

Article

Effect of Active Air-Cooling Configuration During Interlayer Friction Stir Processing on Grain Morphology and Within-Wall Homogeneity in UAMFSP-Fabricated Aluminum 4043 Walls: A Single-Replicate Exploratory Study

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Abstract

The Unified Additive-Deformation Manufacturing Process (UAMFSP) integrates MIG-based Wire Arc Additive Manufacturing (WAAM) with interlayer Friction Stir Processing (FSP) on a single CNC platform, applying severe plastic deformation to each deposited layer. The novelty of this work is the first controlled comparison, to the authors' knowledge, of the spatial position of forced-air cooling applied concurrently with the FSP traverse in such an integrated platform: cooling was directed at the top bead surface (TC), at the bottom substrate (BC), or at both surfaces simultaneously (DC), with a MIG-only wall serving as the baseline. Single-layer, four-bead ER4043 aluminum walls were deposited on AA6061 substrates, and optical micrographs from three within-bead locations per condition were quantified with ImageJ, yielding 2038 to 2723 grains per condition. A lumped-parameter thermal model calibrated to infrared thermography ranked the comparative cooling rates as NC < TC < BC < DC. The principal finding is grain homogenization: every FSP condition reduced the grain area scatter by 55 to 71 percent relative to MIG-only, eliminating the coarse-grain tail of the as-deposited distribution regardless of cooling position. Mean equivalent diameters of all four conditions lie within 0.5 μm of one another, at the resolution limit of the optical measurement, so the mean-size ordering (DC finest, 2.76 μm) is reported as a ranking rather than as net refinement. Vickers microhardness on the MIG-only and uncooled FSP walls revealed a 27 HV within-wall gradient (86 ± 7 HV at the stir-zone center versus 59 ± 2 HV at the bead edge), which motivates bilateral cooling for thermal-field homogenization. Because the design is single-replicate, between-condition differences are presented as hypotheses for replicate study with EBSD and spatially resolved hardness mapping.

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Keywords: friction stir processing; wire arc additive manufacturing; active cooling configuration; aluminum alloys; grain homogenization; within-wall homogeneity; grain area scatter; UAMFSP; grain morphology

1. Introduction

Wire Arc Additive Manufacturing (WAAM) has attracted sustained research interest as a near-net-shape fabrication route for aluminum structural components, which offers high

deposition rates, geometric design freedom, and compatibility with established welding infrastructure [1–3]. The layer-by-layer thermal cycle inherent in WAAM, however, promotes progressive interlayer heat accumulation, dendritic solidification, porosity, and residual stress, each of which degrades the mechanical performance of the deposited material [4–6]. Grain refinement and microstructural homogenization through post-deposition processing are therefore prerequisites for structural applications where toughness, fatigue resistance, and microstructural isotropy are required [7,8].

Friction Stir Processing (FSP), applied as an interlayer deformation step between successive WAAM deposits, addresses these limitations by introducing severe plastic deformation (SPD) into the as-deposited zone, which, in aluminum alloys, is widely associated with dynamic recrystallization (DRX) and grain refinement. The rotating FSP tool fragments the columnar dendritic morphology, redistributes solute and second-phase particles, and can refine the grain structure. Equiaxed grain morphologies with a high proportion of high-angle grain boundaries (HAGBs), inferred from misorientation analysis in the broader FSP literature, have been widely reported, and FSP has also been reported to close solidification porosity in that literature [9–12]. Recent studies have reported that interlayer FSP can reduce mean grain sizes by one to two orders of magnitude and improve hardness and tensile strength relative to the as-deposited WAAM baseline [13–16]. The most recent contributions extend this hybrid route in two directions. A 2026 review of deformation-assisted WAAM across aluminum, titanium, steel, and nickel alloy systems identified interlayer deformation, including rolling and FSP, as among the most effective strategies for converting columnar as-deposited structures to equiaxed grains and recovering fatigue performance [17]. In parallel, Zhou et al. [18] showed that FSP assistance during WAAM of an Al–Cu–Sc alloy refines the deposited wall microstructure and improves tensile properties, which confirms that the benefits of interlayer FSP extend to precipitation-strengthened aluminum systems of current industrial interest.

The patented Unified Additive-Deformation Manufacturing Process (UAMFSP) [19] integrates MIG-based WAAM and FSP within a single CNC platform, which combines material deposition and solid-state deformation in an automated, sequentially controlled workflow. A predecessor study by the present authors [20] reported that the UAMFSP process reduces peak interlayer temperatures from approximately 870 to 1000 °C (MIG-only WAAM) to below 400 °C and that the process produced a roughly 29-fold reduction in mean grain area (from 313.6 μm^2 to 10.9 μm^2) alongside a 45.8 percent improvement in Vickers microhardness in ER4043 aluminum walls. That study employed a three-layer, four-bead-per-layer wall configuration without active cooling beyond natural convection and radiation during the FSP stage.

Although the UAMFSP process operates in a sub-solidus thermal regime, the residual frictional heat generated during each FSP pass, captured by infrared (IR) thermography as a characteristic three-stage profile comprising tool plunge and pre-heating, traverse, and end-of-pass frictional heating, persists within the deposited bead long enough to influence grain boundary migration and thermally activated restoration after deformation. The magnitude and spatial distribution of this residual thermal field depend on the heat-extraction pathway available: in the absence of forced cooling, the primary dissipation routes are natural convection and radiation from the bead surface and conduction through the substrate, which are relatively slow. Active cooling directed at specific surfaces of the deposited wall can accelerate post-deformation heat extraction, which is expected to suppress grain growth and help stabilize the deformation-refined microstructure at a finer length scale.

Active cooling can play two distinct roles in hybrid WAAM-FSP processes. The first role is control of interlayer thermal accumulation across successive deposition passes,

which has been widely studied in wire-arc DED systems as a means to suppress progressive heat buildup and associated distortion [21,22]. Recent work on wire-arc directed energy deposition of AZ31 magnesium demonstrated that active heat extraction during deposition refines the as-built grain size approximately threefold while preserving tensile ductility even at minimal interlayer dwell times [23], which underscores the continuing attention this first role receives in the current literature. The second role, which is the focus of the present work, is control of the retained post-FSP microstructure through accelerated cooling of the stir zone and its immediate thermal surroundings during and after the FSP traverse. These two roles operate on different time scales and address different physical mechanisms: the first governs the initial thermal state before FSP, while the second governs the thermal transient during and after FSP. The present study isolates the second role by applying cooling concurrently with the FSP traverse rather than between deposition passes.

Prior investigations of cooling-assisted FSP in aluminum alloys have demonstrated several configurations. Submerged FSP, in which the workpiece is immersed in water or the tool is water-cooled, accelerates post-deformation cooling rates and has been reported to suppress grain growth below 1 μm [24,25]. Cryogenic cooling applied during FSP has been associated with increased HAGB fraction and dislocation density, which is consistent with restricted dynamic recovery [26]. Forced-air cooling, trailing the FSP tool, has been employed in wire-arc directed energy deposition (DED) systems to control interlayer temperatures and refine grain structure [21,22]. A controlled comparison of cooling nozzle position (top bead, bottom substrate, and simultaneous), applied concurrently during the FSP tool traverse within the UAMFSP process, has not, to the authors' knowledge, been systematically reported. The relative merits of symmetric versus asymmetric cooling configurations, and their effect on both mean grain morphology and within-layer microstructural homogeneity, remain uncharacterized.

This study addresses that gap through an exploratory comparison. Four single-layer, four-bead ER4043 aluminum walls were fabricated using MIG deposition and three UAMFSP variants differing only in their cooling configuration: top-bead air cooling (TC), bottom-substrate air cooling (BC), and simultaneous top-and-bottom air cooling (DC), alongside a MIG-only baseline (no FSP, no cooling). A first-order analytical thermal model calibrated to the IR thermography data of the FSP stage provides model-estimated comparative cooling rates for each configuration. Quantitative grain morphology analysis utilizing ImageJ image analysis software on optical micrographs from three intra-layer sampling positions per condition characterizes how each cooling strategy is associated with variation in grain area, equivalent diameter, perimeter, roundness, and aspect ratio across the four-bead wall cross-section.

The objectives of this work are to: (i) characterize, within an exploratory single-replicate design, the grain population statistics associated with each cooling configuration; (ii) link the observed microstructural differences to model-estimated comparative cooling rates; (iii) describe the within-bead spatial variation in grain morphology as a function of cooling symmetry; and (iv) identify a hypothesized hierarchy of cooling effectiveness to guide future multilayer UAMFSP process design. The single-layer configuration is a deliberate simplification: it isolates the effect of cooling position on the FSP-modified microstructure by removing the interlayer thermal accumulation that develops in multilayer builds. This same simplification means that the present study does not address how cooling position interacts with prior-layer preheating, which is the governing variable in taller builds; the single-layer results are therefore a controlled baseline for the multilayer investigation rather than a direct prediction of multilayer behavior. EBSD texture characterization, comprehensive mechanical testing, and multilayer extension of the study are outside the present scope and discussed as required follow-up work in Section 4.6.

2. Materials and Methods

2.1. Materials

The substrate material was commercial AA6061 aluminum alloy, machined to dimensions of $292 \times 152 \times 12.7 \text{ mm}^3$. The filler material was ER4043 aluminum wire (Blue Demon, Sedalia, MO, USA; 0.045 in., 1.14 mm diameter). The nominal chemical compositions of both materials are listed in Table 1. The FSP tool was fabricated from H13 hot-work tool steel and featured a concave scrolled shoulder of 18 mm in diameter and a tapered pin of 3.5 mm in length, with a root diameter of 4.5 to 5.0 mm, tapering to a tip diameter of approximately 3.0 to 3.5 mm. This geometry was selected to promote effective material flow, consistent plunge engagement, and sufficient deformation across the full four-bead wall width. H13 tool steel was chosen for its high-temperature strength, thermal fatigue resistance, and chemical stability against aluminum adhesion, which is consistent with prior interlayer FSP studies on WAAM-fabricated aluminum alloys [13,27].

Table 1. Chemical compositions of AA6061 substrate and ER4043 filler wire (wt.%).

Material	Mg	Si	Cu	Fe	Mn	Cr	Zn	Ti	Al
AA6061	0.8–1.2	0.40–0.80	0.10–0.40	≤ 0.70	≤ 0.15	0.04–0.35	≤ 0.25	≤ 0.15	Bal.
ER4043	≤ 0.05	4.5–6.0	≤ 0.30	≤ 0.80	≤ 0.05	N/A	≤ 0.10	≤ 0.20	Bal.

2.2. Wall Fabrication and Experimental Design

All walls were fabricated on a HAAS VF-3 CNC machining center (Haas Automation, Inc., Oxnard, CA, USA) retrofitted with a spool gun connected to a multi-process MIG welding power source, as described in the predecessor study [20]. Four single-layer walls were produced, each comprising four overlapping beads deposited at 50 percent lateral overlap. The deposited beads measured approximately 3.8 mm in height, 120 mm in length, and 20 mm in total width. This single-layer configuration was adopted to isolate the effect of the cooling configuration on the FSP-modified microstructure without the compounding influence of multilayer thermal accumulation, which will be addressed in subsequent work.

The MIG deposition parameters were held constant across all four walls: nominal voltage 18 V, current 120 A, deposition speed 330 mm/min, wire type ER4043 (0.045 in.), wire stick-out 9 mm, and shielding gas 99 percent pure argon at 27 CFH. The gap between the wire tip and substrate was maintained at 6 mm throughout to ensure arc stability, with the wire feed rate controlled by the applied current. These parameters are identical to those used in the predecessor three-layer UAMFSP study [20], which enables direct cross-study comparison of the as-deposited baseline microstructure.

For the three UAMFSP walls, FSP was applied after MIG deposition, once the interlayer temperature had decreased to 38 to 40 °C. This threshold was chosen to provide a reproducible and near-ambient starting thermal state for the FSP pass, which eliminates the confounding influence of residual post-MIG heat on the retained post-FSP microstructure. The interlayer temperature was monitored at the mid-length, mid-width position of the bead top surface with the same IR thermography setup used for process monitoring, which ensured that the temperature threshold was applied at a consistent location across all three UAMFSP walls. Real-time temperature profiles during fabrication were captured with an FLIR T300 infrared camera (Teledyne FLIR/FLIR Systems, Wilsonville, OR, USA), with a detector resolution of 320×240 pixels (76,800 px), a standard temperature range of -20 °C to 650 °C extended to 1300 °C, a stated accuracy of ± 2 percent of the reading, and an acquisition rate of 10 fps; the radiometric video was post-processed for temperature–time histories using a fixed surface emissivity of $\epsilon = 0.3$ for the oxidized aluminum surface [28], the same value used in the radiative term of the thermal model (Section 2.3.1). The IR

camera accuracy at near-ambient temperatures (± 2 percent of the reading, on the order of ± 1 °C for the 38 to 40 °C range) is adequate for the threshold decision; the emissivity value used during process monitoring was held for consistency, noting that emissivity variations near ambient temperature are less consequential than during the hot, oxidized processing regime.

A single-pass FSP was performed across the full layer width using the following parameters: tool rotational speed 1200 rpm, traverse speed 55 mm/min, and plunge depth 0.25 mm. This parameter set was selected through preliminary trials to ensure consistent shoulder engagement and sufficient SPD across the four-bead wall width; it was not the product of a formal parameter optimization, which is identified as future work. These FSP parameters differ from those used in the predecessor three-layer study [20] (600/1200 rpm, 50 mm/min, 0.20 mm plunge depth, flat-shoulder tool), reflecting the change to the scrolled-shoulder tapered-pin tool geometry and the four-bead wall configuration of the present study. This distinction is relevant when comparing results across the two studies.

The cooling system employed two air nozzles, each delivering ambient air at approximately 26 °C. Active cooling was applied concurrently during the FSP tool traverse, not between deposition passes; the nozzles trailed the FSP tool by 25 mm along the tool path, acting simultaneously with the processing rather than as an interlayer thermal control strategy. The top nozzle was positioned 30 mm above the bead surface and the bottom nozzle 20 mm beneath the substrate. The air velocity at the nozzle exit was measured with a hot-wire anemometer at three positions across the nozzle face before each run, yielding a working range of 18 to 20 m/s at both positions. This velocity range was selected through preliminary trials: lower velocities did not sufficiently limit thermal retention in the stir zone, whereas higher velocities risked quenching-induced disruption of plastic material flow during the traverse stage. Table 2 summarizes the four fabrication conditions, and Figure 1 illustrates the experimental setup, including the CNC platform, spool gun, FSP tool, and top and bottom nozzle positions.

Table 2. Fabrication conditions, cooling configurations, and model-estimated thermal parameters for the four walls.

Sp.	Label	Description	FSP?	h_{eff} (W/m ² K)	CR (°C/s)
1	MIG only	MIG deposition only; no FSP, no active cooling	No	18.6	N/A
2	BC	MIG + FSP + bottom-substrate air cooling (nozzle 20 mm below substrate)	Yes	268.6	120
3	TC	MIG + FSP + top-bead air cooling (nozzle 30 mm above bead surface)	Yes	118.6	53
4	DC	MIG + FSP + simultaneous top-bead and bottom-substrate air cooling	Yes	368.6	164

Sp. = Specimen; h_{eff} = effective convective heat-transfer coefficient (model estimate); CR = model-estimated comparative cooling rate from the analytical thermal model (Section 2.3).

2.3. Analytical Thermal Model

2.3.1. Cooling Rate Estimation

In the absence of thermocouple measurements for the three cooled conditions, comparative post-FSP cooling rates were estimated using a lumped-parameter thermal model calibrated to the IR thermography data of the uncooled FSP condition. This is a first-order comparative ranking model, not a simulation of the stir-zone thermal field. The model does not resolve the moving heat source character of FSP, plastic work dissipation within the stir zone, spatial temperature gradients beneath the tool shoulder, or local variations in contact conditions. The CR values reported in Table 2 should accordingly be interpreted as relative heat-extraction indices for comparing configurations rather than as robust absolute stir-zone cooling rates.

The model treats the substrate-and-wall assembly as a combined thermal mass dissipating heat through an effective convective coefficient h_{eff} . The instantaneous cooling rate at the post-FSP peak temperature T_{peak} is given by Equation (1):

$$dT/dt = -h_{eff} \cdot A_{eff} / (m \cdot C_p) \cdot (T_{peak} - T_{\infty}) \quad (1)$$

where A_{eff} is the effective heat-transfer area, m is the substrate mass ($\rho = 2680 \text{ kg/m}^3$, $V_{sub} = 292 \times 152 \times 12.7 \text{ mm}^3$), and $C_p = 900 \text{ J/(kg}\cdot\text{K)}$. The IR thermal profile of the un-cooled Layer 1 condition shows a peak temperature in the range of 317 to 331 °C with a mean of $324 \pm 7 \text{ }^{\circ}\text{C}$, which declines to approximately 200 °C over approximately 15 s during the tool traverse; this yields a measured cooling rate of $CR_{NC} \approx 8.3 \text{ }^{\circ}\text{C/s}$. The effective area A_{eff} was back-calculated from this calibration point using $h_{NC} = 18.6 \text{ W/(m}^2\cdot\text{K)}$, estimated as the sum of natural convection ($\sim 10 \text{ W/(m}^2\cdot\text{K)}$) and radiation at 324 °C ($h_{rad} = \sigma \epsilon T^3 \approx 8.6 \text{ W/(m}^2\cdot\text{K)}$, $\epsilon = 0.3$ for oxidized aluminum [28]). This calibration is underdetermined in the sense that A_{eff} is inferred from a single data point rather than measured independently. Numerically, the parameter values are: $m = 1.51 \text{ kg}$, $C_p = 900 \text{ J/(kg}\cdot\text{K)}$, $T_{\infty} = 26 \text{ }^{\circ}\text{C}$, and the per-configuration h_{eff} values listed in Table 2 (18.6, 118.6, 268.6, and 368.6 $\text{W/(m}^2\cdot\text{K)}$ for NC, TC, BC, and DC, respectively). The back-calculated effective area is $A_{eff} \approx 2.0 \text{ m}^2$, which exceeds the geometric surface area of the substrate-and-wall assembly (approximately 0.10 m^2) by a factor of about twenty; A_{eff} therefore acts as a lumped calibration parameter that absorbs unmodeled conduction into the clamping fixture and the moving-source character of the FSP pass, not as a physical surface area. The absolute CR values inherit this single-point calibration and should be read as relative heat-extraction indices; the between-configuration CR ratios are independent of A_{eff} . For each active cooling configuration, incremental convective coefficients were assigned from established heat-transfer correlations as detailed in Section 2.3.3. Since h_{eff} and CR scale linearly at fixed geometry and temperature difference, the cooling rate for each condition follows from Equation (2):

$$CR_i = CR_{NC} \cdot (h_{eff,i} / h_{NC}) \quad (2)$$

The resulting h_{eff} and CR values for all four conditions are reported in Table 2. Direct thermocouple validation of these estimates is identified as a necessary extension for future work.

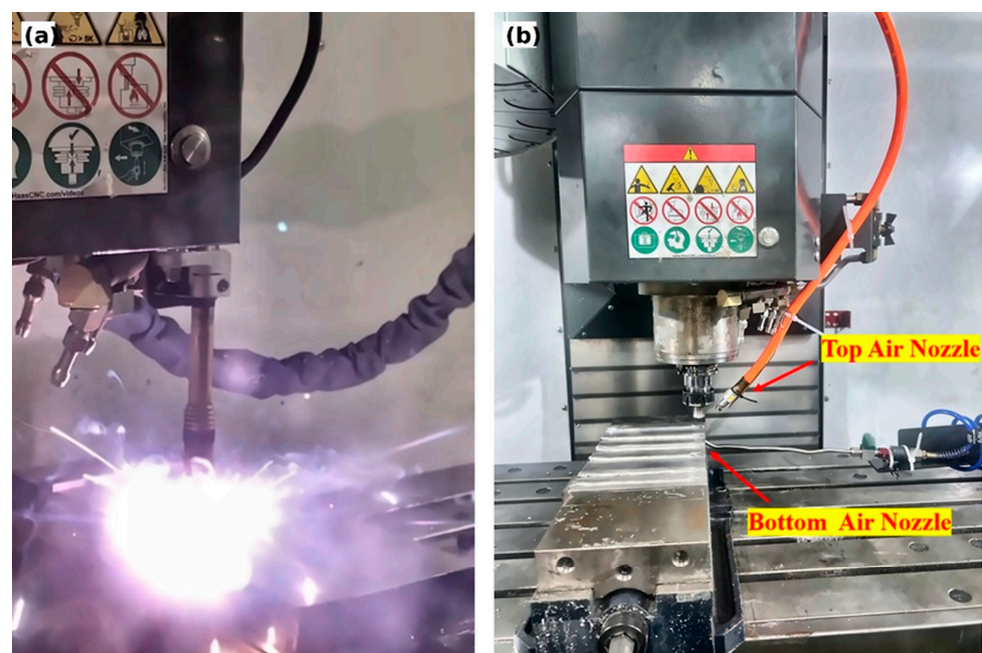


Figure 1. Experimental setup for the UAMFSP process: (a) MIG deposition of ER4043 aluminum wire onto the AA6061 substrate; (b) FSP stage with the top and bottom air-cooling nozzles trailing the tool path by 25 mm. Top nozzle: 30 mm above bead surface; bottom nozzle: 20 mm beneath substrate; air velocity: 18 to 20 m/s at 26 °C, measured with a hot-wire anemometer at the nozzle exit.

2.3.2. Grain Size Ranking Model

The relationship between post-deformation cooling rate and DRX grain size in FSP-processed aluminum alloys is commonly described by a power-law scaling model, given by Equation (3) [29,30]:

$$d = K \cdot CR^{-0.5} \quad (3)$$

where d is the mean equivalent grain diameter (μm), CR is the cooling rate ($^{\circ}\text{C/s}$), and K is an empirical constant. An exponent of 0.5 was adopted, which is consistent with values reported for DRX grain refinement in aluminum FSP [29,31]. The constant K was calibrated to the predecessor study's UAMFSP uncooled condition ($d = 3.20 \mu\text{m}$ at $CR = 11.2 ^{\circ}\text{C/s}$ [20]), which yields $K = 10.71 \mu\text{m} \cdot (^{\circ}\text{C/s})^{0.5}$. The model is applied in this work as a relative ranking tool, which predicts the direction of grain size change with increasing cooling rate across the four conditions. Absolute grain size predictions from this single-point calibration are not treated as quantitatively reliable, for reasons discussed in Section 4.2.1, and the measured ImageJ values are reported directly in Section 3.2.

2.3.3. Convective Coefficient Estimation, Assumptions, and Sensitivity

The incremental convective heat-transfer coefficients reported in Table 2 were estimated from established correlations for impinging air jets and forced convection [32,33]. For the top-bead configuration (TC), the correlation of Martin [32] for a single circular impinging air jet was applied. The Reynolds number, based on the nozzle exit velocity of 18 to 20 m/s, a nozzle-to-surface distance of 30 mm, and the kinematic viscosity of air at $26 ^{\circ}\text{C}$ ($\nu = 1.57 \times 10^{-5} \text{ m}^2/\text{s}$), yielded $Re \approx 2.3 \times 10^4$, which places the jet in the turbulent regime. The corresponding Nusselt number, evaluated at the jet impingement zone on the bead top surface, produced an average heat-transfer coefficient of approximately $100 \text{ W}/(\text{m}^2 \cdot \text{K})$. This value is consistent with literature-reported values for trailing air jets of comparable velocity and standoff [32] and was adopted as Δh_{TC} . For context, average coefficients reported for single circular impinging air jets at exit velocities of 15 to 25 m/s and comparable standoff distances lie in the approximate range of 60 to $150 \text{ W}/(\text{m}^2 \cdot \text{K})$ [32,33], so the adopted value is physically realistic for forced air at 18 to 20 m/s.

For the bottom-substrate configuration (BC), the higher coefficient ($\Delta h_{\text{BC}} = 250 \text{ W}/(\text{m}^2 \cdot \text{K})$) reflects the combined influence of forced convection over the $292 \times 152 \text{ mm}^2$ substrate underside and the volumetric redistribution of the extracted heat through the aluminum substrate, which has a thermal conductivity of approximately $160 \text{ W}/(\text{m} \cdot \text{K})$. In-plane conduction redistributes the locally extracted heat across the full substrate footprint before the heat reaches the deposited wall, which increases the effective heat-transfer area relative to the localized top-jet configuration. Similar substrate-mediated cooling enhancements have been reported for forced-air cooling in wire-arc directed energy deposition systems [22,33].

For the simultaneous dual-cooling configuration (DC), an additive model was adopted ($\Delta h_{\text{DC}} = \Delta h_{\text{TC}} + \Delta h_{\text{BC}} = 350 \text{ W}/(\text{m}^2 \cdot \text{K})$). This assumes that the top and bottom cooling pathways act independently on their respective surfaces, which is a first-order approximation. The approximation is physically motivated by the fact that the 12.7 mm substrate thickness is small relative to the in-plane conduction length scales at the traverse speeds employed here, which supports treating the two surfaces as thermally independent in a lumped sense. Nonlinear coupling between the two cooling fields, such as modified convective boundary layers or altered substrate temperature gradients, is not captured by this formulation. A rigorous treatment would require a coupled transient conduction-convection simulation and is beyond the scope of this first-order analysis.

A sensitivity check was performed to assess the robustness of the relative cooling-rate ranking against uncertainty in the input parameters. Independent ± 25 percent perturbations were applied to each of Δh_{TC} , Δh_{BC} , and Δh_{DC} in turn, as well as to both

coefficients simultaneously in a worst-case combined perturbation. In addition, the radiative component of h_{NC} was evaluated at $\varepsilon = 0.2$ and $\varepsilon = 0.5$, which bounds the range of emissivity reported for oxidized aluminum surfaces [28]. All combinations yield CR variations on the order of ± 15 to 30 percent in absolute terms while preserving the qualitative ranking $NC < TC < BC < DC$ in every case tested. Accordingly, the Δh and CR values reported in Table 2 should be interpreted as a first-order comparative ranking of the three cooling configurations, with the ranking itself more robust than the absolute values.

2.4. Specimen Preparation and Microstructural Analysis

After fabrication, each wall was sectioned at the center of the bead length (avoiding arc initiation artifacts at the deposition start and end zones) to obtain a representative cross-sectional specimen, as illustrated in Figure 2. From each cross-section, three metallographic samples were extracted at positions p1, p2, and p3, which correspond to three laterally distributed locations across the four-bead wall width: the first-bead region, the central overlap region, and the third-to-fourth bead region. This three-point sampling strategy was designed to characterize within-layer microstructural variation, which is a key discriminator between symmetric (DC) and asymmetric (TC, BC) cooling configurations.

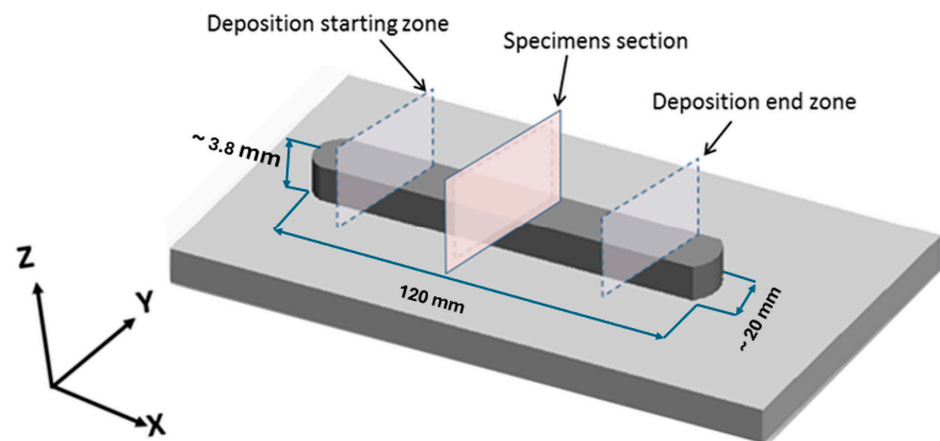


Figure 2. Specimen sectioning and sampling strategy: wall geometry showing bead dimensions (~3.8 mm height, 120 mm length, 20 mm width) with three measurement positions (p1, p2, p3) distributed across the four-bead wall width. Sectioning was performed at the center of the bead length.

Specimens were prepared following standard metallographic procedures: hot mounting in phenolic resin, sequential grinding with 240 to 1200 grit SiC papers, ultrasonic cleaning, cloth polishing with alumina suspension (1.0, 0.3, and 0.05 μm), and final polishing with 0.05 μm colloidal silica. Macroscopic photographs of the mounted cross-sectional specimens for all four conditions are presented in Figure 3. Grain boundaries were revealed by chemical etching with Keller's reagent (2.5 vol% HNO_3 , 1.5 vol% HCl , 1.0 vol% HF in distilled water, 30 to 45 s). Optical micrographs were acquired at 50 $\times$ magnification using a KEYENCE VK-9700 laser confocal scanning microscope (KEYENCE Corporation of America, Itasca, IL, USA), with a scale bar of 100 μm (calibrated to 358 pixels, which yields a pixel scale of 0.279 $\mu\text{m}/\text{px}$).

Grain morphology quantification was performed using ImageJ (National Institutes of Health, Bethesda, MD, USA; version 1.54p, Fiji distribution) for grain segmentation and morphology measurement, with a custom Python script (Python version 3.12, Python Software Foundation, Beaverton, OR, USA) for subsequent statistical characterization. The ImageJ segmentation workflow was held constant across all four conditions: the same threshold-based segmentation recipe, the same boundary-detection kernel parameters, the same minimum grain area filter (excluding features below 0.5 μm^2 to suppress segmentation

noise), and the same edge-touching-grain exclusion rule were applied to every micrograph. No manual cleanup of the segmented boundaries was performed beyond this automated pipeline, which preserves comparability across conditions at the cost of absolute accuracy on individual grains. The three-position micrographs per condition (p1, p2, p3) were pooled into a single grain population per condition for the statistical analysis reported in Section 3.2, with each image contributing equally to the pooled distribution. Grains were characterized by area (A), equivalent diameter (d_{eq} , defined as the diameter of a circle with equal area), perimeter (P), roundness ($R = 4\pi A/P^2$), and aspect ratio ($AR = \text{major}/\text{minor axis length}$). The pooled grain populations per condition ranged from $N = 2038$ to 2723 grains (Table 3). These metrics capture different aspects of grain morphology and are not interchangeable: equivalent diameter is an area-based size descriptor, perimeter is sensitive to boundary irregularity, and roundness and aspect ratio quantify shape. Throughout this manuscript, the term “grain size” is used only when the relevant metric is specified.

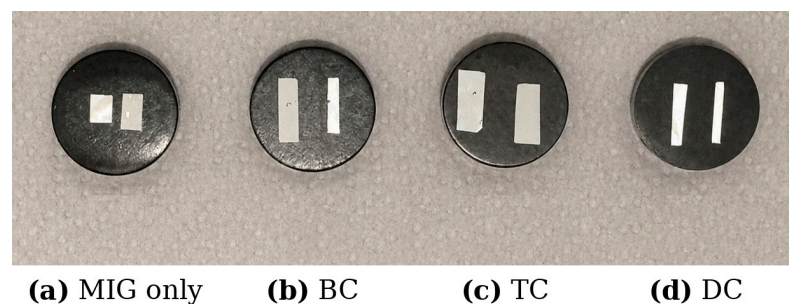


Figure 3. Macroscopic photographs of the mounted metallographic specimens extracted from the four wall conditions: (a) MIG only, (b) BC, (c) TC, and (d) DC. Each phenolic mount contains the wall cross-section samples sectioned as shown in Figure 2 and prepared for optical microscopy.

Table 3. Grain count and imaging parameters per condition.

Condition	Specimen	FSP?	Cooling	Positions	N (Grains)	Magnification/Scale
MIG only	1	No	None	p1, p2, p3	2576	50×, 100 μm bar
BC	2	Yes	Bottom substrate	p1, p2, p3	2723	50×, 100 μm bar
TC	3	Yes	Top bead	p1, p2, p3	2064	50×, 100 μm bar
DC	4	Yes	Top + bottom	p1, p2, p3	2038	50×, 100 μm bar

Resolution and segmentation-limit considerations. The 50× optical magnification with $0.279 \mu\text{m}/\text{px}$ at the pixel scale resolves grains reliably in the approximate 1 to 40 μm range. Sub-micron grains are not individually segmented by the ImageJ pipeline; features below approximately $0.5 \mu\text{m}$ are excluded by the minimum-area filter, and closely spaced grain boundaries near the resolution limit may merge under Keller’s etch contrast, which causes adjacent fine grains to register as a single coarser grain. This has two consequences for the interpretation that follows: the measured mean equivalent diameter is expected to be systematically inflated relative to the true grain population in regions where sub-micron grains are present, and direct quantitative comparison with nano-scale DRX grain sizes predicted by power-law models (Section 4.2.1) is not reliable. EBSD characterization at the sub-micron step size would be required to resolve the fine-grained tail of the distribution. Extended grain-population statistics, the full pairwise Cohen’s d matrix, grain-area scatter reductions, sensitivity-analysis raw values, per-position Vickers microhardness data, and the complete set of optical micrographs for all sampling positions are provided in the Supplementary Materials (Figure S1, Tables S1 to S3, and Sections S1 to S3).

3. Results

3.1. Microstructural Observations

Representative optical micrographs for the MIG-only and one representative UAMFSP-processed condition across the three sampling positions (p1, p2, p3) are presented in Figures 4 and 5. The MIG-only wall exhibited coarse, irregular grains with a network of the eutectic Si phase decorating the grain boundaries. The grain morphology was heterogeneous across positions, with some regions displaying elongated or partially dendritic features. This condition serves as the microstructural baseline, which reflects the as-solidified grain structure without FSP refinement.

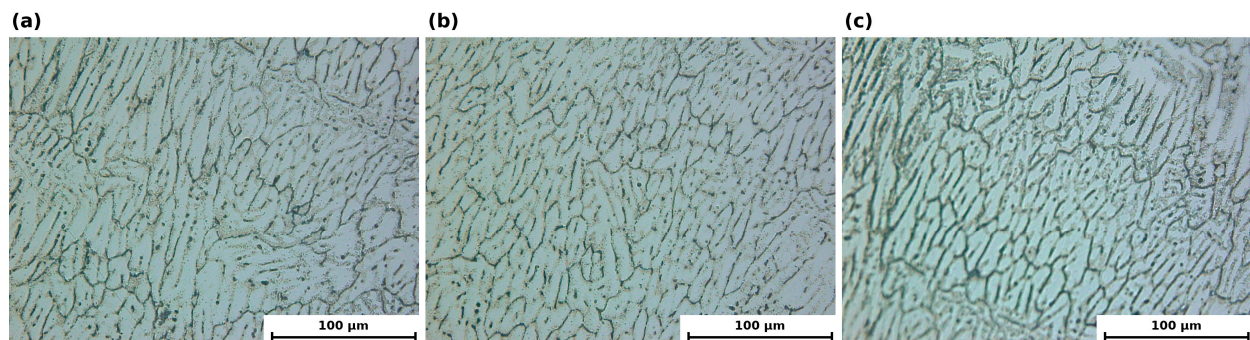


Figure 4. Representative optical micrographs at 50× magnification (scale bar = 100 μm) of the MIG-only condition at sampling positions (a) p1, (b) p2, and (c) p3.

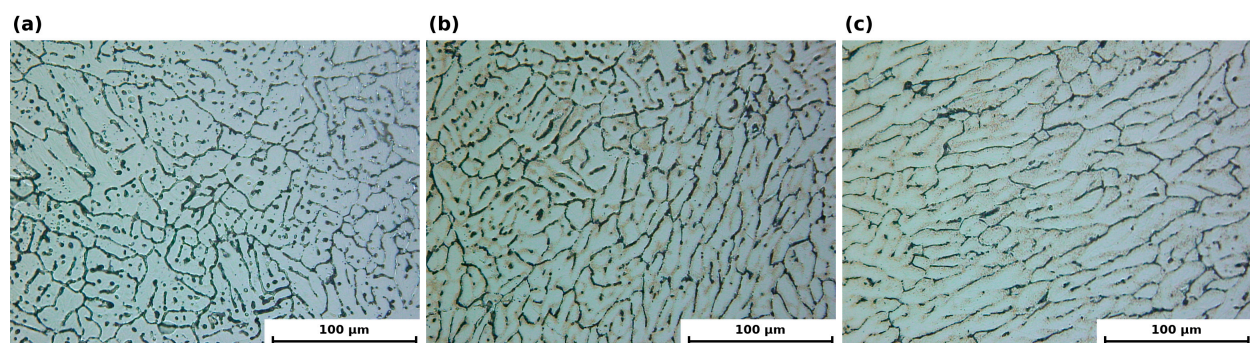


Figure 5. Representative optical micrographs at 50× magnification (scale bar = 100 μm) of the three UAMFSP cooling configurations: (a) BC (bottom-substrate air cooling), (b) TC (top-bead air cooling), and (c) DC (simultaneous top-and-bottom air cooling). A single representative micrograph per condition is shown for brevity; the within-bead spatial variation across positions p1 to p3 is quantified in the grain population statistics reported in Table 4 and discussed in Sections 3.1, 3.2 and 4.4.

Table 4. Grain morphology statistics for all four conditions (ImageJ analysis, pooled across p1 to p3).

Condition	N	d^- (μm)	d^+ (μm)	σ_d (μm)	$\bar{A}$ (μm ²)	σ_A (μm ²)	P^- (μm)	R^-	$\bar{AR}$	Cohen's d vs. MIG
MIG only	2576	2.897	2.352	2.186	10.344	40.352	17.484	0.583	2.247	N/A
BC	2723	3.325	2.833	1.957	11.691	18.112	19.418	0.544	2.213	0.21
TC	2064	3.362	2.846	1.978	11.949	16.604	19.284	0.543	2.369	0.22
DC	2038	2.756	2.310	1.632	8.059	11.544	15.211	0.582	2.349	−0.07

d^- = mean equivalent diameter; d^+ = median equivalent diameter; σ_d = std. dev. of diameter; $\bar{A}$ = mean grain area; σ_A = std. dev. of grain area; P^- = mean grain perimeter; R^- = mean roundness ($4\pi A/P^2$); $\bar{AR}$ = mean aspect ratio. Cohen's d for equivalent diameter is reported against the MIG-only distribution. All values from ImageJ image analysis on grain populations pooled across three sampling positions per condition. Tabulated values are given to the precision returned by the analysis software; the underlying single-grain measurement resolution is one pixel (0.279 μm per pixel; minimum resolvable area approximately 0.5 μm²), so differences in mean equivalent diameter smaller than approximately 0.3 μm should be regarded as within the measurement resolution and interpreted as rankings rather than quantitative differences. Statistical comparisons are at the grain population level; condition-level inference requires replicate walls (see Section 4.6).

The UAMFSP conditions produced visually finer and more uniform microstructures at equivalent sampling positions, reflecting FSP-induced thermomechanical grain homogenization. Qualitative differences in within-bead spatial variation were apparent across the three FSP cooling configurations. The TC condition showed the strongest position-dependent variation across p1 to p3, with elongated, sub-dendritic morphologies in p1 transitioning to more equiaxed grains in p2 and a mixed morphology in p3. The BC condition produced more uniformly distributed grain boundaries across all three positions, though with somewhat wider grain interiors than TC. The DC condition showed the most consistent grain structure across p1 to p3, with dense, polygonal grain boundaries and minimal elongation at all three sampling locations, as expected if bilateral cooling suppresses the within-bead thermal gradient that would drive morphological anisotropy in the single-sided configurations.

3.2. Quantitative Grain Morphology Analysis

Grain population statistics derived from ImageJ image analysis are summarized in Table 4. The statistical comparisons reported in this section operate at the grain population level: each condition is represented by a pooled distribution of 2038 to 2723 grains drawn from a single wall, and the Welch's *t*-test values reported below quantify differences between these pooled distributions. They do not constitute condition-level inference in the sense of a replicated-experiment design, which would require multiple walls per condition. The discussion accordingly emphasizes descriptive ranking, effect sizes, and scatter reductions rather than *p*-values. Replicate-wall experiments are necessary to establish condition-level effects and are identified as a priority for follow-up work. Mean grain area, equivalent diameter, grain area scatter, and the corresponding ranking from the thermal model are compared graphically in Figure 6; the corresponding roundness and aspect ratio data are presented in Figure 7.

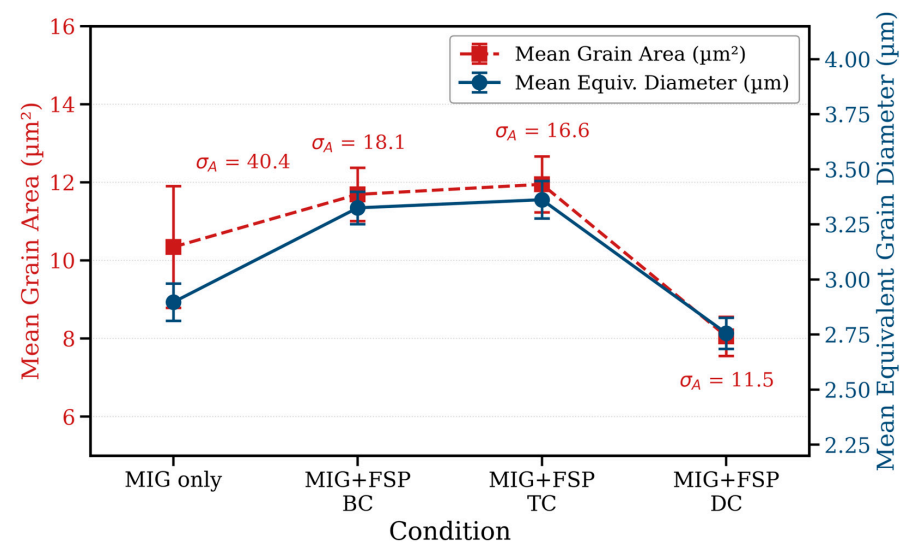


Figure 6. Mean grain area (squares, left axis) and mean equivalent grain diameter (circles, right axis) for all four cooling configurations. Error bars represent 95 percent confidence intervals of the mean ($1.96 \times \text{SE}$) computed from within-wall grain population variability. The σ_A annotation above each data point indicates the standard deviation of grain area, which quantifies distribution heterogeneity. Within this exploratory dataset, DC shows the lowest measured mean equivalent diameter ($2.76 \mu\text{m}$) and the lowest scatter ($\sigma_A = 11.5 \mu\text{m}^2$) among the three FSP conditions. BC and TC produced slightly coarser mean equivalent diameters than MIG-only despite substantially reduced scatter (approximately 55 and 59 percent reductions, respectively), which is the principal observation underlying the homogenization-versus-refinement distinction developed in Section 4.2.

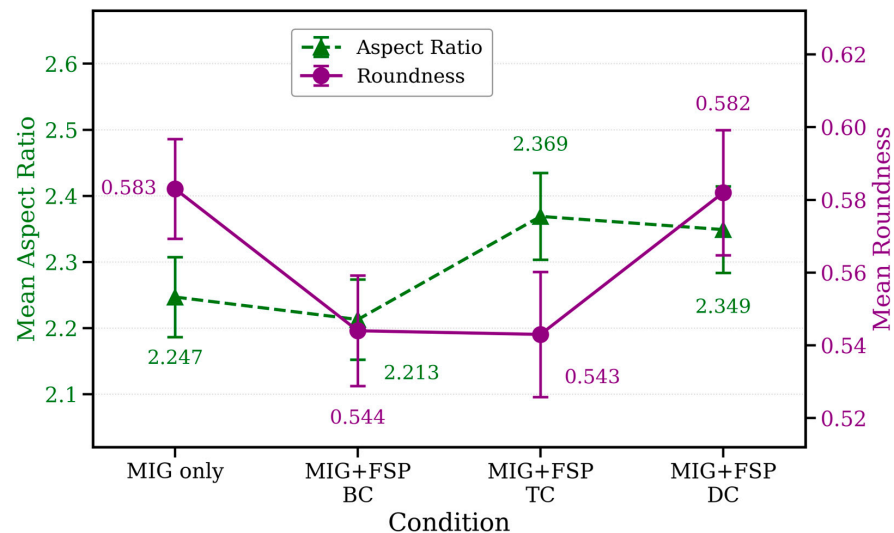


Figure 7. Mean aspect ratio (triangles, left axis) and mean roundness (circles, right axis) for all four cooling configurations. Error bars represent 95 percent confidence intervals computed from within-wall grain population variability. TC exhibits the highest mean aspect ratio (2.37), consistent with the strongest grain elongation under asymmetric top-only cooling. DC and MIG-only conditions show higher roundness values (0.58) compared with BC (0.54) and TC (0.54).

Within this exploratory dataset, DC showed the lowest measured mean equivalent grain diameter ($2.76 \mu\text{m}$) and the smallest mean grain area ($8.06 \mu\text{m}^2$) among all four conditions, along with the lowest standard deviation of grain area ($\sigma_A \approx 11.5 \mu\text{m}^2$). These values correspond to an approximately 5 percent reduction in mean equivalent diameter and an approximately 22 percent reduction in mean grain area relative to the MIG-only baseline, alongside an approximately 71 percent reduction in grain area scatter. The mean-diameter reduction (approximately $0.14 \mu\text{m}$) is at the edge of the optical measurement resolution and is interpreted as a ranking rather than a quantitatively reliable refinement (Section 4.2.1); the scatter reduction, in contrast, is several times larger than any plausible segmentation bias and is the robust effect. The MIG-only condition, despite its comparatively fine mean equivalent diameter ($2.90 \mu\text{m}$), exhibited by far the highest scatter ($\sigma_A \approx 40.4 \mu\text{m}^2$), which is $3.5\times$ higher than DC and mirrors the extreme heterogeneity inherent in the as-solidified dendritic microstructure, where occasional coarse grains dominate the area distribution.

The BC and TC conditions produced slightly coarser mean equivalent diameters than MIG-only (3.33 and $3.36 \mu\text{m}$, respectively), yet showed substantially reduced grain area scatter compared with MIG-only: BC reduced σ_A by approximately 55 percent and TC by approximately 59 percent (Figure 6). These observations support describing the FSP conditions differently from one another with respect to mean refinement: all three FSP conditions homogenized the grain size distribution by eliminating the coarse-grain tail of the MIG-only baseline, but only DC produced net mean refinement of equivalent diameter below the MIG-only baseline within the present measurements. BC and TC did not produce net mean refinement in this dataset. The distinction between homogenization (observed across all three FSP conditions) and net mean refinement (observed only for DC in this study) is central to interpreting the results and is developed further in Section 4.2.

The ranking from the thermal model (Section 2.3.2) matches the descriptive ordering among the three FSP conditions: DC finest, then BC, then TC, consistent with the ImageJ measurements. The absolute grain size values predicted by the power-law model are not quantitatively reliable for reasons discussed in Section 4.2.1.

The grain perimeter data follow the same ordering as the equivalent diameter: DC produces the shortest mean perimeter (15.2 μm), followed by MIG-only (17.5 μm), TC (19.3 μm), and BC (19.4 μm). Mean roundness values are comparable across all four conditions (0.543 to 0.583), with BC and TC showing lower roundness than MIG-only and DC, indicating greater grain elongation in the single-sided cooling conditions. The mean aspect ratio is highest for TC (2.37), the signature of pronounced grain elongation under top-only asymmetric cooling. Welch's *t*-tests on the grain population distributions yielded very small *p*-values ($p < 10^{-13}$ for FSP vs. MIG-only comparisons; $p < 10^{-26}$ for DC vs. BC and DC vs. TC), but these values reflect the sensitivity of the test to the large per-condition grain counts rather than large per-grain effect magnitudes, and they do not constitute condition-level inference; they are reported here only for completeness and are not used to support any claim. Cohen's *d* effect sizes for DC vs. BC and DC vs. TC are 0.32 and 0.34 respectively (small-to-moderate), while BC and TC versus MIG-only yield $d \approx 0.21$ to 0.22 (small). Given the modest effect sizes and the single-replicate design, the scatter reduction quantified by σ_A (55 to 71 percent across the three FSP conditions) is the larger and more interpretively meaningful grain population effect.

3.3. Grain Size Distribution Analysis

Figure 8 presents the probability density functions (PDFs) of equivalent grain diameter for all four conditions, derived from the grain-by-grain ImageJ data. The MIG-only condition shows the broadest distribution, with a heavy right tail extending toward coarse diameters, consistent with the heterogeneous as-solidified microstructure. DC exhibits the narrowest, most sharply peaked distribution, consistent with bilateral cooling producing a tightly clustered grain population. BC and TC occupy intermediate positions, with similar distribution widths and peak locations, and right tails narrower than MIG-only, which supports the interpretation that FSP homogenizes the distribution regardless of cooling position.

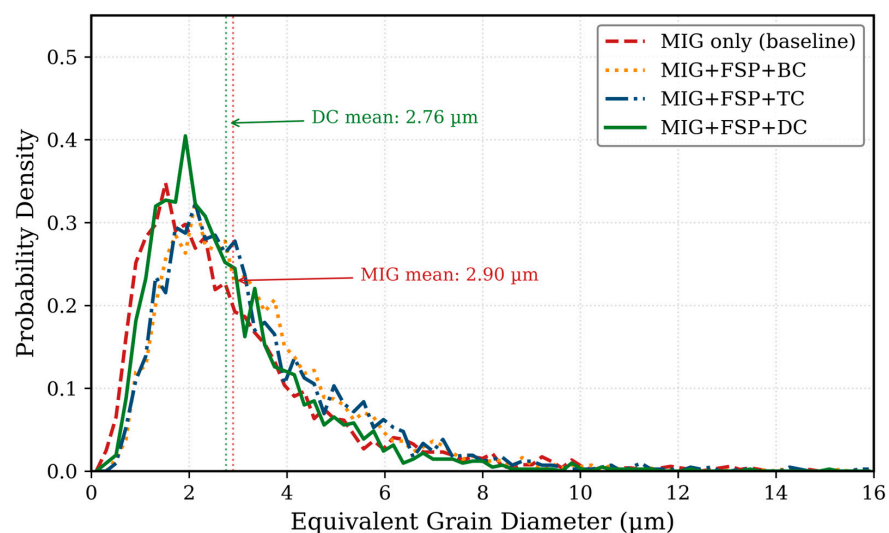


Figure 8. Probability density functions (PDFs) of equivalent grain diameter for all four cooling configurations. MIG-only exhibits the broadest distribution and heaviest right tail; DC shows the narrowest, most tightly clustered distribution. Dashed vertical lines mark mean diameters for DC (2.76 μm) and MIG-only (2.90 μm).

Power-law tail fits for the large-diameter regime (Figure 9) quantify the probability of encountering coarse grains in each condition. The shallowest tail (MIG-only) corresponds to the highest probability of coarse-grain outliers. TC shows a slightly steeper tail than BC despite comparable mean diameters, which is consistent with top-bead cooling being

somewhat more effective at suppressing individual large-grain outliers even when the mean is not reduced. DC shows the steepest tail, consistent with the most effective suppression of coarse grains and the greatest uniformity of the size distribution within this dataset.

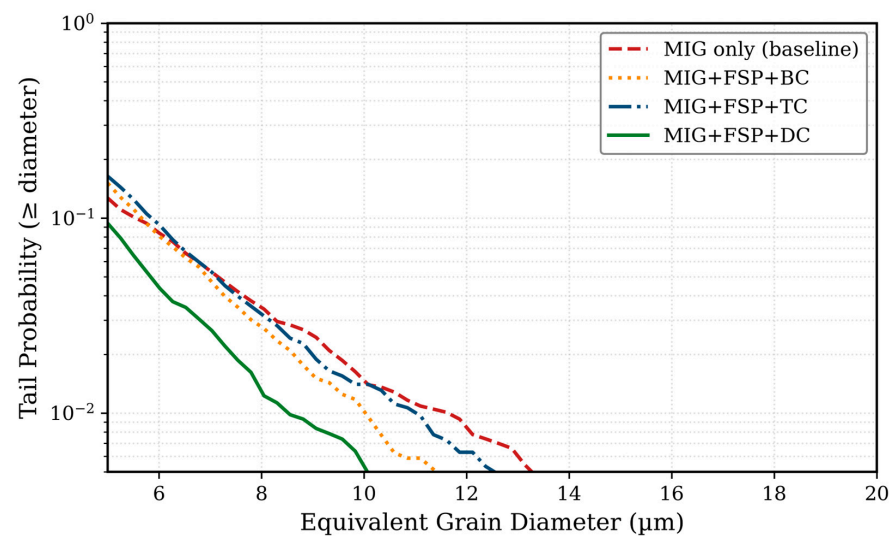


Figure 9. Tail probability curves for the large-diameter regime of the equivalent grain diameter distribution, plotted as the survival function $P(d \geq d_{\text{threshold}})$ on a logarithmic ordinate. The shallowest tail (highest coarse-grain probability) is exhibited by MIG-only; DC shows the steepest tail, consistent with the most effective suppression of coarse-grain outliers within this dataset.

3.4. Microhardness Profile: MIG Only and MIG + FSP (No Cooling)

Vickers microhardness measurements ($HV_{0.2}$, 200 gf load) were performed on the MIG-only and MIG + FSP no-cooling (NC) single-layer specimens along seven sequentially distributed indentation positions (p1 to p7) spanning the full wall width from Bead 1 through Bead 4. The resulting spatial hardness profiles are presented in Figure 10. Hardness measurements for the BC, TC, and DC specimens were not performed in this study. These measurements are therefore reported here as a baseline mechanical characterization of the MIG-only and NC conditions and a motivation for pursuing bilateral cooling, rather than as validation of the mechanical performance of BC, TC, or DC. Direct hardness mapping of the BC, TC, and DC specimens is required before any mechanical performance claims can be made for those conditions and is identified as a priority for follow-up experimental work.

The MIG-only wall exhibited a mean hardness of 52.0 ± 5.5 HV, with a comparatively uniform spatial profile across all seven positions (range: 43.7 to 58.6 HV). This value agrees with the MIG-only hardness reported in the predecessor three-layer study (52.0 ± 1.3 HV [20]), which is consistent with reproducibility of the MIG deposition baseline across different wall configurations. The moderate scatter in the MIG profile ($\sigma = 5.5$ HV) reflects the inherent heterogeneity of the as-solidified dendritic microstructure, where local variations in solidification rate across the four-bead width produce corresponding variations in local hardness.

The MIG + FSP NC condition produced a mean hardness of 70.6 ± 14.9 HV across the seven positions, which corresponds to a 35.9 percent improvement over the MIG-only baseline (Welch's t -test: $t = -3.10$, $p = 0.016$). The more informative observation, however, is not the mean improvement but the pronounced within-wall hardness gradient revealed by the spatial profile. Positions p1 to p3, located within the central stir zone of the FSP pass, exhibited a mean hardness of 85.9 ± 7.3 HV, whereas positions p4 to p7, corresponding to the outer bead regions at the advancing and retreating edges of the tool shoulder, showed a substantially lower mean of 59.2 ± 1.8 HV. This bimodal distribution

reflects the spatially non-uniform thermomechanical field generated by the FSP tool under natural convection cooling alone: the stir zone center experiences the most intense plastic deformation and DRX, which produces fine-grained, high-hardness material, while the shoulder edges deliver progressively less deformation, which leaves a zone of only partially refined microstructure at lower hardness.

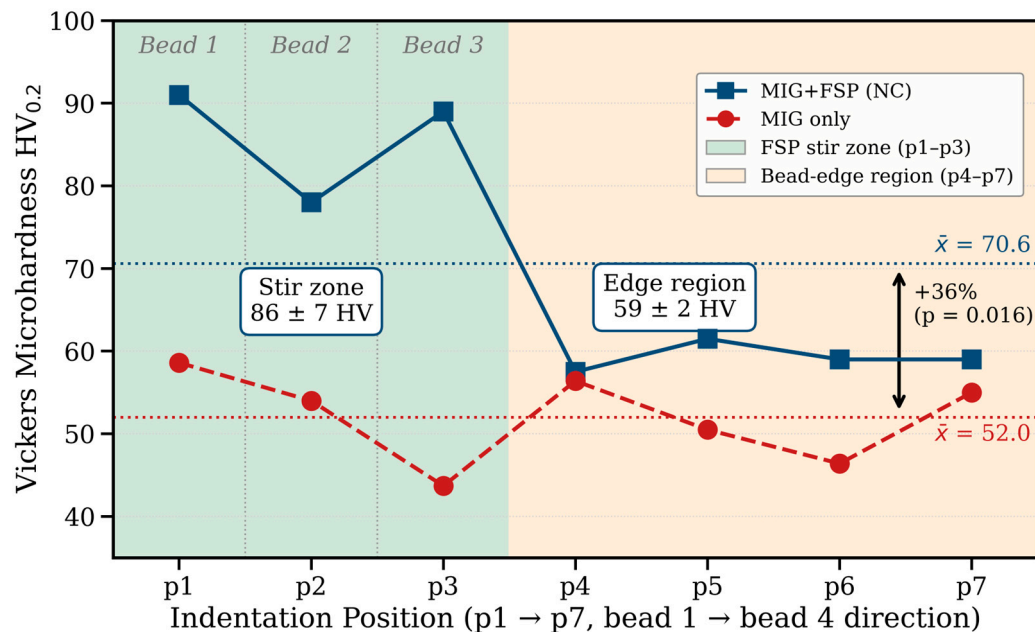


Figure 10. Spatial Vickers microhardness profile ($HV_{0.2}$) across seven indentation positions (p1 to p7) spanning the four-bead single-layer wall width, for MIG-only and MIG + FSP no-cooling (NC) conditions. Shaded regions indicate the FSP stir zone (p1 to p3, green) and bead-edge region (p4 to p7, orange). Dashed horizontal lines mark the overall mean for each condition. Dashed vertical lines indicate the mean equivalent grain diameter for the DC condition (2.76 micrometers) and the MIG-only baseline (2.90 micrometers). The bimodal NC profile (stir zone: 86 ± 7 HV; edge: 59 ± 2 HV) with a 27 HV differential provides the mechanical motivation for pursuing bilateral active cooling to homogenize the within-wall thermal field.

This within-wall hardness gradient is the mechanical property counterpart of the grain morphology heterogeneity documented for the NC condition in Section 3.2, where sampling positions p1 to p3 showed distinct grain character from p4 to p7. It also provides the most direct empirical justification for active cooling in the UAMFSP process: if FSP without cooling already generates a 26.7 HV (45 percent) hardness differential between the stir zone center and the bead edges, a cooling strategy that homogenizes the thermal field across the full wall width, of which the DC configuration is the most effective candidate, is expected to raise the mean hardness and reduce the within-wall gradient simultaneously. This expectation requires experimental validation through hardness mapping of the BC, TC, and DC specimens.

3.5. Expected Hardness Response Based on Grain Size Ranking

In the absence of direct hardness measurements for the BC, TC, and DC specimens, the expected hardness response of each condition can be inferred qualitatively from the ImageJ-measured grain size ranking reported in Table 4. The DC condition, with the finest mean equivalent grain diameter (2.756 μm) and the lowest grain area scatter ($\sigma_A = 11.544 \mu\text{m}^2$) among all four conditions, is expected to produce the highest mean hardness and the most uniform within-wall hardness response. The BC and TC conditions, which produced comparable mean diameters (3.325 and 3.362 μm , respectively) and comparable scatter

reductions relative to the MIG-only baseline, are expected to produce hardness values below DC and above the MIG-only baseline, although the hierarchy between BC and TC cannot be reliably resolved from grain size alone at this length scale.

A quantitative Hall-Petch prediction based on two-point calibration from the predecessor study [20] was considered but not adopted in this manuscript. The calibration data in [20] were obtained at a different optical magnification and with a different segmentation methodology than the present study, which precludes a quantitatively reliable transfer of the Hall-Petch slope across datasets. The qualitative ranking stated above is therefore adopted as the most defensible interpretation of the expected mechanical response. Direct Vickers microhardness mapping of the BC, TC, and DC specimens is required and identified as a priority for follow-up experimental work, which will also quantify the within-wall hardness gradient under each cooling configuration and validate the homogenization hypothesis proposed in Section 4.3.

4. Discussion

4.1. Thermal Regime and Cooling Rate Hierarchy

The analytical thermal model supports a first-order comparative hierarchy of effective cooling rates: NC ($8.3\text{ }^{\circ}\text{C/s}$) < TC ($53\text{ }^{\circ}\text{C/s}$) < BC ($120\text{ }^{\circ}\text{C/s}$) < DC ($164\text{ }^{\circ}\text{C/s}$). These values reflect the heat extraction pathways available to each configuration at a lumped-parameter level. Reported cooling rates for submerged FSP of WAAM aluminum are on the order of 100 to $200\text{ }^{\circ}\text{C/s}$ [21,25], and the BC and DC values fall within that range; this literature consistency should be interpreted cautiously, however, since the thermal boundary conditions of submerged and forced-air cooling differ in detail and the comparison is at the level of order of magnitude rather than direct equivalence. The sensitivity analysis in Section 2.3.3 indicates that the ranking NC < TC < BC < DC is preserved under ± 25 percent variation in each Δh and under emissivity values spanning $\epsilon = 0.2$ to 0.5 , which supports treating the ranking as more robust than the absolute values.

Interpretation of why the configurations are ordered in this way is offered as a plausible physical explanation of the model ranking, not as an independently established mechanism; because the lumped model uses the substrate as its thermal mass (Section 2.3.1), the ordering of the cooling configurations is in part a consequence of that modeling choice, and the physical reasoning below should be read accordingly. Within that framework, the TC configuration is expected to achieve the lowest active cooling rate because the air jet acts on the geometrically limited top bead surface, while the underlying substrate, which remains in thermal contact with the deposited layer throughout the FSP traverse, acts as a heat reservoir that sustains the stir-zone temperature. The BC configuration ranks above TC in the model because the bottom nozzle cools the underside of the full 12.7 mm aluminum substrate; the high in-plane thermal conductivity of the aluminum substrate ($k \approx 160\text{ W/(m}\cdot\text{K)}$) would conduct this heat globally, cooling not just the bead-substrate interface but the entire substrate volume, which would deplete the thermal reservoir that otherwise sustains stir-zone temperatures during traversal. The DC configuration combines both pathways and yields the highest effective cooling rate in the model. If borne out by direct temperature measurement, this would imply that substrate-mediated cooling acts as a volumetric heat-extraction mechanism that modifies the thermal boundary condition of the entire deposited system rather than acting on a localized bead surface, exploiting the substrate conductivity to redistribute heat across the full wall footprint; such a mechanism may transfer to similar aluminum alloy systems and comparable build geometries, pending validation in those specific cases. Confirmation at the level of local stir-zone temperatures requires thermocouple or embedded-probe measurements that are beyond the scope of

this first-order analysis, and until those are available, the BC-above-TC ordering should be treated as a model-derived ranking rather than a measured cooling-rate difference.

4.2. Grain Homogenization and Net Mean Refinement

The results of this study support a distinction between grain homogenization and net mean refinement as separable microstructural outcomes of FSP with active cooling. All three UAMFSP conditions reduced the grain area scatter relative to MIG-only by 55 to 71 percent, which is consistent with FSP homogenizing the as-deposited distribution regardless of cooling position. Only DC also reduced the mean grain area (by approximately 22 percent) and mean equivalent diameter (by approximately 5 percent) below the MIG-only baseline within the present measurements, which achieves both outcomes simultaneously. BC and TC produced slightly coarser mean equivalent diameters than MIG-only, which is consistent with a regime in which FSP eliminates the coarse outliers driving the high MIG-only scatter without achieving net mean refinement.

This distinction has practical implications for UAMFSP process design. When the structural application prioritizes microstructural uniformity and the elimination of coarse-grain outliers, any of the three FSP cooling configurations is expected to deliver the desired homogenization. When the application additionally requires net mean refinement below the as-deposited baseline, the bilateral cooling configuration (DC) is the candidate supported by the present data. This two-axis framing, with homogenization as a broadly achievable outcome of FSP and net mean refinement as an outcome requiring symmetric heat extraction, may serve as a useful process design lever in future UAMFSP work. Validation of this framing at the condition level will require replicate walls and EBSD-resolved measurements to assess whether the net mean refinement observed for DC reflects a reproducible process outcome or falls within wall-to-wall variability.

The practical significance of the 55 to 71 percent scatter reduction extends beyond its statistical robustness. In as-deposited WAAM aluminum, the coarse-grain tail of the size distribution supplies preferential sites for strain localization and fatigue crack initiation, and microstructural scatter propagates directly into the scatter of the mechanical properties on which design allowables are set [8]. Because allowables for additively manufactured structural components are governed by the lower tail of the property distribution rather than its mean, a processing step that narrows the microstructural distribution can raise usable design values even when the mean grain size is essentially unchanged. Reduced within-wall heterogeneity also simplifies process qualification, since location-to-location repeatability of microstructure across a deposit is a recurring certification concern for arc-based additive manufacturing. The homogenization documented here is achieved with inexpensive forced-air hardware rather than submerged or cryogenic media [24–26], which strengthens its industrial relevance; quantifying its transfer to fatigue-limited properties is part of the replicate campaign identified in Section 4.6.

4.2.1. Model Experiment Ranking and Resolution Considerations

The power-law scaling model $d = K \cdot CR^{-0.5}$, calibrated to the predecessor study's uncooled FSP condition ($d = 3.20 \mu\text{m}$ at $CR = 11.2 \text{ }^\circ\text{C/s}$ [20]), predicts a monotonic reduction in grain size with increasing cooling rate across the four conditions. The measured ImageJ diameters ($3.36 \mu\text{m}$ for TC, $3.33 \mu\text{m}$ for BC, and $2.76 \mu\text{m}$ for DC) follow the relative ranking suggested by the model, in which DC is the finest, BC is intermediate, and TC is the coarsest among the three FSP conditions. This agreement on ranking is the appropriate interpretive outcome of the thermal model.

The model calibration rests on a single data point from the predecessor uncooled condition, and the resulting K constant is therefore not well-constrained for extrapolation

to the substantially higher cooling rates of the present active-cooling conditions. When the model is applied at these higher cooling rates, it produces sub-micron absolute values for all three FSP conditions, which are substantially smaller than the measured diameters. These extrapolated numerical values are not reported in this work because they are not physically meaningful predictions of retained grain size: a single-point calibration cannot be expected to produce quantitatively reliable absolute predictions across a cooling-rate range spanning more than an order of magnitude. Two physical factors that the simple power-law formulation does not capture plausibly contribute to the divergence. First, the model describes the grain size at the instant of peak deformation, before any post-deformation grain boundary migration or recovery; some migration is expected to occur between FSP completion and ambient cooling, even at the highest cooling rate employed in this study, which would coarsen any initially refined structure. Second, as stated in Section 2.4, the ImageJ analysis at $50\times$ magnification does not reliably resolve sub-micron grains, which would inflate the measured mean relative to the true underlying grain population when a non-negligible sub-micron fraction is present. Neither factor can be isolated from the present data. EBSD characterization at the sub-micron step size, applied to, at a minimum, the DC and NC conditions, would be required to refit the calibration on a better-constrained dataset and separate the two contributions. Accordingly, only the ranking comparison between model and measurement is treated as defensible in this work.

4.2.2. Candidate Thermally Activated Mechanisms: Recovery, Recrystallization, and the Zener–Hollomon Parameter

The interpretation in this subsection is offered as a plausible mechanistic framework, not as an established mechanism. The present study uses optical microscopy and grain population statistics, which characterize grain size and morphology but cannot distinguish recrystallization from recovery or purely mechanical fragmentation of the dendritic structure. Dislocation density, misorientation angles, high-angle grain boundary fraction, and the recrystallized-versus-recovered partitioning were not measured, and the electron backscatter diffraction (EBSD) characterization that would be required to confirm a recrystallization mechanism is outside the scope of the present work. The refined, broadly equiaxed grain morphology observed after FSP is consistent with the recrystallization-type response widely reported for friction stir processing of aluminum alloys [9,10], but the present data do not by themselves establish that mechanism.

With that limitation stated, the grain size ranking observed here is compatible with, though not demonstrated by, two thermally activated phenomena that the power-law model does not capture. The first concerns the Zener–Hollomon parameter, $Z = \dot{\epsilon} \cdot \exp(Q/RT)$, which governs thermally activated deformation and restoration in aluminum alloys during plastic deformation. Strictly, Z is defined by the strain rate $\dot{\epsilon}$ and temperature T experienced during deformation, and the active cooling applied here acts primarily on the post-deformation thermal transient rather than on conditions during the FSP shoulder pass itself. The relevant effect of accelerated post-deformation cooling is therefore not to change Z during deformation but to reduce the time available for thermally activated recovery and grain boundary migration after deformation has ceased. This effective post-deformation temperature reduction may help retain a finer microstructure by suppressing the restoration processes that would otherwise coarsen the deformation-refined structure established during the pass [29,31].

The strain rates associated with FSP are typically reported in the range of 10 to 100 s^{-1} beneath the rotating shoulder [9,31]. Under rapid post-deformation cooling, the reduced available thermal energy for recovery would be expected to preserve a finer retained grain structure, which provides a plausible physical narrative for the observed cooling-rate ranking: DC, with the highest model-estimated cooling rate, would be expected to

retain the finest deformation-induced microstructure due to suppressed post-deformation recovery. The spatial variation in strain rate and temperature across the stir zone, driven by the coupled gradients beneath the rotating tool shoulder, may also contribute to the within-wall microstructural heterogeneity observed across positions p1 to p3.

The second phenomenon is the competition between dynamic recovery and recrystallization during the FSP traverse. At the lower model-estimated cooling rates (TC: 53 °C/s; BC: 120 °C/s), dynamic recovery is plausibly more active concurrent with the severe plastic deformation. Recovery reduces dislocation density within the stir zone, which would lower the stored energy available to nucleate recrystallization and favor subgrain rearrangement rather than the formation of new high-angle-bounded grains. The grain size data for TC and BC are compatible with such a regime: the final mean equivalent diameter (3.33 to 3.36 µm) slightly exceeds the as-solidified MIG-only baseline (2.90 µm), while the coarse-grain tail responsible for the high MIG-only scatter is eliminated. At the highest model-estimated cooling rate (DC: 164 °C/s), the mean equivalent diameter is marginally the lowest of the four conditions; however, as noted in Sections 3.2 and 4.2.1, this difference is at the edge of the optical measurement resolution and treated as a ranking rather than an established net refinement. Whether the cooling-rate ranking reflects a genuine suppression of post-deformation restoration, as this narrative suggests, cannot be established from optical data alone.

Distinguishing these mechanisms would require EBSD-based characterization (misorientation distribution, high-angle grain boundary fraction, and grain orientation spread), which the present optical study cannot provide; this is a fundamental boundary of the characterization method used here rather than an omission to be remedied with the existing specimens. Accordingly, the partitioning between recrystallized and recovered fractions, the grain boundary character, and the role of the Zener–Hollomon parameter in the retained microstructure are presented as interpretation, not measurement, and the conclusions of this work do not depend on which mechanism operates.

4.2.3. Thermal Symmetry and Grain Morphology

The DC condition's superior within-bead uniformity is compatible with the expected effect of bilateral cooling on the spatial thermal gradient within the stir zone during the FSP traverse. In the TC configuration, heat extraction from the top surface alone would impose a through-thickness temperature gradient $\partial T/\partial z$ (cooler top, warmer bottom), which is expected to promote grain elongation in the build direction and morphological anisotropy. The TC condition's highest mean aspect ratio (2.37) and strongest within-bead position-dependence across p1 to p3 are consistent with this expected behavior. In BC, the opposite gradient is imposed (cooler bottom, warmer top), which, combined with the global substrate cooling effect, is expected to produce more uniform grain sizes but retain some thermal asymmetry. The DC configuration is expected to minimize $\partial T/\partial z$ in both through-thickness directions simultaneously, approaching a thermally symmetric condition ($\partial T/\partial z \approx 0$) that would suppress directional grain growth. The DC observations (finest mean equivalent diameter, lowest scatter, and most morphologically consistent microstructure across all three sampling positions) are compatible with this expected behavior. Direct measurement of the through-thickness thermal gradient during FSP traverse would be needed to test the thermal symmetry interpretation.

4.3. Hardness Profile and Mechanical Consequence of Spatial Cooling Asymmetry

The hardness section of this manuscript serves two roles. First, it provides a baseline mechanical characterization of the MIG-only and NC conditions based on direct measurement. Second, it provides a hypothesis-generating extension toward the BC, TC, and DC

conditions based on grain size ranking. It does not provide mechanical validation of the three cooling configurations, as hardness data for those specimens were not collected in this study.

The MIG-only wall, with its uniform mean of 52.0 ± 5.5 HV and no systematic spatial trend, reflects a microstructure governed entirely by solidification, which is heterogeneous but lacks a preferred spatial direction. The NC condition produces a bimodal profile: the stir zone center (p1 to p3) reaches 85.9 ± 7.3 HV, while the bead edge region (p4 to p7) remains near the MIG-only level at 59.2 ± 1.8 HV. This 26.7 HV differential within a single wall cross-section is consistent with the non-uniform FSP thermomechanical field under natural convection: the center of the tool shoulder path delivers the most intense plastic deformation and frictional heating, which is expected to drive the most complete grain refinement and the associated Hall–Petch strengthening, while the shoulder edges deliver progressively diminishing deformation and refinement.

This within-wall hardness gradient corresponds to the grain morphology heterogeneity described for the NC condition in Section 3.2 and the optical micrographs in Figures 4 and 5. The NC hardness bimodality supports the expectation that a single-pass FSP without active cooling produces a thermomechanically stratified wall, stronger at the center and weaker at the edges, which may manifest as anisotropic and spatially non-uniform mechanical performance under service loading. Bilateral active cooling (DC) is hypothesized to flatten this gradient by homogenizing the thermal field simultaneously from above and below: the substrate heat sink is expected to extract heat globally across the full wall width, which would reduce the temperature differential between the stir zone center and the bead edge region, while the top nozzle is expected to accelerate heat extraction from the shoulder exit face. Based on the grain size ranking reported in Table 4, the DC condition is hypothesized to produce the highest mean hardness and the most spatially uniform hardness response among the three FSP conditions. BC and TC are hypothesized to produce intermediate hardness values. Direct validation of these hypotheses through spatially resolved hardness mapping of the BC, TC, and DC specimens is required before quantitative mechanical performance claims can be made.

4.4. Within-Bead Spatial Variation as a Process Diagnostic

The three-position (p1, p2, p3) sampling strategy shows that the spatial homogeneity of grain morphology across the four-bead wall width may serve as a sensitive indicator of cooling effectiveness. TC shows the strongest position-dependence (elongated grains at p1, equiaxed at p2, mixed at p3), compatible with a spatially heterogeneous thermal field generated by a single top-surface jet that does not reach all four bead positions with equal intensity. BC and DC show progressively weaker position-dependence, which is consistent with their more globally distributed heat extraction mechanisms. DC achieves the most consistent grain morphology across all three positions in the present dataset. This supports the interpretation that bilateral cooling is the most effective configuration for homogenizing the intra-layer thermal environment under the conditions tested. This observation has a practical consequence for multilayer UAMFSP builds: within-layer gradients established in lower layers are expected to propagate through reheating and remelting by subsequent depositions. Minimizing these gradients through bilateral cooling from the first layer is therefore a candidate strategy for achieving layer-to-layer microstructural consistency in taller builds, which requires verification in multilayer studies.

4.5. Comparison with Prior UAMFSP Studies and the Role of Build Geometry

The predecessor UAMFSP study [20] reported $\bar{d}^- = 3.20 \mu\text{m}$ and $\bar{A} = 10.9 \mu\text{m}^2$ for the uncooled FSP condition in a three-layer, four-bead wall using a flat-shoulder cylindrical-pin

tool. The DC condition in the present study yields $d^- = 2.76 \mu\text{m}$ and $\bar{A} = 8.06 \mu\text{m}^2$. A direct causal attribution of this difference to either tool geometry or cooling configuration is not supported because the present study differs from [20] in multiple variables simultaneously: tool geometry (scrolled shoulder with tapered pin versus flat shoulder with cylindrical pin), cooling configuration (bilateral air cooling versus natural convection), build geometry (single-layer versus three-layer), initial thermal state before each FSP pass (ambient in the present study versus preheated by prior layers in [20]), and possibly segmentation and imaging conditions between the two studies. Any cross-study comparison should accordingly be treated as suggestive rather than causally conclusive. A controlled comparison holding tool geometry constant while varying only the cooling configuration is identified as a priority for follow-up work. Cooling-assisted FSP has been reported to produce homogenization effects in other aluminum alloy systems [30], though not under an integrated CNC-based hybrid process; this supports broader applicability of the cooling approach without directly validating the UAMFSP integration.

The single-layer configuration of the present study merits explicit discussion. In the predecessor three-layer build [20], prior-layer preheating raises the ambient thermal state of the build before each successive FSP pass, which increases the thermal energy available for DRX and reduces the stir-zone flow stress. This preheated condition may facilitate more complete recrystallization at the same rotational speed. In the present single-layer build, the substrate is at ambient temperature before FSP, which reduces the DRX driving force and is compatible with the observation that the mean equivalent diameter does not fall below the MIG-only baseline for the single-sided cooling conditions. Extending the cooling configuration comparison to multilayer builds, where the interaction between prior-layer preheating and active cooling becomes the governing variable, is the natural next step.

4.6. Limitations

Several limitations of the present study must be acknowledged explicitly. First, the cooling rate estimates are analytical rather than directly measured; thermocouple-based validation would be required to convert the comparative ranking into absolute stir-zone cooling rates. Second, Vickers microhardness was measured only for the MIG-only and NC conditions (Section 3.4). Hardness data for the BC, TC, and DC specimens were not collected, and the present study therefore does not provide mechanical validation of the three cooling configurations; the expected hardness responses stated in Sections 3.5 and 4.3 are hypotheses derived from the grain size ranking, not measured results. Establishing the mechanical performance of the cooling configurations would require spatially resolved hardness mapping, together with tensile and fatigue testing, on dedicated replicate specimens. Third, and most importantly from a mechanistic standpoint, the present work characterizes grain size and morphology by optical microscopy, which cannot by itself confirm a recrystallization mechanism or quantify grain boundary character. EBSD-based measurement of misorientation distribution, high-angle grain boundary fraction, and crystallographic texture would be required to identify the operative restoration mechanism and degree of isotropy achieved; this is a boundary of the optical method used here, and the conclusions of this study are framed so as not to depend on the mechanism being resolved. Fourth, the study is limited to a single FSP parameter set; the interaction between rotational speed, traverse speed, plunge depth, and cooling configuration is expected to be significant and would need to be investigated through a systematic factorial design.

Fifth, and most consequentially for the statistical interpretation, the present work constitutes a single-replicate, single-factor exploratory study: four walls, one per condition. The statistical comparisons reported in Section 3.2 (Welch's *t*-tests, effect sizes) quantify differences at the grain population level, which is the level at which the data were collected.

Thousands of grains drawn from one wall are not independent process replicates; they are observations nested within a single manufacturing history. Condition-level inference, which would require replicate walls to separate wall-to-wall process variability from treatment effect, is beyond what the present dataset can support. As a concrete design target for that follow-up work, a minimum of two, and preferably three, replicate walls per condition will be required to partition within-wall spatial variability and measurement repeatability from true condition-to-condition differences. Future work will employ a structured factorial design with replicate walls per condition to quantify factor interactions and establish process robustness. Sixth, the optical resolution limit at $50\times$ magnification (approximately $0.5\text{ }\mu\text{m}$) does not resolve the fine-grained tail of the grain size distribution. This limitation is fundamental to the imaging technique used here and is noted in Sections 2.4 and 4.2.1. Seventh, the convective coefficients used in the thermal model are estimated from literature correlations rather than measured at the present experimental setup; the sensitivity analysis in Section 2.3.3 indicates that the cooling-rate ranking is robust to ± 25 percent variation in these values, but the absolute values carry corresponding uncertainty.

5. Conclusions

This exploratory, single-replicate study characterized the grain population response of single-layer, four-bead UAMFSP-fabricated ER4043 aluminum walls to three active air cooling configurations applied concurrently during the FSP traverse. ImageJ-based grain analysis of optical micrographs from four conditions (MIG-only, BC, TC, and DC), combined with Vickers microhardness measurements on the MIG-only and no-cooling FSP specimens, supports the following principal conclusions. These conclusions reflect grain population descriptors and first-order thermal-model ranking within a single-replicate dataset; condition-level statistical inference, absolute thermal quantification, and mechanistic confirmation will require the follow-up work identified in Section 4.6.

- (1) All three UAMFSP cooling configurations reduced the grain area scatter (σ_A) relative to the MIG-only baseline by 55 to 71 percent, which supports the interpretation that FSP homogenizes the as-deposited grain size distribution regardless of cooling position. This homogenization effect was the largest and most consistent grain population response observed across the three FSP conditions within this dataset.
- (2) DC showed the lowest mean equivalent grain diameter ($2.76\text{ }\mu\text{m}$) and lowest grain area scatter ($\sigma_A \approx 11.5\text{ }\mu\text{m}^2$), corresponding to reductions of approximately 5 percent in mean diameter and 71 percent in scatter relative to the MIG-only baseline. The scatter reduction is large and unambiguous. The mean diameter reduction (approximately $0.14\text{ }\mu\text{m}$) lies at the edge of the optical measurement resolution (pixel scale $0.279\text{ }\mu\text{m}/\text{px}$; Cohen's $d \approx -0.07$) and is therefore reported as a directional ranking among the FSP conditions, not as an established net refinement below the as-deposited baseline. Effect sizes for the between-FSP comparisons (Cohen's $d = 0.32$ for DC vs. BC and 0.34 for DC vs. TC) likewise support a directional ranking rather than a condition-level claim.
- (3) BC and TC produced slightly coarser mean equivalent diameters than MIG-only (3.33 and $3.36\text{ }\mu\text{m}$, respectively) while reducing grain area scatter by approximately 55 and 59 percent. This supports describing FSP as homogenizing the distribution by eliminating coarse outliers, even when net mean refinement below the as-deposited baseline is not achieved in a single-layer build. BC and TC did not achieve net mean refinement in this dataset.
- (4) The analytical thermal model, calibrated to the uncooled Layer 1 IR thermography profile (mean maximum $324 \pm 7\text{ }^\circ\text{C}$), supports a first-order comparative ranking of effective cooling rates at $53\text{ }^\circ\text{C/s}$ (TC), $120\text{ }^\circ\text{C/s}$ (BC), and $164\text{ }^\circ\text{C/s}$ (DC). BC

ranks higher than TC by a factor of approximately $2.3\times$ despite a comparable nozzle configuration, pointing to substrate-mediated cooling acting globally through the high-conductivity aluminum substrate rather than on a localized bead surface. The ranking is preserved under ± 25 percent variation in the convective coefficient inputs and under emissivity values spanning $\varepsilon = 0.2$ to 0.5 .

- (5) TC showed the highest mean aspect ratio (2.37) and strongest within-bead position-dependence across p1 to p3, which is compatible with the asymmetric thermal gradient imposed by top-surface-only cooling. DC showed the most morphologically consistent distribution across all sampling positions, which supports bilateral cooling as the most effective configuration for intra-layer thermal homogenization within the conditions tested.
- (6) The measured spatial hardness profile of the MIG + FSP NC condition revealed a bimodal within-wall distribution: the stir-zone center (p1 to p3) reached 86 ± 7 HV compared with 59 ± 2 HV at the bead-edge region (p4 to p7). This differential of approximately 27 HV is consistent with a spatially non-uniform FSP thermomechanical field under natural convection and provides the mechanical motivation for the cooling configurations examined here. Hardness was not measured for the BC, TC, and DC conditions, so this study makes no mechanical performance claim for them; their hardness response remains a hypothesis for dedicated mechanical testing.
- (7) Thermal symmetry emerges as a promising control parameter for microstructural uniformity in hybrid WAAM-FSP systems: bilateral cooling produced the most uniform retained grain morphology under the conditions tested. Confirming these observations will require replicate walls per condition to separate treatment effects from wall-to-wall variability, thermocouple measurement of absolute cooling rates, spatially resolved hardness mapping with tensile and fatigue testing, and EBSD identification of the operative restoration mechanism. The contribution of the present work is the demonstration that cooling-position selection during interlayer FSP produces measurable, systematically ordered differences in grain area homogeneity, offered as the basis for those confirmatory studies.

Supplementary Materials: The following supporting information can be downloaded at <https://www.mdpi.com/article/10.3390/jmmp10070252/s1>, Figure S1: the complete set of optical micrographs at $50\times$ magnification for all three sampling positions (p1 to p3) of the MIG-only, BC, TC, and DC conditions; Table S1: extended grain population statistics; Table S2: the full pairwise Cohen's d matrix; Table S3: grain area scatter reductions; Section S1: sensitivity analysis raw values; Section S2: per-position Vickers microhardness data; Section S3: datasets available for deposition.

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Data Availability Statement: The per-grain morphology datasets (area, equivalent diameter, perimeter, roundness, and aspect ratio for all four conditions) generated and analyzed in this study, together with the raw ImageJ segmentation outputs and the infrared thermography video used for thermal-model calibration, are available from the corresponding author upon reasonable request. The single-grain measurement resolution and minimum-area filter applied during analysis are documented in Section 2.4.

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