\) and \(S_{\phi}\), for various conservation equations, are given in Table 1.

For the momentum conservation equations, the reference temperature \(T_{\text{ref}}\) is taken to be equal to the melting temperature \(T_m\), and \(C_{\text{ref}}\) is the reference concentration, taken to be equal to the concentration of copper at that reference temperature. The source term \(\bar{S}_i\) in the momentum conservation equations originates from the consideration that the morphology of the phase change domain can be treated as an equivalent porous medium offering a frictional resistance towards fluid flow in that region. In a single-domain fixed-grid enthalpy-porosity formulation, this resistance can be conveniently formulated using Darcy’s model in association with the Cozeny-Karman relationship as [28]:

\[
\bar{S}_i = -\frac{K_m (1 - f_i)^2}{f_i^2 + b} \bar{u}_i \tag{4}
\]

where \(f_i\) is the liquid fraction, which is given as: \(f_i = \Delta H / L\), with \(\Delta H\) is the latent enthalpy content of a control volume and \(L\) is the latent heat of fusion. The melting temperature \(T_m\) and the latent heat of fusion \(L\) are evaluated as follows according to locally homogeneous mixing model [14,15,26]:

\[
T_m = (T_{\text{melt}})_{Cu} C + (T_{\text{melt}})_{Ni} (1 - C) \quad \text{and} \quad L = (H)_{Cu} C + (H)_{Ni} (1 - C) \tag{5}
\]

where \(C\) is the mass fraction of copper in a given control volume, \(T_{\text{melt}}\) is the melting point temperature, \(H\) is the latent heat of fusion of pure material, and the subscripts \(Cu\) and \(Ni\) are used to refer to the properties of pure copper and pure nickel respectively. The latent enthalpy content of the computational cell \(\Delta H\) under consideration given by the following expression

\[
\Delta H = f (T) = L \quad : \bar{T} > T_i
\]

\[
= f_i L \quad : T_s \leq \bar{T} < T_i \tag{6}
\]
\[ = 0 \quad : \bar{T} < T_s \]

with \( T_s \) and \( T_l \) being the solidus and liquidus temperatures respectively. In Eq. (4), \( K_m \) is a large number \((\sim 10^8)\) and \( b \) is a small number \((\sim 10^{-30})\) to avoid division by zero. It is important to note that the momentum conservation equations are solved for the whole domain. In the solid phase the liquid fraction \( f_l \) becomes identically zero which leads to a large magnitude for the sink term \( \bar{S}_l \), which numerically ensures that fluid velocity is zero in the solid phase. In the fully molten state \( f_l \) is equal to unity and as a result of this the contribution of \( \bar{S}_l \) becomes identically zero in the liquid phase (see Eq. (4)) and the momentum conservation equations take the usual form for pure liquid phase. The details of this formulation may be found in Brent et al. [28]. This technique has been used in several solid-liquid phase change related problems in the past [10, 11, 14-16, 18, 23-26].

**Modelling of Reynolds stress terms \(- \rho \bar{u}_i \bar{u}_j\)**

In the present analysis, the Reynolds stress terms \(- \rho \bar{u}_i \bar{u}_j\) appearing in momentum conservation are modelled by a gradient hypothesis of the form:

\[
- \rho \bar{u}_i \bar{u}_j = \mu_i \left( \frac{\partial \bar{u}_i}{\partial x_j} + \frac{\partial \bar{u}_j}{\partial x_i} \right) - \frac{2}{3} \delta_{ij} \rho \frac{k}{\varepsilon} \tag{7}
\]

where the eddy viscosity \( \mu_i \) is given by:

\[
\mu_i = \sqrt{f_i} C_p \rho k^2 / \varepsilon \tag{8}
\]

where \( k \) and \( \varepsilon \) are turbulent kinetic energy and the dissipation rate of turbulent kinetic energy respectively. It is important to note that in a standard high Reynolds number \( k - \varepsilon \) model, the factor \( \sqrt{f_i} \) does not appear in the eddy viscosity expression. In the present problem, one encounters both solid and liquid phases in the computational domain. As a result, the formulation should ensure a reduction of eddy viscosity, eddy thermal conductivity and eddy mass diffusion coefficient (all of which is referred to as eddy diffusivity) in such a manner that the effective diffusivity (molecular + eddy) values approach their respective molecular values along the solid-liquid interface, and gradually become equal to the single-phase turbulent flow conditions in the fully liquid region. In order to satisfy this requirement, Shyy et al. [19] used a modified eddy viscosity expression in the context of a low Reynolds number \( k - \varepsilon \) model where the damping factor \( \sqrt{f_i} \) was used to address the solid-liquid phase change problem. The present work is based on a high Reynolds number \( k - \varepsilon \) model, and accordingly, the wall-damping factor used by
Shyy et al. [19] in the context of a low Reynolds number \(k - \varepsilon\) model is not employed. According to Eq. (8), the eddy viscosity goes to zero in the solid phase (since liquid fraction is identically zero in solid), and assumes the standard expression for single-phase flow problems in a fully liquid phase (since \(\sqrt{f_s}\) is identically unity in liquid phase).

**Modelling of Turbulent heat and species fluxes**

The turbulent heat and species fluxes (Reynolds heat and mass fluxes) appearing in the energy and species conservation equations respectively are given by the following expressions:

\[
-\bar{u}_i T = \alpha_t \frac{\partial T}{\partial x_i} \quad \text{and} \quad -\bar{u}_i C = \alpha_e \frac{\partial C}{\partial x_i}
\]

where \(\alpha_t\) is the eddy thermal diffusivity. From the analogy of laminar flow, \(\alpha_t\) can be expressed as:

\[
\alpha_t = \mu_t / \rho \sigma_t \quad \text{and} \quad \alpha_e = \mu_e / \rho \sigma_e
\]

where \(\sigma_t\) and \(\sigma_e\) are the turbulent Prandtl number and turbulent Schmidt number respectively.

In the present case \(\sigma_t\) and \(\sigma_e\) are taken to be 0.9.

**Transport equations of \(k\) and \(\varepsilon\)**

The modelled governing equations for \(k\) and \(\varepsilon\) are given in Table 1. The model constants are given by: \(\sigma_k = 1.0, \sigma_\varepsilon = 1.3, C_\mu = 0.09, C_{\varepsilon_1} = 1.44, C_{\varepsilon_2} = 1.92\). The quantities \(\sigma_k, \sigma_\varepsilon, C_\mu, C_{\varepsilon_1}\) and \(C_{\varepsilon_2}\) are standard model constants for high-Reynolds number \(k - \varepsilon\) model and these model constants were evaluated by calibrating the computational simulation results with experimental data under decaying homogeneous isotropic turbulence and homogeneous shear flow configurations. For more information on these model constants, interested readers are directed to Durbin and Pettersson Reif [29, see pp.120-122].

**Boundary conditions**

In this study, the entire work-piece is taken to be the computational domain. The heat flux due to laser beam \(q^*\) is assumed to follow a Gaussian profile in the following manner

\[
q^*(r) = (\eta Q / \pi a^2) \exp(-r^2 / a^2).
\]

All the above conservation equations are solved in a coupled manner, in conjunction with a set of appropriate boundary conditions. Boundary conditions pertinent to various governing equations are summarized in Table 2.
**k-\varepsilon transport at near wall regions**

For $k$ and $\varepsilon$ transport, the phase changing interface is treated as a solid wall. For a high Reynolds number $k-\varepsilon$ model, eddy diffusivities are specified at grid points next to the solid-liquid interface with the help of wall functions [29]. Mean velocities gradient, turbulent kinetic energy and turbulent kinetic energy dissipation rate at the grid points next to the wall are described by:

$$\frac{\partial \bar{u}_w}{\partial y} = \frac{u_\tau}{\kappa y_P} ; \quad \varepsilon = \frac{u_\tau^3}{\kappa y_P} ; \quad u_\tau = \sqrt{\frac{\tau_w}{\rho}} = C_\mu^{1/4} \kappa \sqrt{k} \quad \text{and} \quad \mu_{eff} = \mu + \mu_s = \frac{\kappa \mu y_P}{\ln(9 y_P)} \quad \text{(11)}$$

where $y_P = \rho u_\tau y_P / \mu$ and $\bar{u}_w$ represents the mean velocity component tangential to the solid-liquid interface at the near wall grid point. The quantity $y_P$ represents the normal distance of the grid-point next to the interface, $\tau_w$ represents the wall shear stress, and $\kappa$ is the Von Karman’s constant which will be taken as $\kappa = 0.4$. The thermal and solutal diffusivities at near wall points are given as [30]:

$$\frac{K}{c} + \frac{\mu_s}{\sigma_s} = \frac{\mu y_P^2}{\sigma_s (1 / \kappa) \log(y_P^+)} + (3.85 Pr^{1/3} - 1.3)^2 + 2.12 \log(Pr) \quad \text{(12i)}$$

$$\rho D + \frac{\mu_s}{\sigma_s} = \frac{\mu y_P^2}{\sigma_s (1 / \kappa) \log(y_P^+)} + (3.85 Sc^{1/3} - 1.3)^2 + 2.12 \log(Sc) \quad \text{(12ii)}$$

where $Pr$ and $Sc$ represent the molecular Prandtl and Schmidt numbers respectively. Values of $k$ and $\varepsilon$ are identically zero in the solid phase.

**3. NUMERICAL IMPLEMENTATION**

In this study, the simulations were carried out based on finite volume approach using the well-known SIMPLER algorithm [31]. The convective terms were discretised using the power law scheme proposed by Patankar [31]. The time advancement has been carried out in an implicit manner. The phase change aspect is accounted for by the enthalpy-porosity technique in which the latent heat enthalpy content of every computational cell is updated using the following expression [28,32]:

$$[\Delta H]_{m+1} = [\Delta H]_m + \left( \frac{a_P}{a_P^0} \right) \lambda c \left[ T_P \right]_{n+1} - T_{melt} \right) \quad \text{(13)}$$

where $a_P$ is the nodal point coefficient and $a_P^0$ is the coefficient related with the transient part of the discretised energy conservation equation.
The physical properties used in the present study for both Ni and Cu are listed in Table 3 [27]. For a mixture of Ni and Cu, properties such as specific heat \( c \), thermal conductivity \( K \) and viscosity \( \mu \) are calculated with the use of the following expression:

\[
P = P_{Cu} \overline{C} + P_{Ni}(1 - \overline{C})
\]  

(14)

where \( P \) represents the property concerned and subscripts Cu and Ni represent copper and nickel respectively. The mixture properties are calculated at each time step after solving the species conservation equation. The results presented in this paper correspond to process parameters typical of dissimilar metal laser welding (\( U_{scan} = 0.008 \text{ m/s, } Q = 3.5 \text{ kW, } \eta = 0.12, r_i = 0.5 \text{ mm} \)).

**Grid resolution**

The grid resolution in the present study is determined by two criteria which are as follows:

- Resolution of boundary layer formed beneath the top surface.
- Resolution of the flow physics in the vicinity of the phase changing interface acting as a wall to the fluid motion inside the molten pool.

In order to meet the first requirement, the boundary layer thickness \( \delta \) is estimated based on scaling arguments following Dutta et al. [10] as:

\[
\frac{U_x}{\delta} = \frac{\partial \sigma_{\text{surf}}}{\partial T} = \frac{\Delta T}{R}
\]

(15)

where \( U_x \) represents the surface velocity scale, \( \Delta T/R \) represents the thermal gradient scale, \( R \) stands for maximum pool width and \( \Delta T \) represents the temperature difference between the area just beneath the laser source and the phase changing boundary. About five grid points are kept within the boundary layer thickness.

In the context of high Reynolds number \( k - \varepsilon \) model, the quantity \( x^+ = \rho C_{\mu}^{1/4} k^{1/2} \Delta x / \mu \) has to be greater than 40 (i.e. \( x^+ > 40 \)) where \( \Delta x \) is the grid spacing. However, in order to have an accurate estimation of wall heat flux, the grid spacing has to be small. This is taken care of by ensuring that \( x^+ \) remains within the following range \( 20 < x^+ \leq 50 \), which ensures that the near wall points fall either within the buffer layer or within the logarithmic layer.

From the above indicated criteria it becomes clear that the grid size spacing along the \( x_2 \) axis is governed by criterion 1, and the grid size spacing along the \( x_1 \) and \( x_3 \) directions are principally
governed by criterion 2. For the topmost five grids a spacing of $2.42 \times 10^{-6}$ m is chosen, $4.8 \times 10^{-6}$ for the three grids immediately beneath, followed by a grid spacing of $7.2 \times 10^{-6}$ m and subsequently a grid spacing of $9.6 \times 10^{-6}$ m over six consecutive grids after which grid spacings of $1.2 \times 10^{-5}$ m and $1.45 \times 10^{-5}$ respectively follow in the negative $x_2$ direction. For the remaining part of the pool, a uniform grid spacing of $2.42 \times 10^{-5}$ m is employed. In regions outside the molten pool, a non uniform less dense grid is chosen. For $x_1$ and $x_3$ directions near the solid-liquid interface, the grid spacing assigned is to be $6.43 \times 10^{-5}$ m after which the grid spacing gradually increases to $1.34 \times 10^{-4}$ m away from the molten zone. In order to discretise the domain of $8mm \times 5mm \times 8mm$ a 69x48x69 Cartesian grid is used. The above choice of grid size is an optimal compromise between numerical accuracy and computational economy. The present study assumes that the laser torch is located at $x_1=4mm$, $x_2=5mm$ and $x_3=4mm$ with respect to the moving co-ordinate system. The computational domain in the region $x_3 \leq 4mm$ is taken to be the Cu work piece and the rest (i.e. $x_3 \geq 4mm$) is considered to be the Ni work piece. The $x_1$-$x_2$ plane at $x_3 = 4mm$ corresponds to the initial contact plane between Cu and Ni work pieces.

Time advancement

For the present problem parameters, the initiation of melting takes about $0.6 ms$. Before melting, heat transfer takes place in conduction mode, where a relatively larger time step (about $0.2 ms$) is chosen so that melting temperature is reached within three time steps. However, with the inception of melting, the high temperature gradient in the pool sets up a strong surface tension-driven flow, leading to a convection-dominated transport. Since, over this time span, convection in the molten pool develops rapidly, a time step size only as small as about $0.05 ms$ can lead towards monotonic convergence of the solution during this period of initial transience. Typically after about $5 ms$, the molten pool reaches a state when changes in dependent variables between the consecutive time steps are relatively small. At this stage, slightly larger time steps (typically about $0.07 ms$) can safely be used to reduce computational time. After $10 ms$, the pool becomes sufficiently developed, which allows time steps as high as $0.0001s$ without any oscillation. After $25 ms$ the laser power is switched off and the molten pool is allowed to cool down for $7.0ms$ as done previously by Phanikumar et al. [15]. By this time, the molten pool resolidifies and the final composition pattern in the weld pool is obtained.

Convergence criteria
The convergence for all the independent variable \((\overline{u_1}, \overline{u_2}, \overline{u_3}, \overline{T}, \overline{C}, k, \varepsilon)\) values are checked after each iteration within a given time step. The convergence is assessed at each grid point based on the method of relative error given by the following criterion:

\[
\left| \frac{\phi - \phi_{old}}{\phi_{max}} \right| \leq 10^{-4}
\]  

(16)

The thermo-solutal dependence of surface tension and simultaneous convergence of all the primitive variables make the simulation procedure computationally intensive. During the pure conduction stage the convergence was achieved in about 50 iterations with a given time step and each time step took about 5 minutes CPU time on a single dual core Intel Xeon processor with 8GB RAM and Intel 5000X motherboard. For both the laminar and turbulent simulations each time step took about 500 iterations to converge during the inception of surface tension driven convection and an each time step typically took 45 minutes to complete. Once the pool is reasonably well developed it takes about 100 and 300 iterations to converge for each time step which required about 9 minutes and 30 minutes of CPU time for laminar and turbulent simulations respectively.

4. RESULTS & DISCUSSION

The Prandtl numbers of molten copper and nickel are about 0.008 and 0.04 respectively. This indicates that, for a given Reynolds number, the Peclet number assumes a much smaller value in the copper molten pool than in the nickel molten pool. This suggests that thermal conduction primarily governs energy transport in the copper molten pool, whereas this effect is likely to be weaker in nickel. For the present values of process parameters, Prandtl number and pool dimensions \(d/d_p\), where \(d\) is the penetration of the molten pool), the molten pool convection in the case of nickel falls within the turbulent flow regime (where turbulence has significant effects on momentum transport and has marginal influence on thermal transport), but also lies near the boundary with the fully turbulent regimes (where turbulence significantly affects both momentum and thermal transport) in the regime diagram as shown in Fig. 1b. By contrast, the molten pool convection in the case of copper falls in the boundary of conduction dominated (where thermal conduction plays the major role in molten pool transport and convection has marginal role in determining molten pool morphology) and turbulent flow regimes (see Fig. 1b). In Fig. 1b, the non-dimensional parameter \(Ch\) is given by [33]:

13
\[ Ch = \left( \frac{\mu}{\rho \left( \frac{\partial \sigma_{sur}}{\partial T} \right)} \right)^{1/3} \]  

(17i)

The non-dimensional parameter \( Ch \) is closely related to Marangoni number \( Ma \) in the following manner:

\[ \frac{(d/r_q)^{2/3}}{Ch^2} \sim Ma = \frac{\rho U_s r_q}{\mu} \]  

(17ii)

where Marangoni number can be taken as a surface-tension driven Reynolds number.

Figure 1b suggests that thermal transport in dissimilar welding of the copper-nickel couple is primarily driven by molecular diffusion (thermal conduction) in the molten pool. As a result, turbulence in the molten pool significantly affects the momentum transport, but the temperature field distribution only gets marginally affected.

The temperature fields obtained from laminar and turbulent transport models after 25ms of laser heating are shown Fig. 2. The isotherms in Figs. 2a-d also indicate that the extent of the molten region in the copper side is smaller than that in the nickel side although the melting point of copper is smaller than the melting point of nickel. Because of the greater value of thermal diffusivity of copper (i.e. \( \alpha_{Cu} > \alpha_{Ni} \), \( \alpha_{Cu} \approx 7 \times 10^{-5} \text{ m}^2 / \text{s} \) (solid); \( \alpha_{Cu} \approx 4 \times 10^{-5} \text{ m}^2 / \text{s} \) (liquid) and \( \alpha_{Ni} \approx 2 \times 10^{-5} \text{ m}^2 / \text{s} \) (solid) ; \( \alpha_{Ni} \approx 1.4 \times 10^{-5} \text{ m}^2 / \text{s} \) (liquid)) the energy input by the laser diffuses more rapidly in copper than in nickel. Thus nickel melts earlier than copper in the case of copper-nickel dissimilar welding. This is consistent with earlier observations [14,15,26]. The temperature fields obtained from laminar and turbulent transport models are qualitatively similar to each other, but the maximum mean temperature obtained from the turbulent simulation is found to be smaller than the maximum temperature attained in the laminar simulation. The enhanced level of thermal diffusion in the turbulent pool acts to reduce the maximum mean temperature in the turbulent transport model (e.g. 2840\(^\circ\)C and 2486\(^\circ\)C are the maximum temperatures obtained from laminar and turbulent simulations respectively), which is found to be in agreement with previous studies [21,24-26].
The temperature field obtained from the melting stage in the welding process provides quantitative information about the solidification parameters at the solidification front. The temperature gradient to solidification rate under quasi-steady condition is given by \( G/(U_{\text{scan}} \cos \beta) \) where \( G \) is the characteristic temperature gradient, \( \beta \) is the angle between the welding direction and normal on the solid-liquid interface on the solidification [34]. The value of \( G/(U_{\text{scan}} \cos \beta) \) can be scaled as: \( G/(U_{\text{scan}} \cos \beta) \sim \Delta T/r_q U_{\text{scan}} \) where temperature gradient is scaled as \( G \sim \Delta T/r_q \). As \( G/(U_{\text{scan}} \cos \beta) \sim \Delta T \times 10^7 \text{K.s/m}^2 \) is much smaller than the characteristic temperature difference to solute diffusivity ratio \( \Delta T/D \sim \Delta T \times 10^9 \text{K.s./m}^2 \) the criterion for planar front stability (i.e. \( G/(U_{\text{scan}} \cos \beta) \geq \Delta T/D \) ) is not satisfied in this particular case and the solidification front is likely to be non-planar. Some of the above non-planar features of solidification in Cu-Ni dissimilar were observed in the experimental data of Phanikumar et al. [35].

The molecular diffusion driven thermal transport can be substantiated by examining the ratio of the turbulent thermal diffusivity to the molecular thermal diffusivity in the molten pool after 25\textit{ms} of laser aided heating as shown in Figs. 3a-c. Figure 3 clearly suggests that the eddy thermal diffusivity remains of the same order of molecular thermal diffusivity. However, as the Prandtl numbers of molten copper and nickel are small in magnitude, momentum transport is likely to be significantly influenced by turbulence while thermal transport gets marginally affected. The velocity vectors and Cu mass fraction distribution obtained from both laminar and turbulent simulations after \( t = 25\text{ms} \) of laser aided heating are shown in Figs. 4 and 5 respectively.

The surface tension gradients \( \partial \sigma_{\text{sur}}/\partial x_1 \) and \( \partial \sigma_{\text{sur}}/\partial x_3 \) at the top surface initiates the convection process in the molten pool. This surface tension gradients \( \partial \sigma_{\text{sur}}/\partial x_1 \) and \( \partial \sigma_{\text{sur}}/\partial x_3 \) are determined by both temperature and concentration gradients as:

\[
\frac{\partial \sigma_{\text{sur}}}{\partial x_1} = \frac{\partial \sigma_{\text{sur}}}{\partial T} \frac{\partial T}{\partial x_1} + \frac{\partial \sigma_{\text{sur}}}{\partial C} \frac{\partial C}{\partial x_1} \quad \text{and} \quad \frac{\partial \sigma_{\text{sur}}}{\partial x_3} = \frac{\partial \sigma_{\text{sur}}}{\partial T} \frac{\partial T}{\partial x_3} + \frac{\partial \sigma_{\text{sur}}}{\partial C} \frac{\partial C}{\partial x_3} \tag{18i}
\]

where according to the present mixing model \( \partial \sigma_{\text{sur}}/\partial T \) and \( \partial \sigma_{\text{sur}}/\partial C \) are given by:

\[
\frac{\partial \sigma_{\text{sur}}}{\partial T} = \left( \frac{\partial \sigma_{\text{sur}}}{\partial T} \right)_{\text{Cu}} C_{\text{Cu}} + \left( \frac{\partial \sigma_{\text{sur}}}{\partial T} \right)_{\text{Ni}} (1-C_{\text{Ni}}) \quad \text{and} \quad \frac{\partial \sigma_{\text{sur}}}{\partial C} = (\sigma_{\text{sur}})_{\text{Cu}} - (\sigma_{\text{sur}})_{\text{Ni}} \tag{18ii}
\]
For both Cu and Ni, $\sigma_{\text{surf}}$ decreases with temperature. Therefore $\partial\sigma_{\text{surf}}/\partial T$ remains negative throughout the molten pool and is in the order of $(-10^{-4}\text{N/m.K})$ (see Table 3). For the molten pool temperature $\partial\sigma_{\text{surf}}/\partial C = (\sigma_{\text{surf}})_\text{Cu} - (\sigma_{\text{surf}})_\text{Ni}$ also remains negative and its magnitude is in the order of $10^{-1}\text{N/m}$. This indicates that the surface tension differential due to temperature gradient $((\partial\sigma_{\text{surf}}/\partial T)\partial T/\partial x_1$ or $(\partial\sigma_{\text{surf}}/\partial T)\partial T/\partial x_3)$ will act in opposition to the surface tension differential due to composition $((\partial\sigma_{\text{surf}}/\partial T)\partial C/\partial x_1$ or $(\partial\sigma_{\text{surf}}/\partial T)\partial C/\partial x_3)$ when temperature and concentration gradients are of different signs. The values of surface tension gradients $\partial\sigma_{\text{surf}}/\partial x_1$ or $\partial\sigma_{\text{surf}}/\partial x_3$ will be of the different signs of the surface tension differential due to temperature gradient $((\partial\sigma_{\text{surf}}/\partial T)\partial T/\partial x_1$ or $(\partial\sigma_{\text{surf}}/\partial T)\partial T/\partial x_3)$ when

$$\left|\frac{\partial\sigma_{\text{surf}}}{\partial T} \frac{\partial T}{\partial x_1}\right| < \left|\frac{\partial\sigma_{\text{surf}}}{\partial C} \frac{\partial C}{\partial x_1}\right| \quad \text{and} \quad \left|\frac{\partial\sigma_{\text{surf}}}{\partial T} \frac{\partial T}{\partial x_3}\right| < \left|\frac{\partial\sigma_{\text{surf}}}{\partial C} \frac{\partial C}{\partial x_3}\right|$$

(19i)

which indicates

$$\left|\frac{\partial T}{\partial x_1}\right| \left|\frac{\partial C}{\partial x_1}\right| \sim O(10^3) \quad \text{and} \quad \left|\frac{\partial T}{\partial x_3}\right| \left|\frac{\partial C}{\partial x_3}\right| \sim O(10^3)$$

(19ii)

The temperature gradient $\partial T/\partial x_1$ and $\partial T/\partial x_3$ can be scaled as $\Delta T/l_\text{ref}$ where $\Delta T$ is the characteristic temperature difference between the maximum temperature location and the pool boundary, and $l_\text{ref}$ is a length scale characterising the pool width ($l_\text{ref} \sim r_\text{q}$). Similarly, $\partial C/\partial x_1$ and $\partial C/\partial x_3$ can be scaled as $\Delta C/l_\text{ref}$ where $\Delta C$ the characteristic difference in Cu concentration between the pool centre and the pool boundary. Comparing Figs. 2a and 2b with Figs. 5a and 5b it is evident that $\Delta T \sim O(10^3)$, whereas $\Delta C \sim O(10^4)$ in the region where surface tension differential due to temperature is in opposition to surface tension differential due to composition. As a result of this, in the present case, $\left|\partial T/\partial x_1\right|/\left|\partial C/\partial x_1\right|$ and $\left|\partial T/\partial x_3\right|/\left|\partial C/\partial x_3\right|$ remain close to the limiting value (in the order of 1000), and the relative strength of thermo-solutal variation determines the surface tension gradient in the top surface. From the velocity vector plots in Figs. 4a and 4b it is evident that, for both laminar and turbulent simulations, surface tension differential due to temperature prevails over the surface tension differential due to composition and the velocity is directed towards the pool boundary from the maximum temperature location. However, it is worth noting from the velocity vector plots (see Figs. 4a and 4b) that the vectors are not exactly radially divergent from the hot temperature.
location, which should have been the case if the convection were determined solely by the temperature induced surface tension differential. In case of turbulent transport, the mean thermal and concentration gradient decreases due to the increase in diffusion strength. This eventually decreases the strength of mean convection in the turbulent pool. Moreover, the maximum value of velocity magnitude in the turbulent pool decreases in comparison to that obtained in case of laminar transport (e.g. the maximum surface velocities obtained from laminar and turbulent simulations are 3.16m/s and 1.90m/s respectively), caused by the greater degree of momentum transport due to turbulent dispersion. As the velocity vectors are directed towards the pool boundary from the maximum temperature location, the heat is transported from the maximum temperature location to pool boundaries by means of advection, in addition to thermal diffusion. In case of turbulent transport, the mean advection is weaker compared to that obtained in the case of laminar transport. This tends to decrease the width of the turbulent pool in comparison to the pool obtained in laminar simulation, which is countered by the increased thermal diffusion strength in the case of turbulent transport. The relative strength of these two mechanisms ultimately determines the pool width in the turbulent pool. It can be seen from comparing Figs. 5a and 5b that the pool width in the turbulent case is found to be marginally smaller than that obtained from the corresponding laminar case.

The fluid flow from the hottest part of the pool to the pool boundary (i.e. solid-liquid interface) transports heat in the longitudinal and sidewise directions. The fluid stream turns in the downward direction as the solid-liquid boundary is approached, as governed by the continuity relation. This gives rise to an asymmetrical toroidal circulation pattern in the molten pool, as evident from the velocity vector plots in longitudinal and cross-sectional mid-planes as shown in Figs. 4c-f for both laminar and turbulent simulations.

The Schmidt number \((Sc = \mu / \rho D)\) of the molten copper-nickel mixture is about \(10^6\) (see Table 3). This suggests that turbulence in the molten pool significantly affects the mass transport in the dissimilar metal weld pool. The eddy thermal diffusivity \(\alpha_t\) is equal to the eddy mass diffusivity \(\alpha_c\) in the present analysis because of the same values of turbulent Prandtl and Schmidt numbers (see Eqs. (8) and (9)). As \(\alpha_t\) remains of the same order of molecular thermal diffusivity in the molten pool (see Figs. 3a-c) the ratio of eddy mass diffusivity to molecular diffusivity (i.e. \(D_t / D\)) remains in the order of \(Sc / Pr\) (i.e. \(D_t / D \sim 10^7 - 10^8\)). This suggests that turbulent diffusion will have significant effects on species distribution in the molten pool, which
can be substantiated by comparing the Cu mass fraction distribution obtained from laminar simulation with the Cu concentration field obtained from the corresponding turbulent simulation. It is apparent from Figs. 5a and 5b that the distribution of Cu concentration in the turbulent pool is significantly different from the corresponding laminar pool. In both laminar and turbulent cases fresh copper is added at the melting front in the copper rich side (the pool boundary in the direction of laser torch movement), which locally increases the copper concentration in the molten pool. The added copper further diffuses towards the centre and then carried towards the Ni side of the molten pool by advective transport owing to fluid flow towards the boundary. In the Ni side of the pool the copper concentration is diluted by the fresh addition of molten Ni inside the molten pool. On the solidification front (pool boundary opposite to the laser torch movement) preferential rejection of solute (Ni here) in the Cu rich side of the molten pool decreases the copper concentration. The preferential rejection of Cu takes place in the solidification front of the Ni rich side, which locally increases the Cu concentration. These effects are evident in both laminar and turbulent pools. However, in the case of laminar pool the iso-concentration contours are severely deformed as the species transport in the laminar pool is primarily advection-driven because of the negligible value of mass diffusivity. By contrast, in case of the turbulent pool, the species distribution pattern shows clear stratification of Cu concentration, which is a characteristic of diffusion process with decreasing Cu concentration from the Cu to Ni side of the molten pool. In the turbulent pool, the mean advection strength becomes weaker than that in the corresponding laminar pool due to greater amount of momentum diffusion. Moreover, mass diffusion strength increases in the turbulent pool because of turbulent mixing; as a result of which eddy mass diffusion plays a significant role in species transport in turbulent molten pools. This leads to the so-called “banded” structure in the Cu concentration field in the turbulent molten pool [14,15]. The fresh nickel added at the melting front on the Ni side ultimately diffuses towards the middle of the molten pool which causes the iso-concentration contours of Cu mass fraction to be concave towards the fresh Ni side of the molten pool. The extent of this behaviour is more prominent in the turbulent pool because of enhanced diffusion rate of Ni. The iso-concentration contours of Cu close to Ni side of the molten pool appear as micro-structural banding in the post-weld experimental analysis of the weld pool section [15]. The sparsely packed iso-concentration contours on the Ni side and densely packed contours on the Cu side are also in agreement with earlier experimental observations [15]. Moreover, the asymmetrical pool shape in Cu and Ni side of the pool is also consistent with earlier findings [13-15].
The laser power is switched off after 25\textit{m}s and the workpiece is allowed to cool down for 7\textit{m}s. By this time, the workpiece is subjected to convective and radiative cooling with the same values of heat transfer coefficients used for different surfaces of the domain during the melting stage. The temperature contours after 7.0\textit{m}s of cooling of molten pools obtained from laminar and turbulent transport models are shown in Fig. 6. Figure 6 clearly indicates that the maximum temperature is smaller than the melting temperature ($T < T_{\text{mel}}$) for both cases. This suggests that the weld pool resolidifies within 7\textit{m}s, and that the temperature fields obtained after re-solidification based on laminar melting simulation are qualitatively similar to those obtained based on turbulent melting simulations. The copper mass fraction fields after re-solidification, resulting from both laminar and turbulent transport simulations are shown in Fig. 7. Comparing Figs. 5 and 7 it can be seen that the composition of the molten pool after re-solidification remains similar to that obtained after the melting stage. The differences between Cu mass fraction fields obtained using laminar and turbulent transport models discussed in the context of Fig. 5 are therefore also valid for Fig. 7. This essentially indicates that the final composition pattern in the Cu-Ni dissimilar weld pool is essentially determined at the melting stage, and the mass transport during the solidification stage does not have a significant effect on the solute distribution in the molten pool.

The macrographs of the top surface and cross-sectional view of a Cu-Ni welded joint for the same laser power and scanning speed obtained from experimental data [15] are shown in Figs. 8a and b respectively. The comparison between the macrographs presented in Figs. 8a and b and the computational Cu mass fraction variations presented in Figs. 5 and 7 suggests good qualitative agreement between the computational and experimental results. In order to assess the quantitative agreement between experimental and computational data, the variations of Ni mass fraction distribution with the normalised distance along the pool width for both laminar and turbulent simulations are compared with the experimental results reported by Phanikumar \textit{et al.} [15] in Fig. 8c for the same laser power and scanning speed. In the case of laminar transport, molten copper is advected to a comparatively larger distance in the molten pool than in the turbulent transport simulation, which decreases the concentration of Ni in comparison to that obtained using turbulent transport, as the major part of the molten pool is created in the Ni-rich side of the welded joint. This effect is much weaker in the turbulent pool due to enhanced mixing, and the decrease in mean advective transport. This is consistent with greater (smaller) mean Cu (Ni) concentration in laminar pool in comparison to turbulent pool as depicted in Fig. 8c. It should also be noted that the concentration profile in laminar simulation is found to be steeper than that
obtained in the case of turbulent simulation. This is due to the smaller mean concentration gradient in turbulent pool than the corresponding laminar pool owing to the more efficient mixing in turbulent transport. It is clearly evident that although both laminar and turbulent simulations capture the qualitative variation of the Ni mass fraction, the concentration profile obtained from turbulent simulation provides more accurate agreement with the experimental profile. Comparing Ni mass fraction distributions obtained from melting phase and after complete solidification, it is evident that the concentration pattern does not get altered significantly in the cooling and solidification stages for both laminar and turbulent transport models. This behaviour can be explained in the following manner. With decreasing temperature, the temperature gradient at the top surface starts to decrease which also weakens the strength of surface-tension driven advection in the molten metal pool. Moreover, the velocity gradient in the molten pool is directly proportional to the temperature gradient in Marangoni convection (see Eq. 18), which also affects the turbulent kinetic energy generation rate in the case of turbulent transport. This suggests that the generation of turbulent kinetic energy decreases with decreasing temperature, which results in a decay in the value of turbulent diffusivity and the flow ultimately re-laminarises before the solidification process. Decreasing advection strength in both laminar and turbulent transport reduces the extent of species transport in the molten pool. In the case of turbulent transport, the decay of turbulent diffusivity decreases the extent of diffusive transport, which further limits the extent of mass transport in the molten pool. As a result of this, the solute distribution in the weld pool is principally determined by the advective and diffusive transport during the melting stage and it does not get altered significantly during the solidification stage of dissimilar metal welding.

5. CONCLUSIONS

Melting and solidification stages of a conduction mode laser dissimilar welding of Cu-Ni couple have been numerically simulated using a finite-volume based approach. The solid-liquid phase change is accounted for by a modified enthalpy porosity technique [28]. The momentum, heat and mass transports in the molten pool are studied using both laminar and turbulent transport models for identical values of laser power and scanning speed. In the laminar transport model, the governing equations of mass, momentum and energy are solved simultaneously in three dimensions, whereas unsteady Reynolds averaged Navier Stokes simulations are carried out for the turbulent simulation, in which turbulent transport is accounted for a high Reynolds number $k-\varepsilon$ model. It has been found that temperature and velocity fields obtained from laminar and turbulent simulations are qualitatively similar to each other. However, the maximum values of mean temperature and velocity magnitude obtained from turbulent simulation are found to be
smaller than the corresponding values obtained from the laminar simulation due to the enhanced level of diffusive transport in the case of turbulent simulation. The species distribution gets significantly affected by the enhanced diffusive strength in the turbulent molten pool and the species distribution obtained from turbulent transport shows a clear structure of concentration stratification. The stratified structure of species distribution, as obtained from the turbulent simulation, where iso-concentration contours are almost parallel to the interface between Cu and Ni workpieces is found to be consistent with earlier experimental results for the same process parameters [15]. The species distribution in the final welded joint is found to be determined principally during the melting stage, and the species distribution does not get altered significantly during cooling and solidification stages of the process because of the weakening of convection strength. This effect is further aided by the decrease in eddy diffusion strength with decreasing temperature in turbulent pools.

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REFERENCES
### Table 1: Diffusion coefficients and source terms for various conservation equations

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<tr>
<th>Governing equations</th>
<th>$\phi$</th>
<th>$f$</th>
<th>$\Gamma$</th>
<th>$S_p$</th>
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<tr>
<td>Continuity</td>
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<td>1</td>
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<td>0</td>
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<tr>
<td>Momentum in $i^{th}$ direction</td>
<td>$\bar{u}_i$</td>
<td>1</td>
<td>$\mu$</td>
<td>$-\frac{\partial P}{\partial x_i} + \rho g \delta_{i} \beta_{i} (\bar{T} - T_{ref}) + \rho g \delta_{i} \beta_{c} (\bar{C} - C_{ref}) + S_f + \rho U_{scan} \frac{\partial \bar{u}_i}{\partial x_1}$</td>
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<td>Energy</td>
<td>$\bar{T}$</td>
<td>$c$</td>
<td>$K$</td>
<td>$-\frac{\partial (\rho \Delta H)}{\partial t} - \frac{\partial}{\partial x_i} (\rho \bar{u}<em>j \Delta H) - U</em>{scan} \frac{\partial (\rho \bar{T})}{\partial x_1}$</td>
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<tr>
<td>Species (Cu mass fraction)</td>
<td>$\bar{C}$</td>
<td>1</td>
<td>$\rho D$</td>
<td>$-\rho U_{scan} \frac{\partial \bar{C}}{\partial x_1}$</td>
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<tr>
<td>Turbulent kinetic energy</td>
<td>$k$</td>
<td>1</td>
<td>$\frac{\mu}{\sigma_k}$</td>
<td>$\mu \left( \frac{\partial \bar{u}_i}{\partial x_j} + \frac{\partial \bar{u}<em>j}{\partial x_i} \right) \frac{\partial \bar{u}<em>i}{\partial x_j} - \frac{\mu}{\sigma_k} g \beta</em>{k} \frac{\partial \bar{C}}{\partial x_2} - \frac{\mu}{\sigma_k} g \beta</em>{r} \frac{\partial \bar{T}}{\partial x_2}$</td>
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<tr>
<td>Dissipation rate of turbulent kinetic energy</td>
<td>$\varepsilon$</td>
<td>1</td>
<td>$\frac{\mu}{\sigma_{\varepsilon}}$</td>
<td>$-C_{el} \mu \left( \frac{\partial \bar{u}<em>i}{\partial x_j} + \frac{\partial \bar{u}<em>j}{\partial x_i} \right) \frac{\partial \varepsilon}{\partial x_j} - \frac{C</em>{el} \mu}{\sigma</em>{\varepsilon}} g \beta_{r} \frac{\partial \bar{T}}{\partial x_2} \frac{\varepsilon}{k} - \frac{C_{el} \rho}{k} \frac{\varepsilon^2}{k} - \rho U_{scan} \frac{\partial \varepsilon}{\partial x_1}$</td>
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Table 2: Table of boundary conditions

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<th>Governing Equation</th>
<th>Top surface</th>
<th>Bottom surface</th>
<th>Side faces</th>
<th>Solid/liquid interface</th>
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<td>Mass and momentum conservation equations</td>
<td>$\bar{u}_2 = 0$</td>
<td>$\bar{u}_1 = 0$</td>
<td>$\bar{u}_1 = 0$</td>
<td>Not required (follows from enthalpy-porosity formulation)</td>
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<td></td>
<td>$\tau_{ij} = \mu \frac{\partial \bar{u}_i}{\partial x_j}$</td>
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<td>$\bar{u}_2 = 0$</td>
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<td>$\partial \sigma_{sur} \frac{\partial \bar{T}}{\partial x_i} + \frac{\partial \sigma_{sur}}{\partial C} \frac{\partial \bar{C}}{\partial x_i}$ (where $i = 1$ and 3)</td>
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<td>Energy conservation equation</td>
<td>$- \bar{K} \frac{\partial \bar{T}}{\partial x_2} + q^*(r) + h(T - T_a) + \epsilon_r \sigma_{rad} \left( T^4 - T_a^4 \right)$</td>
<td>$\frac{\partial \bar{T}}{\partial x_2} = 0$</td>
<td>$- \bar{K} \frac{\partial \bar{T}}{\partial n} = \frac{h(T_{wall} - T_a)}{n}$</td>
<td>Not required</td>
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<td>$\frac{\partial \bar{C}}{\partial x_2} = 0$</td>
<td>$\frac{\partial \bar{C}}{\partial n} = 0$</td>
<td>Freezing front: $- D \frac{\partial \bar{C}}{\partial n} = v_n (1 - k_p) \bar{C}$</td>
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<td>Fusion front: $- D \frac{\partial \bar{C}}{\partial n} = v_n \bar{C}$</td>
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<td>$\epsilon = 0$</td>
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<td>$\beta_T$ (Compressibility)</td>
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<td></td>
<td>$1453^\circ \text{C (Ni)}$</td>
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<td>$L$ (Latent heat)</td>
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<td>$2.90 \times 10^5 \text{ J/kg (Ni)}$</td>
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<td>$7900 \text{ kg/m}^3 \text{ (Ni)}$</td>
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</tr>
<tr>
<td>$D$ (Mass diffusivity Cu in Ni)</td>
<td>$1 \times 10^{-9} \text{ m}^2/\text{s}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$\sigma_{sur}$ (Surface tension)</td>
<td>$1.285-1.3 \times 10^{-4} (T - T_m) \text{ N/m (Cu)}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$1.778-3.4 \times 10^{-4} (T - T_m) \text{ N/m (Ni)}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$T_{ref}$ (Reference temperature)</td>
<td>$1085^\circ \text{C}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$C_{ref}$ (Reference composition)</td>
<td>0.0</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$\mu$ (Viscosity)</td>
<td>$2.85 \times 10^{-3} \text{ Pa.s (Cu)}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$3.68 \times 10^{-3} \text{ Pa.s (Ni)}$</td>
<td></td>
<td></td>
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</tr>
</tbody>
</table>

**$K$ (Thermal conductivity for Cu):**
- Solid phase: 279.5 W/mK
- Liquid phase: 170.7 W/mK

**$K$ (Thermal conductivity for Ni):**
- Solid phase: 75.0 W/mK
- Liquid phase: 62.0 W/mK

**$c$ (Specific heat for Cu):**
- Solid phase: 440.0 J/kgK
- Liquid phase: 484.0 J/kgK

**$c$ (Specific heat for Ni):**
- Solid phase: 515.0 J/kgK
- Liquid phase: 595.0 J/kgK

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1. Based on private communication with Prof. H. K. Bhadeshia, Cambridge University
FIGURE CAPTIONS

Fig. 1: (a) Schematic diagram of continuous laser welding of a Cu-Ni dissimilar couple; (b) The location of molten pool convection for copper and nickel on the regime diagram for thermal transport proposed by Chakraborty [36] for the present processing parameters.

Fig. 2: Isotherms after 25ms of laser heating at the top surface: (a) laminar simulation, (b) turbulent simulation; at the longitudinal mid-plane: (c) laminar simulation, (d) turbulent simulation; at the cross-sectional mid-plane: (e) laminar simulation, (f) turbulent simulation. All the temperatures are shown in °C. In moving co-ordinate system the co-ordinate of the laser torch is \( x = 4 \text{mm} \), and \( z = 4 \text{mm} \). At time \( t = 0 \text{s} \), \( z = 4 \text{mm} \) indicates the separation line between Cu and Ni workpieces.

Fig. 3: The ratio of eddy thermal diffusivity to molecular thermal diffusivity \( \alpha / \alpha \) distribution in the molten pool after 25ms of laser heating: (a) top surface, (b) longitudinal mid-plane, (c) cross-sectional mid-plane. In moving co-ordinate system the co-ordinate of the laser torch is \( x = 4 \text{mm} \), and \( z = 4 \text{mm} \). At time \( t = 0 \text{s} \), \( z = 4 \text{mm} \) indicates the separation line between Cu and Ni workpieces.

Fig. 4: Velocity vectors after 25ms of laser heating at the top surface: (a) laminar simulation, (b) turbulent simulation; longitudinal mid-plane: (c) laminar simulation, (d) turbulent simulation; cross-sectional mid-plane: (e) laminar simulation, (f) turbulent simulation. The isotherms corresponding to \( 1000^0C \), \( 1500^0C \), \( 2000^0C \) and \( 2200^0C \) are shown in Figs. 4a and b by dotted lines. The temperature value increases from circumference to the centre of the plot. In Figs. 4c-f isotherms corresponding to \( 1200^0C \), \( 1500^0C \), \( 2000^0C \) and \( 2200^0C \) are shown by dotted lines. In moving co-ordinate system laser torch located at \( x = 4 \text{mm} \), and \( z = 4 \text{mm} \). At time \( t = 0 \text{s} \), \( z = 4 \text{mm} \) indicates the separation line between Cu and Ni workpieces.

Fig. 5: Contours of Cu mass fraction after 25ms of laser heating at the top surface: (a) laminar simulation, (b) turbulent simulation; at the longitudinal mid-plane: (c) laminar simulation, (d) turbulent simulation; at the cross-sectional mid-plane: (e) laminar simulation, (f) turbulent simulation. In moving co-ordinate system the co-ordinate of the laser torch is \( x = 4 \text{mm} \), and \( z = 4 \text{mm} \). At time \( t = 0 \text{s} \), \( z = 4 \text{mm} \) indicates the separation line between Cu and Ni workpieces.

Fig. 6: Isotherms after 7ms of cooling at the top surface: (a) laminar simulation, (b) turbulent simulation; at the longitudinal mid-plane: (c) laminar simulation, (d) turbulent simulation; at the cross-sectional mid-plane: (e) laminar simulation, (f) turbulent simulation. All the temperatures are shown in °C. In moving co-ordinate system the co-ordinate of the laser torch is \( x = \)
4mm, and $z = 4mm$. At time $t = 0s$ $z= 4.0mm$ indicates the separation line between Cu and Ni workpieces.

Fig. 7: Contours of Cu mass fraction after 7ms of cooling at the top surface: (a) laminar simulation, (b) turbulent simulation; at the longitudinal mid-plane: (c) laminar simulation, (d) turbulent simulation; at the cross-sectional mid-plane: (e) laminar simulation, (f) turbulent simulation. In moving co-ordinate system the co-ordinate of the laser torch is $x = 4mm$, and $z = 4mm$. At time $t = 0s$ $z= 4.0mm$ indicates the separation line between Cu and Ni workpieces.

Fig. 8: Experimental macrographs of the Cu-Ni welded joint for $Q = 3.5KW$ and $U_{scan} = 8mm/s$ according to Phanikumar et al. [15]: (a) top view and (b) cross-sectional view.

(c) comparison between laminar and turbulent simulation predictions and the corresponding experimental results of Phanikumar et al. [15] for Ni mass fraction distribution along the width of the weld pool in the molten state after 25ms of laser heating and in the solidified state after 7ms of cooling following 25ms of laser heating (The distance in the abscissa is normalised with respect to the pool width).
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