



Modelling and analysis of induction preheating of moving filler wire for directed energy deposition

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ABSTRACT

Induction preheating of filler wire is an emerging auxiliary process that allows precise control of wire temperature before melting by a main energy source in directed energy deposition (DED). This can enhance deposition rate and reduce defects. The induction heating mechanism for DED applications needs to be understood for establishing a robust process window that integrates coil design and key process parameters. This study investigates the evolution of electromagnetic and thermal fields during induction heating of a stainless-steel filler wire moving through a helical coil. A coupled electromagnetic-thermal model of a moving wire was developed to determine the magnetic flux, eddy current, temperature, and energy transfer efficiency. The wire temperatures predicted by the multiphysics model are consistent with experimental measurements under diverse conditions, with an error of less than 7 % after the heating reaches a steady state. The typical energy transfer efficiency for a wire diameter of 1.6 mm ranges in 3 %-9 %, which can be significantly enhanced through increasing the wire diameter and reducing the radial distance to the coil. The model enables a deeper understanding of the electromagnetic-thermal mechanisms governing both the transient and steady-state temperature distributions in the wire. In the steady state, the peak temperature is located immediately outside the exit end of the coil, and the temperature gradient across the wire diameter is marginal. A sensitivity analysis to identify dominant parameters was also carried out, showing that the wire feed speed (up to 150 mm/s), coil current (up to 700 A) and frequency (up to 500 kHz) are most influential. This study demonstrates an effective modelling approach to induction heating of moving wire, and it also provides critical insights for designing and optimising the induction coil and process for preheating filler wires in additive manufacturing and other similar processes (e.g. welding and cladding).

1. Introduction

Metal additive manufacturing (AM) utilises metal wire or powder as feedstock to produce or repair structural components through a layer-by-layer process. This technology plays an increasingly important role in next-generation manufacturing industry. Wire-based directed energy deposition (wire-DED) is a major variant of metal AM [1], which is suitable for large-scale and medium-complex structures and has attracted extensive interest from aerospace, maritime, automotive and energy industries, thanks to several advantages such as high deposition rate, efficient material utilisation and cost effectiveness [2–5].

Despite these advantages, higher deposition rate and more precise control of material melting are still demanded to achieve better productivity and accuracy [6]. Filler wire preheating is identified as a particularly effective approach to meeting these demands and offers

additional advantages of low energy consumption [7]. Rui et al. [8] found that the hot wire feed significantly enhanced deposition rate and reduced porosity in wire-DED of an aluminium alloy. For similar parameters, hot wire gas tungsten arc welding (GTAW) led to a wider weld bead and deeper penetration than cold wire GTAW [9]. In previous studies, resistive heating was mostly employed to preheat the filler wire, which effectively reduced the main arc heat input by utilising the extra power supply [10]. However, resistive heating necessitates direct contact between the wire and the melt pool, potentially causing operational issues. For example, excessive arc voltage can cause premature melting of the wire tip, leading to the formation of a bypass arc. The resulting molten droplets may become unstable and explode upon detachment, thereby inducing spatter formation [11]. Moreover, an additional technique would be required to monitor the position of the wire, increasing the complexity of the control system of wire-DED [12].

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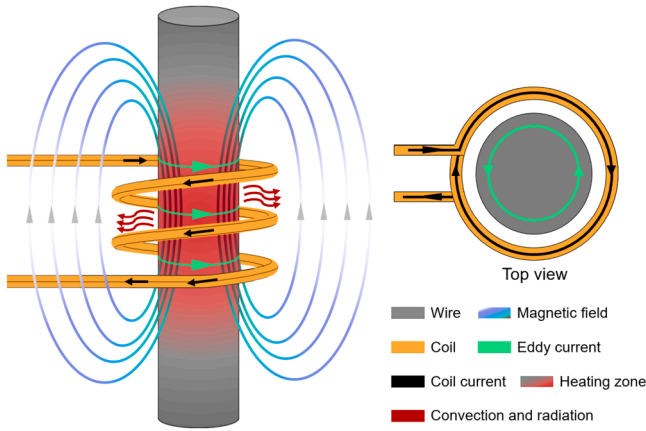


Fig. 1. Schematic of IH-based filler wire preheating for wire-DED.

Bypass heating is another technique to preheat wire under the same power supply as the main electric arc. In this technique, an adjustable resistor connects the bypass to control the heat input distribution between the workpiece and the wire [13,14]. One common limitation of the resistive and bypass preheating techniques is that they can only be applied to feedstock with high resistivity. In addition, the magnetic blow phenomenon associated with the resistive and bypass heating causes arc bias to one side of the hot wire or the opposite side, due to the electromagnetic interactions between the preheated wire and the main arc source, which reduces arc stability [15]. Extensive research has been conducted to mitigate these challenges, with AC and pulse power supplies being employed to minimise the adverse effects of magnetic blow [16,17]. However, a more robust and ideally non-contact method is desirable [12].

Recent experimental studies have investigated the applicability of induction-enabled preheating of filler wire in wire-DED. Induction heating (IH) represents a promising technique for a reliable wire preheating due to its highly controllable, non-contact, and rapid heating characteristics. In contrast to resistive and bypass arc heating, induction preheating can be flexibly integrated into the deposition system and suitable for providing stable heat input during high-speed wire preheating, since it eliminates the need for physical contact between the

wire and electrodes [18,19]. Previous investigations examined various coil current inputs and observed that IH-based wire preheating can enhance GTAW deposition rate under conditions without magnetic blowing. This is because the eddy current generation is confined to the magnetically coupled region near the coil, rather than being distributed along the entire wire [20]. Before wire melting, the temperature needs to be properly controlled. For instance, non-uniform preheating leads to fluctuating melting behaviour and destabilisation of surface forces in the melt pool, resulting in irregular bead formation and heterogeneous grain structures [21,22]. Furthermore, inconsistent temperature of metal droplets intensifies thermal gradients during solidification [23,24], promoting stress concentration and increasing the risk of defects such as cracking and warping [25]. Oppositely, the beads deposited with uniformly preheated wire exhibit grain refinement, which enhances both hardness and tensile properties [26]. A reduction in the welding current due to wire preheating also leads to lower net heat input and arc force, thereby mitigating distortion caused by residual stresses [27–29].

The IH-based wire preheating was also explored for laser wire-DED, which achieved wire temperatures ranging from 100°C to 800°C. Previous investigations into coil configurations suggest multiple coils produce greater heat when the material is below Curie temperature. However, due to the paramagnetic behaviour of the material at elevated temperatures, employing additional coils does not provide further enhancement above the Curie temperature, meaning that fewer coils can exhibit sufficient energy efficiency of IH system [30]. One limitation of the previous process evaluations is that they focused on performance variations among parts deposited with wires preheated at different IH current levels, rather than analysing the specific thermal state of the heated wire. Such an approach overlooks potentially critical insights into how the thermal state of the wire is established and what its implications are for the deposition process and the properties of the final part. For instance, non-uniform preheating can lead to differences in heat distributions or peak temperatures within the wire, thereby affecting melt pool dynamics or material transfer, which remains underexplored.

Obtaining electromagnetic feedback and assessing the temperature field in the wire present significant challenges in practice. The limited availability and operational constraints of measurement equipment hinder the direct experimental evaluation of the magnetic fields and eddy currents. Surface temperature distribution during preheating is typically recorded using thermocouples or infrared imaging; however,

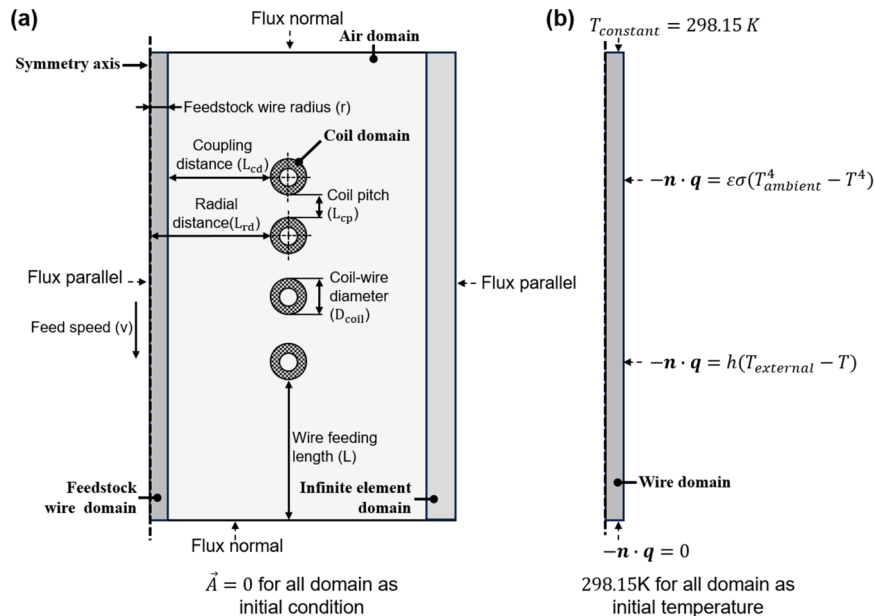


Fig. 2. Illustration of different domains, geometric parameters and boundary conditions for the IH of a moving filler wire: (a) electromagnetic field, (b) thermal field.

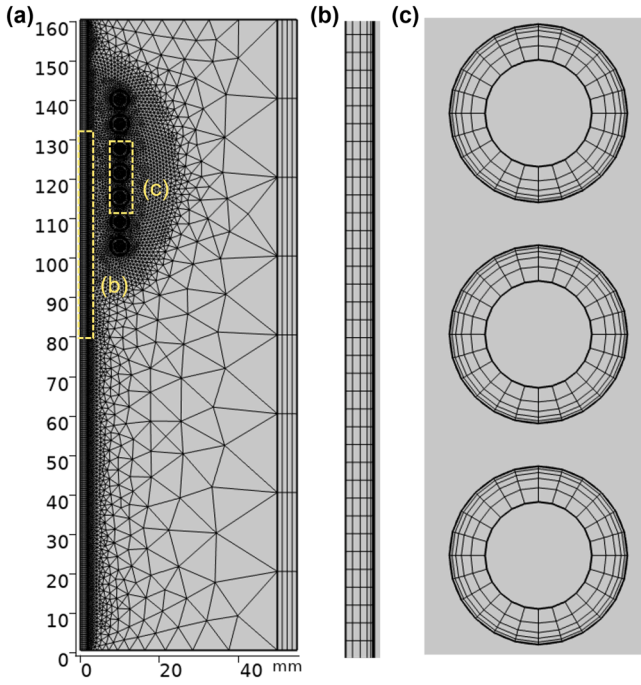


Fig. 3. (a) Mesh of finite element model and zoomed-in views of the boundary layer meshes of the wire (b) and the coil (c).

Table 1

Chemical composition of the ER307 stainless steel filler wire (unit: wt%).

C	Cr	Ni	Mo	Mn	Si	P	S	Cu	Fe
0.07	18.6	8.0	1.2	7.1	0.8	0.03	0.03	0.75	Balance

these measurement techniques are impossible to obtain the temperature distribution inside the wire. Therefore, the complexity of IH process variables necessitates an in-depth modelling study to understand the wire's electromagnetic and thermal behaviours.

Multiphysics modelling has been introduced to understand and optimise IH processes. Most previous numerical simulations of IH aimed to evaluate the efficiency in metal processing and heat treatment industries, such as quenching and hardening, which have similarities to the feedstock preheating of wire-DED in terms of the basic mechanisms [31–34]. However, there are significant differences in material form and process parameters. Most wire-DED feedstocks are wires with a diameter less than 3 mm [35,23] and the deposition uses a wire feed speed ranging from 5 mm/s to 200 mm/s [36,37], which are markedly different from traditional IH-based heat treatment [38,39]. On the other hand, several studies evaluated induction-based wire melting processes to optimise coil geometry and process parameters to efficiently melt stationary wires [40–42]. To date, none of the available modelling studies were found to address induction preheating of moving filler wire. Therefore, although previous IH process simulations demonstrate the principles and benefits of IH, a moving wire preheating model with high computational efficiency still needs to be developed for wire-DED.

In this study, multiphysics numerical modelling is conducted to investigate the electromagnetic and thermal phenomena of IH for preheating of a moving filler wire under different process parameters and coil designs. The aim is to unravel the complex interactions between magnetic flux, eddy current, and material temperature in the context of wire-DED, and provide insights into the parametric sensitivity.

2. Material and methods

2.1. Numerical modelling: physical principles and multiphysics formulation

During the IH process the alternating coil current generates in its surroundings a magnetic field with the same frequency, as shown in Fig. 1. The magnetic field induces eddy currents in a conductor located inside or outside the coil, and the direction is opposite to the coil current. The flow of eddy currents heats the workpiece by Joule heat due to the resistance of the workpiece. In ferromagnetic materials, heat is also generated by hysteresis, which is the loss of energy when the internal magnetic domains realign under the influence of an external changing magnetic field.

Under complex electromagnetic interaction, the eddy current distribution within a conductor or workpiece is not uniform, resulting in temperature gradients. The skin effect means that the current distribution is not uniform when the alternating eddy current flows through a workpiece, and the current is concentrated in the outer area of the conductor but less in the inner region. The maximum current density is located on the conductor's surface, which has uniform electromagnetic physical properties. This effect becomes more significant when the frequency of the alternating current increases, which means that the effective resistance of the conductor increases, causing faster heating with a smaller heating depth. The general formula for skin depth is [43]:

$$\delta = \sqrt{\frac{2\rho_{ER}}{\omega\mu}} = \sqrt{\frac{1}{\pi f\mu\sigma}} \quad (1)$$

where δ is skin depth (m), ρ_{ER} is electrical resistivity ($\Omega\cdot\text{m}$), μ is magnetic permeability (H/m), i.e., $\mu = \mu_0 \times \mu_r$ where μ_0 is vacuum magnetic permeability and μ_r is relative magnetic permeability, ω is angular frequency of current, f is frequency (Hz), and σ is electrical conductivity (S/m).

2.1.1. Electromagnetic field

Maxwell's equations express the laws of the electromagnetic field and describe the relationship between the basic electromagnetic variables. Eq. (2) describes an alternating current in a coil inducing a dynamic magnetic field. Eq. (3) relates the magnetic flux density to an electric field generated by the coil. Eq. (4) and Eq. (5) describe space constraints on electric and magnetic field distributions, respectively.

$$\nabla \times \vec{H} = \vec{j} + \frac{\partial \vec{D}}{\partial t} = \vec{j}_e + \vec{j}_s + \frac{\partial \vec{D}}{\partial t} \quad (2)$$

$$\nabla \times \vec{E} = -\frac{\partial \vec{B}}{\partial t} \quad (3)$$

$$\nabla \times \vec{B} = 0 \quad (4)$$

$$\nabla \times \vec{D} = \rho_{ED} \quad (5)$$

where $\vec{H}$ is the magnetic field intensity (A/m), $\vec{j}$ is the current density (A/m²), $\vec{j}_e$ is the externally generated current density (A/m²), $\vec{j}_s$ is the source current density (A/m²), $\vec{D}$ is the displacement (C/m²), $\vec{E}$ is the electric field intensity (V/m), $\vec{B}$ is the magnetic flux intensity (A/m), and ρ_{ED} is the electric density (C/m³).

Since the magnetic field is a closed loop, a physical condition is required for the continuity of the magnetic flux in the computational domain. The magnetic vector potential $\vec{A}$ is an auxiliary variable and defined as the curl of the magnetic flux density $\vec{B}$ in Eq. (6) to ensure that the divergence of magnetic field is 0 based on Helmholtz's theorem. The electric field intensity $\vec{E}$ can also be described by $\vec{A}$ by

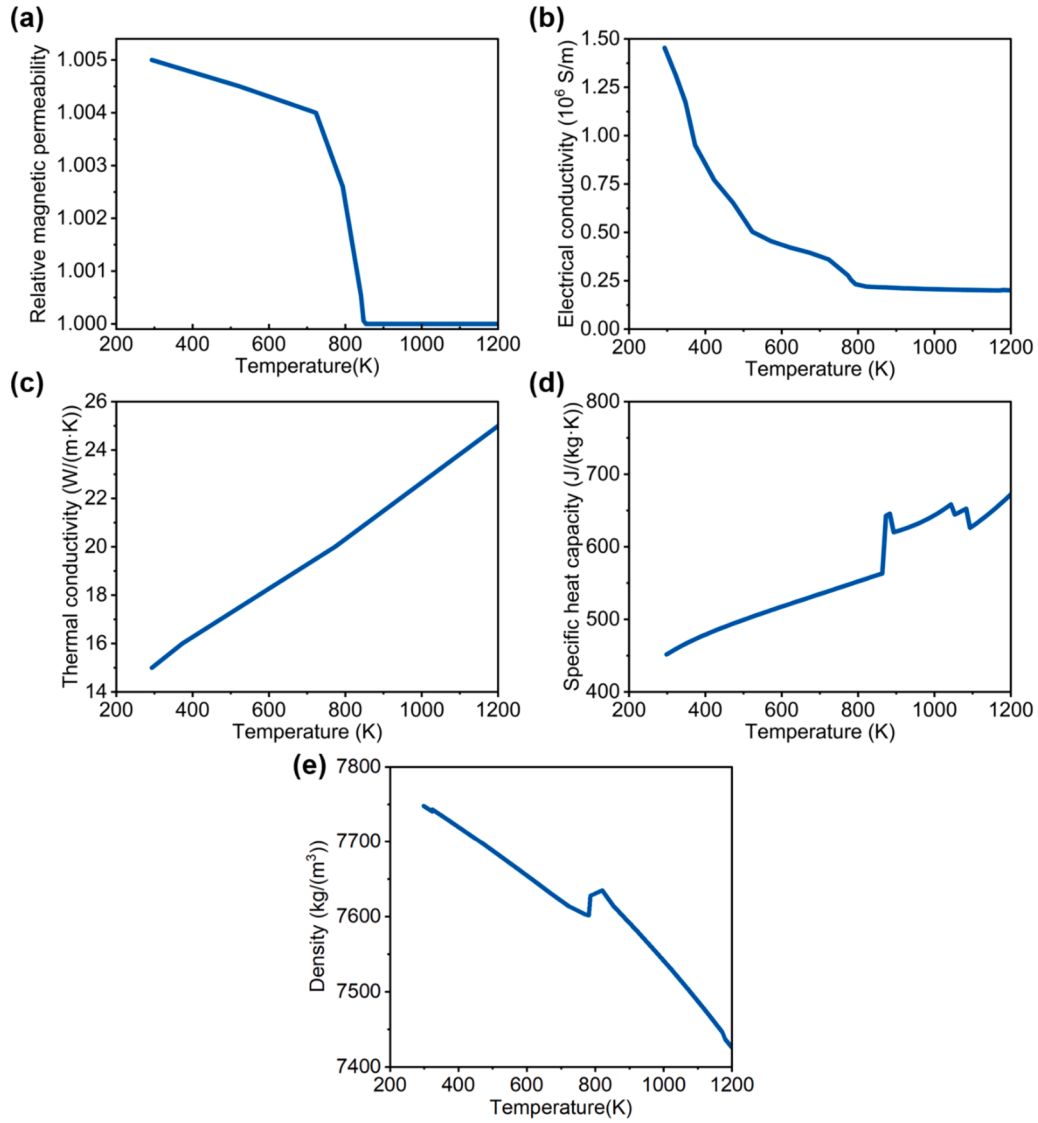


Fig. 4. Electromagnetic and thermal properties versus temperature for ER307 stainless steel: (a) relative magnetic permeability, (b) electrical conductivity, (c) thermal conductivity, (d) specific heat capacity, and (e) density.

Table 2

Mesh sensitivity analysis in terms of the differences in results and computational time (the percentage in bracket shows the relative difference from the model with the finest mesh, i.e. 0.25 maximum element size).

Maximum element size (mm)	0.25 mm	0.5 mm	1 mm	2 mm
Total number of elements	103126	32351 (-69 %)	11593 (-89 %)	3394 (-97 %)
Peak temperature (K)	1033.654	1033.654 (0 %)	1033.654 (0 %)	1033.536 (-0.01 %)
Peak magnetic flux density in the air domain (T)	0.0965	0.0968 (0.3 %)	0.0971 (0.6 %)	0.0995 (3.1 %)
Peak magnetic flux density on the wire (T)	0.0602	0.0602 (0 %)	0.0603 (0.2 %)	0.0597 (-0.8 %)
Peak eddy current density (A/m ²)	5.2234e7	5.2234e7 (0 %)	5.223e7 (-0.008 %)	5.228e7 (0.09 %)
Calculation time (s)	11966	3098 (-74 %)	1773 (-85 %)	890 (-93 %)

substituting Eq. (6) into Eq. (3). The electromagnetic induction phenomenon in the wire is governed by Eq. (7) [18].

$$\vec{B} \rightarrow = \nabla \times \vec{A} \rightarrow \quad (6)$$

$$\vec{E} \rightarrow = -\frac{\partial \vec{A} \rightarrow}{\partial t} \quad (7)$$

Eqs. (8)–(10) define the constitutive behaviour of the material. The electromagnetic constitutive relations are derived through the magnetic vector potential $\vec{A} \rightarrow$ to predict the eddy current distribution in the wire [44–46]. Both the alternating magnetic field and the motion of a wire with varying cross-sectional area can contribute to the generation of eddy currents. To build the magnetic field model, the variations in the magnetic flux and the eddy currents induced by the translational motion of the wire are assumed to be negligible, given the high dimensional precision of the stainless-steel wire used in the experiment (diameter deviation < 0.2 % [47]).

$$\vec{B} \rightarrow = \mu \vec{H} \rightarrow \quad (8)$$

$$\vec{D} \rightarrow = \epsilon_0 \vec{E} \rightarrow \quad (9)$$

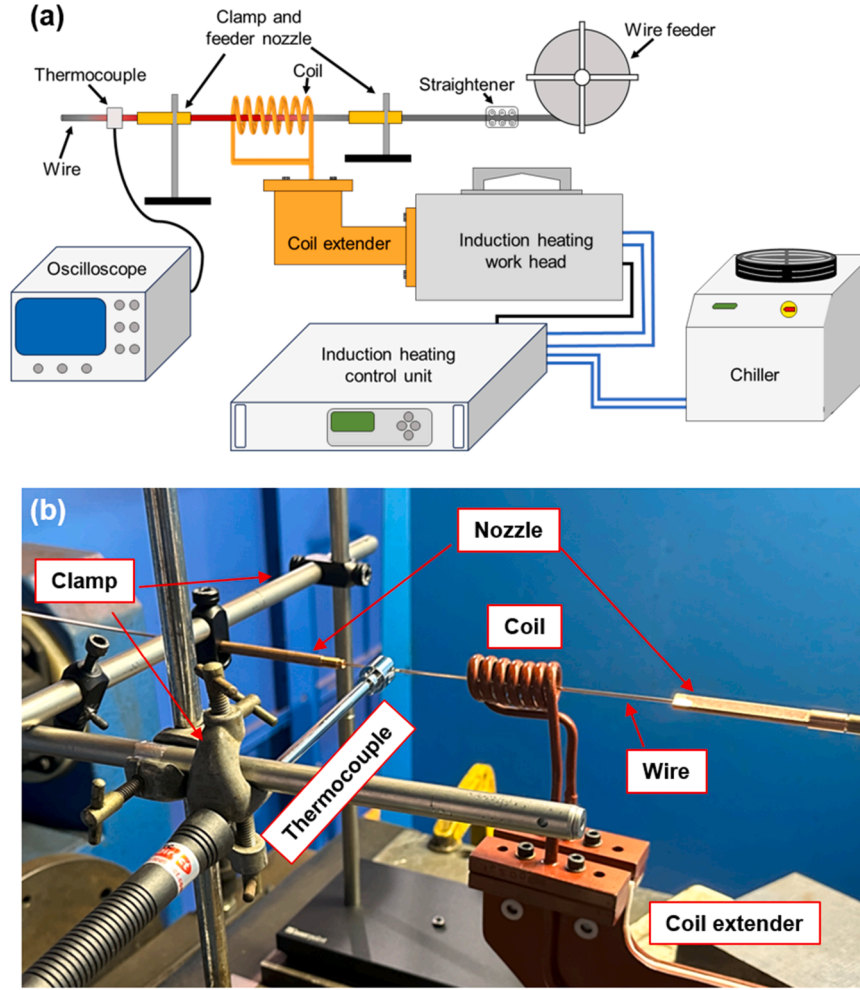


Fig. 5. Schematic (a) and actual experimental setup (b) of the IH system for preheating a moving filler wire typically used in wire-DED.

Table 3
Experimental process parameters for moving filler wire heating.

Parameters	Case 1	Case 2	Case 3	Case 4	Case 5	Case 6			
Current (A)	100	200	300	100	200	300			
Frequency (kHz)	355 (all cases)								
Wire feed speed (mm/s)	10 (Cases 1-3)			20 (Cases 4-6)					
Wire diameter (mm)	1.6 (all cases)								
Heating time (s)	60 (all cases)								

Table 4
Finite element model parameters for IH of the moving filler wire.

Parameters	Values
Wire length (mm)	160
Coil turns	7
Radial distance (mm)	7.5
Coil-wire diameter (mm)	5
Coil pitch (mm)	1.2

$$\vec{j}_s = \sigma \vec{E} \quad (10)$$

where μ is the magnetic permeability (H/m), and ϵ_0 is the permittivity of vacuum (F/m).

In IH electromagnetic analysis, the frequency-domain method is

preferred over the time-domain method as it eliminates temporal derivatives and facilitates the efficient computation of steady-state solutions for alternating magnetic fields. By integrating constitutive relations Eqs. (6)–(8) into Eq. (2) and performing the field transformation in the time domain, the frequency-domain equation is as follows:

$$(j\omega\sigma - \epsilon_0\omega^2 + \mu^{-1}\nabla^2)\vec{A} = \vec{j}_e \quad (11)$$

To solve Eq. (11) in each computational domain to obtain the key variables of electromagnetic-thermal coupling in the wire, the following boundary conditions are specified in the computational domain to describe the electromagnetic field. The magnetic insulation boundary condition requires that the tangential component of the magnetic vector potential is zero, which is expressed as:

$$\vec{n} \times \vec{A} = 0 \quad (12)$$

where $\vec{n}$ is the unit normal of the boundary.

For the Dirichlet (flux parallel) boundary condition, we have:

$$\vec{A} = 0 \quad (13)$$

For the Neumann (flux normal) boundary condition, we have:

$$\frac{\partial \vec{A}}{\partial n} = 0 \quad (14)$$

The energy transfer efficiency to the wire is given by Eq. (15) [48]. The electromagnetic loss density $P_{\text{magnetic field}}$ in the magnetic field

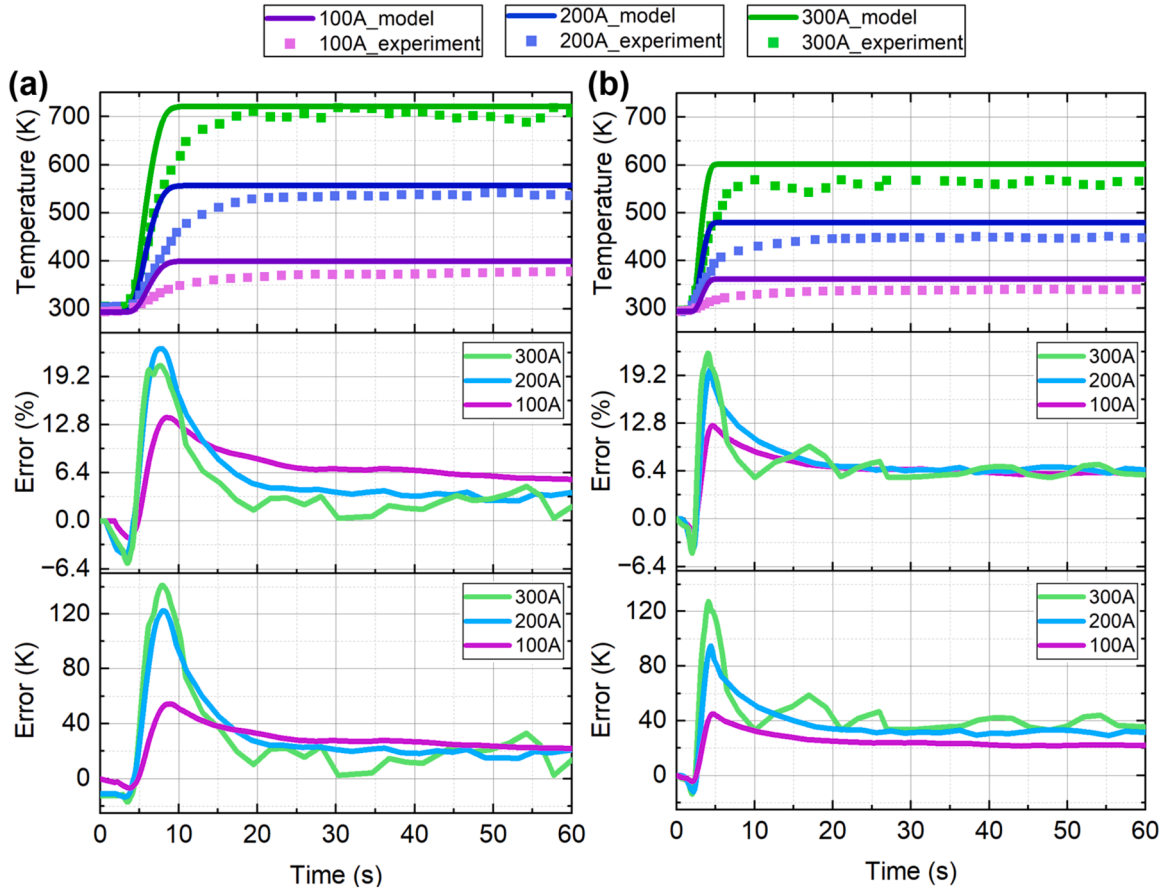


Fig. 6. Comparison between model predictions and thermocouple measurements for coil currents of 100 A, 200 A, and 300 A with a wire feed speed of: (a) 10 mm/s; (b) 20 mm/s.

analysis is used as the IH heat source for thermal modelling. It represents the transferred power to the wire. The power of the coil, P_{coil} , refers to the total energy input per unit time in the IH system.

$$\begin{aligned} \text{Energy transfer efficiency} &= \left(\frac{P_{\text{magnetic field}}}{P_{\text{coil}}} \right) 100\% \\ &= \left(\frac{\int \int \int u}{\left(\sum_{i=1}^n V_{\text{coil}i} \right) \times I_{\text{coil}}} \right) 100\% \end{aligned} \quad (15)$$

where $P_{\text{magnetic field}}$ is the power transferred by the electromagnetic field to the wire (W), P_{coil} is the coil power (W), u is the electromagnetic volumetric loss density in the wire (W/m^3), $\int \int \int u$ is the transferred power to the wire, V_{coil} is the local coil potential, n is the number of coil turns, and I_{coil} is the coil current (A).

2.1.2. Thermal field

Joule heat and hysteresis heat are the primary heating modes during IH process. Continuous motion of a wire, as it passes through a coil, introduces dynamic changes in temperature distribution in the frequency-transient analysis; the governing equation of the heating process can be described as follows:

$$\rho_M C_p \frac{\partial T}{\partial t} + \rho_M C_p \mathbf{u} \cdot \nabla T - \nabla \cdot (k \nabla T) = Q_e = Q_{rh} + Q_{ml} \quad (16)$$

$$Q_{rh} = \frac{1}{2} Re(J \cdot E) \quad (17)$$

$$Q_{ml} = \frac{1}{2} Re(j \omega B \cdot H) \quad (18)$$

where ρ_M is density (kg/m^3), C_p is specific heat capacity ($\text{J}/(\text{kg} \cdot \text{K})$), $\mathbf{u}$ is wire feed speed (m/s), ∇T is temperature gradient (K/m), k is thermal conductivity ($\text{W}/(\text{m} \cdot \text{K})$), Q_e is the heat source, Q_{rh} is the resistive losses of eddy current (W/m^3), Q_{ml} is the magnetic losses (hysteresis) (W/m^3), Re is the real part of the product, j is the imaginary unit, and ω is the angular frequency (rad/s).

For a constant wire feed speed, each wire segment experiences the same heating cycle to pass through the coil, and a repetitive and predictable pattern of temperature distribution is produced, which does not vary significantly with time. In this context, the IH of a moving wire is considered in a quasi-steady state. For steady-state analysis, the impact of time factors on the system state is not considered.

$$\rho C_p \mathbf{u} \cdot \nabla T - \nabla \cdot (k \nabla T) = Q_e = Q_{rh} + Q_{ml} \quad (19)$$

Convection and radiation are the boundary conditions of the thermal field, and the heat flux formulae are as follows:

$$-\mathbf{n} \cdot \mathbf{q} = h(T_{\text{external}} - T) \quad (20)$$

$$-\mathbf{n} \cdot \mathbf{q} = \varepsilon \sigma (T_{\text{ambient}}^4 - T^4) \quad (21)$$

where $\mathbf{q}$ is heat flux (W/m^2), h is convection coefficient ($\text{W}/(\text{m}^2 \cdot \text{K})$), T_{external} is temperature of external environment (K), ε is emissivity of wire surface, σ is Stefan-Boltzmann constant ($5.670374419 \times 10^{-8} \text{ W}/(\text{m}^2 \cdot \text{K}^4)$), and T_{ambient} is ambient temperature (K).

2.2. Finite element model: numerical technique and model definition

There is a relative motion between the wire and the coil during the wire feeding process. The thermal effect of IH depends on not only the

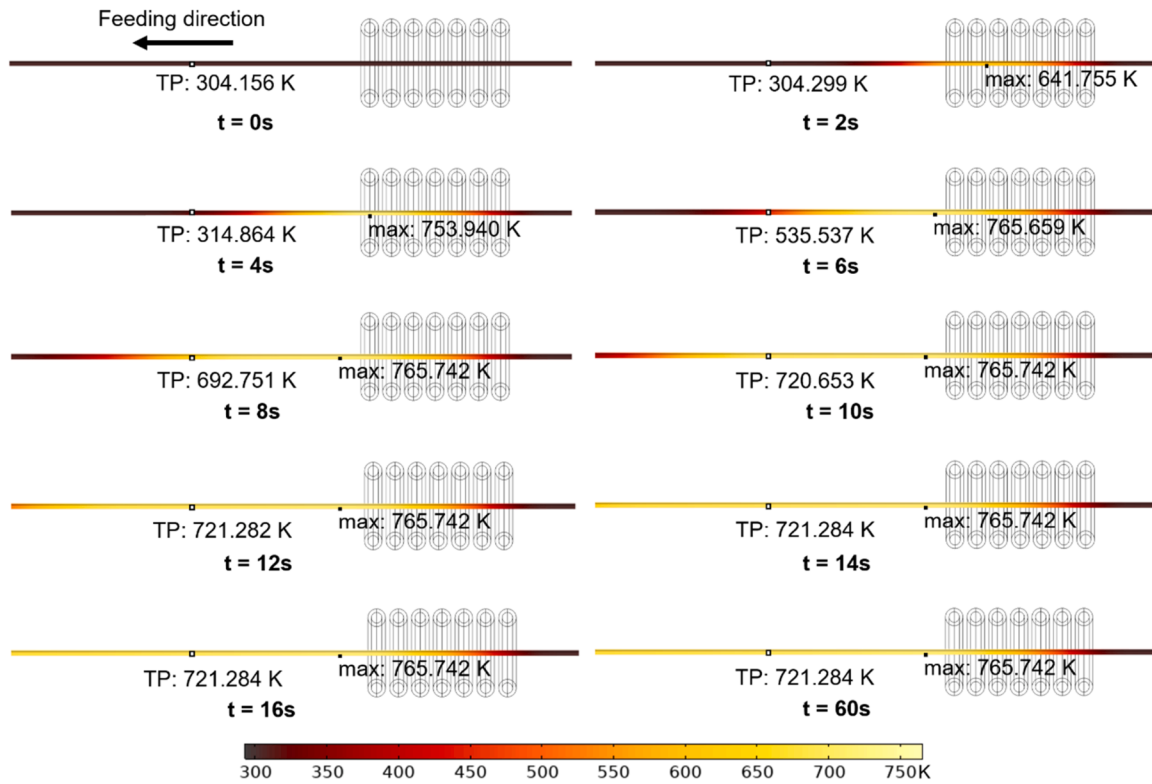


Fig. 7. Evolution of the predicted temperature distribution on the wire with a coil current of 300 A and a wire feed speed of 10 mm/s (TP is the thermocouple position, t is the time after switching on the inductor, max is the point where the maximum temperature is reached). The axisymmetric model result is visualised in 3D.

position of the wire but also the thermal history of the wire that has passed through the coil. Previous studies used various dynamic modelling techniques to calculate the transient thermal effect. For instance, Sun et al. [49] and Prasad et al. [50] used the moving mesh method to simulate the dynamic interaction between the wire and coil. The finite element model was divided into moving and static parts; continuity conditions were set at the boundaries of the two parts to ensure the continuity of fields and fluxes. This approach avoids rebuilding the model with a new mesh after each solution step, and there is no requirement to change material properties at different locations before the next step. However, when the moving mesh method deals with relative motion with large displacements, the mesh will distort during the motion, adversely affecting the computational convergence and precision. In addition, small time steps and the actual length of the wire need to be considered for the model to accurately calculate the heat distribution in the wire, which leads to high demand on computing resources.

In this study, a wire at a given feed speed translates through the coil and the motion path is predefined. Given the steady translational motion and geometrical regularity of the wire, a Eulerian frame method is adopted with greater robustness than the moving mesh method to avoid distortion. This method assumes that the wire translates along a predetermined trajectory, and all domains update boundary conditions at each time step to provide feedback on the new position of the wire. This method reduces the complexity of the model by simulating the “flow” of the wire within the existing mesh. It obtains results at a low computational cost without mesh motion or update [51].

Owing to the axial symmetry of the geometry and process, an efficient 2D axisymmetric numerical model was developed to couple electromagnetic and thermal fields using the COMSOL Multiphysics software package. A schematic of the IH system is shown in Fig. 2a, which consists of wire, coil, and air domain. The coil is assumed to be a toroid, and the heating effect associated with the helical shape is

neglected. The simplification of the helical coil shape is the same as that reported in Ref. [52], which has good consistency in the overall temperature of the workpiece. In this study, the wire with a given motion speed continuously passes through the coil; all domains are considered in the electromagnetic analysis, while only the wire domain, for which the heating is of primary interest, is considered in the thermal analysis. This treatment is helpful to improve the computational efficiency.

Fig. 2a also shows the electromagnetic boundary conditions. Magnetic insulation boundary condition $\mathbf{n} \times \vec{\mathbf{A}} = 0$ was set to the outermost boundary of the air domain. The direction of the induced magnetic field is parallel to the symmetry axis. The flux parallel boundary condition was imposed on boundaries parallel to the symmetry axis; conversely, the boundaries perpendicular to the symmetry axis were subjected to the flux vertical boundary condition. In addition, the initial magnetic vector of the whole model was set to be zero.

The electromagnetic field extends infinitely and gradually decays in space. When the air domain in the model is too small, the magnetic insulation boundary condition truncates the magnetic flux, causing field reflection and distortion near the wire, which leads to inaccurate predictions of the induced current and temperature. Although enlarging the air domain can improve accuracy, the remote magnetic field contributes negligibly to wire heating while significantly increasing computational cost. Therefore, an infinite element domain was added at the outer boundary to terminate the air domain, reducing the model size by mapping the radial axis in the cylindrical coordinate system for converting an infinite air domain to a finite domain. The radial coordinate r and the mapping coordinate ξ can be described by the following relation, $\Delta r = \frac{\xi}{\gamma - \xi} \Delta p - \Delta_w \xi$, in which Δ_w is the width of the infinite element domain, Δ_p is pole distance, and γ is a number larger than one. This transformation allows the magnetic field to decay naturally while maintaining high accuracy near the coil and wire [53,54].

Fig. 2b shows the thermal boundary conditions. Since the wire has

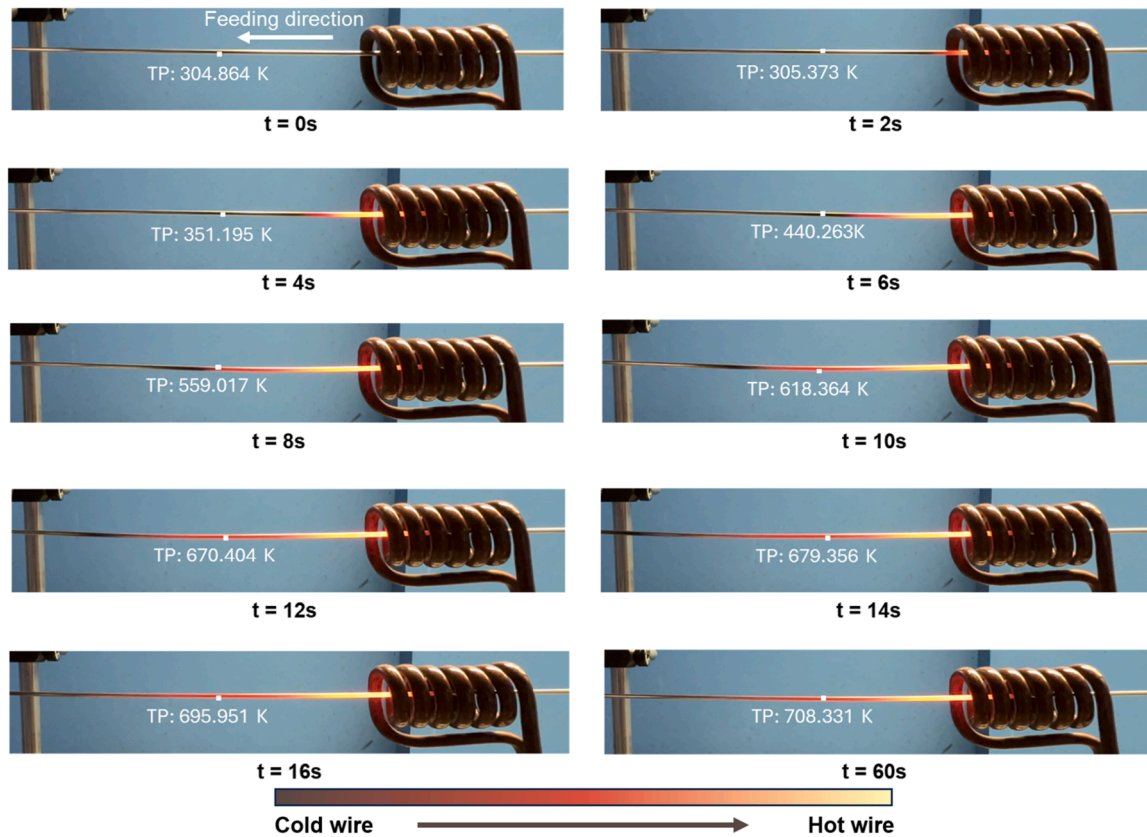


Fig. 8. Experimental observation of the wire heating at different moments when the coil current was 300 A and the wire feed speed was 10 mm/s (TP is the thermocouple position, and t is the time after switching on the inductor). The colours of wire after heating qualitatively indicate the temperature level, and the range from high temperature to low temperature corresponds to the colours of white, yellow, orange, bright red and dark red.

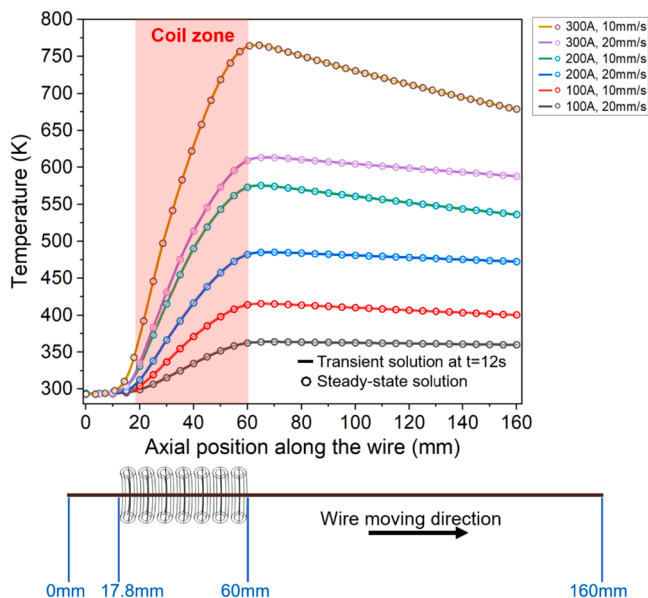


Fig. 9. Temperature distributions on the wire surface at various coil currents and wire feed speeds.

been continuously fed before the induction heating system starts working, the model only considers a representative part of the moving wire. The top boundary of the wire inlet end is at a constant room temperature to represent the initial wire temperature before passing the coil. The bottom outlet end is an imaginary interior end without

exposure to air, so no convection and radiation are involved. The heat flux at this end due to thermal conduction is difficult to predefine as the thermal gradient is unknown beforehand. Nevertheless, as this end is located far from the coil and the end area is much smaller than the surface area, a thermal insulation boundary condition was applied as an approximation. For the wire surface, the model adopted a convection coefficient of $15 \text{ W}/(\text{m}^2 \cdot \text{K})$ and an emissivity of 0.5 [55].

Fig. 3 shows the geometry and mesh of the finite element model. Quadrilateral elements were used to discretise the wire and coil domains, while triangular elements were used for the air domain. Infinite elements on the outer boundary of the air domain also have quadrilateral shape. Due to the skinning effect, fine-graded meshes were used in the wire's eddy current area and the coil's outer surface. The surface element thickness was one-tenth of the skin depth [34,56,57]; the thickness increased by 1.5 times for a larger distance from the surface. In the air domain, fine meshes were also used near the coil and wire to capture the complicated geometry and the gradient of the physical variables, respectively.

The MUMPS solver in COMSOL was employed due to its high numerical robustness and suitability for large-scale, nonlinear coupled multiphysics problems. As a direct sparse solver, MUMPS offers improved stability over iterative methods, particularly in handling ill-conditioned systems. Furthermore, it supports efficient parallel execution across multiple processor cores, which significantly accelerates computation. These features make MUMPS particularly well-suited for steady-state and time-dependent simulations involving strong coupling between physics interfaces. The numerical simulations were performed using an AMD Ryzen PRO 5945WX (12 cores). Linear interpolation was employed, and the time integration was carried out using the backward Euler scheme, with the maximum time step limited to 0.01 s.

Table 1 lists the chemical composition of the austenitic stainless steel

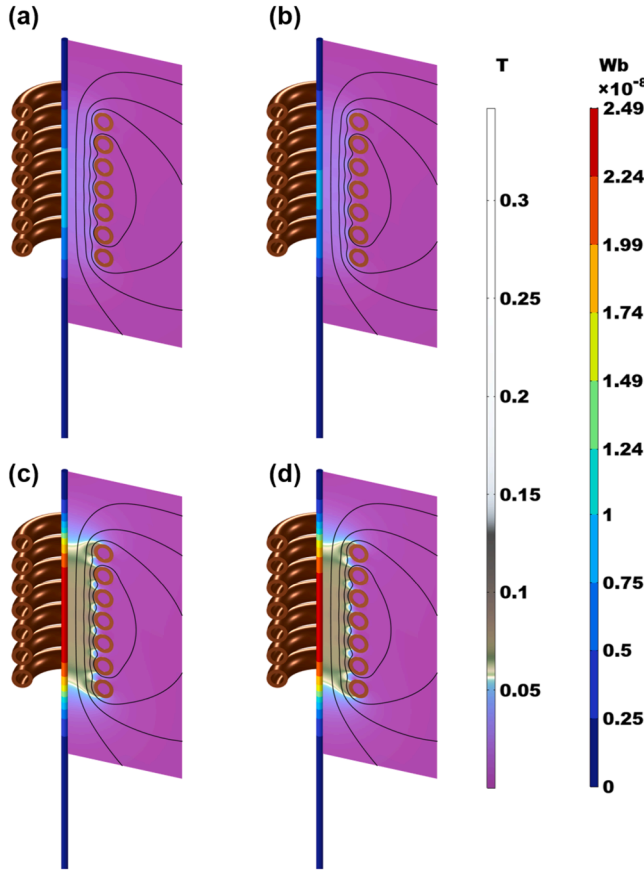


Fig. 10. Magnetic field lines and magnetic flux density (unit: T) in the air domain slices, and magnetic flux (unit: Wb) in the wire domain under the steady state: (a) current: 100 A, feed speed: 5 mm/s, (b) current: 100 A, feed speed: 20 mm/s, (c) current: 300 A, feed speed: 5 mm/s, (d) current: 300 A, feed speed: 20 mm/s (the axisymmetric model result is visualised in 3D).

(ER307) filler wire with a diameter of 1.6 mm. Fig. 4 shows the

thermophysical properties used for the numerical simulation [58,59]. Austenitic steel is classified as paramagnetic steel, with a relative magnetic permeability close to 1.005 [60–62] and a Curie temperature of 820 K [63], and the melting point is 1673 K and the latent heat is 285 kJ/kg.

To eliminate the potential influence of mesh density on the simulation results, a mesh sensitivity analysis was conducted. The different trials were performed under a coil current of 250 A, a frequency of 300 kHz, and a wire feed speed of 10 mm/s. From Table 2, it is clearly seen that the results were almost unchanged when the maximum element size reduced from 1 mm to 0.25 mm, indicating a sufficient mesh density, which was adopted in the simulations presented in Section 3.

2.3. Validation experiment

A series of experiments were conducted to validate the IH model. The schematic diagram of moving wire heating setup is shown in Fig. 5, which comprises an IH system with helical coil, coil extender, power supply (Ambrell EASYHEAT), chiller, wire feeding system, wire straightener, K-type sliding thermocouple with a measurement range of 273.15 K – 873.15 K, clamp, and acquisition oscilloscope (DL750). The thermocouple was placed 50 mm away from the coil to monitor the temperature of the wire surface without interference from the magnetic field generated by the inductor. The flexible spring allowed the thermocouple to maintain consistent contact with the wire surface, ensuring accurate temperature recording without disturbing the wire motion. The choice of contact thermocouple over thermal camera is due to the emissivity calibration challenge and resolution limit of infrared imaging for the thin wire. It should be noted that the experimental setup shown in Fig. 5 does not represent wire feeding in a wire-DED process but rather a bespoke experiment for validating the IH model. In the wire-DED process with induction preheating, the wire is directly fed into the melt pool after leaving the coil. The present study focuses exclusively on the induction preheating of the moving filler wire. The clamps in the experiment were used only to hold the wire nozzle and the thermocouple in place during the measurement, while these clamps are not essential for a wire-DED process.

Table 3 lists the experimental process parameters. Two groups of experiments with different wire feed speeds and coil currents were

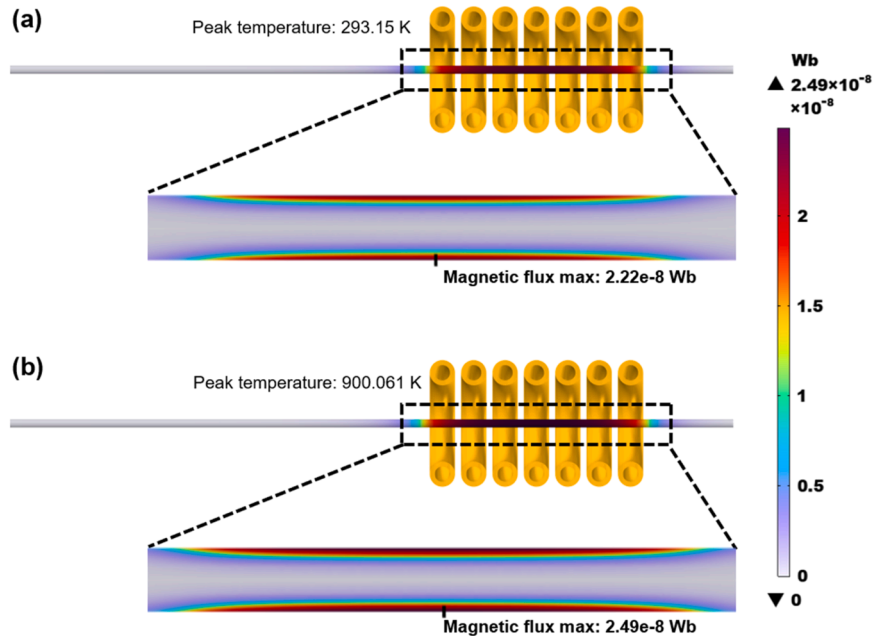


Fig. 11. Magnetic flux on wire surface and cross-section for the current of 300 A and feed speed of 5 mm/s: (a) inductor switch-on moment, $t = 0$ s, (b) steady state, $t = 60$ s (the axisymmetric model result is visualised in 3D).

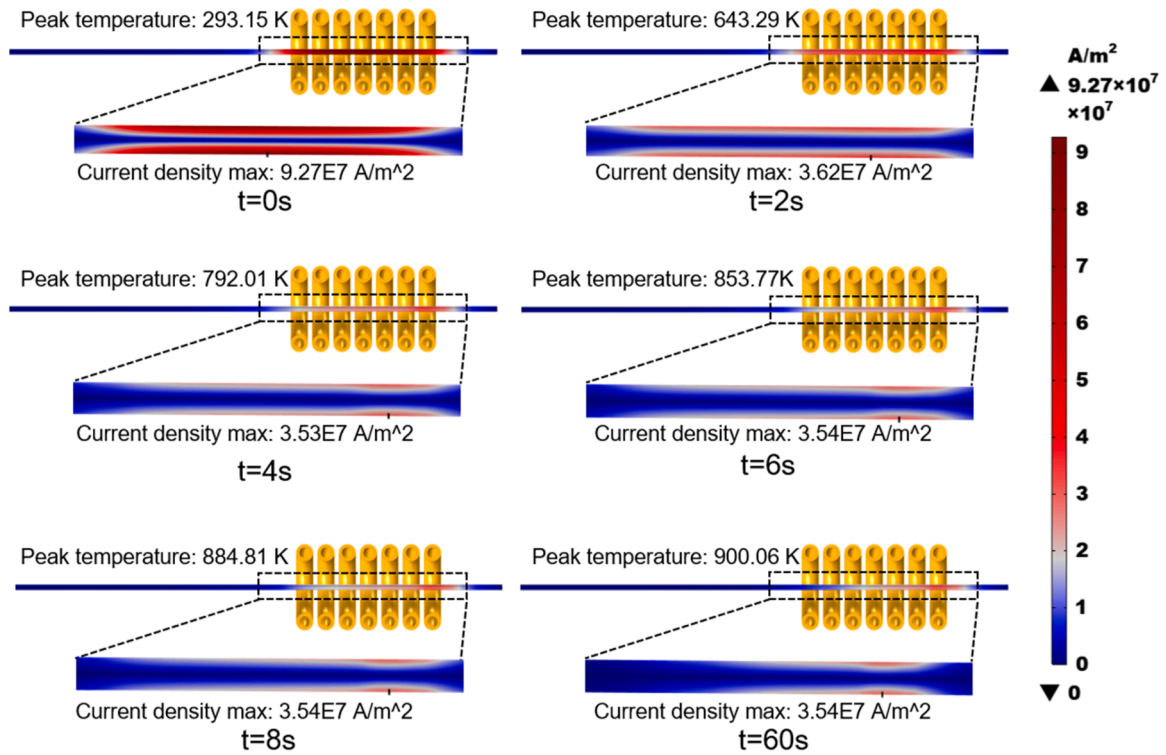


Fig. 12. The eddy current density in the wire when the coil current was 300 A and the wire feed speed was 5 mm/s (inductor activated at $t = 0$ s). The zoomed-in view shows the cross-section distribution of Eddy current (the axisymmetric model result is visualised in 3D).

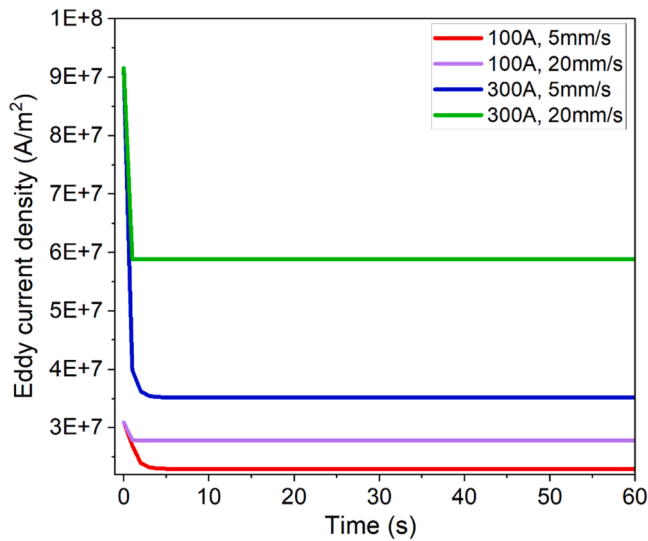


Fig. 13. Maximum eddy current density in the wire vs heating time of the wire.

conducted to investigate their effects. The inductor was switched on after the wire feeding reached the specified speed.

3. Results and discussion

3.1. Experimental validation of the multiphysics model

3.1.1. Predicted and measured temperatures

A transient model was created using the parameters same as those in the experiments (Tables 3 and 4), and Fig. 6 compares the modelling and experimental results. The temperature at the thermocouple position rapidly increased and then remained stable. The time to attain the

steady state was significantly shorter for the 20 mm/s wire feeding than for the 10 mm/s wire feeding. The transient model prediction and experimental measurement demonstrate a good overall agreement, meaning that the surface temperature is accurately predicted, with most errors below 13 % and the steady-state temperature prediction error is even less than 7 %. The slight overprediction of the steady-state temperature in the model could be attributed to the simplifications (e.g. 2D representation of 3D helical coil) made in the model and the uncertain fluctuation of the current and frequency in the IH system. Another discrepancy is that the temperature reached the steady state more rapidly in the model than in the experiment. This could be due to the limitation of the thermocouple recording, i.e., the diameter of the wire is much smaller than that of the contact probe, and the thermal inertia of the heavy spring could slow down the response time, resulting in the measured temperature taking longer to reach the steady state. Nevertheless, similar discrepancy was reported in the previous IH study using a thermal camera for temperature measurement, which involved uncertainty in emissivity calibration [44]. Considering the size and speed of the wire, a thermal camera with sufficiently high resolution and frame rate is required to capture the temperature profile. Our experiments indicate that the thermocouple can circumvent the challenge of the emissivity calibration and the limited resolution of infrared imaging for thin wires, but some errors could be present due to challenges in using contact method to measure the temperature of such a thin wire.

Fig. 7 shows the predicted temperature distributions during the heating with a current of 300 A and a feed speed of 10 mm/s. As the wire moved, the temperature at the thermocouple position (TP) increased rapidly and reached a steady state after 12 s. The temperature distribution in the wire is non-uniform along the axial direction, and the model predictions are qualitatively consistent with the experimental observations shown in Fig. 8. The location of the peak temperature was near the centre of the coil zone when the heating started, but then it gradually moved to the edge and stabilised at a location immediately outside the coil after 8 s. A previous study on IH-based wire melting showed similar variation in maximum temperature in the coil zone, but

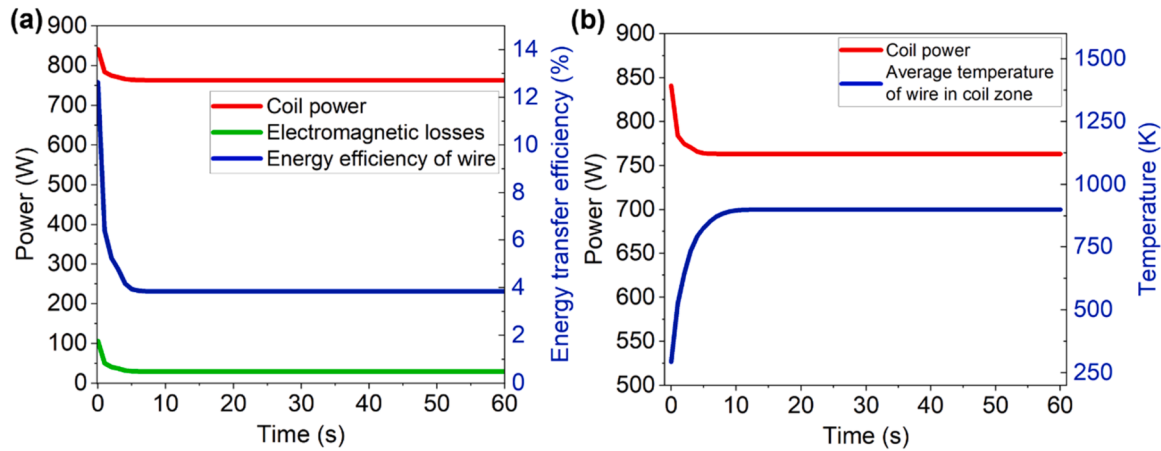


Fig. 14. (a) Coil power and energy transfer efficiency, and (b) coil power and wire temperature variations with heating time for coil current of 300 A and wire feed speed of 5 mm/s.

Table 5

Energy transfer efficiency for various values of wire diameter and radial distance between the wire and the coil (Current = 300 A, feed speed = 5 mm/s, other parameters are same as those in Tables 3 and 4).

Wire diameter (mm)	Energy transfer efficiency (%)	Radial distance (mm)	Energy transfer efficiency (%)
1	0.96	3	8.79
1.6	3.84	4	6.91
2	7.54	5	5.67
3	24.06	6	4.78
4	44.58	7	4.12
5	61.51	7.5	3.84
6	72.52	8	3.60
7	79.06	9	3.18

it did not consider wire temperature after passing through the coil since the wire was melted inside the coil [50].

3.1.2. Steady-state model for efficient analysis

The transient model (Section 3.1.1) predicted the temperature field in the steady-state period shortly after the heating initiation. To save computational time, a steady-state model was also developed with the same parameters (Table 4). The computational time was 36 s for the steady-state model but 1666 s for the transient model, meaning 97.8 % saving by the former. Fig. 9 shows the temperature distribution on the wire surface, as predicted by the steady-state model and transient model ($t = 12$ s), when currents were 100 A, 200 A and 300 A, and feed speeds were 10 mm/s and 20 mm/s. The total length of the wire in the model was 160 mm. The region from 0 mm to 17.8 mm is outside the coil, indicating that the wire does not enter the coil. The region from 17.8 mm to 60 mm is the coil zone, which represents the length of the seven-turn helical coil and the central heating area. The region from 60 mm to 160 mm corresponds to the heated wire after passing through the coil. The temperature of the wire rose rapidly after entering the coil zone, and the highest temperature was achieved when the wire left the coil. The identical temperature profiles predicted by the transient and steady-state models confirm that the steady-state model is accurate for extensive parametric sensitivity analysis in the subsequent sections. However, the transient model is still needed for analysing the evolution of the electromagnetic and thermal fields, which are of fundamental interest and also guide practical management of the unstable preheating when the wire feeding and inducting heating are just initiated in wire-DED deposition.

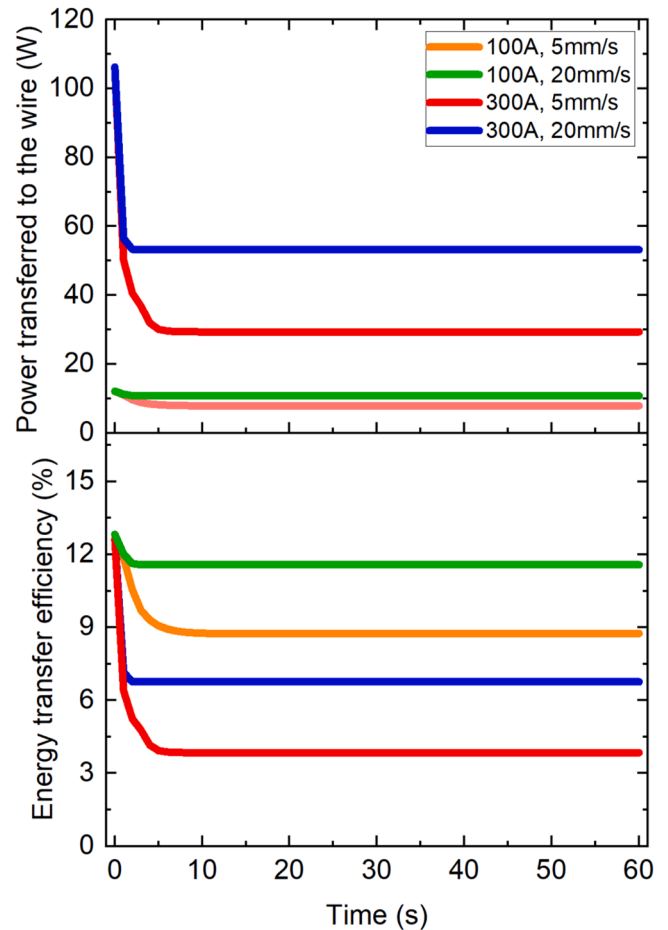


Fig. 15. Power transferred to wire and energy efficiency for different coil currents and wire feed speeds.

3.2. Electromagnetic field analysis

Four sets of IH process parameters are considered for analysing the magnetic field, as shown in Fig. 10. The magnetic field is concentrated on the inner side of the helical coil, and the magnetic field intensity in the air area close to the coil is much greater than that in the surrounding area. Consequently, the magnetic flux density of the wire is also concentrated in the coil zone, i.e., the peak is at the centre of the coil.

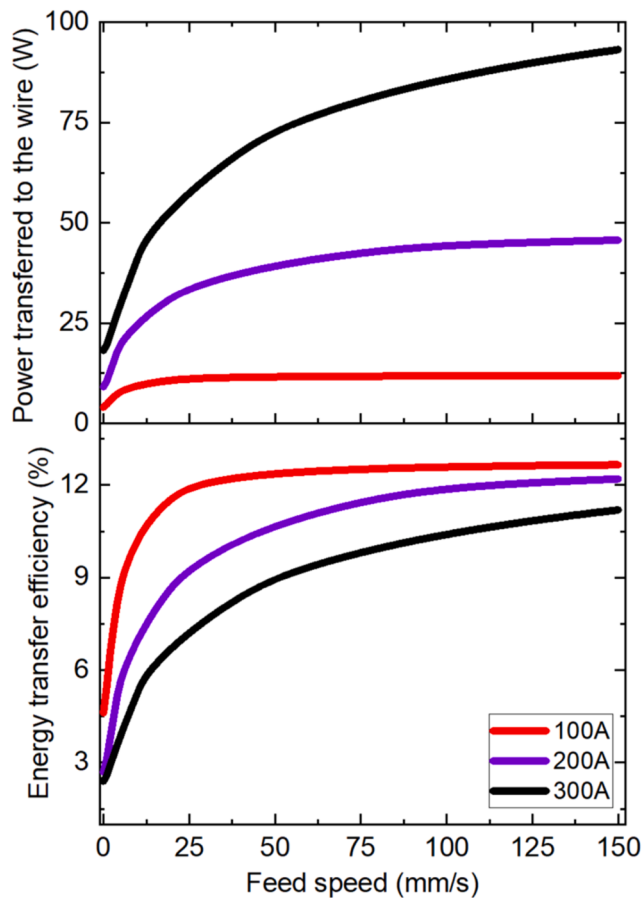


Fig. 16. Steady-state power transfer and energy efficiency with various feed speeds for currents of 100 A, 200 A and 300 A.

The magnetic field excited by the coil depends on the input current and frequency, which are constant during heating because the model ignores the potential heat loss and energy fluctuations in the IH system. The peak magnetic flux densities were 0.12 T and 0.35 T at the coil currents of 100 A and 300 A, respectively. The secondary magnetic field generated by the eddy current in the wire has negligible effect, as demonstrated by previous studies on the magnetic field strength during IH processes [52]. A decrease in magnetic flux on the wire is observed when the wire feed speed increased from 5 mm/s to 20 mm/s, which can be attributed to the temperature rise during the wire heating, which causes decreases in the relative magnetic permeability and electrical conductivity of the material (Fig. 4).

Fig. 11 shows the influence of the wire temperature on the magnetisation based on the transient model, which is helpful for understanding the evolution of the magnetic field. The magnetic flux penetrates more deeply into the interior of the wire in the steady state than at the initial stage, indicating that the wire has a high reluctance path and is less effective in concentrating the magnetic field. As a result, the reduction in magnetisation properties at elevated temperatures weakens the ability to guide the magnetic flux and increases the volume of material through which the magnetic field lines pass, distributing the magnetic flux over a larger area within the wire [64].

Fig. 12 shows the predicted eddy current density in the wire when the coil current was 300 A and the wire feed speed was 5 mm/s. The transient model enables analysis of the evolution of the eddy current distribution. The initial current density was concentrated near the wire surface with a large radial gradient, distributed symmetrically around the coil centre in the axial direction, and then quickly decayed from the coil zone edges where the wire entered. When the inductor was just activated ($t = 0$ s), the wire was at room temperature without any

thermal gradient and the eddy current density distribution inside the coil was approximately uniform along the wire axis. Subsequently, the eddy current density decreased significantly within 2 s and approached a steady state after 4 s (see, for example, peak current in Fig. 13), which occurred due to the complex interplay between eddy current and heat, i. e., the current density decreased when the temperature rose (Fig. 7). These phenomena are consistent with the magnetic response of a stainless-steel billet at different IH temperatures reported in the literature [63]. As the heating progressed, the wire temperature in the coil zone was non-uniformly increased, leading to the rapid variation in eddy current density distribution. Generally, the coil current affects the eddy current through electromagnetic induction. A higher coil current induces greater eddy currents in the workpiece. Regarding the effect of wire feed speed, the exposure time of wire to the magnetic field plays a significant role. A reduced feed speed prolongs the exposure, thereby elevating the wire temperature and diminishing eddy current magnitude. Additionally, at higher temperatures, the attenuated skin effect causes a less concentrated eddy current distribution.

Energy transfer efficiency is another important consideration for evaluating the IH-based filler wire preheating, which is examined to quantify the proportion of the energy transferred to the wire. The energy transfer efficiency is calculated using Eq. (15). Fig. 14a shows that the maximum efficiency was 12.63 % at 0 s, followed by a rapid decrease to 3.84 %. The low efficiency can be attributed to the long coupling distance and the weak magnetic properties of the austenitic stainless steel.

An additional analysis through varying wire diameter and coil size was conducted to identify factors influencing the efficiency, as summarised in Table 5. It is found that increasing the wire diameter significantly enhanced the energy transfer efficiency from 0.96 % ($\varnothing 1$ mm) to 79.06 % ($\varnothing 7$ mm). The reduced coupling distance and increased wire volume enhanced the magnetic flux. In contrast, reducing the radial distance (coil size) only slightly increased the efficiency, from 3.18 % (9 mm) to 8.79 % (3 mm), as the constant wire volume and input current limit the magnetic field intensity and flux.

Fig. 14a also shows the power reduction over time in the coil during the process, as predicted by the transient model. This trend is caused by the coil being excited by the introduction of an external electric field, which relates to an exciting potential difference in the model, inducing the current density equal to the set current [65]. As in the experiment, the coil excitation was set to a constant current in the model. Since the eddy current in the wire works through induction to oppose the current flowing in the coils, and the electrical conductivity of steel decreases with temperature, the system reduces power to maintain a prescribed current in the coil, as shown in Fig. 14b. This finding supports the feasibility of combining energy transfer efficiency and circuit regulation mechanisms to evaluate the performance of IH systems, thereby predicting the power demand for IH and designing efficient coils to meet the target heating temperature. Despite the low efficiency for the stainless steel, the IH offers unique advantages for integration with wire-DED due to its ability to precisely control wire preheating temperature and non-contact nature, which enables accurate thermal management. Furthermore, the efficiency of IH for ferromagnetic materials (e.g. mild steel) is expected to be much higher [57]. Therefore, IH remains a highly competitive solution to filler wire preheating.

3.3. Thermal field analysis

The energy transfer and loss are critical for determining the IH temperature field in the wire. Fig. 15 shows that the transient peak transferred power was 12.1 W and 106.23 W for the coil currents of 100 A and 300 A, respectively, which was attained at the beginning of heating ($t = 0$ s), independently of the wire feed speed. The power quickly dropped and reached steady state afterwards. The transferred power in the steady state period is dependent on the wire feed speed: the faster the wire feed, the higher the transferred power and energy efficiency (Fig. 15). Considering the electromagnetic material properties,

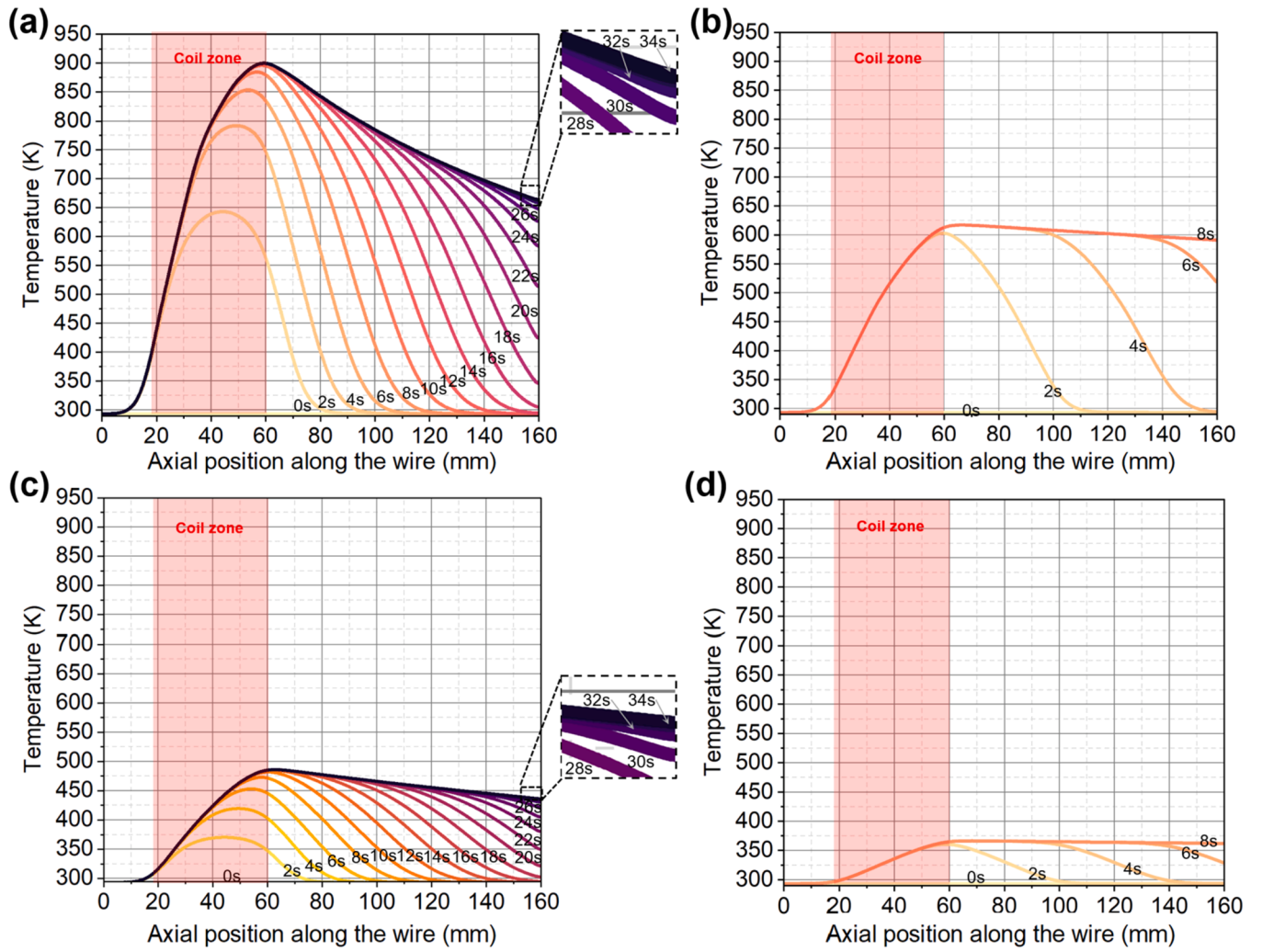


Fig. 17. Surface temperature distributions along wire axis at different moments: (a) current: 300 A, feed speed: 5 mm/s; (b) current: 300 A, feed speed: 20 mm/s; (c) current: 100 A, feed speed: 5 mm/s; (d) current: 100 A, feed speed: 20 mm/s.

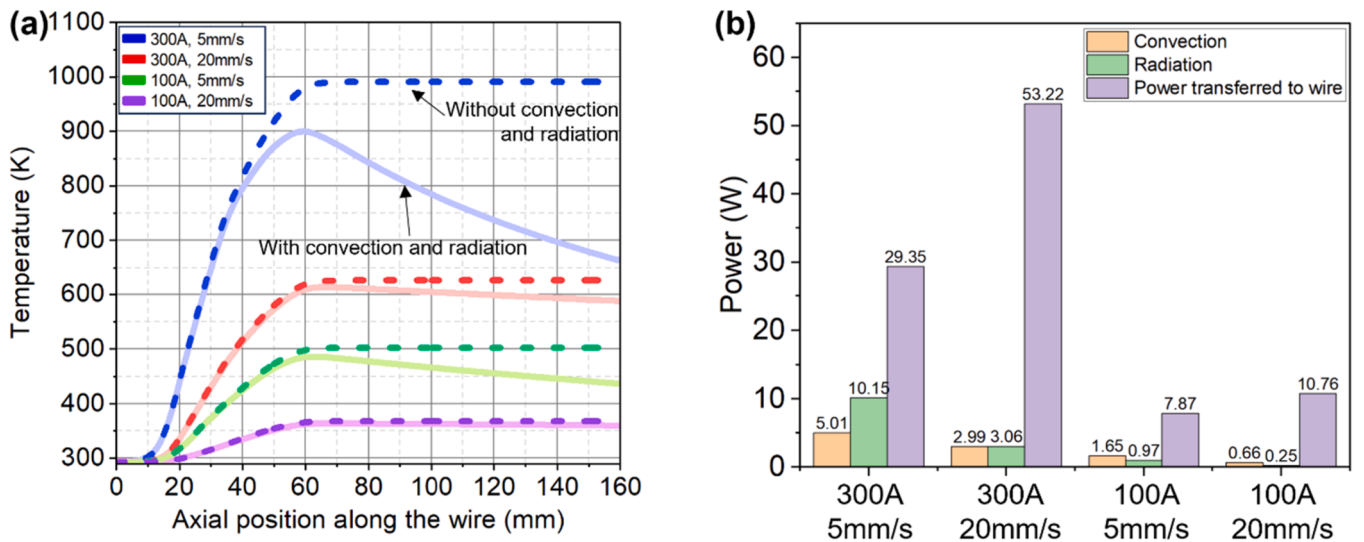


Fig. 18. (a) Surface temperature distribution of wire in the steady state; (b) Power analysis for each case.

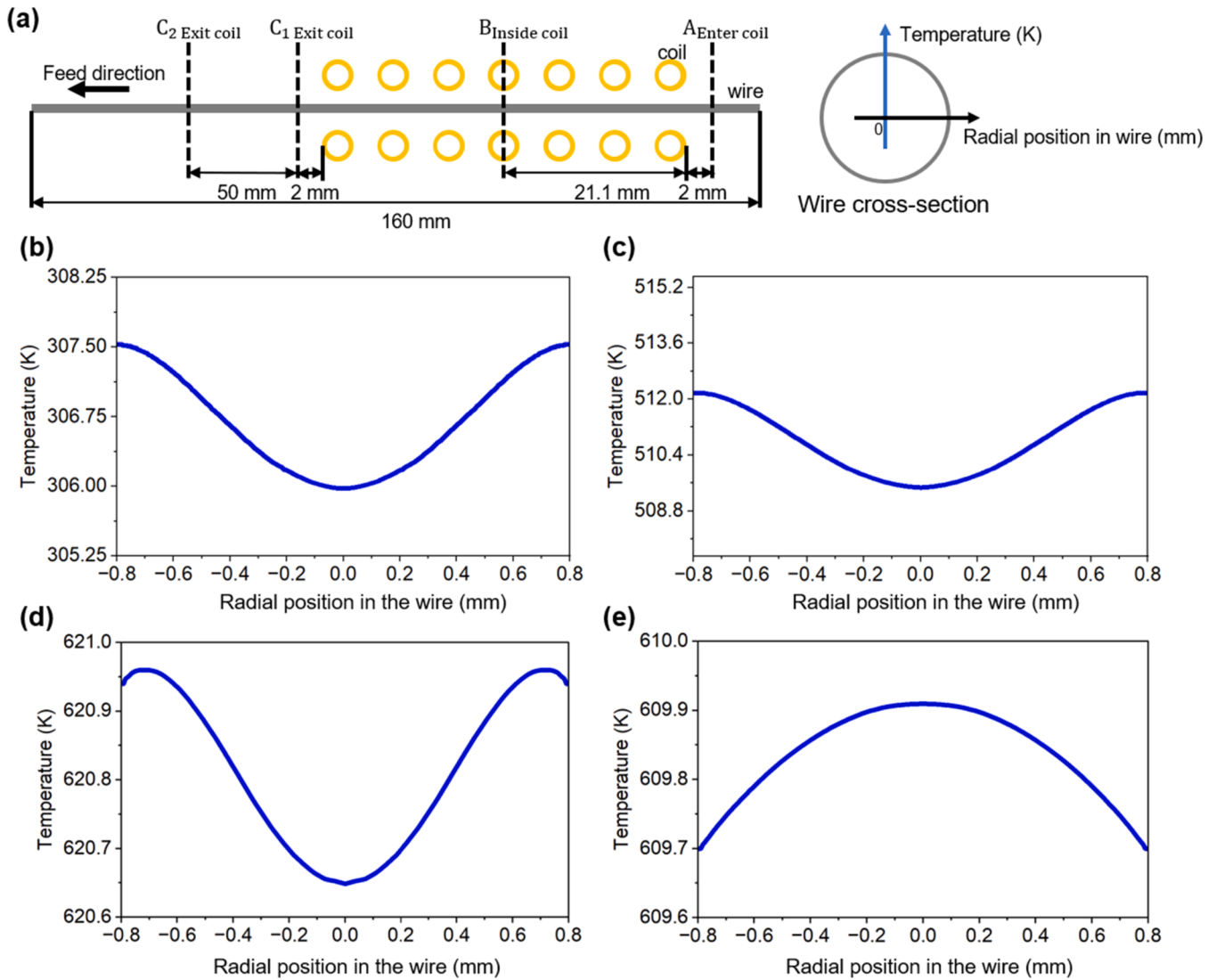


Fig. 19. (a) Schematic of wire cross-section in the computational domain. Radial temperature distributions in the cross-section under steady state with 300 A current and 20 mm/s feed speed: (b) before wire entering the coil, $A_{\text{Enter coil}}$; (c) wire at the coil centre, $B_{\text{Inside coil}}$; (d) wire leaving the coil, C_1 ; (e) wire outside the coil, C_2 .

Table 6
Parameters in the model for sensitivity analysis.

Parameters	Minimum value	Maximum value	Increment	Reference value
Current (A)	10	700	10	250
Frequency (kHz)	10	500	10	300
Feed speed (mm/s)	1	150	1	10
Wire diameter (mm)	0.1	6	0.1	2
Coil turns	1	20	1	4
Radial distance (mm)	3	20	0.1	11
Coil pitch (mm)	1	10	0.1	2
Coil-wire diameter (mm)	4	10	0.1	5

this is most likely due to the lower heating temperature when wire feeding is faster (Fig. 9), which allows better magnetisation performance of the material and causes a higher intensity eddy current field, enabling more energy generation. Furthermore, a higher wire feed speed allows more wire to pass through the coil during the same time, meaning a larger volume of material for energy transfer, but it leads to lower

heating temperature due to the distribution of the energy over a larger mass (Figs. 6 and 9). This analysis provides new physical insights into the energy transfer of induction preheating of a filler wire under dynamic feeding conditions.

To further investigate the energy conversion rate of the IH system, the variations in steady-state power with wire feed speed for different currents are compared in Fig. 16. It is seen that the power transfer for a stationary wire is 3.97 W, 9.09 W and 18.12 W for currents of 100 A, 200 A and 300 A, respectively. With the wire feed speed increases, the power transferred to the wire rises gradually until it reaches a critical speed and then becomes stable. A similar trend is also observed in the energy transfer efficiency. A higher current leads to a larger critical feed speed. The stabilisation of the transferred power can be attributed to the reduced residence time of the wire within the electromagnetic field under the increased feed speed, which weakens the electromagnetic coupling. Consequently, the heated wire reaches a lower and stable temperature. Once the temperature no longer changes, the electromagnetic properties of the wire remain constant, resulting in a steady transferred power. This explanation is further supported by the peak temperature results presented in Section 3.4, which indicate that the wire temperature drops to a much lower level at higher feed speeds.

The energy transfer and heat dissipation determine the temperature

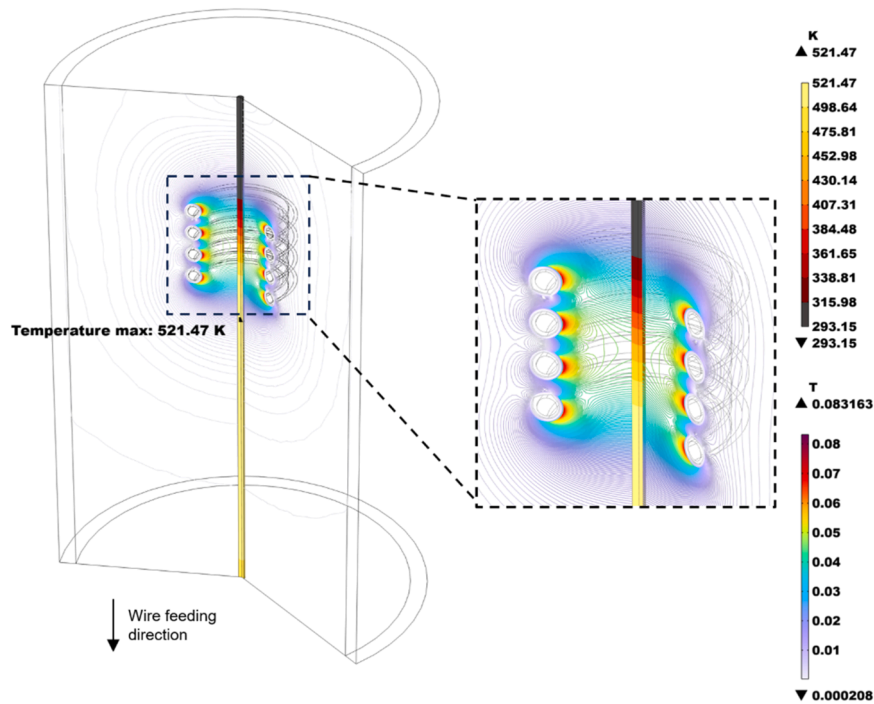


Fig. 20. Temperature and magnetic flux density of reference model with baseline settings (the axisymmetric model result is visualised in 3D).

field of the wire. Fig. 17 shows the evolution of surface temperature distribution in the computational domain of the wire. Here the axial positions represent fixed locations in the space, and the plotted curves indicate the temperature of the wire as it passes through these positions. The surface temperature exhibits a single-peaked distribution, with the peak initially appearing at the centre of the coil zone, then shifting towards the wire feeding direction, eventually moving to a location immediately outside the coil. The temperature increased rapidly in the coil zone, where a steady state was reached first. The wire segments located outside the coil zone remained relatively cool until a steady heated state was established later. Therefore, the temperature along the wire length is non-uniform; however, there is no effect on the stability of the wire-DED process, as the wire experiences the same thermal history and maintains a consistent temperature upon entering the melt pool. The mechanisms for establishing the steady state are different between the inside and the outside of the coil zone. The main reason for the steady state inside the coil zone is the continuous motion of the wire, which ensures that each wire segment undergoes an identical IH cycle within the coil zone. In contrast, the temperature of the wire outside the coil zone resulted from the heating by motion and conduction of each hot wire segment and the cooling by conduction to cooler wire segment and convection and radiation to environment. Under faster wire feeding, less time was taken for reaching the final steady state and the steady-state temperature was lowered due to shorter heating time inside the coil; the temperature gradient along the wire axis was also reduced due to shorter cooling time outside the coil. These findings have practical implications for optimising thermal management and energy consumption when preheating a filler wire in wire-DED.

Based on the surface temperature of the wire, the effect of thermal boundary conditions on the steady-state temperature is further analysed by deactivating convection and radiation in the model. With absence of heat loss to the environment, as shown in Fig. 18a, the peak temperature rises slightly, and no thermal gradient is observed beyond the coil exit end, indicating that convection and radiation are important influential factors, especially for high current and low feed speed. Fig. 18b quantifies the power components when the wire reached the steady state. The energy dissipated through convection and radiation accounts for only a small portion of the total energy transferred to the wire, except when the

current is high and wire feed speed is low, which leads to high heat loss as the wire temperature is much higher than the environment temperature. Given that the goal is high preheating temperature and the convection and radiation are the main causes of wire cooling, it is recommended to minimise the effects by limiting the convection and radiation or reducing the distance between the coil and the melt pool.

The internal temperature is also important for wire heating effectiveness. Four typical axial positions, as depicted in Fig. 19a, are considered for evaluating the cross-sectional temperature profile in the wire. The radial distributions of the steady-state temperature in the cross sections are shown in Fig. 19b, 19c, 19d and 19e for axial positions A, B, C₁ and C₂, respectively. As the wire diameter was 1.6 mm and the skin depth was 0.7 mm, the central region with 0.2 mm diameter was not directly heated. The eddy current (Fig. 12) heated the surface first, and then the heat was quickly conducted to the inner region of the wire, leading to marginal radial temperature gradient. Fig. 19b shows the internal temperature of the wire before it entered the coil, where the surface temperature was slightly higher than the wire core temperature, due to the concentration of eddy current on the wire surface (Fig. 12). The difference between the surface temperature and the core temperature increased when the wire reached the centre of the coil, as shown in Fig. 19c. When the wire just moved out of the coil (C₁), the radial temperature gradient reduced and the radial peak temperature moved from the surface to a radial position of 0.73 mm inside the wire. In contrast, Fig. 19e shows that when the wire was 52 mm away from the coil, the surface temperature was lower than the core temperature. This is because the material was not heated by the inductor anymore but subjected to surface cooling through convection and radiation. These phenomena are qualitatively consistent with those reported in the literature [34,66], where induction heating was employed in a furnace for heating large cylindrical components. The new finding here is that in wire-DED applications, the small wire diameter facilitates heat diffusion to the wire centre and reduces the temperature gradient in radial direction, resulting in marginal difference between internal and surface temperatures. The homogenous distribution of heating temperature is a key requirement for IH effectiveness [67,68].

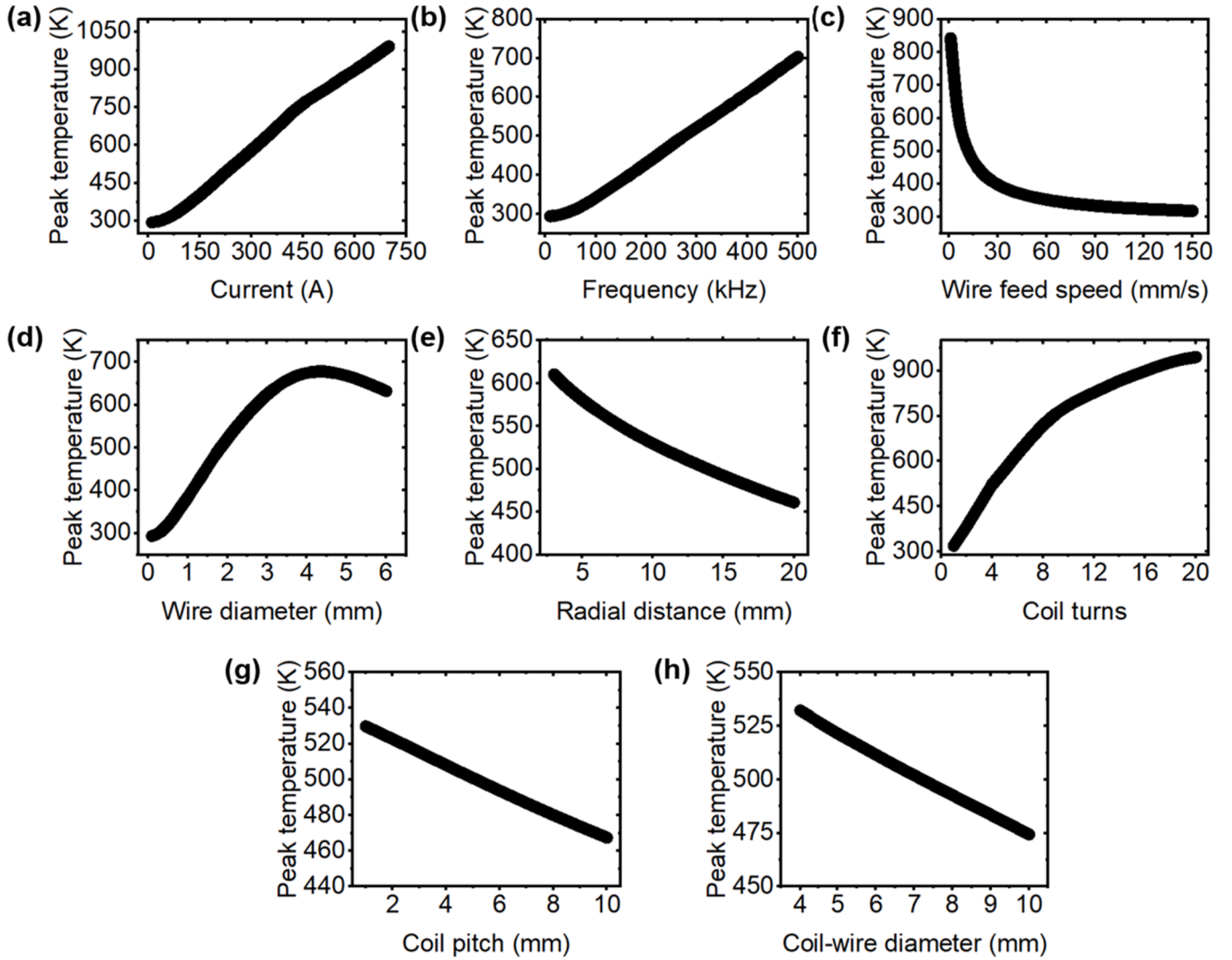


Fig. 21. Variations of wire peak temperature with IH parameters under steady state: (a) current, (b) frequency, (c) wire feed speed, (d) wire diameter, (e) radial distance, (f) coil turns, (g) coil pitch, (h) coil-wire diameter.

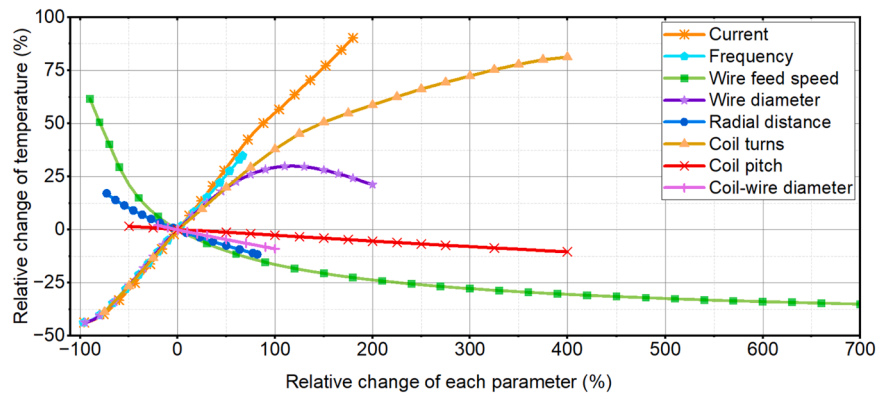


Fig. 22. Sensitivity of wire peak temperature to different IH parameters.

3.4. Heating parameter sensitivity analysis

To further understand the sensitivity of the wire temperature to major influential parameters of the IH process, a parametric analysis was carried out, and one set of parameters was used as a reference (Table 6). This parametric sensitivity analysis can help identify the optimal

conditions for achieving the desired wire temperature. Each calculation changed one parameter value while keeping other parameters same as reference values. The radial distance, instead of the coupling distance (Fig. 2), was used here as it was independent of the wire diameter. The radial distance was set to be 3 mm as the minimum value to avoid potential contact between the wire and coil due to thermal expansion or

Table 7

Sensitivity and importance ranking of IH parameters for preheating austenitic stainless steel filler wire in wire-DED.

Rank	IH Parameters	Considerations
1	Wire feed speed	Usually fixed in wire-DED for the required deposition rate. Reduction in the wire feed speed can increase the preheating temperature, most significantly at low feed speeds.
2	Current	Adjustable range is large to precisely control the preheating temperature.
3	Frequency	Controls the skin depth and the thermal gradient in the radial direction in the wire
4	Coil turns	Multi-turn coils are more efficient in preheating. The temperature gain gradually diminishes as the number of turns increases, and the magnetic field distribution is saturated when exceeding 19 turns.
5	Wire diameter	When wire diameter < skin depth, a larger diameter increases preheating temperature in a thin wire with negligible radial thermal gradient, and the maximum allowable diameter depends on specific wire-DED process requirements. Peak temperature reduces when wire diameter > skin depth.
6	Radial distance	Minimal as possible but ensuring enough clearance between the wire and coil.
7	Coil-wire diameter	Low sensitivity for preheating effect and a minimum diameter is required to carry the current and provide thermal stability.
8	Coil pitch	Lowest sensitivity for preheating effect but can be used to adjust the size of the heating zone.

vibration during heating. The ranges of the current and frequency were set according to the specifications of the Ambrell 10KW EASYHEAT IH system [69]. It is essential to recognise that in order to manage the temperature of the wire accurately, some practical limits were implemented. The examined wire diameter and wire feed speed were selected based on the process parameters in the literature [38,70–72], excluding values that could result in wire melting or insufficient heating. The minimum coil pitch was 1 mm to prevent contact between each turn [73]. For the reference case, Fig. 20 shows the magnetic field generated by the coil and the distribution of the wire temperature induced by the eddy current.

Fig. 21 compares the peak temperatures achieved for various parameters under a steady state. These new findings of the comprehensive relationships between the peak temperature and influential parameters have significant implications for identifying the most effective method to control the filler wire preheating temperature for wire-DED applications. Fig. 21a shows the substantial increase in peak temperature with coil current. Similar response was observed for the inductor frequency and coil turns, as shown in Fig. 21b and Fig. 21f, respectively. These correlations can be rationalised by the electromagnetic heating mechanism, according to which high current increases magnetic flux density, high frequency accumulates eddy current density on the wire surface, and multi-turn coils increase the coupling time between the wire and the magnetic field, all leading to an increase in the peak temperature.

Conversely, increases in wire feed speed, radial distance, coil pitch and coil-wire diameter cause monotonic reduction in the peak temperature, as shown in Fig. 21c, Fig. 21e, Fig. 21g and Fig. 21h, respectively. This is because an increase in wire feed speed, radial distance or coil pitch reduces the coupling efficiency between the wire and the magnetic field, thereby reducing the wire heating temperature. For a larger coil-wire diameter, given constant current input and same coupling distance, the magnetic field tends to spread over a larger area, leading to a less concentrated, weaker magnetic field, which reduces the peak temperature.

It is interesting to see complicated non-monotonic variation of the peak temperature with wire diameter (Fig. 21d). When the wire diameter is 4.3 mm, the peak temperature has a maximum value of 678 K, followed by a gradual decline with further increasing the wire diameter. When the wire diameter is below 4.3 mm, increasing the diameter is

instrumental to higher energy transfer efficiency as presented in Table 5, thereby producing higher peak temperature. However, when the wire diameter is above 4.3 mm, a larger diameter means that the skin depth is much smaller than the wire radius and the heat is transferred from the surface to the interior of the wire via thermal conduction, leading to lower peak temperature. Therefore, ideally the wire radius should match the skin depth of the IH process to enable most effective heating and homogenous temperature distribution across the wire.

For a comparative analysis, the percentage changes in the peak temperature for different parameters relative to the reference model are shown in Fig. 22. A ranking method for optimising the wire preheating is provided, as summarised in Table 7. The wire feed speed and wire diameter are two key parameters in wire-DED process, meaning that the IH-based wire preheating should be flexible and adaptable to various wire-DED conditions. The impact of wire preheating on wire-DED presents a nuanced trade-off. The preheating reduces the temperature difference between the heated wire and the melt pool to allow high-speed feeding; thus, higher deposition rate is achieved as it is proportional to the wire feed speed [74]. On the other hand, achieving effective preheating requires sufficient energy transferred to the wire, which is strongly influenced by the wire's residence time in the magnetic field. This necessitates slower wire feed speed to ensure adequate temperature rise before the wire enters the melt pool. A lower wire feed speed is most effective in enhancing preheating temperature (Fig. 22), but it reduces the deposition rate and leads to low productivity in wire-DED. However, low feed speed can offer other benefits such as lowering the energy input required from the main heat source, potentially improving the microstructure [75,76], and reducing residual stress [77]. This trade-off must be carefully managed through optimising coil design, power input, and wire trajectory to achieve both adequate preheating and desirable deposition rates. A larger wire diameter is helpful for increasing the peak preheating temperature, but it may cause difficulty in handling and feeding the wire with higher stiffness during melting by the wire-DED process.

Other parameters are solely related to IH and independent of wire-DED process. The coil current is the secondly most influential parameter to control the preheating temperature (Fig. 22), and its working range is mainly limited by the capacity of the IH system. Frequency is the thirdly most influential parameter, but its efficacy through controlling skin depth is dependent on the wire diameter. High frequency is always beneficial for wire preheating, which helps increase the preheating temperature when the current remains unchanged. Therefore, in the ideal case, choosing the current and frequency for a given wire feed speed and wire diameter is recommended, which will determine the required IH power source. Next, the coil geometry can be optimised with guidance from the parametric analysis. Intuitively, the coil turns should be as many as possible, but coils with more than 19 turns do not further enhance the preheating. An increased number of coil turns imposes additional load to the IH system, and a low power system also constrains the achievable current level. The radial distance to coil should be kept as low as possible, but the minimum should be set based on practical considerations related to the potential contact with the filler wire. The coil-wire diameter has a low effect on the wire temperature. It is recommended to use a coil-wire diameter as low as possible to avoid dispersion of the magnetic field, but it should be also large enough to enable internal water cooling and thermal stability. The wire temperature is least sensitive to coil pitch, which, however, can be used to adjust the extent of the heating region. In principle, these strategies are also applicable to multi-wire setups and other wire-based manufacturing processes (e.g. welding and cladding). However, it is advisable to employ separate coils for multiple wires with different materials, due to the potentially significant variations in their electromagnetic and thermal properties, which can lead to differing responses to IH.

Typical scenarios for applying IH-based filler wire preheating in wire-DED include increasing the deposition rate and reducing the energy demand of the main heat source (this will lead to lower heat input to the

deposited part). The coil current and frequency, wire feed speed, and wire diameter are recommended to be flexibly selected according to the specific requirements of the deposition to determine the optimal configuration for the induction preheating. For the purpose of high deposition rate, achieved through either a high feed speed or a large wire diameter, higher coil current and frequency are essential to increase the wire preheating temperature and facilitate the subsequent melting. Similarly, parameter combinations that achieve higher preheating temperatures while reducing the energy input of the main heat source are generally beneficial.

Evaluating the IH energy transfer efficiency is another important consideration. Ferromagnetic and less strong magnetic materials are recommended for IH-based wire preheating due to their superior electromagnetic coupling, whereas non-magnetic materials are more difficult to heat inductively, although preheating can still be achieved under high induction input. On the other hand, the geometric conformity between the coil and the wire is a key factor for heating efficiency. For round, flat, or multi-wire configurations, the coil geometry should be designed to resemble the wire shape, ensuring maximum heating efficiency and uniform preheating [23]. In most cases, a radial distance approximately equal to the wire diameter provides good coupling while avoiding physical contact between the coil and wire. The minimum values identified in Table 6 are recommended for the coil pitch and coil wire diameter to minimise the heat losses of the coil. An eight-turn coil is recommended based on the critical increase of peak temperature observed in Fig. 21f. In addition, careful consideration is given to practical factors such as coil manufacturing cost, mechanical strength, and system integration to flexibly adjust coil configuration. In summary, these recommendations define a process window for the optimal configuration and applicability of induction preheating in wire-DED, highlighting the balance between efficiency and practical feasibility, which have not been established in the existent literature.

4. Conclusions

A multiphysics model was developed to investigate the induction heating of a moving filler wire for the application of feedstock preheating in wire-DED. The model examined electromagnetic flux, eddy current, temperature field and energy transfer efficiency. In addition, a parametric sensitivity analysis was performed with consideration of eight influential parameters. The following conclusions are drawn:

- The electromagnetic-thermal model can accurately predict the distribution and evolution of the temperature in the moving filler wire heated by an inductor with a helical coil, as corroborated by the experimental measurements and observations for different coil currents and wire feed speeds.
- Induction heating can effectively preheat the moving filler wire for wire-DED, achieving almost homogenous temperature across the wire diameter and medium level of preheating temperature for austenitic stainless steel. However, the energy transfer efficiency is low (<10 %) for typical wire diameters and feed speeds, mainly due to the thin section and low magnetic properties of the austenitic steel wire. The heating efficacy can be potentially improved for materials with high magnetic permeability and hysteresis. In addition, increasing the wire diameter and reducing the radial distance to coil can significantly enhance the energy transfer efficiency.
- The electromagnetic flux is distributed within the helical coil and partially penetrates in the wire. The induced eddy current heats the wire from the outer surface towards the core, limited by the skin depth, and its intensity and penetration drop when the wire temperature is increased through both electromagnetic heating and thermal conduction. Shortly after switching on the inductor, the maximum eddy current shifts to the wire entry end of the coil, and the non-uniform current distribution is stabilised.

- A steady-state wire temperature field is established near the inductor coil shortly after induction heating initiation. The peak temperature is located close to the wire exit end of the coil and the temperature gradient across the wire diameter is marginal. The peak temperature increases with the inductor current (C) and frequency (F), and the number of coil turns (CT), and decreases with wire feed speed (WFD), radial distance (RD) between wire and coil, and coil-wire diameter (CD) and coil pitch (CP). The peak temperature initially increases with wire diameter (WD), due to enhanced energy transfer efficiency, but it drops after WD exceeds a critical value, after which more core material dissipates heat. The parametric sensitivity and importance ranking for induction preheating of austenitic steel filler wire is as follows: $WFD > C > F > CT > WD > RD > CD > CP$.

This study reveals the complex mechanism of induction preheating of feedstock for hot wire DED additive manufacturing applications. The mechanism can be applicable to other similar manufacturing processes such as welding and cladding. The findings can guide the design and optimisation of induction heating for filler wire preheating.

The current model focuses on the electromagnetic and thermal behaviours of a single moving filler wire and does not consider the thermal effects of the main heat source or the melt pool. Future study would incorporate melt pool dynamics to directly simulate the wire-DED process with single or multiple wire preheating. The research would also be extended to other materials and include a comparative analysis of energy efficiency among induction, resistive, and bypass preheating methods, to reveal the benefits of induction preheating and its industrial applicability.

CRediT authorship contribution statement

Ruofeng Cao: Writing – original draft, Visualization, Validation, Methodology, Investigation, Formal analysis. **Yongle Sun:** Writing – review & editing, Supervision, Project administration, Conceptualization. **Wojciech Suder:** Writing – review & editing, Supervision, Conceptualization. **Xin Chen:** Writing – review & editing, Formal analysis. **Zhiyong Li:** Writing – review & editing, Formal analysis. **Stewart Williams:** Writing – review & editing, Resources, Funding acquisition, Conceptualization.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

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Data availability

Data underlying this study can be accessed through the Cranfield University repository at <https://doi.org/10.57996/cran.ceres-2755>.

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