

Numerical analysis of thermal accumulation and cooling design in SCAC-assisted wire-arc directed energy deposition: coupled effects of copper-block length and heat-transfer conditions

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ABSTRACT

Thermal accumulation limits stability and productivity in directed energy deposition–arc with metals (DED-Arc/M), commonly referred to as wire-arc directed energy deposition (WA-DED). This study numerically examined thermal accumulation in WA-DED with solid-contact active cooling (SCAC) and the coupled effects of cooling-block length and heat-transfer conditions for a short-tracking SCAC configuration. A three-dimensional transient heat-conduction model for AZ31 magnesium-alloy thin walls was calibrated using substrate-temperature histories and melt-pool lengths from previous experiments conducted with water-cooled copper blocks covering the full wall length. The so-called volume-averaged stored-thermal-energy density was introduced to evaluate thermal accumulation in the substrate and the deposited wall. The model reproduced continuing temperature increase under natural cooling (NC) and early thermal stabilization under SCAC. Despite its shorter interpass times, SCAC maintained the volume-averaged stored-thermal-energy density at levels comparable to or lower than those during late NC. A parametric study was conducted for a short block tracking the heat source during 24-pass deposition. Block length and the wall–block and block–water heat-transfer coefficients were each varied at three levels. Increasing these factors reduced both the late-stage accumulation rate and final accumulation level. However, stronger water-side cooling had a limited effect under low wall–block heat transfer. Thermal accumulation continued for a 5 mm block even under the strongest heat-transfer conditions, nearly stabilized for a 10 mm block, and slightly decreased for a 20 mm block. The lowest final accumulation level was approximately 94% below the maximum across 27 conditions. Numerical results indicate that short-tracking SCAC requires sufficient wall–block heat transfer, appropriate block length, and water-side heat rejection.

Introduction

Directed energy deposition–arc with metals (DED-Arc/M), commonly referred to as wire-arc directed energy deposition (WA-DED), is an additive manufacturing (AM) process in which metal wire is melted and deposited on a substrate to build three-dimensional structures (Cao et al., 2024; Costello et al., 2023; Li et al., 2023; Tapia Cabrera et al., 2026; Irfan et al., 2025). WA-DED generally requires a simpler equipment configuration than AM processes using laser or electron-beam heat sources (Chowdhury et al., 2022; Ladani and Sadeghilaridjani, 2021; Bhatt et al., 2025; Riazi et al., 2025) and, as a wire-fed DED process, offers material-cost and material-utilization advantages over powder-fed processes. In addition, WA-DED is particularly attractive for

the efficient fabrication of large metallic components because its high wire-melting capability enables high deposition rates. Like other fusion-based deposition processes, WA-DED can exhibit thermal accumulation when the energy supplied during deposition exceeds heat extraction through conduction, convection, and radiation. For the thin-walled structures considered here, the limited cross-sectional area available for conductive heat extraction can promote increases in the interpass temperature and the overall component temperature as the build progresses. Excessive thermal accumulation can enlarge the melt pool, destabilize bead geometry, and reduce dimensional accuracy (Wu et al., 2017; Williams et al., 2024). It may also affect solidification microstructures and precipitate formation, potentially degrading mechanical properties (Wu et al., 2018; Manvatkar et al., 2014). Therefore,

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thermal accumulation should be controlled to simultaneously achieve high productivity and stable build quality in WA-DED.

Interpass temperature is widely used as a process-control variable in multi-pass welding and fusion-based metal additive manufacturing, and in WA-DED it can be influenced by reducing the heat input or extending the interpass time (Kozamernik et al., 2020; Montevecchi et al., 2018; Israr et al., 2021). Farias et al. also examined interpass-temperature prediction for a wire-arc additive-manufactured wall (Farias et al., 2021). However, the heat input cannot be reduced indefinitely because sufficient melting of the feed wire and its adequate fusion with the previously deposited material must be maintained. Extending the interpass time directly increases the total build time and diminishes a principal advantage of WA-DED: high productivity. This necessitates active cooling methods that extract heat from the deposited material during fabrication rather than reducing the arc input or prolonging interruptions.

Active cooling has also been investigated as a means of maintaining deposition productivity while controlling the thermal state during WA-DED. For example, Ma applied side water cooling during WA-DED of AZ31 magnesium alloy and demonstrated improvements in deposition efficiency, microstructural refinement, and mechanical properties (Ma et al., 2025). In this context, the authors previously proposed solid-contact active cooling (SCAC), in which water-cooled copper blocks (hereinafter “blocks”) are placed in direct contact with both sides of a deposited wall (hereinafter “wall”) (Kudo et al., 2026). Under natural cooling (NC), geometric instability occurred despite an interpass time of 120 s. By contrast, SCAC enabled the stable fabrication of AZ31 magnesium-alloy thin walls with an average interpass time of approximately 2.5 s. SCAC increased the production rate from 90 cm³/h to 882 cm³/h and resulted in refined grains and precipitates while maintaining or improving tensile properties. These experimental results demonstrated that SCAC suppresses excessive temperature increases and enables high-productivity deposition with short interpass times. However, in our previous experimental study of SCAC (Kudo et al., 2026), the thermal assessment was based mainly on the temperature history measured at a single point in the substrate and the crater length obtained after arc interruption. Despite revealing differences in temperature evolution and melt-pool length across cooling conditions, these measurements did not quantify the thermal accumulation state throughout the substrate and the previously deposited wall as fabrication progressed. Moreover, the temperature measured at a single substrate location cannot represent the thermal accumulation state of the entire substrate and the wall, which have highly nonuniform temperature distributions. The thermal basis for the high-productivity fabrication enabled by SCAC should be clarified by evaluating the thermal energy retained in the substrate and the wall while accounting for differences in the deposited volume. Consequently, whether thermal accumulation continues to increase or approaches a stable range as deposition proceeds can be determined.

The blocks used in the previous study covered the full wall length. This configuration is effective for cooling straight thin walls but has limited applicability to curved walls and structures with locally varying geometries. To improve geometric adaptability, a short-tracking SCAC configuration can be considered, in which shortened blocks cover only the region near the heat source and follow the torch during deposition. Such localization of the cooling blocks is expected to increase geometric flexibility; however, shortening the blocks simultaneously changes the range and duration of contact with the wall, the amount of heat that can be absorbed by the blocks, and the conditions governing heat rejection to the water. Therefore, thermal accumulation during continuous deposition must be examined as a function of block length, wall–block heat transfer, and block–water heat rejection. Experimental evaluation of these design factors will require numerous deposition trials involving changes in block geometry, contact mechanism, and cooling-channel configuration. Reproducing identical contact conditions in every test will be difficult because of variations in wall-surface geometry and block

pressing conditions. Numerical analysis can be used to complement experimental investigations by systematically comparing the effects of these design factors on thermal accumulation.

In this study, three-dimensional transient heat-conduction analysis was conducted on AZ31 magnesium-alloy thin walls to clarify thermal accumulation in SCAC-assisted WA-DED and develop thermal design guidance for short-tracking SCAC. First, a numerical model was developed for the previous experiments, where water-cooled blocks covered the full wall length, and was calibrated against previously measured substrate-temperature histories and melt-pool lengths. A volume-averaged measure of the thermal energy stored in the substrate and the wall was then used to evaluate the progression and stabilization of thermal accumulation under NC and SCAC conditions. A continuous-deposition condition was subsequently considered, where a short block followed the heat source. A full-factorial parametric study was conducted under 27 conditions with varying block lengths, effective wall–block contact heat-transfer coefficients, and effective block–water heat-transfer coefficients, with three levels each. Under each condition, the late-stage thermal accumulation rate and final thermal accumulation level were evaluated to determine how the combined design factors affected thermal accumulation during continuous deposition. Relative to the previous experimental study, the present work extends the thermal assessment from single-point substrate-temperature and crater-length measurements to a component-wide evaluation using the volume-averaged stored-thermal-energy density and systematically examines the coupled effects of block length, wall–block heat transfer, and block–water heat transfer in short-tracking SCAC.

Methods

Experimental procedure

The deposition conditions and experimental data used to calibrate the numerical model and assess its reproduction of experimental thermal behavior were obtained from the authors' previous study (Kudo et al., 2026). An AZ31 magnesium-alloy wire with a diameter of 1.2 mm was deposited on an AZ31 substrate to fabricate thin-walled structures. The substrate dimensions were 150 mm × 40 mm × 10 mm, and the wall length was fixed at 90 mm.

Deposition was performed using a six-axis robotic arm and a Cold Metal Transfer (CMT) arc-welding power source (Pickin et al., 2011; Wang et al., 2021). The walls were fabricated by depositing one stringer bead per layer. Two cooling conditions were considered: NC and SCAC. Under the SCAC conditions, water-cooled blocks were placed in contact with both sides of the wall (Fig. 1), allowing heat to be extracted from the deposited material through the blocks to the water. Each block measured 200 mm × 22 mm × 22 mm and contained a cooling channel with a diameter of 14.5 mm. Water was circulated at an initial temperature of approximately 25 °C, a flow rate of 13 L/min, and an average velocity of 1.3 m/s. Two block spacings were used, 11.3 and 8.2 mm, which are hereinafter denoted as AC-W11 and AC-W8, respectively, where AC denotes active cooling and W denotes the spacing between the blocks. The smaller spacing in AC-W8 than in AC-W11 produced a larger effective contact area between the wall and the blocks. The arc conditions were identical for all cases. The welding current, arc voltage, wire-feed speed, and torch-travel speed were 132 A, 22.1 V, 16.0 m/min, and 500 mm/min, respectively. Under the NC condition, the interpass time was 120 s. The 120 s interval was the condition used in the previous experiment and was retained here to reproduce that deposition schedule. Under SCAC, four layers were deposited continuously, followed by an arc-off period of approximately 10 s (Fig. 1). During this interruption, both blocks were moved upward by four layer heights so that their lower surfaces were positioned approximately 5 mm below the top of the wall. Deposition resumed after the blocks reached the new position. This deposition sequence resulted in an average interpass time of approximately 2.5 s under SCAC.

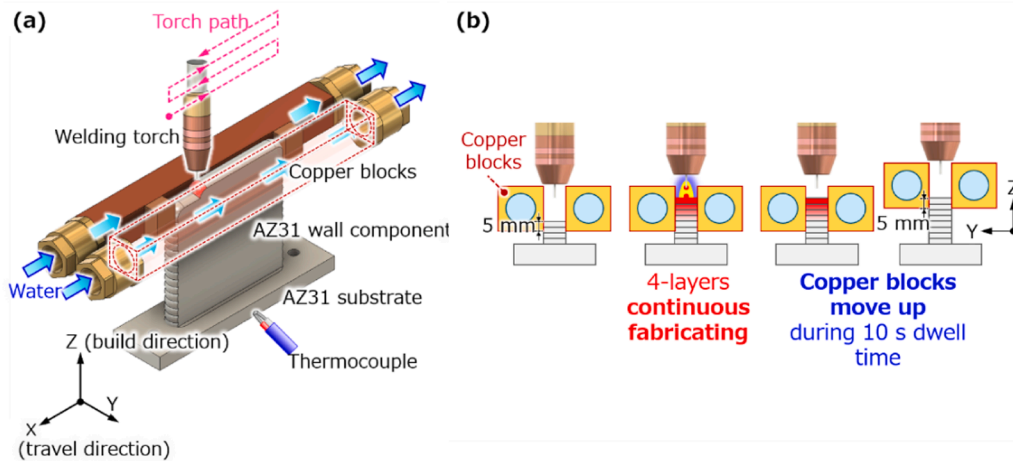


Fig. 1. Schematic illustration of SCAC process used in previous experiments. (a) Experimental configuration: water-cooled blocks placed on both sides of deposited AZ31 wall. (b) Deposition sequence: continuous deposition of four layers followed by upward movement of both blocks during the approximately 10 s arc-off period. The lower surfaces of the blocks were repositioned approximately 5 mm below the top of the wall, and deposition resumed after the blocks reached the new position (Kudo, et al., 2026).

The substrate-temperature histories and melt-pool lengths measured in the previous study were used to calibrate the numerical model and assess its reproduction of the experimental thermal behavior. The substrate temperature was measured using a K-type thermocouple located at the longitudinal center of the wall and at the mid-thickness of the substrate. Melt-pool length was evaluated by abruptly interrupting the arc near the longitudinal center of the wall during the deposition of the target layer and then measuring the longitudinal length of the resulting crater. Eleven, fifteen, and twelve deposited layers were used to compare the substrate-temperature histories and melt-pool lengths under NC, AC-W11, and AC-W8, respectively. Under SCAC, the substrate-temperature histories entered a stable regime within these deposition ranges. By contrast, under NC, the substrate temperature immediately before each pass was still increasing in the 11th layer, indicating continuing thermal accumulation. Further details about the equipment configuration, specimen fabrication, and experimental results are provided in the authors' previous study (Kudo et al., 2026). No synchronized, calibrated full-field temperature map was available from the previous experiment for comparison with the calculated spatial temperature field.

Numerical analysis procedure

Numerical model and material properties

A three-dimensional transient heat-conduction model was developed to evaluate thermal behavior during WA-DED under NC and SCAC conditions. A schematic diagram of the numerical model is shown in Fig. 2. Under NC, the substrate and progressively deposited wall were included in the computational domain. Under SCAC, the blocks placed on both sides of the wall and the water channels inside the blocks were additionally modeled. Because the wall was assumed to be symmetric about its mid-plane in the width direction, a half model was used, where the x - z plane at the wall centerline was defined as the symmetry plane. The x , y , and z directions corresponded to the wall longitudinal, width, and build-height directions, respectively. The computational domain was discretized using a Cartesian grid with spacings of $\Delta x = 2.5$ mm, $\Delta y = 0.25$ mm, and $\Delta z = 0.25$ mm. Each grid point was assigned a material identifier corresponding to the substrate, wall, block, or water channel. Grid points outside these material regions were excluded from the heat-conduction calculation.

The geometric and deposition conditions are summarized in Table 1. The substrate dimensions and wall length were based on the experimental conditions. The wall width and layer height were determined from the geometries of the fabricated walls obtained under each cooling

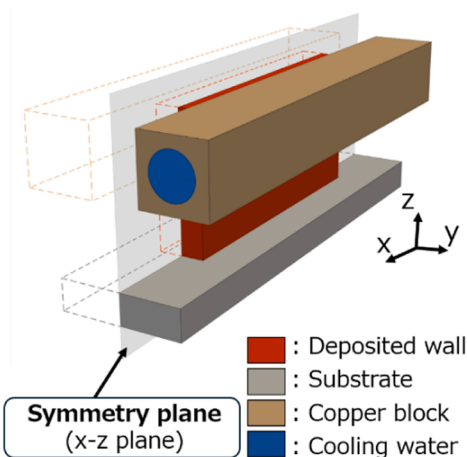


Fig. 2. Schematic diagram of the numerical model and analysis conditions.

Table 1
Geometric and deposition conditions used in thermal analysis.

	NC	AC-W11	AC-W8	Unit	Note
Substrate size	150 × 40 × 10			mm	Same as experimental
Wall length	90			mm	Same as experimental
Wall width	13.0	11.0	8.0	mm	Based on fabricated wall geometry
Layer height	2.0	2.75	3.75	mm	Based on fabricated wall geometry
Number of passes	40	32	24	-	Same as experimental
Block size	-	150 × 22 × 22		mm	SCAC only; block length reduced from experimental 200 mm
Cooling-channel diameter	-	14.5	14.5	mm	SCAC only

condition. The numbers of passes were identical to those used in the previous experiments, namely, 40, 32, and 24 for NC, AC-W11, and AC-W8, respectively. Because the layer height under SCAC was greater than that under NC, the final wall heights were comparable to or greater than that under NC despite the smaller number of passes.

The experimental blocks had a longitudinal length of 200 mm. In the numerical model, the block length was reduced to 150 mm, equal to the substrate length, to reduce the computational cost. The influence of the block ends on the thermal behavior near the wall center was assumed negligible. The block cross-sectional dimensions and cooling-channel diameter were identical to those used experimentally.

The transient heat-conduction equation included a volumetric heat-source term.

$$\rho c_p(T) \frac{\partial T}{\partial t} = \nabla \cdot (k(T) \nabla T) + q, \quad (1)$$

where T is temperature, t is time, ρ is density, c_p is specific heat, k is thermal conductivity, and q is the volumetric heat-input rate.

Temperature-dependent thermal conductivity and specific heat were assigned to the AZ31 substrate and the wall (Figs. 3(a) and 3(b), respectively). The substrate density was 1.70×10^{-6} kg/mm³ and assumed to be temperature independent. Latent heat associated with melting and solidification was incorporated using an effective-specific-heat formulation over the temperature range between the solidus and liquidus temperatures. The latent heat was 360 kJ/kg, and the solidus and liquidus temperatures were 525 °C and 649 °C, respectively. The specific heat shown in Fig. 3(b) represents only the sensible-heat component and does not include the additional effective-specific-heat contribution used to account for latent heat.

The block density, specific heat, and thermal conductivity were 8.96×10^{-6} kg/mm³, 385 J/(kg K), and 0.385 W/(mm K), respectively, and assumed to be temperature independent. The water was represented by a heat-transfer boundary condition at the block–water interface instead of directly solving the fluid flow inside the cooling channels. The water temperature and block–water heat-transfer condition are described in Section “Thermal boundary and contact conditions”.

Moving heat source and deposition sequence

The external heat input was applied using Goldak’s double-ellipsoidal heat-source model (Goldak et al., 1984), which is widely used in thermal and thermo-mechanical analyses of welding and WA-DED processes (Ding et al., 2011; Singh et al., 2021; Han, 2023). A local coordinate system (x' , y' , z'), where x' , y' , and z' denote the torch-travel, wall-width, and depth directions, respectively, was defined, with its origin being the heat-source center. The volumetric heat-input distributions in the front region ($x' \geq 0$) and rear region ($x' < 0$) were

$$q_f = \frac{6\sqrt{3}f_f Q}{\pi\sqrt{\pi}a_f b c} \exp\left(-3\frac{x'^2}{a_f^2} - 3\frac{y'^2}{b^2} - 3\frac{z'^2}{c^2}\right), \quad (x' \geq 0), \quad (2)$$

$$q_r = \frac{6\sqrt{3}f_r Q}{\pi\sqrt{\pi}a_r b c} \exp\left(-3\frac{x'^2}{a_r^2} - 3\frac{y'^2}{b^2} - 3\frac{z'^2}{c^2}\right), \quad (x' < 0),$$

where q_f and q_r are the volumetric heat-input rates in the front and rear regions, respectively; Q is the effective heat input; a_f and a_r are the front and rear heat-source radii in the torch-travel direction, respectively; b is the heat-source radius in the wall-width direction; and c is the heat-source radius in the depth direction. The effective heat input was

$$Q = \eta IV, \quad (3)$$

where I is the welding current, V is the arc voltage, and η is thermal efficiency. The current, voltage, and torch-travel speed were set to the experimental values of 132 A, 22.1 V, and 500 mm/min, respectively. Thermal efficiency was set to 0.6 for all conditions. Together with the heat-source dimensions and effective boundary and contact parameters, $\eta = 0.6$ was used as part of the calibrated effective model that reproduced the measured temperature histories and melt-pool lengths, rather than as a universal process-efficiency factor for gas metal arc welding. The front and rear heat-partition coefficients, f_f and f_r , were

$$f_f = \frac{2a_f}{a_f + a_r}, \quad f_r = \frac{2a_r}{a_f + a_r} \quad (4)$$

such that $f_f + f_r = 2$.

The heat-source radius in the wall-width direction, b , was set to 6.5

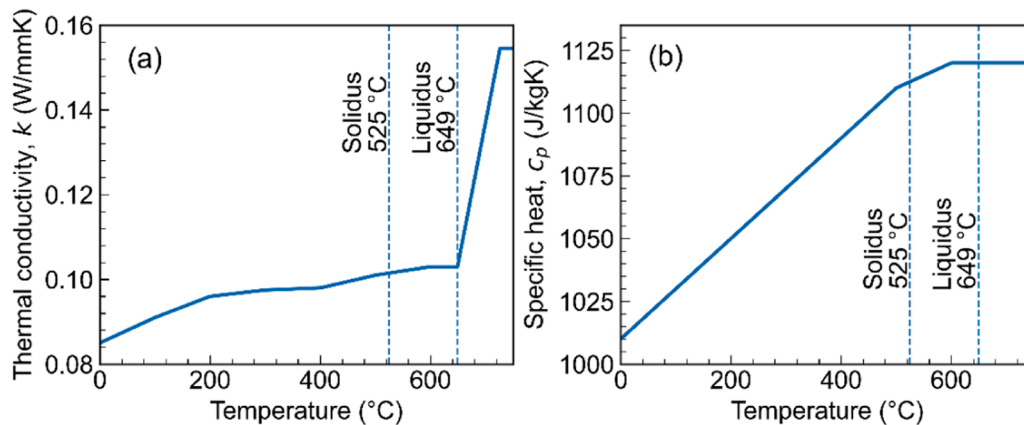


Fig. 3. Temperature-dependent thermal properties of AZ31 alloy used in numerical analysis: (a) thermal conductivity and (b) specific heat. The specific heat in (b) represents the sensible-heat component and does not include the effective specific-heat increase used to account for latent heat.

mm for all conditions based on the wall geometry obtained under NC. The depth-direction radius, c , was set to 0.8 times the layer height for each condition. The front radius, a_f , was fixed at 3 mm for all conditions. The rear radius, a_r , was determined for each condition as the difference between the experimentally measured melt-pool length and a_f . Accordingly, a_r was set to 22.8, 15.9, and 8.8 mm for NC, AC-W11, and AC-W8, respectively. These heat-source parameters were treated as calibrated effective values representing the melt-pool geometry under each cooling condition. The heat-source term was applied only to the existing magnesium-alloy region comprising the substrate and the wall. Any portion of the prescribed heat-source distribution overlapping the inactive domain, blocks, or water channels was excluded from the heat-input calculation. Under SCAC, this treatment approximately represented the restriction of the lateral arc and molten-metal spreading by the blocks.

The deposited-wall region was activated progressively as the torch moved. At each time step, wall grid points located up to 8 mm ahead of the heat-source center in the torch-travel direction were activated and included in all subsequent heat-conduction calculations. This advanced activation ensured that the prescribed moving heat-source distribution was applied to the newly deposited wall region. The deposition time per pass was 10.8 s, corresponding to a torch-travel speed of 500 mm/min over the 90 mm wall length. As in the experiments, the torch-travel direction was reversed after each pass, resulting in alternating deposition directions between adjacent layers.

Under NC, a cooling period of 120 s was imposed after every pass. Under SCAC, four passes were deposited continuously. Immediately after completion of each four-pass set, the numerical block domains were shifted upward by four layer heights. The lower surfaces of the repositioned blocks were located 5 mm below the top of the wall. The numerical analysis was then advanced for the prescribed effective cooling interval with the blocks held at the new position. This interval was not obtained directly from the experimentally recorded arc-off period of approximately 10 s. Instead, it was treated as an effective duration representing the combined effects of torch motion, the upward movement of the blocks, and the accompanying changes in thermal contact. The effective cooling intervals were calibrated against the substrate-temperature histories and melt-pool lengths and set to 12.5 s for AC-W11 and 15 s for AC-W8.

Thermal boundary and contact conditions

Analysis under NC considered heat transfer from the substrate and deposited-wall surfaces exposed to the ambient air, together with heat transfer from the substrate bottom surface to a jig attached to the substrate for heat flow. Analysis under SCAC additionally included heat transfer between the wall and blocks, between the substrate and blocks, and between the blocks and water, together with heat loss from the block surfaces exposed to the ambient air. The initial temperature, ambient-air temperature, water temperature, surface heat-transfer coefficients, interfacial contact heat-transfer coefficients, and lumped-jig parameters used in the analysis are summarized in Table 2. The heat-transfer and contact heat-transfer coefficients were treated as effective values selected to reproduce the experimentally measured substrate-temperature histories and melt-pool lengths. The ambient-air temperature was fixed at 25 °C, and heat conduction within the surrounding air was not solved. For the substrate, wall, and blocks, a grid face was identified as an exposed surface when the adjacent grid point lay outside the computational material domain. The corresponding material-specific heat-transfer coefficient was then applied to that face. No heat transfer was applied across the symmetry plane at the wall-width center.

In the experiments, heat flowed from the substrate bottom surface to the jig attached to the substrate. The entire jig was represented as a zero-dimensional isothermal lumped-capacitance model instead of being discretized in three dimensions. The jig temperature was assumed to be spatially uniform, and its evolution was determined from the heat

Table 2

Thermal boundary, contact, and lumped-jig parameters used in thermal analysis.

Parameter	Symbol	Value	Unit	Note
Initial temperature	T_0	25	°C	All regions including jig
Ambient temperature	T_{amb}	25	°C	Fixed external temperature used for convective heat loss; ambient air region was not solved as a heat-conducting region
Water temperature	T_{water}	25	°C	Fixed during analysis
Substrate–air heat-transfer coefficient	$h_{sub-air}$	1×10^{-5}	W/mm ² K	Exposed substrate surface
Wall–air heat-transfer coefficient	$h_{wall-air}$	1×10^{-5}	W/mm ² K	Exposed wall surface
Block–air heat-transfer coefficient	h_{Cu-air}	1×10^{-5}	W/mm ² K	SCAC only
Block–water heat-transfer coefficient	$h_{Cu-water}$	1.5×10^{-3}	W/mm ² K	SCAC only
Wall–block contact heat-transfer coefficient	$h_{wall-Cu}$	4×10^{-3}	W/mm ² K	Reduced to 0.3 times below T_{trig}
Substrate–block contact heat-transfer coefficient	h_{sub-Cu}	3×10^{-4}	W/mm ² K	Considered only during passes 1–4 when adjacent
Contact threshold temperature	T_{trig}	324.5	°C	0.5 times AZ31 liquidus temperature of 649 °C
Substrate bottom–jig heat-transfer coefficient	h_{bottom}	6×10^{-4}	W/mm ² K	Coupled with zero-dimensional jig model
Jig–air heat-transfer coefficient	$h_{jig-air}$	5×10^{-5}	W/mm ² K	Effective value
Effective jig–air heat-transfer area	A_{jig}	6000	mm ²	Equal to substrate plan area
Jig heat capacity	C_{jig}	300	J/K	Lumped model

Table note: Under AC-W11, $h_{wall-Cu}$ was multiplied by a pass-dependent correction factor F_i (i : pass number, starting from 0): $F_i = 0, 0.25, 0.25, 0.5, 0.75, 1.0$ for $i = 0, 1, 2, 3, 4$, respectively, where $i \geq 5$.

transferred from the substrate and the heat dissipated from the jig to the ambient air. The jig energy balance was

$$C_{jig} \frac{dT_{jig}}{dt} = \sum_{i \in \Gamma_{bottom}} h_{bottom} A_i (T_i - T_{jig}) - h_{jig-air} A_{jig} (T_{jig} - T_{amb}), \quad (5)$$

where T_{jig} is the jig temperature, C_{jig} is the total heat capacity of the jig, h_{bottom} is the heat-transfer coefficient between the substrate bottom surface and the jig, and $h_{jig-air}$ is the heat-transfer coefficient between the jig and ambient air. Ω_{bottom} denotes the set of grid points located on the substrate bottom surface, A_i is the representative bottom-surface area assigned to grid point i , T_i is the temperature at that grid point, $A_{jig-air}$ is the effective heat-transfer area between the jig and ambient air, and T_{amb} is the ambient-air temperature. In the present analysis, $A_{jig-air}$ was set to 6000 mm², corresponding to the substrate plan area, and C_{jig} was set to 300 J/K. Instead of the ambient-air temperature, the instantaneous jig temperature was used in the heat-transfer calculation at the substrate bottom surface.

Under SCAC, the water channels inside the blocks were represented as regions with a distinct material identifier. The water was assumed to circulate at a sufficiently high flow rate for its temperature to remain fixed at 25 °C throughout the analysis. The block–water heat-transfer coefficient (Table 2) was applied to the grid faces adjacent to the water region instead of directly solving the heat conduction and fluid flow within the water region. Wall–block and substrate–block thermal contacts were modeled by introducing contact thermal resistance between adjacent solid grid points. The effective wall–block contact heat-transfer coefficient was used to represent the combined effects of real contact area, wall-surface waviness and unevenness, block pressing conditions, and interfacial gaps, rather than resolving the detailed

contact geometry explicitly. The conductive resistances from the grid-point centers to the interface on both sides and the interfacial contact resistance were combined in series and converted into equivalent thermal conductivity. This equivalent conductivity was incorporated into the solid heat-conduction calculation. The grid spacing in the wall-width (build-height) direction was used for the wall–block (substrate–block) interface.

The wall–block contact condition was varied according to the wall-side interface temperature. When the wall-side temperature was equal to or higher than the threshold temperature, T_{th} , the equivalent thermal conductivity was calculated using the reference contact heat-transfer coefficient (Table 2). When the wall-side temperature was below T_{th} , the equivalent thermal conductivity was reduced to 0.3 times its high-temperature value to represent a deterioration in effective contact caused by solidification shrinkage and interfacial-gap formation. The threshold temperature was set to 324.5 °C, corresponding to 0.5 times the AZ31 liquidus temperature of 649 °C. This threshold was used as an empirical switching parameter for the effective contact condition in the calibrated model, not as a representation of the material-specific phase-transformation temperature. For AC-W11, a pass-dependent correction factor was additionally applied to the reference wall–block contact heat-transfer coefficient to approximately account for insufficient contact during the early stages of deposition. The correction factors are listed in the note below Table 2. No pass-dependent correction was applied for AC-W8. The substrate–block contact heat-transfer coefficient listed in Table 2 was likewise applied wherever substrate and block grid points were adjacent in the build-height direction. After the upward movement of the blocks removed this geometric adjacency, substrate–block heat transfer was no longer applied.

The initial temperature of all solid regions and the jig was 25 °C. When the blocks were moved, their temperature field before movement was translated to the new position such that the heat stored in the blocks was retained. The water region at the new position was reset to 25 °C.

Numerical solution

The governing heat-conduction equation was spatially discretized using a finite-difference formulation on the Cartesian grid described in Section “Numerical model and material properties”, and a semi-implicit scheme was used for time integration. The heat input from the moving heat source, the heat transfer to the ambient air, the heat transfer between the blocks and water, and the heat transfer between the substrate bottom surface and the jig were evaluated explicitly using the temperature field at the beginning of each time step. Heat conduction within the substrate, wall, and blocks was evaluated implicitly. The contact thermal resistances at the wall–block and substrate–block interfaces were incorporated into the implicit heat-conduction calculation through the equivalent thermal conductivities (Section “Thermal boundary and contact conditions”). At each time step, the temperature changes caused by the moving heat source and the explicit heat-transfer terms were calculated from the temperature field at the beginning of the time step and then added to the previous temperature field. The resulting intermediate temperature field was then used as the source term for the implicit solution of heat conduction, yielding the temperature field at the next time step. The water temperature was fixed at 25 °C during all iterative calculations. Grid points outside the material regions were also maintained at the ambient temperature of 25 °C. The temperature-dependent thermal conductivity and specific heat were evaluated using the temperature field at the beginning of each time step and held constant within that time step. The latent-heat contribution associated with melting and solidification was likewise incorporated into the specific heat using the effective-specific-heat formulation (Section “Numerical model and material properties”) based on the temperature at the beginning of each time step.

The linear system resulting from the implicit finite-difference discretization was solved using the red–black successive over-relaxation (SOR) method (Li et al., 2020; Mittal, 2014). The computational grid

was divided into red and black subsets according to the parity of the grid indices, and the two subsets were updated alternately. The SOR factor was set to 1.5. Convergence was assessed using the maximum temperature change between two solution states separated by a prescribed number of SOR iterations, normalized by the maximum absolute temperature in the computational domain. Convergence was assumed when this relative error fell below the specified tolerance. The convergence criterion was evaluated every 30 SOR iterations, with 500 being the maximum number of iterations per time step. The time-step size was 0.01 s.

The temperature of the zero-dimensional jig model was updated explicitly at each time step using the heat transferred from the substrate bottom surface and the heat dissipated from the jig to the ambient air (Eq. (5)). When the blocks were repositioned, their temperature field was transferred to the corresponding grid points at the new position to retain their thermal history and stored heat.

Model calibration and reproduction of experimental thermal behavior

The geometric conditions, heat-source parameters, heat-transfer coefficients, contact heat-transfer coefficients, and effective cooling intervals listed in Tables 1 and 2 were selected by considering their reproduction of the previously measured substrate-temperature histories and melt-pool lengths. In particular, the heat-source shape parameters, wall–block and substrate–block contact heat-transfer coefficients, block–water heat-transfer coefficient, and effective cooling intervals under SCAC were deemed calibrated effective values based on the experimental results. This section describes how the reproduction of the experimental thermal behavior was assessed using the calibrated model.

For the substrate-temperature histories, temperature was extracted from the numerical grid point corresponding to the thermocouple location in the experiments (at the longitudinal center of the wall and at the mid-thickness of the substrate). For NC, AC-W11, and AC-W8, the calculated temperature histories before arc interruption were compared with the experimental measurements. The temperature histories after the abrupt arc interruption during melt-pool length measurement were excluded from model calibration and reproduction assessment because the subsequent thermal sequence differed from that of normal deposition.

In the experiments, the melt-pool length was evaluated by abruptly interrupting the arc near the longitudinal center of the wall during the deposition of the target layer and then measuring the longitudinal length of the resulting crater (Section “Experimental procedure”). The target layers were the 11th, 15th, and 12th layers for NC, AC-W11, and AC-W8, respectively.

In the numerical analysis, the time history of the maximum temperature within the transverse section at the longitudinal center of the wall was extracted for the corresponding target layer. This section was considered to remain molten while its maximum temperature was equal to or higher than the AZ31 liquidus temperature of 649 °C. The numerically estimated melt-pool length, L_{mp} , was then calculated by multiplying this molten-state duration by the torch-travel speed.

$$L_{mp} = v_{torch} \Delta t_{liq}, \quad (6)$$

where v_{torch} is the torch-travel speed and Δt_{liq} is the duration in which the maximum temperature in the wall-center section remained at or above the liquidus temperature. In this analysis, v_{torch} was 500 mm/min.

The calibrated model’s reproduction of the experimental thermal behavior was assessed by comparing the calculated and measured substrate-temperature histories and melt-pool lengths for NC, AC-W11, and AC-W8. The results are presented in Section “Reproduction of experimental thermal behavior”. The calibrated model was then used to evaluate thermal accumulation in the substrate and the previously deposited wall under the experimentally applied NC and SCAC

conditions. The same numerical framework was subsequently used for a parametric study in which the block geometry and heat-transfer characteristics were changed under a common continuous-deposition condition. The thermal-accumulation evaluation index and parametric study conditions are described in Sections “Evaluation index for thermal accumulation” and “Parametric study conditions”, respectively.

Evaluation index for thermal accumulation

Thermal accumulation in the entire deposited component under the NC, SCAC, and parametric study conditions was assessed using a volume-averaged measure of the thermal energy stored in the substrate and the wall deposited up to each time (volume-averaged stored-thermal-energy density). During WA-DED, the temperature field near the heat source varies sharply, whereas the overall thermal accumulation appears as a temperature rise over a broader region of the substrate and the previously deposited material. Therefore, thermal accumulation was evaluated using the energy stored throughout the target region, not the temperature history at a single location.

The specific enthalpy of AZ31 at temperature T , including sensible and latent heat, was

$$H_{\text{Mg}}(T) = \int_0^T c_p(\theta) d\theta + Lf_l(T), \quad (7)$$

where c_p is the temperature-dependent sensible-heat specific heat shown in Fig. 3(b), θ is the integration variable, L is the latent heat of melting and solidification, and $f_l(T)$ is the liquid fraction. The sensible-heat contribution was evaluated through the trapezoidal integration of the tabulated specific-heat values. For temperatures above the upper limit of the tabulated range, 726 °C, the specific heat at this limit was held constant, and the sensible enthalpy was linearly extrapolated. The liquid fraction was set to 0 below the AZ31 solidus temperature of 525 °C and to 1 above the liquidus temperature of 649 °C, with a linear variation between these temperatures. The latent heat was 360 kJ/kg.

The specific enthalpy increment relative to the initial and ambient temperature, $T_{\text{amb}} = 25^\circ\text{C}$, was

$$\Delta H_{\text{Mg}}(T) = H(T) - h_{\text{Mg}}(T_{\text{amb}}). \quad (8)$$

Thus, $\Delta H_{\text{Mg}}(T)$ represents the thermal energy retained per unit mass relative to AZ31 at 25 °C. The total thermal energy stored in the substrate and the wall at time t , $E_{\text{Mg}}(t)$, was

$$E_{\text{Mg}}(t) = \sum_{i \in \Omega_{\text{Mg}}(t)} \rho_{\text{Mg}} \Delta H_{\text{Mg}}(T_i(t)) V_i, \quad (9)$$

where $\Omega_{\text{Mg}}(t)$ is the set of grid points belonging to the substrate and the wall deposited up to time t , $T_i(t)$ is the temperature at grid point i , V_i is the representative volume assigned to that grid point, and ρ_{Mg} is the density of AZ31. Only the substrate and deposited-wall regions were included in the evaluation. The blocks, water, and nonmaterial regions were excluded. Because the deposited-wall region was progressively activated, the evaluated AZ31 vol increased as deposition proceeded. The representative volume, V_i , was determined by considering the presence or absence of neighboring AZ31 grid points in each coordinate direction. Grid points inside the evaluation region were assigned the full control volume determined using the grid spacings. For grid points on a boundary, the representative length in a direction without an adjacent AZ31 grid point was halved. The control volumes of the substrate and the wall were calculated separately and then combined.

The total stored thermal energy was normalized by the instantaneous AZ31 vol to enable comparison of the thermal energy per unit volume among conditions with different wall widths, layer heights, numbers of passes, and deposited volumes. The volume-averaged stored-thermal-energy density, $e_{\text{Mg}}(t)$, was

$$e_{\text{Mg}}(t) = \frac{E_{\text{Mg}}(t)}{\sum_{i \in \Omega_{\text{Mg}}(t)} V_i}. \quad (10)$$

The time evolution of e_{Mg} (J/mm³) was used to evaluate thermal accumulation under the experimentally reproduced NC and SCAC conditions. It was also used in the parametric study to compare the relative effects of the cooling-design factors on thermal accumulation under a common continuous-deposition condition.

Parametric study conditions

A parametric study was conducted using the calibrated numerical model to evaluate how cooling-design factors in a short-tracking SCAC configuration affect thermal accumulation during continuous deposition. In the previous experiments and corresponding reproduction analyses, water-cooled blocks covered the full wall length and remained stationary in the torch-travel direction. In the parametric study, a more geometrically adaptable configuration was considered: a short block covered only the region near the heat source and moved along the wall while following the torch. Because this short-tracking configuration was not examined experimentally, the parametric study was treated as an extrapolative sensitivity analysis intended to identify relative thermal-design trends rather than to provide direct device-level predictions. The numerical model for short-tracking SCAC is schematically illustrated in Fig. 4.

The substrate geometry, wall geometry, number of passes, heat-source conditions, material properties, and basic thermal boundary conditions were based on AC-W8. The block spacing in this SCAC condition was 8.2 mm (Section “Experimental procedure”). In the fixed SCAC configuration used in the previous experiments, the blocks were moved upward after every four deposition passes, and an effective cooling interval was included during block repositioning. By contrast, in the short-tracking SCAC model, the block moved along the wall together with the heat source and shifted upward by one layer after each pass. Block movement was not interrupted, and all 24 passes were deposited continuously. Three design factors were varied: the block length in the torch-travel direction, L_{Cu} ; the effective wall–block contact heat-transfer coefficient, $h_{\text{wall-Cu}}$; and the effective block–water heat-transfer coefficient, h_{water} . Each factor was assigned three levels, resulting in a full-factorial set of 27 conditions. The parameter values are summarized in Table 3. All other geometric conditions, deposition parameters, heat-source conditions, material properties, and thermal boundary conditions were kept identical across the 27 cases.

The block length, L_{Cu} , was set to 5, 10, or 20 mm. The longitudinal center of the short block was aligned with the heat-source center, and it moved along the wall in the torch-travel direction. Its position was updated discretely according to the computational grid. The block temperature field before each movement was translated to the new position to retain the heat stored in the block. The vertical position was updated by one layer after each pass. The lower surface of the block was positioned at the higher of two locations: 17 mm below the top of the wall, or the substrate top surface. This arrangement represented the localized contact cooling of the regions near and immediately below the heat source.

The effective wall–block contact heat-transfer coefficient, $h_{\text{wall-Cu}}$, was assigned three levels: the value used to reproduce the AC-W8 experiment (Table 2) and values 10 and 100 times larger. These levels were used to represent differences in effective thermal contact caused by factors such as the block pressing conditions, geometric conformity between the copper-block surface and the wall surface, surface condition, and interfacial gaps. The temperature threshold for switching the effective contact condition and the low-temperature reduction factor of 0.3 (Section “Thermal boundary and contact conditions”) were fixed across cases.

The effective block–water heat-transfer coefficient, h_{water} , was like-

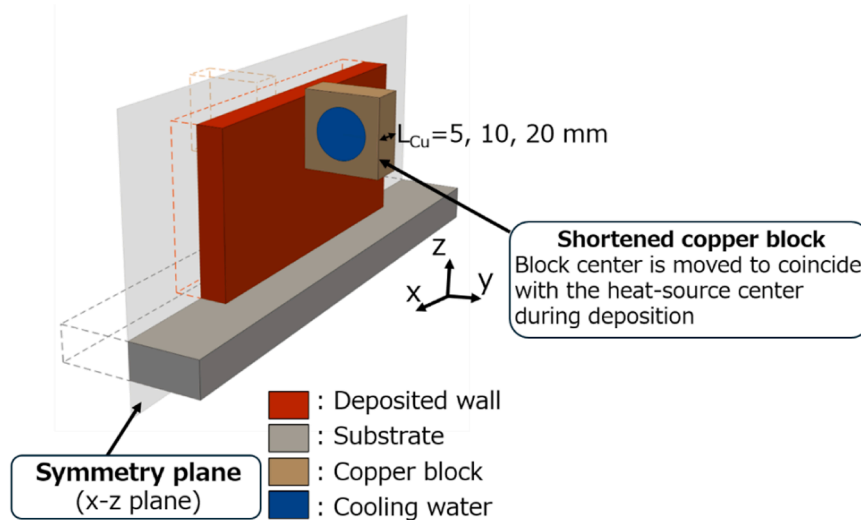


Fig. 4. Schematic illustration of the numerical model for short-tracking SCAC condition in parametric study. The block followed the heat source in the torch-travel direction and moved upward by one layer after each pass. The lower surface of the block was maintained 17 mm below the top of the wall but was not allowed to move below the substrate top surface.

Table 3

Parametric conditions used to evaluate effects of block length and heat-transfer conditions in short-tracking SCAC model on thermal accumulation.

Parameter	Value	Unit	Note
Block length in torch-travel direction, L_{Cu}	5, 10, 20	mm	Block tracking heat source
Wall–block contact heat-transfer coefficient, $h_{wall-Cu}$	4×10^{-3} , 4×10^{-2} , 4×10^{-1}	W/mm ² K	Effective wall–block contact condition
Block–water heat-transfer coefficient, h_{water}	1.5×10^{-3} , 1.5×10^{-2} , 1.5×10^{-1}	W/mm ² K	Effective water-side cooling capacity

wise assigned three levels: the value in Table 2 and values 10 and 100 times larger. Instead of representing specific water velocities or channel geometries directly, these values represented a broad range of effective water-side cooling capacities that could result from changes such as increases in water flow, the presence of multiple or serpentine channels, and increases in internal heat-transfer area.

The 10- and 100-fold-higher heat-transfer coefficients were used as sensitivity levels, not as predictions of directly realizable device conditions. The coefficients were spaced by one order of magnitude to examine changes in the dominant thermal resistance and to expose the transition between wall–block-limited and water-side-limited cooling behavior within a compact full-factorial design. The parametric study systematically compared how heat transfer from the wall to the block and heat rejection from the block to the water influenced thermal accumulation. Thus, its results should not be interpreted as direct predictions of the practical performance of a short-tracking SCAC device. Translating these effective coefficients into realizable manufacturing conditions would require specification of the wall–block contact mechanism, block pressing conditions, cooling-channel geometry, water-flow conditions, and internal heat-transfer area.

The time history of the volume-averaged stored-thermal-energy density, $e_{Mg}(t)$ (Section “Evaluation index for thermal accumulation”), was calculated for each condition. The mean value of e_{Mg} during each pass was obtained to show whether thermal accumulation continued, stabilized, or decreased after the initial thermal buildup. The second half of the 24-pass deposition sequence was selected to minimize the influence of the initial transient from the 25 °C initial condition on the comparison of thermal accumulation behavior among the analyzed

conditions. Linear regression was therefore applied to the pass-averaged values from the 13th pass to the 24th pass, and the resulting slope was the thermal accumulation rate, G_e . In addition, the mean value of e_{Mg} during the 24th pass represented the final thermal accumulation level.

Results and discussion

Reproduction of experimental thermal behavior

Fig. 5 compares the experimentally measured and numerically calculated substrate-temperature histories under NC, AC-W11, and AC-W8. In the experiments, the arc was abruptly interrupted during the deposition of the 11th, 15th, and 12th layers for NC, AC-W11, and AC-W8, respectively, to evaluate the melt-pool length. The vertical dashed lines in Fig. 5 indicate the arc interruption times. Because the thermal sequence after arc interruption differed from that during normal deposition, the shaded regions (after arc interruption) were excluded from model calibration and reproduction assessment.

Under NC, the substrate temperature increased rapidly during each pass and then decreased during the subsequent 120 s interpass period, producing a periodic temperature history. The numerical analysis broadly reproduced both the temperature rise associated with each pass and the subsequent cooling behavior. In both the experiment and analysis, the substrate temperature immediately preceding a pass gradually increased as deposition proceeded. Therefore, thermal accumulation was still continuing in the 11th layer under NC despite the 120 s interpass time. Together with the geometric instability observed in the corresponding NC experiment, this progressive pre-pass temperature rise supports the interpretation that thermal accumulation was still continuing at this stage.

Under SCAC, the substrate temperature initially increased under both AC-W11 and AC-W8 and then exhibited progressively smaller fluctuations. The numerical results broadly reproduced the rapid temperature rise during the initial passes and the stable subsequent temperature histories. In both the experiment and analysis, the substrate temperature under AC-W8 was lower than that under AC-W11. Quantitatively, the RMSEs between the experimentally measured and numerically calculated substrate-temperature histories before arc interruption were 15.25, 15.41, and 6.34 °C for NC, AC-W11, and AC-W8, respectively. Thus, the smaller block spacing in AC-W8 improved the effective contact between the wall and blocks, thereby increasing heat extraction from the deposited component. However, the effective

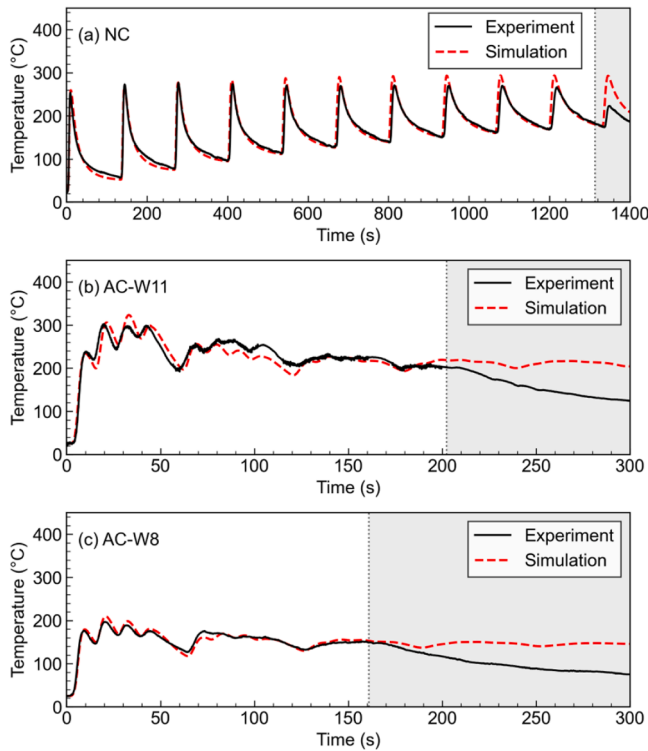


Fig. 5. Comparison of experimentally measured and numerically calculated substrate-temperature histories under (a) NC, (b) AC-W11, and (c) AC-W8. The vertical dashed lines indicate the times at which deposition was intentionally stopped by interrupting the arc for melt-pool-length measurement. The subsequent shaded regions were excluded from model calibration and reproduction assessment. Different time ranges are used in the three panels for clarity. The RMSEs between the experimentally measured and numerically calculated substrate-temperature histories before arc interruption were 15.25, 15.41, and 6.34 °C for NC, AC-W11, and AC-W8, respectively. The calculations reproduced the progressive temperature rise under NC and the early stabilization under SCAC.

cooling interval following each four-pass set in the SCAC analyses was not identical to the experimentally recorded arc-off duration. It was a calibrated parameter representing the combined effects of torch motion, the upward movement of the blocks, and changes in contact condition before and after block repositioning. Therefore, the agreement shown in Fig. 5 does not indicate direct prediction of individual equipment motions. Instead, it demonstrates the ability of the effective heat-conduction model to reproduce the experimentally observed overall temperature histories.

Fig. 6 compares the experimentally measured crater lengths with the numerically calculated melt-pool lengths. The experimental values were 25.8, 18.9, and 11.8 mm for NC, AC-W11, and AC-W8, and the corresponding numerical values were 24.2, 17.0, and 13.8 mm, respectively. The analysis reproduced the decrease in experimental melt-pool length as the cooling condition strengthened from that under NC to that under AC-W11 and then AC-W8. The relative differences between the calculated and experimental values were -6.2%, -10.1%, and 16.9%, respectively, with a maximum difference of approximately 17%. The rear heat-source radius, a_r , was calibrated for each condition using the experimentally measured melt-pool length. Therefore, instead of being an independent validation of melt-pool-length prediction, the comparison in Fig. 6 confirms that the calibrated heat-source model represented the cross-condition differences in molten-state behavior on an effective basis.

Overall, the calibrated model broadly reproduced the periodic temperature variation and continuing temperature rise under NC, the early

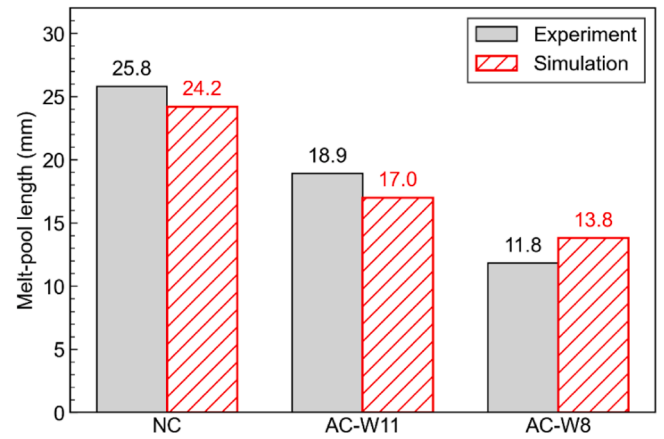


Fig. 6. Comparison of experimentally measured crater lengths and numerically calculated melt-pool lengths under NC, AC-W11, and AC-W8. Both experiment and calculation showed a progressive decrease in melt-pool length from NC to AC-W11 and AC-W8.

stabilization of the temperature history under SCAC, and the decrease in melt-pool length with increasing cooling intensity. However, because the previous experiment provided only a point thermocouple history and no synchronized, calibrated full-field thermography, the calculated spatial temperature field could not be independently assessed. Future validation using synchronized full-field thermography would enable direct assessment of the calculated spatial temperature distributions. This model was used to evaluate thermal accumulation throughout the substrate and the previously deposited wall (Section “Thermal accumulation behavior under experimentally reproduced NC and SCAC conditions”), which could not be directly assessed from single-point temperature measurement. The subsequent analysis in Section “Coupled effects of block length and heat-transfer conditions” extends this calibrated framework to short-tracking SCAC configurations that were not examined experimentally. Accordingly, the results in Section “Coupled effects of block length and heat-transfer conditions” are interpreted as an extrapolative sensitivity analysis for identifying relative thermal-design trends rather than as independently validated predictions of specific device performance.

Thermal accumulation behavior under experimentally reproduced NC and SCAC conditions

Fig. 7 shows the time histories of the volume-averaged stored-thermal-energy density, e_{Mg} , under the experimentally reproduced NC, AC-W11, and AC-W8 conditions. Here, e_{Mg} represents the thermal energy stored in the substrate and the wall deposited up to each time, divided by the corresponding AZ31 vol.

Under NC, e_{Mg} increased rapidly during each pass and decreased during the subsequent 120 s interpass period, resulting in a periodic variation. However, despite decreasing during each interpass cooling period, e_{Mg} did not return to its initial level; both the maximum and minimum values in each cycle gradually increased as deposition proceeded. Thus, some of the heat supplied during each pass remained in the substrate and the previously deposited wall until the next pass, accumulating as the number of layers increased. After approximately 2000 s from the start of deposition, corresponding to approximately the 15th–16th passes, e_{Mg} under NC approached a periodic variation within a nearly constant range. Therefore, the heat supplied during each pass and the heat released through the substrate, wall, and jig during the interpass period approached a cycle-averaged balance. However, the substrate temperature immediately before each pass was still increasing near the 11th pass (Section “Reproduction of experimental thermal behavior”). Thermal accumulation in the overall component was

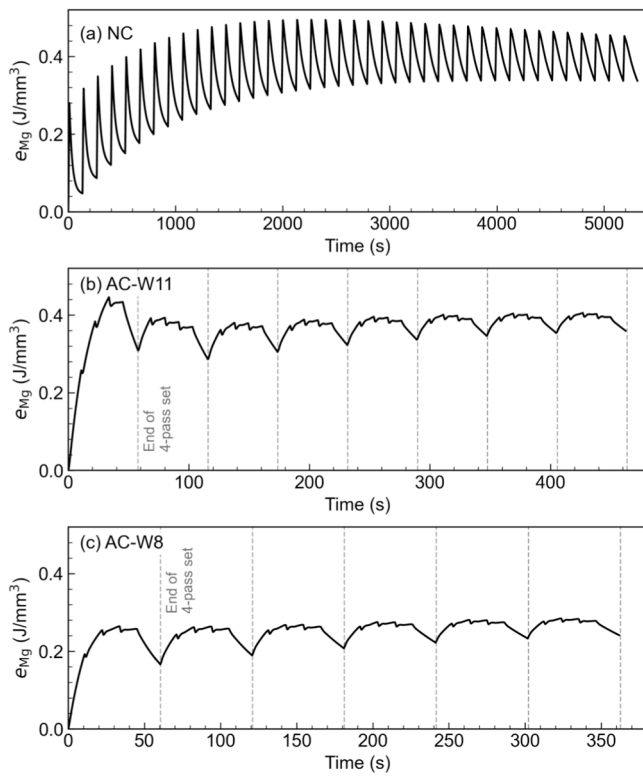


Fig. 7. Time histories of volume-averaged stored-thermal-energy density under experimentally reproduced (a) NC, (b) AC-W11, and (c) AC-W8. Different time ranges are used because the deposition schedules and total process times differed across conditions. The vertical dashed lines in (b) and (c) indicate the end of each four-pass set. NC showed continuing accumulation during the early passes, whereas both SCAC conditions reached an early periodic range, with AC-W8 remaining lower than AC-W11.

therefore still continuing in that stage.

The time evolution of e_{Mg} under SCAC differed from that under NC because four passes were deposited continuously and followed by a cooling interval associated with block repositioning. Under AC-W11 and AC-W8, e_{Mg} increased during the initial stage and then entered a periodic range in an early stage. The vertical dashed lines in Figs. 7(b) and 7(c) indicate the end of each four-pass set.

Under AC-W11, e_{Mg} remained within a range broadly comparable to that reached during the later stage of NC although deposition proceeded over a substantially shorter total process time. A 120 s interpass period was applied after every pass under NC, whereas only a short effective cooling interval was applied after each set of four continuous passes under SCAC. Thus, under the deposition sequences used in the previous experiments, heat extraction through the SCAC blocks maintained the overall thermal accumulation at approximately the late-NC-stage level without relying on prolonged NC. However, the NC and SCAC conditions differed not only in interpass time but also in wall width, layer height, number of passes, and heat removal path. Therefore, the comparable e_{Mg} levels under AC-W11 and late-stage NC do not imply that SCAC was thermally equivalent to a 120 s NC interval. Instead, they reflected the thermal accumulation states under the respective deposition conditions used in the previous experiments.

Under AC-W8, e_{Mg} remained clearly lower than that under AC-W11, consistent with the lower substrate temperature under AC-W8 (Section “Reproduction of experimental thermal behavior”). The smaller block spacing in AC-W8 produced a larger effective contact region between the wall and blocks, thereby promoting heat extraction from the previously deposited material.

Although e_{Mg} under AC-W11 was broadly comparable to the late-NC-

stage level, the melt-pool length under AC-W11 was shorter than that under NC. This difference arose because e_{Mg} and melt-pool length represent different thermal states: the former reflects thermal accumulation throughout the substrate and the wall, whereas the latter reflects the local molten state near the heat source.

Therefore, for the numerically reproduced SCAC conditions corresponding to the previous experiments, overall thermal accumulation stabilized in an early stage and remained at a level comparable to or lower than that reached during late-stage NC despite the substantially shorter process time. The previous experiments demonstrated that SCAC enabled high-productivity deposition with an average interpass time of approximately 2.5 s. This analysis explains the thermal basis for this behavior in terms of the thermal energy retained throughout the substrate and the previously deposited wall.

Coupled effects of block length and heat-transfer conditions

This section elucidates how the combination of block length (L_{Cu}), effective wall–block contact heat-transfer coefficient ($h_{wall-Cu}$), and effective block–water heat-transfer coefficient (h_{water}) affected thermal accumulation during continuous deposition under short-tracking SCAC. First, representative time histories are used to illustrate changes in thermal accumulation behavior with cooling conditions. The thermal accumulation rate during passes 13–24 and the final thermal accumulation level are then compared for all 27 conditions. Finally, the design implications and limitations of the short-tracking SCAC configuration are discussed.

Thermal accumulation behavior during continuous deposition

Fig. 8 shows the time histories of the volume-averaged stored-thermal-energy density e_{Mg} for five representative short-tracking SCAC conditions during continuous deposition. In Cases 1–3, $h_{wall-Cu}$ and h_{water} were fixed at their lowest values, while L_{Cu} was set to 5, 10, and 20 mm, respectively. In Case 4, L_{Cu} was fixed at 20 mm, and both heat-transfer coefficients were increased to 10 times their baseline values. In Case 5, L_{Cu} was also 20 mm, and both coefficients were increased to 100 times their baseline values. The time histories for all 27 conditions are presented in Supplementary Fig. S1.

In Case 1, e_{Mg} increased rapidly from the beginning of deposition and continued rising until the end of the 24th pass. The mean value during the 24th pass was 1.197 J/mm^3 , which was the highest among the five representative cases. In Cases 2 and 3, e_{Mg} also increased throughout deposition, but its level decreased as the block length increased. The mean values during the 24th pass were 0.977 J/mm^3 in Case 2 ($L_{Cu} = 10 \text{ mm}$) and 0.689 J/mm^3 in Case 3 ($L_{Cu} = 20 \text{ mm}$). These values were

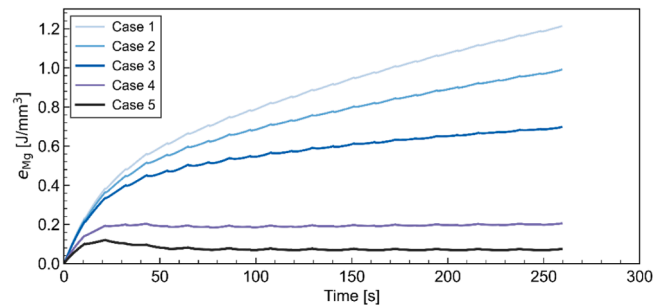


Fig. 8. Time histories of volume-averaged stored-thermal-energy density under five representative short-tracking SCAC conditions. In Cases 1, 2, and 3, $h_{wall-Cu} = 4.0 \times 10^{-3} \text{ W/mm}^2\text{K}$ and $h_{water} = 1.5 \times 10^{-3} \text{ W/mm}^2\text{K}$, with $L_{Cu} = 5, 10, 20 \text{ mm}$, respectively. In Cases 4 and 5, $L_{Cu} = 20 \text{ mm}$, with $(h_{wall-Cu}, h_{water}) = (4.0 \times 10^{-2}, 1.5 \times 10^{-2})$ and $(4.0 \times 10^{-1}, 1.5 \times 10^{-1}) \text{ W/mm}^2\text{K}$, respectively. The representative cases ranged from continued thermal accumulation to nearly stabilized or slightly decreasing behavior as the block length and heat-transfer conditions were enhanced.

approximately 18% and 42% lower, respectively, than that in Case 1. Thus, increasing the block length reduced thermal accumulation during continuous deposition under fixed heat-transfer conditions. A longer block increased the contact range and duration at each wall location while increasing the block heat capacity and water-side heat-transfer area. The differences between Cases 1–3 therefore resulted from the combined effects of these geometric changes. Nevertheless, e_{Mg} increased gradually during the later part of Case 3, indicating that increasing L_{Cu} to 20 mm was insufficient to stabilize thermal accumulation under the baseline heat-transfer conditions.

In Case 4, L_{Cu} was 20 mm, and both $h_{wall-Cu}$ and h_{water} were 10 times their values in Case 3. The value of e_{Mg} increased to approximately 0.200 J/mm³ during the initial stage and then remained within a nearly constant range. Its mean value during the 24th pass was 0.200 J/mm³, approximately 71% lower than that in Case 3. Thus, strengthening both heat-transfer paths, in addition to increasing the block length, substantially suppressed the continued increase in thermal accumulation.

In Case 5, L_{Cu} was 20 mm and both heat-transfer coefficients were set to their maximum values. The value of e_{Mg} increased to approximately 0.120 J/mm³, decreased, and then remained near 0.070 J/mm³. Its mean value during the 24th pass was 0.070 J/mm³, approximately 94% and 90% lower than those in Cases 1 and 3, respectively. Under this condition, thermal accumulation during the later part of continuous deposition was nearly stabilized despite the absence of interpass interruptions.

The representative cases demonstrate that thermal behavior ranged from continued accumulation until the end of deposition, to nearly stable or decreasing accumulation, depending on the cooling conditions. The following section compares the thermal accumulation rate and final

thermal accumulation level for all 27 conditions.

Effects of cooling conditions on progression and level of thermal accumulation

Fig. 9 summarizes the thermal accumulation rates G_e for all 27 short-tracking SCAC conditions. For each condition, G_e was obtained via linear regression of the pass-averaged e_{Mg} values from the 13th pass to the 24th pass (Section “Parametric study conditions”). A positive G_e indicates that thermal accumulation continued during the second half of deposition, whereas a value close to zero indicates that the thermal state was nearly stabilized. A negative value indicates that the volume-averaged stored-thermal-energy density decreased as deposition proceeded. Fig. 10 shows the mean e_{Mg} during the 24th pass for the 27 conditions. Figs. 9 and 10 represent the progression rate of thermal accumulation and the final thermal accumulation level, respectively.

At $L_{Cu} = 5$ mm, G_e was positive under all heat-transfer conditions, indicating that thermal accumulation continued during the second half of deposition. In particular, at the lowest $h_{wall-Cu}$, increasing h_{water} reduced G_e and the mean e_{Mg} only slightly during the 24th pass. By contrast, increasing $h_{wall-Cu}$ substantially reduced both quantities. Even when both heat-transfer coefficients were set to their maximum values, G_e remained positive at approximately 0.0022 J/mm³/pass. Thus, stronger wall- and water-side heat transfer markedly suppressed thermal accumulation for the 5 mm block but did not completely halt its progression.

At $L_{Cu} = 10$ mm, both G_e and the mean e_{Mg} during the 24th pass were generally lower than those obtained for the 5 mm block. When both heat-transfer coefficients were set to their maximum values, G_e decreased to approximately 0.0006 J/mm³/pass, indicating that the

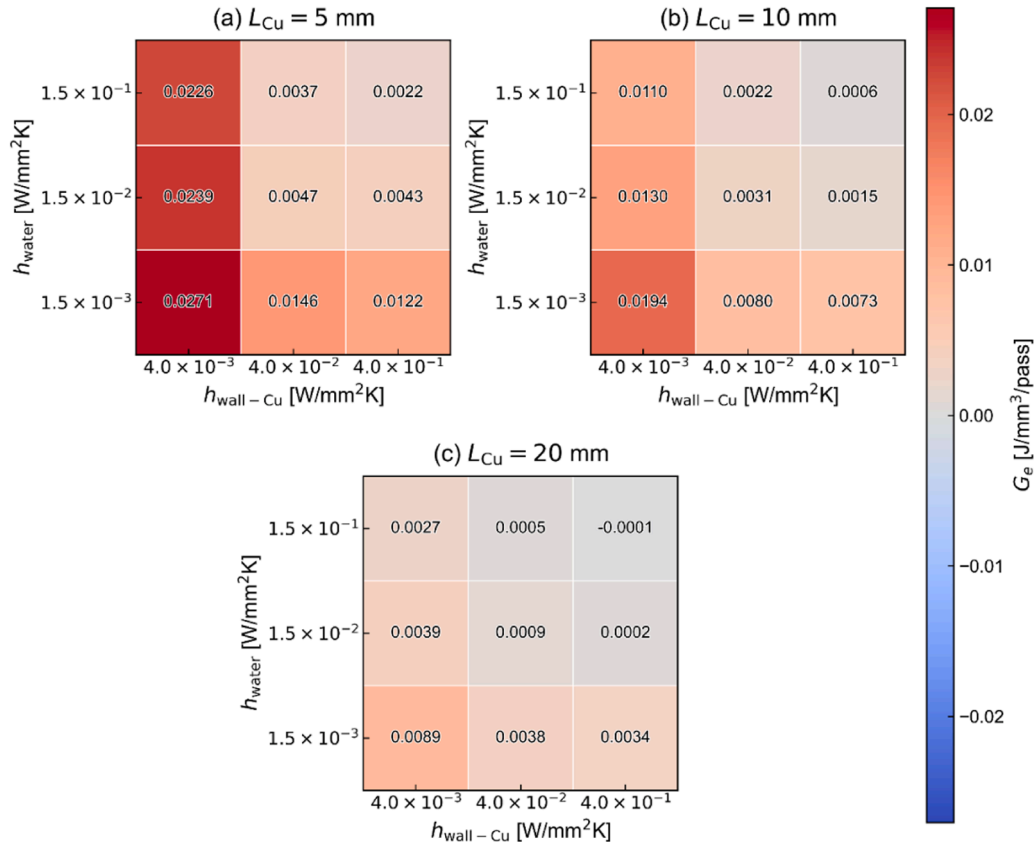


Fig. 9. Thermal accumulation rates G_e for all 27 short-tracking SCAC conditions: (a) $L_{Cu} = 5$ mm, (b) $L_{Cu} = 10$ mm, and (c) $L_{Cu} = 20$ mm. For each condition, G_e was obtained through linear regression of the pass-averaged volume-averaged stored-thermal-energy density, e_{Mg} , from pass 13 to 24. This range corresponded to the second half of the 24-pass deposition sequence and was selected to reduce the influence of the initial thermal transient on thermal accumulation. Positive, near-zero, and slightly negative values indicate continuing, nearly stabilized, and slightly decreasing thermal accumulation, respectively.

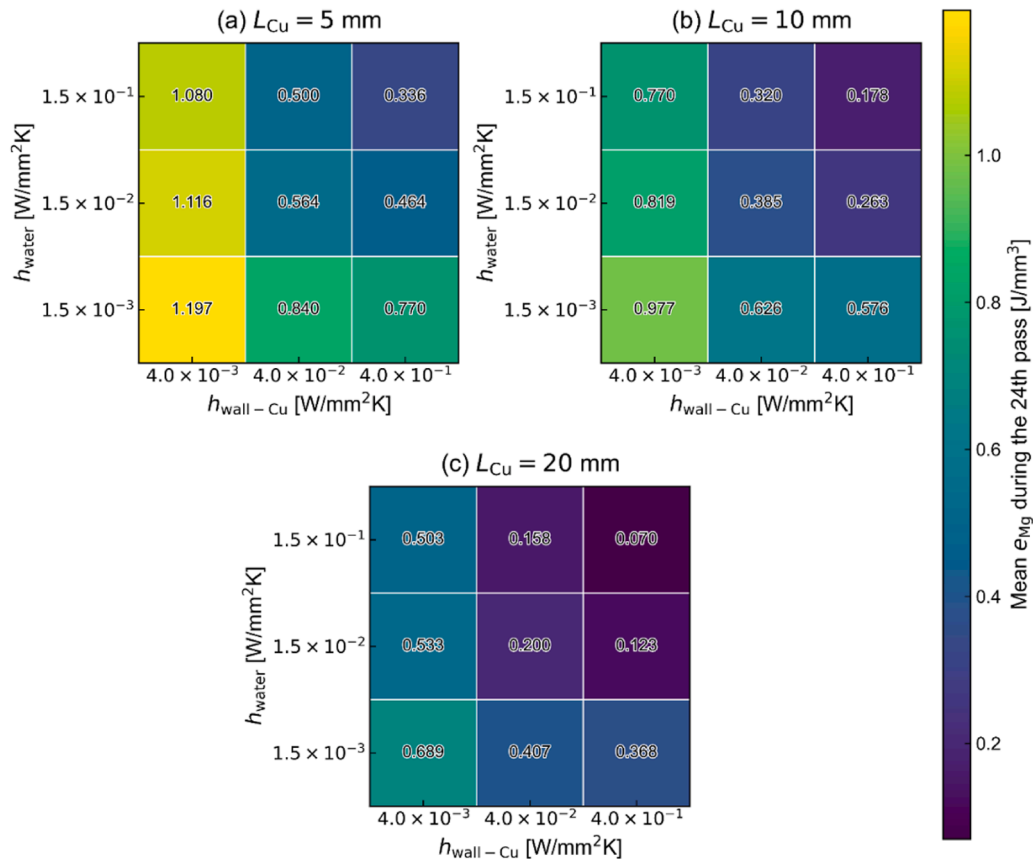


Fig. 10. Combined effects of wall–block contact heat-transfer coefficient and block–water heat-transfer coefficient on mean volume-averaged stored-thermal-energy density during the 24th pass: (a) $L_{\text{Cu}} = 5$ mm, (b) $L_{\text{Cu}} = 10$ mm, and (c) $L_{\text{Cu}} = 20$ mm. A common color scale is used for all panels. The benefit of stronger water-side heat transfer became pronounced only when sufficient wall–block heat transfer was provided.

progression of thermal accumulation was nearly suppressed. At $L_{\text{Cu}} = 20$ mm, the lowest G_e was obtained when both heat-transfer coefficients were at their maximum levels. Under this condition, G_e was -0.0001 J/mm³/pass, and the mean e_{Mg} during the 24th pass was 0.070 J/mm³. The thermal accumulation state was therefore nearly constant or slightly decreasing, consistent with the time history of Case 5 in Fig. 8.

Overall, increasing the block length reduced both the thermal accumulation rate during passes 13–24 and the final thermal accumulation level. However, the magnitude of this effect depended on the heat-transfer conditions, and thermal accumulation continued even with a longer block when both heat-transfer coefficients were low. For example, at $L_{\text{Cu}} = 20$ mm and the lowest values of $h_{\text{wall-Cu}}$ and h_{water} , G_e remained positive at 0.0089 J/mm³/pass, and the mean e_{Mg} during the 24th pass was 0.689 J/mm³. Thus, increasing the block length alone was insufficient for stabilizing thermal accumulation during continuous deposition.

At all three block lengths, the effect of h_{water} on thermal accumulation depended strongly on $h_{\text{wall-Cu}}$. For example, at $L_{\text{Cu}} = 20$ mm and the lowest $h_{\text{wall-Cu}}$, increasing h_{water} by a factor of 100 reduced the mean e_{Mg} during the 24th pass from 0.689 J/mm³ to 0.503 J/mm³ only. By contrast, at the highest $h_{\text{wall-Cu}}$, the same increase in h_{water} reduced the corresponding value from 0.368 J/mm³ to 0.070 J/mm³. Thus, under limited wall–block heat transfer, the heat flow from the wall to the block becomes the controlling resistance, and additional water-side cooling cannot be fully utilized. Improving wall–block heat transfer allows more heat to enter the block, enabling the increased water-side heat-transfer capacity to promote heat rejection to the water.

These results demonstrate that both the progression and final level of thermal accumulation in short-tracking SCAC were governed by the combined effects of block length and the two heat-transfer conditions. In

particular, sufficient wall–block heat transfer was required to utilize enhanced water-side cooling effectively. Combining a longer block with improved heat transfer at both interfaces could nearly halt thermal accumulation during uninterrupted continuous deposition, substantially reducing the final thermal accumulation level.

Design implications and limitations of short-tracking SCAC

The parametric study results showed that the cooling performance of short-tracking SCAC was governed not by the block length alone but by its combination with heat transfer from the wall to the block and heat rejection from the block to the water. In particular, at a low $h_{\text{wall-Cu}}$, increasing h_{water} mitigated thermal accumulation only slightly. Therefore, the effective use of the water-side cooling capacity required a stable contact condition, achieved through conformity to the wall geometry, appropriate block pressing, and suppression of interfacial gaps. Greater wall-surface waviness or poorer conformity would reduce the real contact area, decrease the effective wall–block contact heat-transfer coefficient, and weaken cooling performance.

Under sufficient wall–block heat transfer, increasing h_{water} substantially reduced thermal accumulation. Therefore, under improved contact conditions, heat rejection from the block may be enhanced by adjusting the water flow rate, the number and geometry of cooling channels, or internal heat-transfer area. However, the h_{water} values used in this analysis did not represent these device modifications individually, instead serving as effective parameters representing differences in the overall water-side cooling capacity.

Increasing the block length also reduced G_e and the mean e_{Mg} during the 24th pass. However, a longer block may be disadvantageous in terms of device compactness, ability to follow curved geometries, and interference with the torch and surrounding equipment. In this analysis,

thermal accumulation continued for the 5 mm block even under the highest heat-transfer conditions, whereas it was nearly halted for the 10 mm block and showed a slight decrease for the 20 mm block. Thus, improved heat-transfer conditions could partially compensate for the loss of cooling performance caused by shortening the block but could not completely eliminate the influence of block length. In practical device design, the block length should therefore be selected by balancing the required suppression of thermal accumulation against geometric adaptability.

The parametric study assumed that the short block followed the heat source and moved accurately to the prescribed position after each pass. Although the short-tracking configuration is intended to improve geometric adaptability relative to the full-length-block configuration used in the previous experiments, the present analysis considered only a straight thin wall contacted from both sides. Application to curved or locally varying geometries will require adaptive block trajectories and contact mechanisms. In an actual system, the contact condition would likely vary temporally and spatially because of block motion and positioning accuracy, variations in wall geometry, contact pressure, solidification shrinkage, and surface unevenness. The water temperature was also fixed at 25 °C, and the fluid flow and temperature rise within the cooling channels were not solved directly. Practical implementation will therefore require synchronized block motion with the torch, controlled positioning and contact pressure, reliable cooling-water delivery, and process monitoring to maintain stable contact without interfering with the torch and surrounding equipment.

The numerical predictions are subject to uncertainty in calibrated and effective inputs, including thermal efficiency and heat-source geometry, wall–block and block–water heat-transfer coefficients, external boundary heat-transfer coefficients, wall geometry and real contact area, and effective cooling intervals. Higher effective heat input or lower external heat loss would increase the predicted thermal accumulation, whereas larger interface heat-transfer coefficients would reduce it. Uncertainty in wall geometry, real contact area, and cooling intervals may therefore shift the absolute accumulation levels and the block length at which near-stabilization is predicted. The full-factorial study directly evaluates sensitivity to the two interface heat-transfer coefficients; however, a formal joint uncertainty quantification of all model inputs is beyond the scope of the present study.

Furthermore, the present heat-conduction model did not account for molten-pool convection, including Marangoni flow, discrete metal transfer, free-surface deformation, changes in bead geometry, or mechanical interaction between the wall and the blocks. These omitted phenomena may redistribute heat locally and affect the predicted melt-pool geometry, cooling rates, and thermal gradients. Accordingly, future local-scale predictions should couple heat conduction with melt-pool flow, metal transfer, and geometry evolution, whereas the present results are interpreted primarily at the component scale. Consequently, a numerical condition that suppresses thermal accumulation does not directly guarantee stable bead formation or adequate interlayer fusion in an actual build. The practical applicability of short-tracking SCAC and its cooling-performance equivalence to the fixed SCAC configuration used in the previous experiments were not verified in this study.

Accordingly, the present results should be interpreted as thermal design guidance for combining block length, wall–block contact heat transfer, and water-side cooling capacity, not as direct predictions of the performance of a specific short-tracking SCAC device. Experimental validation using a short-tracking cooling system is required to assess contact conditions, water conditions, build stability, and the resulting component geometry, microstructure, and mechanical properties.

Conclusions

In this study, three-dimensional transient heat-conduction analysis was conducted on AZ31 magnesium-alloy thin walls to clarify thermal accumulation in SCAC-assisted WA-DED and obtain design guidance for

short-tracking SCAC. First, the numerical model was calibrated against previous experiments using water-cooled blocks that covered the full wall length, and thermal accumulation under NC and SCAC conditions was evaluated. A full-factorial parametric study was then performed for 27 continuous-deposition conditions by varying the block length, effective wall–block contact heat-transfer coefficient, and effective block–water heat-transfer coefficient. The principal conclusions are summarized as follows:

1. The calibrated model broadly reproduced the periodic substrate-temperature variation and progressive temperature rise under NC, the early stabilization of temperature histories under SCAC, and the decrease in melt-pool length with stronger cooling. The maximum difference between the calculated and experimental melt-pool lengths was approximately 17%. However, because the heat-source geometry was calibrated against previously measured melt-pool lengths, this comparison does not represent an independent validation of melt-pool prediction.
2. Under NC, thermal accumulation continued during the initial stage despite the 120 s interpass time, and the volume-averaged stored-thermal-energy density approached a nearly periodic range after around pass 15 to 16. Under SCAC, the thermal accumulation state stabilized in an early stage during the deposition sequence, which consisted of four continuous passes followed by a short effective cooling interval.
3. Under AC-W11, the volume-averaged stored-thermal-energy density remained broadly comparable to that reached during the late-NC-stage despite the substantially shorter total process time. Under AC-W8, an even lower thermal accumulation level was obtained. In terms of the thermal energy retained throughout the substrate and previously deposited wall, these results explain the thermal basis for the high-productivity deposition achieved by SCAC in the previous experiments.
4. In the short-tracking SCAC configuration, increasing the block length, the effective wall–block contact heat-transfer coefficient, and the effective block–water heat-transfer coefficient reduced both the thermal accumulation rate during passes 13–24 and the final thermal accumulation level. However, increasing the block length alone did not stabilize the thermal accumulation state when both the effective wall–block and block–water heat-transfer coefficients were low.
5. The effect of an increase in water-side cooling capacity on thermal accumulation depended strongly on the wall–block heat-transfer condition. Under low wall–block heat transfer, increasing the block–water heat-transfer coefficient alleviated thermal accumulation only slightly. Under enhanced wall–block heat transfer, enhanced water-side cooling produced a much larger reduction. Stable wall–block contact is therefore a prerequisite for effectively utilizing water-side cooling capacity.
6. For a copper-block length in the torch-travel direction of $L_{Cu} = 5\text{ mm}$, the thermal accumulation rate remained positive even when both heat-transfer coefficients were at their maximum levels. Under the same heat-transfer conditions, thermal accumulation nearly stopped at $L_{Cu} = 10\text{ mm}$ and remained nearly constant or slightly decreased at $L_{Cu} = 20\text{ mm}$. For the 20 mm block with both heat-transfer coefficients at their maximum levels, the mean volume-averaged stored thermal energy density during the 24th pass was 0.070 J/mm^3 , approximately 94% lower than the maximum value among the 27 conditions.

These numerical results demonstrate that the cooling performance of short-tracking SCAC is determined by the combined effects of block length, wall–block contact condition, and water-side cooling capacity, not by any single factor. In particular, the loss of cooling performance caused by block shortening cannot be compensated for by merely strengthening water-side cooling. Sufficient wall–block heat transfer must first be secured, after which the block length and water-side heat-

rejection capacity should be designed in combination.

CRedit authorship contribution statement

Houichi Kitano: Writing – review & editing, Writing – original draft, Visualization, Validation, Software, Methodology, Investigation, Formal analysis, Data curation, Conceptualization. **Hyu Kudo:** Writing – review & editing, Investigation, Formal analysis, Data curation. **Hideaki Nagamatsu:** Writing – review & editing, Supervision, 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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Supplementary materials

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Data availability

Data will be made available on request.

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