

Article

Experimental and Numerical Investigation of Smoke Transportation Characteristics and Flame Self-Extinction in Sealed Underground Deep Vertical Space

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Abstract

Underground deep vertical spaces are a new form of architectural structure for the efficient use of land resources. However, their slender geometry can intensify smoke transport and thermal hazards during fires. Sealing is a potential emergency strategy, but its influence on smoke dynamics and flame extinction in deep shafts remains insufficiently quantified. This study investigates smoke transportation characteristics and flame self-extinction in a sealed deep vertical space using 1:26 reduced-scale experiments combined with CFD simulations. Across the tested conditions, sealing consistently increased the characteristic upper-shaft centerline temperature rise, with enhancements ranging from 43.2% to 374.9%. The vertical centerline temperature above the fire exhibits a segmented decay behavior: in the plume-rise region it follows a power-law trend, while the decay coefficient deviates from the ideal-plume expectation, consistent with the thermal shielding effect associated with the confined upper hot-gas layer. Under sealed conditions, a distinct “ghosting” flame behavior and eventual self-extinction were observed. The combined flame, thermal, and simulated flow-field evidence is consistent with an oxygen-limited interpretation, although this mechanism was not directly verified by gas-species measurements. Based on the reduced-scale dataset, the self-extinction time was normalized by the characteristic oxygen-consumption timescale t_{O_2} , yielding $\frac{t_{ext}}{t_{O_2}} = 2.074 \left(\frac{h}{1 \text{ m}} \right)^{-0.394} \left(\frac{Q}{1 \text{ kW}} \right)^{-0.071}$, which provides a quantitative description of flame self-extinction under the tested sealed conditions.

Keywords: underground deep vertical space fire; sealing effect; smoke accumulation; recirculation; vertical temperature decay; flame self-extinction



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1. Introduction

Underground deep vertical space is a new smart solution to parking difficulties, which has been recently adopted in some metropolises in China due to its high efficiency and land-saving potential [1,2]. Taking the underground deep vertical garage of Nanjing Children's Hospital as an example, which is the first underground deep vertical space in China, the garage has a depth of 68 m and a diameter of 15.8 m, providing 200 parking stalls within a footprint of less than 150 m², making its land utilization rate over 10 times that of traditional garages. Underground deep vertical space has significant advantages in improving land utilization rates; however, its fire risks cannot be ignored. Once a fire occurs, the hot

smoke rises due to buoyancy, and vehicles above or adjacent to the fire source could be ignited. The skeleton structural strength would be weakened dramatically. Unfortunately, its long, narrow vertical space, closed bottom, and sole open roof, as illustrated in Figure 1, distinguish the smoke transportation characteristics and flame morphology from those in other confined spaces, which complicates the fire emergency response for underground deep vertical space.

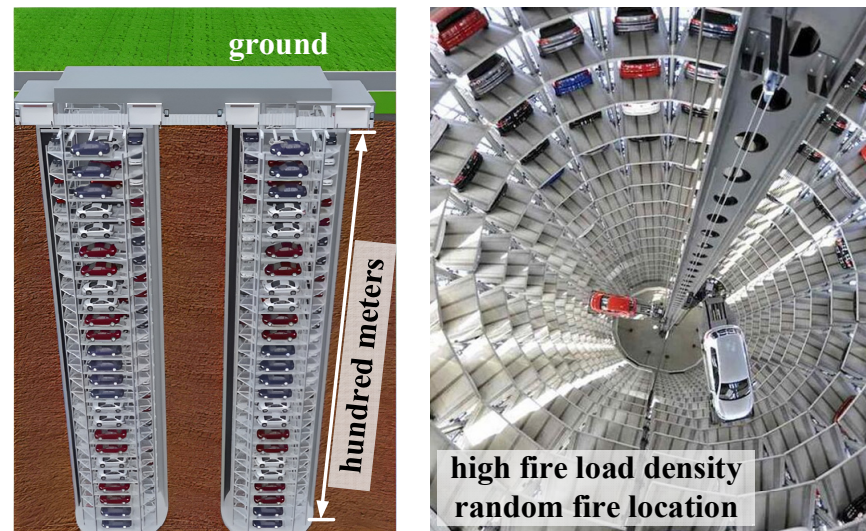


Figure 1. Underground deep vertical garage, one of the forms of underground deep vertical space.

Regarding smoke transportation characteristics, the fire dynamics in underground deep vertical spaces share fundamental similarities with vertical shafts in high-rise buildings and tunnel ventilation shafts, a subject that has been extensively studied. Klote [3] and Black [4] established the foundational theory of the stack effect, elucidating how pressure differentials drive vertical smoke movement. Chow and Zhao [5] further quantified the smoke spreading velocity in elevator shafts under varying heat release rates. In the context of enclosure fires with openings, Harish and Venkatasubbaiah [6] investigated the influence of opening positions and Grashof numbers on buoyancy-driven flows, while Yang et al. [7] proposed a critical criterion for the upward convection of smoke through horizontal openings. Additionally, the smoke filling modes and vertical temperature distributions in confined spaces have been explored [8,9]. However, the underground deep vertical space differs significantly due to its uniquely large aspect ratio. While McGrattan et al. [10] and Li et al. [11] emphasized the role of turbulent mixing at the interface of long shafts, and Ahn et al. [12] and Xu et al. [13] explored smoke movement in deep underground garages, their focus remained primarily on scenarios with open ceilings or natural ventilation [14–16]. In such “open” configurations, hot smoke is continuously exhausted, preventing the severe heat accumulation and flow recirculation characteristic of sealed conditions.

When the space is sealed, the flame could self-extinguish due to lack of oxygen. This behavior has been systematically investigated in various confined spaces. In their seminal work, Utiskul and Quintiere [17] theoretically demonstrated that combustion in poorly ventilated spaces is governed by oxygen availability, defining the transition from quasi-steady burning to extinction based on the global equivalence ratio. Early experiments by Takeda and Akita [18] also observed the extinction of pool fires in airtight compartments due to oxygen depletion. Subsequently, critical conditions for self-extinction regarding fuel types and vent sizes were identified [19–21]. More recently, the self-extinction mechanism has been extended to tunnel fires. Lin et al. [22] and Wang et al. [23] found that

in long enclosed tunnels, the smoke settling behavior significantly blocks air entrainment, leading to spontaneous extinction. Regarding sealed compartments, Wang et al. [24] and Jin et al. [25] further quantified the mass loss rate and extinction limits for pool and jet fires. Detailed studies on the influence of sidewalls and fuel depth have also enriched the understanding of burning characteristics in confined environments [26–28]. However, these studies are mostly limited to conventional compartments or horizontal tunnels with low aspect ratios. The vertical transport distance in deep shafts is significantly larger, which may induce different oxygen transport and smoke settling timescales compared to these conventional geometries.

In summary, while open deep shafts and sealed horizontal compartments have been studied separately, the smoke transportation characteristics and fire dynamics in sealed underground deep vertical spaces remain largely unexplored. The coupling of the slender geometry (deep and narrow) with the sealed boundary condition is hypothesized to induce complex vortex structures and distinct thermal stratification, which differs fundamentally from the well-mixed assumption in standard enclosure fires. Existing prediction models derived from conventional geometries [17,24] may not be directly applicable to this specific scenario. Therefore, it is urgent to investigate the influence of sealing on smoke transportation modes and flame self-extinction characteristics in deep vertical spaces through both experiments and numerical simulations.

2. Materials and Methods

2.1. Experimental Setup

To accurately reproduce the smoke transportation dynamics of the full-scale deep vertical garage in the reduced-scale model, the experimental design was strictly based on the Froude modeling scaling law. According to Quintiere [29], for fire-induced flows where buoyancy and inertial forces dominate over viscous forces, preserving the Froude number ($Fr = \frac{V^2}{gL}$) ensures dynamic similarity between the model and the prototype. The specific scaling relationships used in this study, derived based on the geometric scale ratio of $\lambda = L_m/L_f = 1/26$, are summarized in Table 1.

Table 1. Scaling correlations based on Froude modeling.

Physical Quantity	Symbol	Scaling
Heat Release Rate	Q	$Q_m/Q_f = \lambda^{5/2}$
Velocity	V	$V_m/V_f = \lambda^{1/2}$
Time	t	$t_m/t_f = \lambda^{1/2}$
Temperature	T	$T_m = T_f$

Note: Subscripts m and f denote the model and full-scale prototype, respectively.

It is acknowledged that reduced-scale fire experiments cannot simultaneously preserve the Reynolds number and Froude number. Consequently, turbulence intensity, flame radiation, wall heat transfer, and thermal inertia may not be perfectly scaled. Because smoke movement in the deep vertical shaft is primarily driven by thermal buoyancy, Froude scaling was adopted to represent the macroscopic flow structures and temperature-profile trends. However, the present 1:26 experiments do not constitute full-scale validation, and absolute temperatures, heat losses, and self-extinction times may differ at larger scales. The results and correlations should therefore be interpreted within the tested reduced-scale configuration; their application to full-scale engineering design requires further larger-scale validation, including representative thermal boundaries and ventilation conditions.

Following the above similarity requirements, a 1:26 reduced-scale deep-shaft model was constructed, as shown in Figure 2. The model consists of five prefabricated cylindrical

steel sections connected by flanges, creating a shaft with a total depth of 3.0 m and a diameter of 0.6 m. Each section is 0.6 m high and fabricated from 2 mm thick steel plates. Each section incorporates a 5 mm thick transparent glass–ceramic observation door spanning one-third of the circumference (120°) for visualizing smoke transport and flame behavior. The remaining steel wall is insulated with asbestos to reduce heat loss to the surroundings, and the flange connections and other potential leakage paths are sealed using aluminum-foil tape.

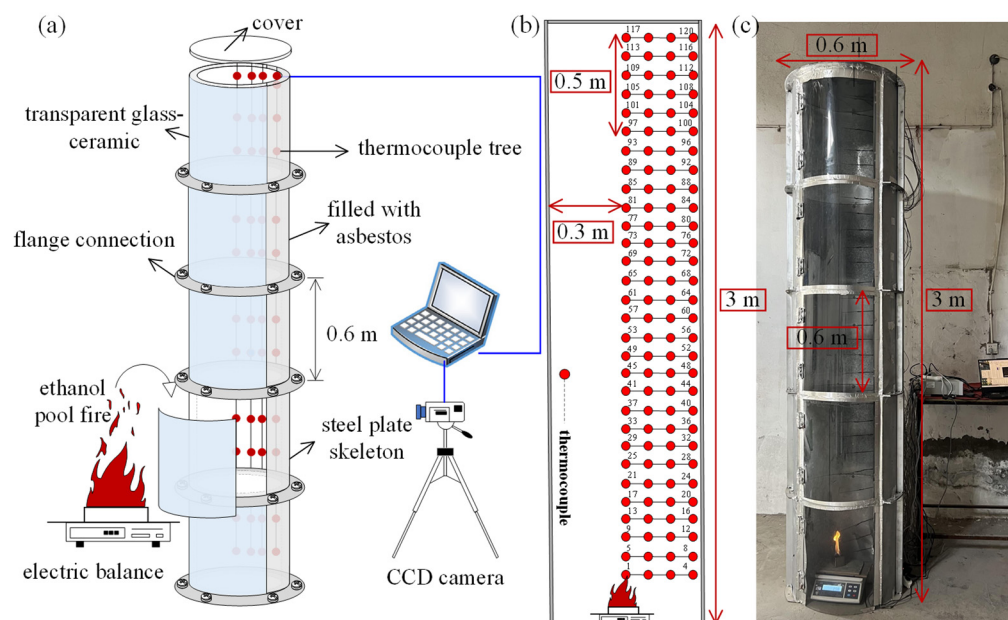


Figure 2. Experimental platform for the underground deep vertical space: (a) schematic diagram of the experimental setup; (b) thermocouple layout and numbering; (c) photograph of the experimental platform.

Four rectangular stainless steel pans (2 mm thick) with different dimensions were employed: 6 cm × 6 cm, 8 cm × 8 cm, 10 cm × 10 cm, and 10 cm × 15 cm. These sizes were selected to represent typical full-scale vehicle fire scenarios with heat release rates ranging from 3 MW to 10 MW. The HRR values reported in Table 2 were calculated from the experimentally measured fuel mass-loss-rate histories for each test case as

$$Q(t) = \chi \dot{m}(t) \Delta H_{c,eff} \quad (1)$$

where $\Delta H_{c,eff}$ is the effective heat of combustion (taken as the LHV of ethanol) and χ is the combustion efficiency (assumed as 1.0 in this study). Table 2 summarizes the quasi-steady HRR ranges (plateau stage) for each pan size across the tested fire source depths (h). Anhydrous ethanol (purity > 99.7%) was used as the fuel, with a combustion efficiency assumed to be roughly constant for HRR calculation. The fuel depth was maintained at 3 cm before each test to ensure consistent burning conditions.

Table 2. Heat release rates for different ethanol pool fire sizes.

Pan Size	Approximate HRR (kW)
10 cm × 15 cm	4.23–4.66 kW
10 cm × 10 cm	3.01–3.17 kW
8 cm × 8 cm	1.93–2.31 kW
6 cm × 6 cm	0.84–0.98 kW

The measurement system consists of temperature and mass loss acquisition subsystems. To capture the transient thermal field, K-type thermocouples (1 mm diameter) were arranged on four vertical thermocouple trees with a spatial interval of 0.1 m, as illustrated in Figure 2b. The temperature data were recorded by a multi-channel data logger (MT500P, Shenzhen Shenhuaxuan Technology Co., Ltd., Shenzhen, China) at a sampling frequency of 1 Hz. Simultaneously, the mass loss rate of the fuel was monitored by a precision electronic balance (TP-3200, Changsha Xiangyi Balance Instrument Equipment Co., Ltd., Changsha, China; readability: 0.1 g), which was placed at the bottom of the shaft. The heat release rate (HRR) was calculated based on the mass loss data recorded at 1 s intervals.

To systematically investigate the influence of sealing and fire source position, five typical fire source depths (h) were selected: 0.45 m, 1.05 m, 1.65 m, 2.25 m, and 2.85 m. For each depth, the four pan sizes were tested under both open- and sealed-ceiling conditions, giving 20 matched open–sealed pairs and 40 tests in total, as detailed in Table 3. The ambient temperature during the experiments ranged from 20 °C to 25 °C.

Table 3. Experimental tests.

Test No.	Fire Source Depth h (m)	Boundary Condition	Ambient Temperature
S1–S4	0.45	Sealed	20–25 °C
S5–S8	1.05	Sealed	20–25 °C
S9–S12	1.65	Sealed	20–25 °C
S13–S16	2.25	Sealed	20–25 °C
S17–S20	2.85	Sealed	20–25 °C
S21–S24	0.45	Open	20–25 °C
S25–S28	1.05	Open	20–25 °C
S29–S32	1.65	Open	20–25 °C
S33–S36	2.25	Open	20–25 °C
S37–S40	2.85	Open	20–25 °C

Note: For each test group (e.g., S1–S4), four different pan sizes (6×6 , 8×8 , 10×10 , 10×15 cm) were tested sequentially, and h is measured from the sealed ceiling to the fuel surface.

No mechanically imposed supply or exhaust airflow was used in the experiments. Under the open condition, the shaft ceiling remained open, and the airflow and smoke movement were driven solely by natural buoyancy. Under the sealed condition, the ceiling was closed, with no mechanical ventilation or prescribed supply-air flow rate.

2.2. Numerical Simulation

To investigate the detailed flow field structure and smoke transportation mechanism inside the underground deep vertical space, which cannot be fully captured by thermocouples, numerical simulations were performed using the computational fluid dynamics software ANSYS FLUENT 19.2 (ANSYS Inc., Canonsburg, PA, USA). The simulations were based on the fundamental conservation equations of mass, momentum, energy, and species. Given the high Reynolds number and complex turbulent flow characteristics in the deep vertical space fire, the Reynolds-Averaged Navier–Stokes (RANS) approach was adopted. Specifically, the standard k - ε two-equation model [30] was employed to resolve the turbulence closure, which has been proven to provide a good compromise between computational cost and accuracy for buoyancy-driven flows.

The Eddy-Dissipation (ED) model [31] was adopted to support the analysis of the macroscopic temperature and flow fields under the experimentally prescribed burning conditions. Under its fast-chemistry assumption, the ED model treats the reaction rate as being controlled primarily by turbulent mixing. It does not resolve detailed finite-rate or unsteady extinction chemistry and cannot provide a reliable prediction of intermediate species such as CO. In the present simulations, a UDF converts the experimentally measured fuel

mass-loss-rate history into the pool-surface mass flux/source term and confines its spatial extent to the pan footprint. Consequently, the CFD calculations are not an independent prediction of the fuel-burning rate or self-extinction time; they are used to examine the associated flow structure and temperature field. The simulated species fields are therefore not used as direct evidence of local oxygen depletion or of the detailed chemical extinction process. When the measured mass-loss-rate history is used as input, the surface mass flux is given by

$$\dot{m}''(t) = \dot{m}(t)/A_p \quad (2)$$

where $\dot{m}(t)$ is the experimentally measured fuel mass loss rate, and A_p is the pan area. The corresponding mass source term is imposed on the control volumes near the fire source and updated in time. The SIMPLE algorithm is used for pressure–velocity coupling, and the convective terms are discretized using a second-order upwind scheme. Structured hexahedral meshes were generated using ICEM. A grid independence test was conducted using grid sizes of 0.95 cm, 1.00 cm, 1.10 cm, and 1.25 cm. Considering both calculation accuracy and time efficiency, a grid size of 1.0 cm was adopted for all simulation cases.

All solid boundaries, including the shaft sidewall, bottom, and sealed ceiling, were treated as zero-thickness no-slip walls subjected to external convective heat transfer. A uniform heat-transfer coefficient of $3.5 \text{ W m}^{-2} \text{ K}^{-1}$ and a free-stream temperature of 300 K were specified. Shell conduction and radiative heat transfer were not enabled. The transparent glass–ceramic observation doors were not represented as separate material regions; therefore, the same thermal boundary condition was applied to the glass–ceramic doors and the remaining insulated metal walls.

2.3. Model Validation

To evaluate the reliability of the numerical model over a broader range of operating conditions, the simulated centerline temperature-rise profiles were compared with the experimental measurements at three fire source depths, $h = 0.45, 1.65$, and 2.85 m , for four pan sizes, as shown in Figure 3. For each condition, the experimentally measured temperatures were averaged over the selected quasi-steady period, and the corresponding simulated profiles were extracted along the shaft centerline at the same axial locations. Here, ΔT denotes the centerline temperature rise relative to the ambient baseline. Overall, the numerical model reproduces the principal axial-decay characteristics of the measured temperature profiles and captures the general variation associated with pan size and fire source depth. Larger discrepancies occur at several locations close to the fire source, where the temperature field is strongly influenced by plume entrainment, flame intermittency, and local heat transfer. Nevertheless, the model provides a reasonable representation of the overall temperature distribution within the tested parameter range.

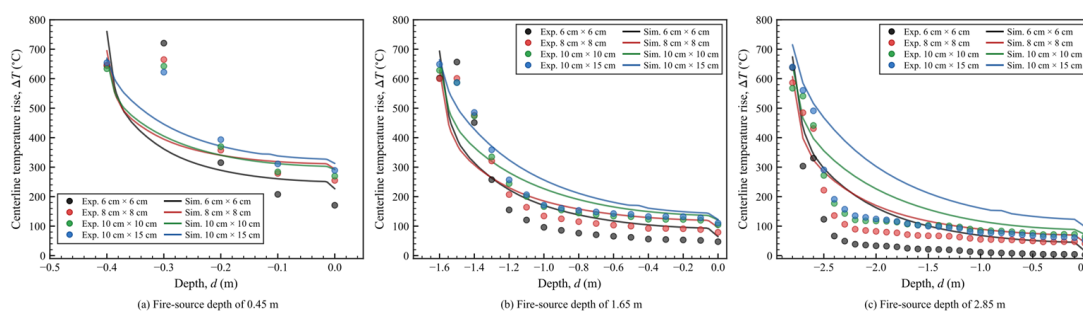


Figure 3. Comparison of the experimental and simulated centerline temperature-rise profiles under sealed conditions at fire source depths of (a) 0.45 m, (b) 1.65 m, and (c) 2.85 m for four pan sizes.

The remaining discrepancies between the simulations and experiments may be associated with the simplified thermal boundary treatment and turbulence closure. In addition, the deep vertical geometry may enhance turbulence anisotropy, whereas the standard $k - \varepsilon$ model assumes isotropic turbulence. Accordingly, the CFD results are used primarily to support the interpretation of the macroscopic flow structure and temperature field within the tested parameter range.

3. Results

3.1. Effect of Sealing on Smoke Transportation and Temperature Distribution

To compare the sealing effect consistently across all fire source depths, a characteristic upper-shaft centerline temperature rise was calculated for every test. At each time t , the temperature rises measured by the five uppermost centerline thermocouples were averaged after subtracting the initial baseline of each channel, defined as the median of the first three samples. The characteristic value ΔT_{u*} was then defined as the maximum 30 s moving average of this spatially averaged temperature rise. For each matched pair with the same h and pan size, the sealing-induced enhancement was calculated as $\eta_T = \left(\frac{\Delta T_{u*}^{\text{sealed}}}{\Delta T_{u*}^{\text{open}}} - 1 \right) \times 100\%$.

As summarized in Table 4, η_T was positive for all 20 matched pairs, ranging from 43.2% to 374.9%. Although the magnitude of the enhancement varied with fire source depth and pan size, the consistently positive values demonstrate that sealing increased the characteristic upper-shaft temperature rise across all tested conditions.

Table 4. Sealing-induced enhancement of the characteristic upper-shaft centerline temperature rise, η_T .

Fire Source Depth h (m)	6 cm $\times$ 6 cm η_T	8 cm $\times$ 8 cm η_T	10 cm $\times$ 10 cm η_T	10 cm $\times$ 15 cm η_T
0.45	54.2%	55.0%	96.4%	43.2%
1.05	374.9%	276.2%	211.4%	113.7%
1.65	224.1%	206.9%	160.4%	81.6%
2.25	109.0%	162.5%	114.0%	82.6%
2.85	101.0%	252.3%	175.2%	48.9%

Figure 4 retains $h = 2.85$ m as a representative case to show the complete centerline temperature histories at four heights for the four pan sizes. Under the open-ceiling condition, the temperature history exhibits the typical rapid-growth, quasi-steady, and decay stages of an unconfined pool fire, while hot smoke is discharged directly from the shaft and pronounced heat accumulation is limited.

Under the sealed-ceiling condition at $h = 2.85$ m, the temperature continues to rise before flame extinction instead of remaining at the open-case quasi-steady level. Using the unified metric above, η_T is 101.0%, 252.3%, 175.2%, and 48.9% for the 6 cm $\times$ 6 cm, 8 cm $\times$ 8 cm, 10 cm $\times$ 10 cm, and 10 cm $\times$ 15 cm pans, respectively. The time histories therefore provide a representative illustration of the heat-accumulation process, whereas Table 4 provides the all-depth quantitative comparison.

To clarify the flow mechanism underlying this accumulation, the velocity field inside the shaft was examined (Figure 5). Compared with the open condition, the sealed boundary induces a persistent recirculation pattern characterized by an upward core flow and a compensating downward return flow. Such a return-flow-driven recirculation suppresses net exhaust, enhances the trapping of hot gases, and continuously elevates the overall thermal level in the shaft.

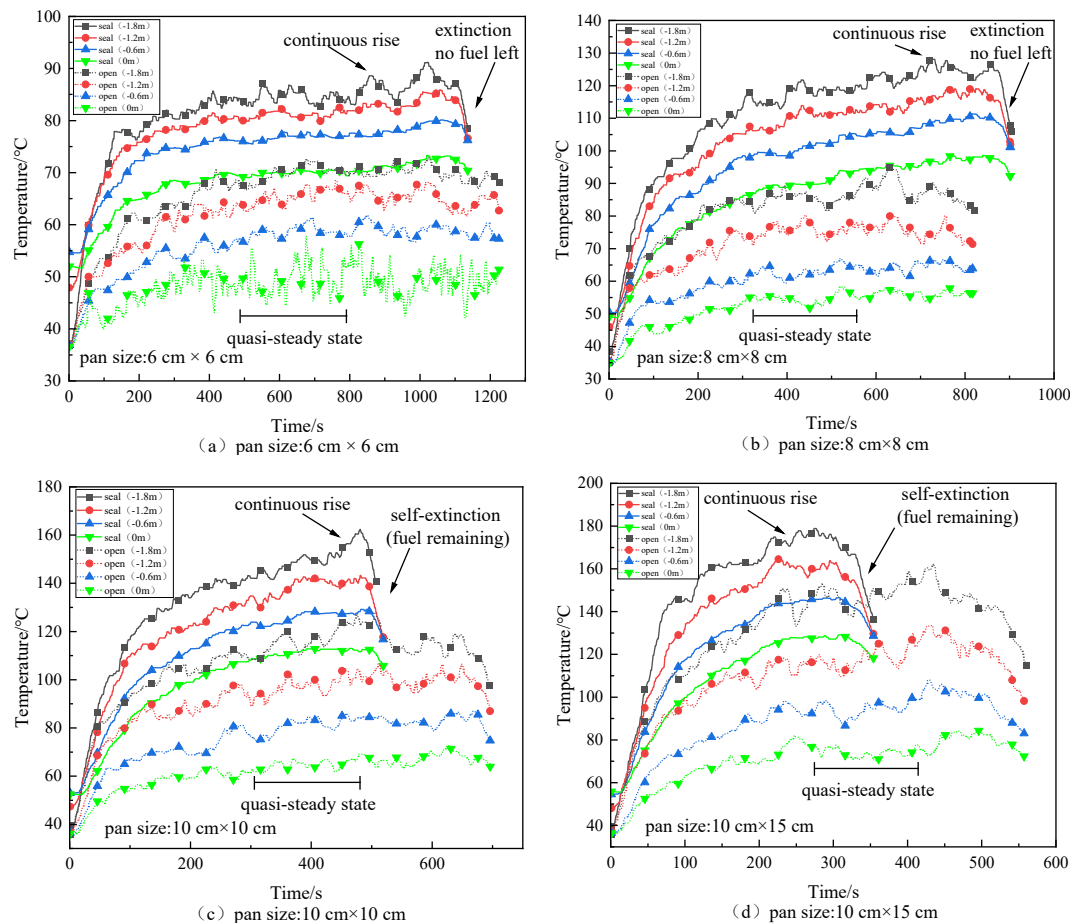


Figure 4. Centerline temperature histories at different measurement heights under open- and sealed-ceiling conditions at a fire source depth of $h = 2.85$ m for different pan sizes.

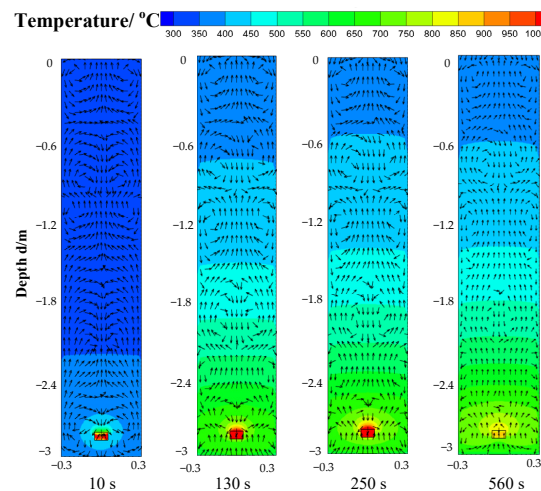


Figure 5. Velocity field distribution illustrating the development of a return-flow-driven recirculation (pan size: 10 cm × 10 cm, $h = 2.85$ m). The arrows indicate the local flow direction.

The thermal consequence of this return-flow-driven recirculation is further confirmed by the temperature fields in Figure 6. Under the open condition (top row), the hot plume rises freely and is discharged from the shaft, resulting in a relatively low ambient temperature. In contrast, under the sealed condition (bottom row), a pronounced high-temperature stratified layer develops and occupies a large portion of the shaft. This stratification is consistent with the recirculation identified in Figure 5, which prolongs the residence of hot gases and facilitates heat accumulation.

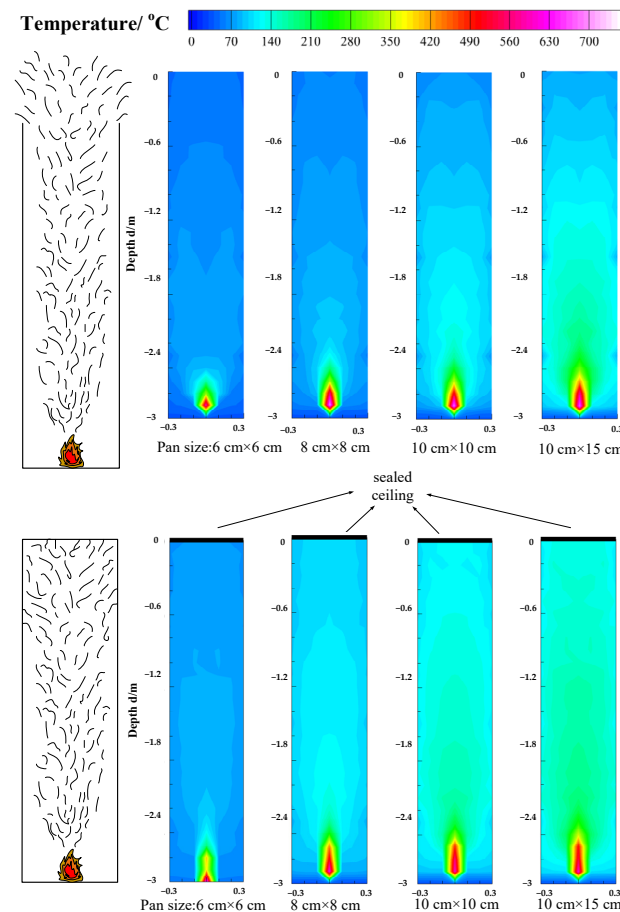


Figure 6. Temperature contours at the time of maximum temperature rise.

In summary, the paired experimental data across all five fire source depths show that sealing consistently elevates the characteristic upper-shaft thermal level, while the magnitude of the increase varies with both h and pan size. For the representative $h = 2.85$ m configuration, the combined temperature histories (Figure 4), velocity fields (Figure 5), and temperature contours (Figure 6) indicate that sealing changes the smoke transport from free exhaust to a return-flow-driven recirculation, thereby promoting heat accumulation within the shaft. In both boundary conditions, the centerline temperature rise still generally decreases with height because plume cooling and ceiling/wall heat losses remain effective; sealing elevates the overall thermal level and weakens the apparent vertical decay rather than reversing the vertical gradient.

3.2. Influence of Fire Source Depth on Vertical Temperature Decay Under Sealed Conditions

Under sealed conditions, the vertical decay of the plume centerline temperature rise, ΔT , is used as a quantitative indicator of smoke accumulation and entrainment in the deep shaft. In this section, we focus on how the fire source depth h (Table 3) modulates the vertical ΔT profile along the shaft centerline for different heat release rates Q (Table 2). Particular attention is paid to the near-fire plume-rise region and the upper region influenced by ceiling impingement and the accumulated hot-smoke layer, which exhibit distinct decay characteristics.

Figure 7 shows the vertical profiles of the plume centerline temperature rise (ΔT) for different fire source depths (h). In the near-fire region, all cases exhibit a pronounced temperature rise. With increasing height, however, the decay trends diverge with h : when the fire source is shallow ($h = 0.45$ m), ΔT decreases more rapidly, whereas for the deeper case ($h = 2.25$ m), elevated temperatures are maintained up to the ceiling. This contrast is

consistent with a thermal preheating (shielding) effect induced by smoke accumulation in the sealed shaft. For deeper fires, the upward-moving hot gases impinge on the sealed ceiling and generate a large-scale return flow and recirculation, progressively filling the volume above the source with hot smoke. As a result, the rising plume entrains preheated smoke rather than cold ambient air, reducing the effective cooling and leading to a weaker apparent decay of ΔT along the centerline. Ceiling impingement can occur in all cases once the plume reaches the sealed ceiling; however, for shallow fires it occurs over a very short rise distance, and the near-ceiling heat losses and radiation/ceiling-jet effects dominate, which inhibits the formation of a thick, well-insulating hot-smoke layer and results in a more pronounced centerline decay.

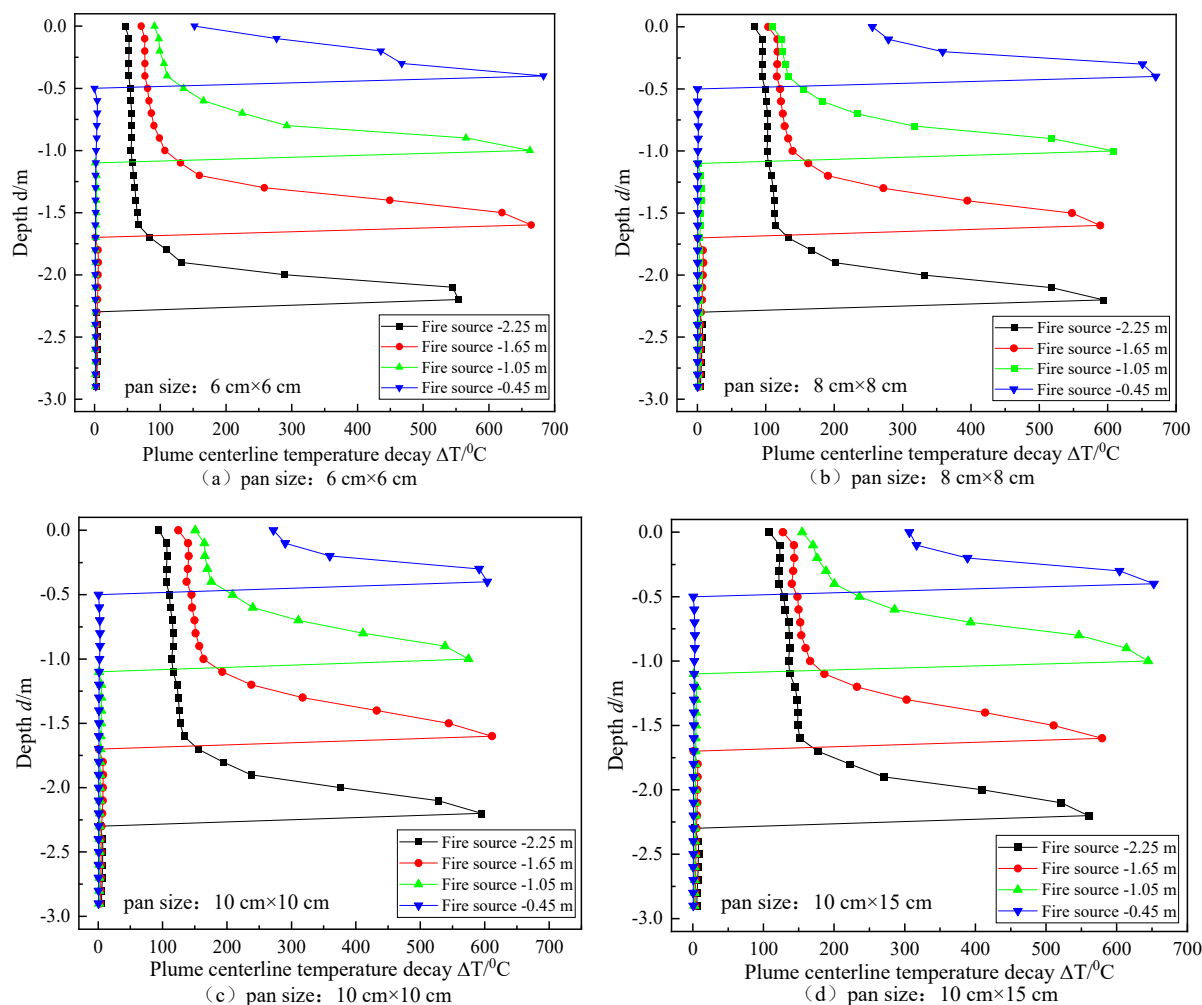


Figure 7. Vertical centerline temperature decay profiles under different fire source depths.

Furthermore, distinct from the height-dependent decay trends above the fire source, the region below the fire source exhibits a consistent thermal characteristic across all cases: the temperature rise is negligible. This indicates that a stable density stratification, with relatively cool and dense air occupying the lower part of the shaft, effectively suppresses the downward penetration of the hot-smoke layer. Even when a ceiling-impingement-induced return flow and recirculation develop in the sealed shaft, buoyancy still limits the ability of the hot gases to intrude into the lower region. To visually verify this global thermal distribution and illustrate the spatial influence of the fire source depth, temperature contours at the moment of maximum temperature rise are presented in Figure 8.

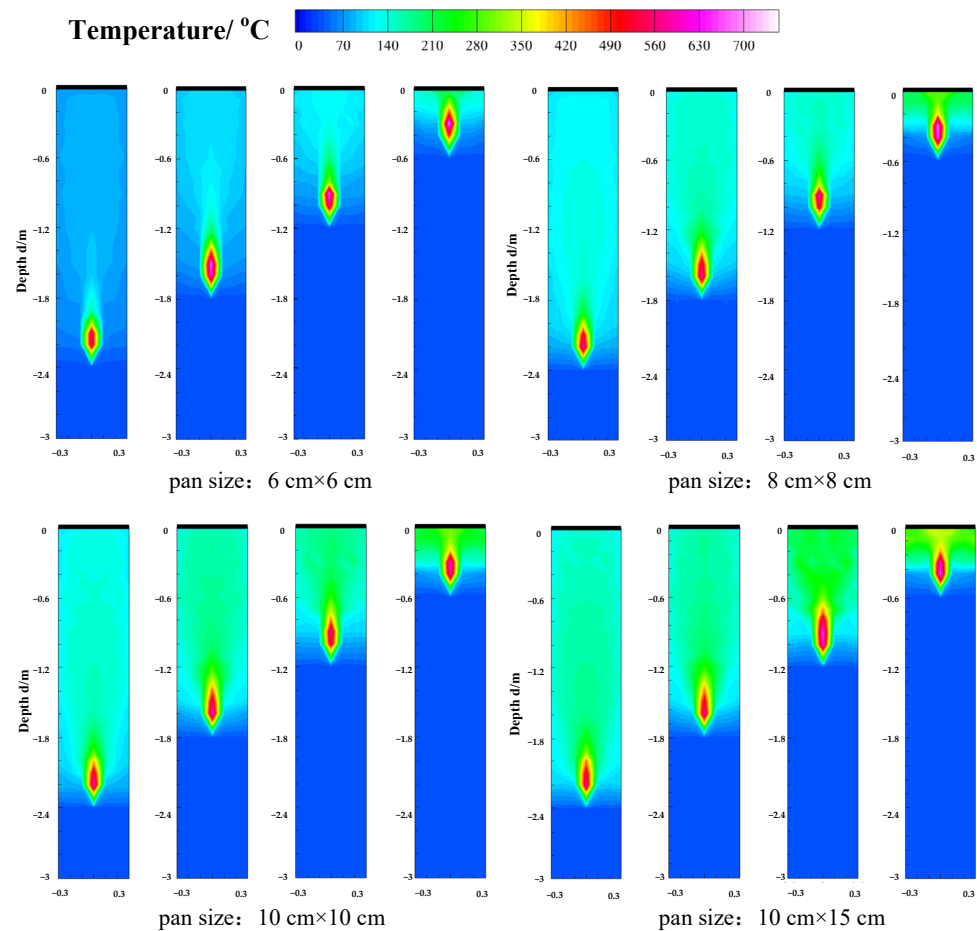


Figure 8. Temperature contours at the time of maximum temperature rise for different fire source depths (sealed ceiling).

As shown in Figure 8, the high-temperature zone is primarily confined to the region above the fire source, while the lower part of the shaft remains close to ambient across all cases, which is consistent with the negligible temperature rise observed below the fire source in Figure 7. Qualitatively, deeper fire sources tend to produce a broader warmed region above the source, indicating a thicker accumulated hot-smoke layer under sealed conditions, whereas shallow fires exhibit a more localized hot region near the ceiling with a relatively cooler bulk region, reflecting the stronger influence of near-ceiling heat loss and ceiling-jet/radiation effects.

To further quantify the vertical temperature decay characteristics, the experimental data were analyzed based on the plume theory. According to the ideal plume theory, assuming no radiant heat loss, the centerline temperature rise ΔT at height z above the fire source can be expressed as:

$$\Delta T = 5.0 \left(\frac{T_{\infty}}{g c_p \rho_{\infty}^2} \right)^{\frac{1}{3}} \dot{Q}^{\frac{2}{3}} \cdot z^{-\frac{5}{3}} \quad (3)$$

where T_{∞} and ρ_{∞} are the ambient temperature and density, respectively; g is the gravitational acceleration; c_p is the specific heat capacity of air; and $\dot{Q}$ is the heat release rate. However, in the reduced-scale experiments, the fire size is varied by changing the pan dimensions. Since the fire sources are rectangular pans, an equivalent diameter is introduced to represent the characteristic fire size, defined as

$$De = \sqrt{4A/\pi} \quad (4)$$

where A is the planform area of the pan. D denotes the equivalent diameter De for non-circular fire sources. To account for the finite size of the fire source, a virtual origin correction z_0 proposed by Heskestad [32] is introduced:

$$\Delta T = 9.1 \left(\frac{T_\infty}{g c_p \rho_\infty^2} \right)^{\frac{1}{3}} \dot{Q}^{\frac{2}{3}} \cdot (z - z_0)^{-\frac{5}{3}}; z_0 = 0.083 \dot{Q}^{\frac{2}{5}} - 1.02D \quad (5)$$

This modification relaxes the point-source assumption of the ideal plume and corrects for density variations. To investigate the relationship between temperature decay and height, Equation (5) is transformed into logarithmic form:

$$\lg \Delta T = C - \frac{5}{3} \lg(z - z_0); \quad C = \lg \left[9.1 \left(\frac{T_\infty}{g c_p \rho_\infty^2} \right)^{\frac{1}{3}} \dot{Q}^{\frac{2}{3}} \right] \quad (6)$$

Equation (6) indicates that for a Heskestad plume, there is a linear relationship between $\lg \Delta T$ and $\lg(z - z_0)$ with a theoretical slope of $-5/3$. The experimental data were plotted in this coordinate system as shown in Figure 9 to evaluate the specific decay characteristics under sealed conditions.

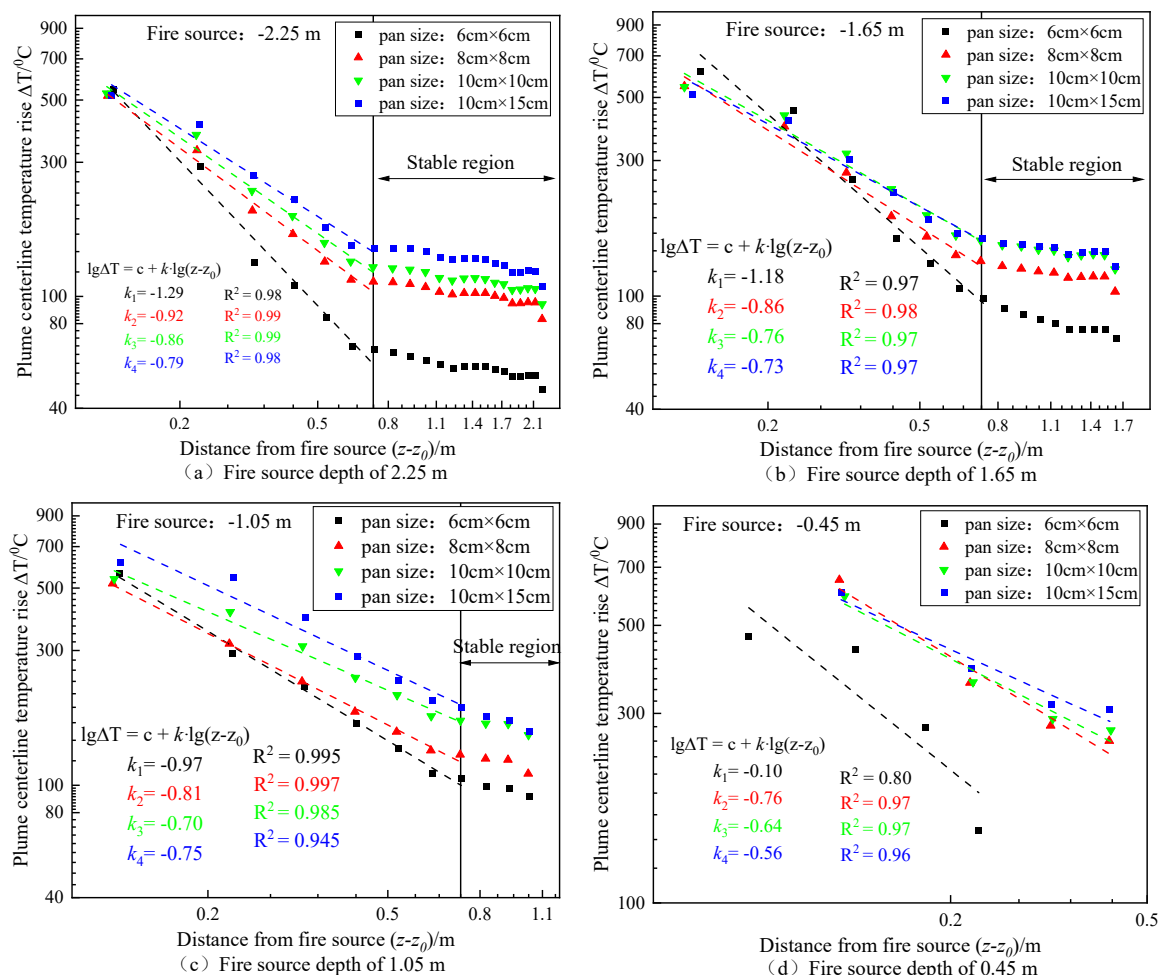


Figure 9. Log-log plot and linear fitting of centerline temperature decay under different fire source depths.

As illustrated in Figure 9, the plume centerline temperature rise profiles exhibit a clear segmented structure under sealed conditions. First, in the region below the fire source, the temperature rise is negligible because buoyancy prevents hot smoke from penetrating into

the lower part of the shaft. Second, within the plume-rise region (i.e., within 0.75 m above the fire source), the centerline temperature rise follows a Heskestad-type power-law decay when the virtual origin correction z_0 is applied. The linear fitting of $\lg \Delta T$ versus $\lg(z - z_0)$ yields coefficients of determination R^2 consistently above 0.94, and the fitted slopes k are systematically less negative than the theoretical value of $-5/3$, indicating a slower decay than an ideal unconfined plume. This deviation is attributed to confinement-induced smoke accumulation and recirculation in the sealed shaft, which reduces the entrainment of cold ambient air and leads the plume to entrain relatively preheated gases, thereby weakening the effective cooling along the centerline. Third, in the region more than 0.75 m above the fire source (when present), the centerline temperature becomes relatively uniform due to the formation of a well-stratified hot-smoke layer caused by continuous accumulation; the thickness of this layer increases with fire source depth. The shallow case ($h = 0.45$ m) is an exception: due to its proximity to the ceiling, the thermal field is strongly influenced by flame radiation and ceiling-jet impingement, and a stable decay region suitable for power-law fitting is difficult to establish.

The above analysis suggests that the decay slope k within the plume-rise region is governed by both the heat release rate Q and the fire source depth h . To establish a generalized correlation, the experimentally derived k values (from the log-log fitting in Figure 9) were fitted in the (Q, h, k) space using a non-linear regression (data for $h = 0.45$ m were excluded due to the dominant radiation and ceiling-jet impingement effects). Figure 10 compares the measured values with the corresponding model predictions. The resulting empirical correlation can be written as

$$k = -C_k \left(\frac{Q}{Q_r} \right)^{-0.31} \left(\frac{h}{h_r} \right)^{0.25} \quad (7)$$

where Q is the heat release rate, h is the fire source depth, and k is dimensionless under the present definition $\lg \Delta T = c + k \lg(z - z_0)$. The regression yields a high coefficient of determination ($R^2 = 0.95$), indicating good agreement with the experimental data within the tested range under sealed conditions.

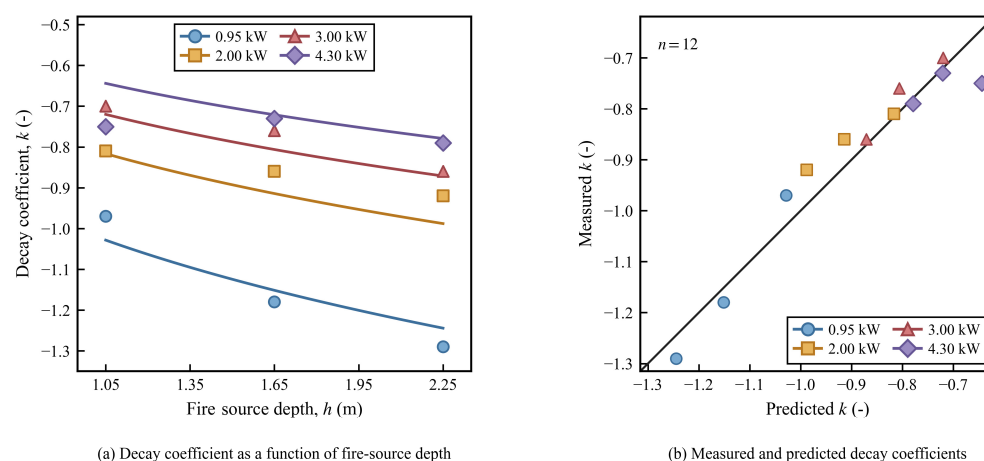


Figure 10. Comparison between measured and predicted decay coefficients: (a) decay coefficient as a function of fire source depth, where symbols denote experimental values and colored solid lines denote the predictions of Equation (7); (b) comparison between measured and predicted decay coefficients, where the black solid line denotes the equality line.

Substituting Equation (7) into Equation (5) yields the centerline temperature-decay profiles under different fire source depths:

$$\Delta T = 9.1 \left(\frac{T_\infty}{g c_p \rho_\infty^2} \right)^{\frac{1}{3}} \dot{Q}^{\frac{2}{3}} \cdot (z - z_0)^k$$

$$k = -C_k \left(\frac{Q}{Q_r} \right)^{-0.31} \left(\frac{h}{h_r} \right)^{0.25} \quad (8)$$

$$z_0 = 0.083 \dot{Q}^{\frac{2}{5}} - 1.02 D_e$$

Note that Equations (7) and (8) are empirical correlations verified only for the present 1:26 reduced-scale sealed experiments and should be applied within the tested ranges of h and $\dot{Q}$.

In summary, under sealed conditions, the vertical thermal response in the shaft can be described by three characteristic regions along the centerline temperature rise, as illustrated in Figure 11. (i) Below the fire source, the temperature rise remains negligible, indicating that a stable density stratification suppresses the downward penetration of hot smoke. (ii) Within the plume-dominated decay region, extending from the fire source level to approximately 0.75 m above it (when this rise distance exists), the centerline temperature rise exhibits a Heskestad-type power-law decay with height after applying the virtual-origin correction. (iii) Above approximately 0.75 m, the decay becomes markedly weaker, corresponding to an upper weak-gradient region influenced by ceiling impingement and the progressive accumulation of hot smoke; this region reflects a relatively stratified hot-gas layer with reduced entrainment of cold ambient air, and its extent depends on the fire source depth and available headspace.

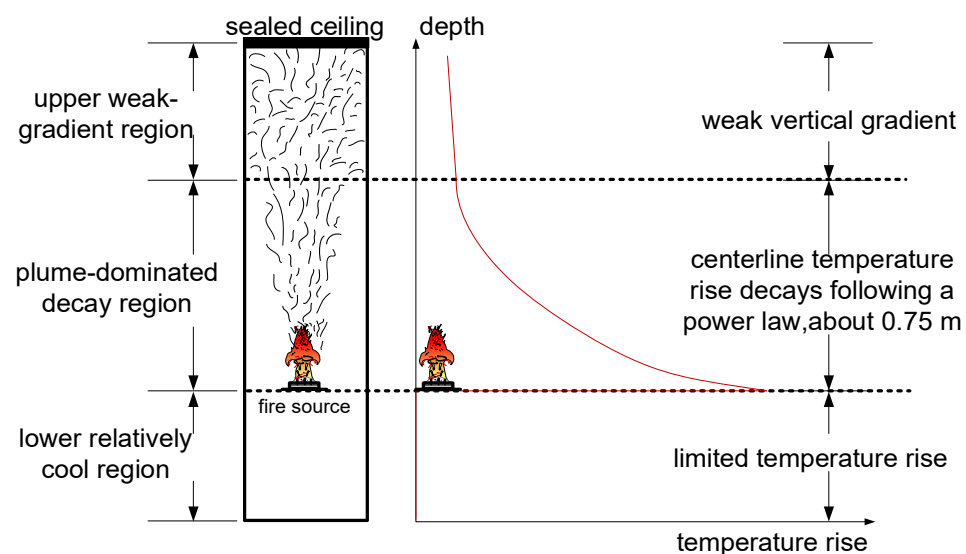


Figure 11. Illustration of three smoke regions (sealed ceiling).

3.3. Flame Self-Extinction Characteristics and Prediction Model Under Sealed Conditions

Under sealed conditions, the flame behavior differs markedly from that in an open environment. Figure 12 illustrates the flame evolution for a representative case (shaft depth $d = 3$ m, fire source depth $h = 2.85$ m, and pan size $10 \text{ cm} \times 10 \text{ cm}$). Prior to extinction, the flame exhibits a distinct “ghosting” phenomenon, detaching from the fuel surface and wandering above the pan. This behavior is consistent with an oxygen-limited interpretation. Nevertheless, because time-resolved measurements of $O_2/CO/CO_2$ were not obtained, local oxygen depletion near the flame base cannot be regarded as directly demonstrated. This interpretation is therefore based on flame behavior, temperature evolution, and simulated confinement-driven flow patterns.

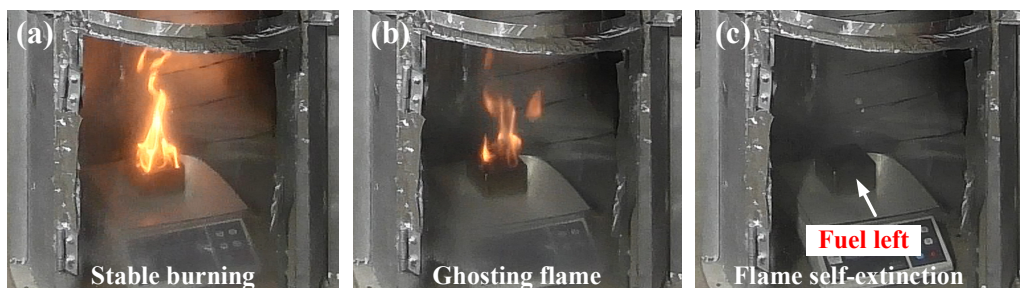


Figure 12. Flame evolution before self-extinction: (a) stable burning; (b) ghosting flame; (c) flame self-extinction with fuel remaining.

To connect this qualitative mechanism to the observed parametric trends in a reproducible manner, a characteristic timescale to reach a critical oxygen deficit, denoted as t_{O_2} , is introduced as an order-of-magnitude indicator. The buffer volume above the fire is approximated as $V_b = A_s h$, where A_s is the shaft cross-sectional area and h is the fire source depth (defined as the vertical distance from the sealed ceiling to the fuel surface). This approximation treats the region between the fire and the sealed ceiling as the dominant buffer volume for vitiated-gas accumulation. Self-extinction is assumed to occur when the oxygen volume fraction decreases from $y_{O_2,0} \approx 0.21$ to a critical level $y_{O_2,cr} \approx 0.13$. It should be noted that the limiting oxygen concentration for sustaining flaming combustion depends on fuel and conditions, and typical values reported in the literature fall in the range of approximately 10–15% [33]. Under the ideal-gas approximation, the corresponding oxygen consumption requirement is estimated as

$$\Delta n_{O_2} = (y_{O_2,0} - y_{O_2,cr}) P V_b / (RT) \quad (9)$$

where $A_s = 0.283 \text{ m}^2$, $P = 1.01 \times 10^5 \text{ Pa}$, $T = 298 \text{ K}$, $M_{O_2} = 0.032 \text{ kgmol}^{-1}$, and $\Delta H_{O_2} = 13.1 \text{ MJkg}^{-1}$ [34]. The corresponding oxygen mass and energy are $m_{O_2,cr} = \Delta n_{O_2} M_{O_2}$ and $E_{cr} = m_{O_2,cr} \Delta H_{O_2}$, respectively, yielding the characteristic timescale $t_{O_2} = E_{cr} / Q$. The representative quasi-steady HRR levels used here are 0.95, 2.00, 3.00, and 4.30 kW for the 6 cm × 6 cm, 8 cm × 8 cm, 10 cm × 10 cm, and 10 cm × 15 cm pans, respectively. Thus, t_{O_2} is used as an order-of-magnitude reference timescale rather than a direct reconstruction of the local oxygen history.

Table 5 compares the measured self-extinction time t_{ext} with the calculated characteristic time t_{O_2} at $d = 3 \text{ m}$. This comparison provides a physically interpretable reference for extinction but does not constitute direct evidence of local oxygen depletion.

It is worth noting that the sealed shaft exhibits complex recirculation and spatially non-uniform vitiation (Section 3.1), which makes the direct application of classical self-extinction criteria based on idealized, well-mixed assumptions non-trivial in the present configuration. Therefore, within the tested parameter space, an empirical correlation was developed to quantify the combined influence of fire source depth h and heat release rate Q on the measured self-extinction time t_{ext} .

Assuming the power-law form $t_{ext} = ah^b Q^c$, a nonlinear least-squares regression using the Levenberg–Marquardt algorithm was performed for the 14 cases in which self-extinction was observed (Table 5). Cases without observed self-extinction, denoted by “–”, were excluded. The original time-domain fit was $t_{ext} / (1 \text{ s}) = 802.612 [h / (1 \text{ m})]^{0.606} [Q / (1 \text{ kW})]^{-1.071}$. To obtain a dimensionless and more physically interpretable form, this correlation was normalized by t_{O_2} , yielding Equation (10).

$$\frac{t_{ext}}{t_{O_2}} = 2.074 \left(\frac{h}{1 \text{ m}} \right)^{-0.394} \left(\frac{Q}{1 \text{ kW}} \right)^{-0.071} \quad (10)$$

Table 5. Flame self-extinction time.

Pool Size	Fire Source Depth h (m)	t_{ext} (s)	t_{o_2} (s)	Relative Deviation δ (%)
6 cm $\times$ 6 cm	0.45 m	560 s	183 s	67.3%
	1.05 m	843 s	428 s	49.2%
	1.65 m	-	672 s	N/A
	2.25 m	-	916 s	N/A
	2.85 m	-	1161 s	N/A
8 cm $\times$ 8 cm	0.45 m	246 s	87 s	64.6%
	1.05 m	449 s	203 s	54.8%
	1.65 m	-	319 s	N/A
	2.25 m	-	435 s	N/A
	2.85 m	-	551 s	N/A
10 cm $\times$ 10 cm	0.45 m	147 s	58 s	60.5%
	1.05 m	194 s	135 s	30.4%
	1.65 m	286 s	213 s	25.5%
	2.25 m	361 s	290 s	19.7%
	2.85 m	527 s	368 s	30.2%
10 cm $\times$ 15 cm	0.45 m	128 s	40 s	68.8%
	1.05 m	139 s	94 s	32.4%
	1.65 m	174 s	148 s	14.9%
	2.25 m	311 s	202 s	35.0%
	2.85 m	353 s	256 s	27.5%

Note: “-” denotes that self-extinction was not observed under the present test protocol; therefore, the relative deviation was not applicable (N/A). The relative deviation is defined as $\delta = (t_{ext} - t_{O_2})/t_{ext} \times 100\%$.

The original time-domain regression yielded $R^2 = 0.9542$ and an adjusted $R^2 = 0.9458$; the root-mean-square error and mean absolute error were 41.8 s and 38.4 s, respectively. Figure 13 compares the measured self-extinction times with the corresponding predictions of Equation (10). Equation (10) applies only to the present 1:26 reduced-scale sealed shaft, within $h = 0.45 - 2.85$ m and $Q = 0.95 - 4.30$ kW, and only to the tested combinations in which self-extinction was observed. It should not be extrapolated to open-ceiling conditions, different geometries, mechanically ventilated conditions, cases without observed self-extinction, or full-scale applications without further validation.

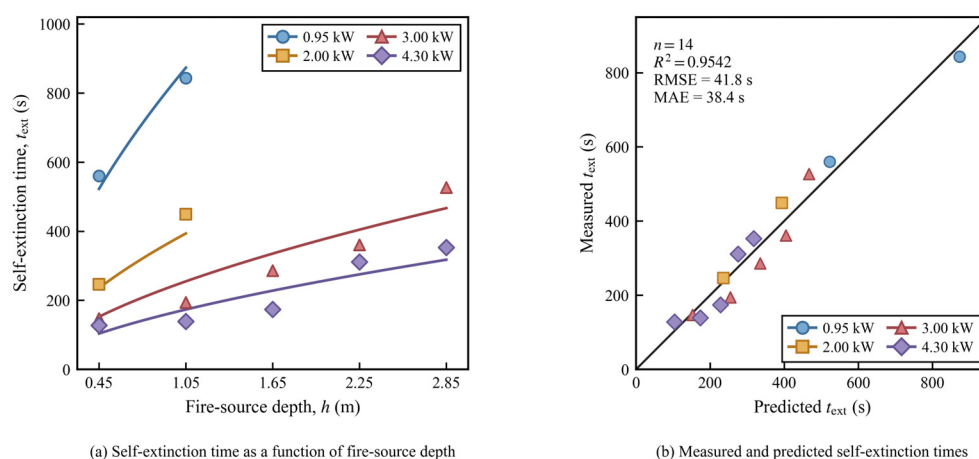


Figure 13. Comparison between measured and predicted flame self-extinction times. Symbols denote experimental values, colored solid lines denote the predictions of Equation (10), and the black solid line in panel (b) denotes equality. Prediction curves in panel (a) are shown only for the tested combinations in which self-extinction was observed.

4. Discussion

4.1. Sealing-Induced Recirculation and Thermal Accumulation

This pronounced temperature increase is attributed to smoke accumulation induced by ceiling impingement. Once the buoyant plume reaches the sealed ceiling, the upward motion is blocked and a descending return flow is established, which promotes global recirculation within the shaft. The recirculated hot products increase their residence time and are repeatedly re-entrained, thereby leading to progressive heat accumulation in the confined space. Notably, in both the open and sealed cases, the centerline temperature generally decreases with height as the plume approaches the ceiling. A monotonic increase toward the ceiling—expected for an idealized fully smoke-filled hot layer—is not observed. This indicates that plume cooling and ceiling/wall heat losses remain effective in the present reduced-scale configuration. Therefore, the primary effect of sealing here is to elevate the overall thermal level in the shaft and to weaken the apparent vertical decay rate of the centerline temperature rise.

4.2. Interpretation of Flame Self-Extinction and Parametric Trends

The analysis of vertical temperature decay in Section 3.2 shows that the sealing boundary promotes a pronounced heat-accumulation effect, reflected by a decay coefficient k that is algebraically larger (less negative) than the classical theoretical value. The simulated return-flow-driven recirculation provides a plausible transport pathway by which combustion products may return toward the fire source. Together with the observed ghosting behavior and self-extinction with fuel remaining, this pattern is consistent with, but does not directly demonstrate, an oxygen-limited interpretation.

Within the present sealed shaft, increasing Q makes the fitted decay coefficient k less negative, which indicates a weaker apparent axial decay of the centerline temperature rise. A stronger fire increases the flux of hot combustion products and promotes upper-shaft heat accumulation and recirculation, thereby raising the background thermal level over the fitted plume-rise region. Conversely, increasing h provides a longer plume-development distance before ceiling interaction, allowing greater entrainment and dilution and therefore producing a more negative decay coefficient. The $h = 0.45$ m cases were excluded from this correlation because their short development distance makes ceiling-jet impingement and radiation especially important. These trends describe the coupled effects of entrainment, ceiling interaction, and confinement-induced heat accumulation in this apparatus; they are not universal plume-scaling exponents.

A possible interpretation of the observed self-extinction is provided by the conceptual recirculation–buffer-volume framework developed from the observations in Sections 3.1 and 3.2. As the buoyant plume impinges on the sealed ceiling, the simulated flow field shows a large-scale return flow and the experiments show progressive heat accumulation above the fire. This recirculation may restrict fresh-air entrainment and return combustion products toward the combustion zone. These conditions could reduce local oxygen availability and contribute to extinction even when fuel remains. However, without time-resolved gas-species measurements, the relative contributions of local oxygen depletion, heat loss, and unsteady combustion chemistry cannot be quantified.

First, decreasing h leads to earlier extinction. For instance, with a 10 cm × 10 cm pan, reducing h from 2.85 m to 0.45 m shortens t_{ext} by 72.1%. This trend is consistent with the buffer-volume interpretation: a deeper fire source provides a larger volume above the fire for the accumulation of hot, vitiated gases, which increases the oxygen inventory that must be consumed to approach the critical condition and thus yields a longer t_{O_2} . In contrast, a shallower source restricts the effective accommodation volume, so the characteristic depletion timescale t_{O_2} becomes shorter and extinction occurs earlier.

Second, increasing Q significantly accelerates extinction. As shown in Table 5, at $h = 1.05$ m, increasing the pan size from $6\text{ cm} \times 6\text{ cm}$ (approximately 1 kW) to $10\text{ cm} \times 15\text{ cm}$ (approximately 4 kW) shortens t_{ext} by 83.5%. Mechanistically, a larger Q implies a higher oxygen-consumption rate, which reduces t_{o_2} and drives the system toward oxygen-limited conditions more rapidly. Meanwhile, stronger heat release also promotes the accumulation of hot gases in the confined upper region, further weakening the effective entrainment of fresh air and facilitating earlier extinction. These parametric interpretations are qualitative and do not constitute direct evidence of the local oxygen-concentration history.

To interpret the discrepancy between the estimated characteristic time and the measured extinction time in Table 5, it should be emphasized that the timescale model relies on simplified assumptions. First, the oxygen field above the fire is generally non-uniform under the sealed, recirculation-dominated flow; therefore, the local oxygen availability in the flame region can differ from the volume-averaged estimate, which may delay extinction relative to the idealized prediction. Second, the plume-induced entrainment from the lower region may intermittently supply additional fresh air to the flame base, extending the burning duration. Third, the fire does not maintain a strictly constant HRR throughout the experiment; instead, the HRR typically ramps up before reaching a quasi-steady stage, whereas the present estimate uses a representative quasi-steady Q , which tends to bias the predicted time earlier. These limitations justify treating t_{o_2} as an interpretive reference timescale rather than a strict predictor.

The exponents in the original time-domain form of Equation (10) are qualitatively consistent with the characteristic timescale. The fitted exponent of Q , -1.071 , is close to the inverse dependence expected when oxygen consumption increases approximately with heat release rate, while the positive exponent of h , 0.606 , is consistent with a larger buffer volume above a deeper fire source. The departure of the fitted exponents from the reference values of 1 and -1 implied by the simplified oxygen-inventory estimate is plausibly associated with spatially non-uniform vitiation, plume entrainment, recirculation, heat loss, and transient burning. Accordingly, t_{o_2} supplies the leading-order physical scaling, whereas the dimensionless factor in Equation (10) remains an empirical correction specific to the tested configuration.

4.3. Limitations and Applicability

Finally, the limitations of the present work should be emphasized. First, paired open-sealed tests covered five discrete fire source depths and four pan sizes in one reduced-scale shaft geometry. Second, the oxygen-starvation pathway inferred under sealing was supported primarily by flow/thermal-field evidence and flame observations; however, time-resolved gas composition measurements (e.g., $o_2/co/co_2$) were not available to directly quantify oxygen depletion in the combustion zone. Accordingly, local oxygen depletion was not directly demonstrated, and the relative contributions of oxygen limitation, heat loss, and unsteady combustion chemistry cannot be quantified from the present measurements.

The combustion-model formulation introduces an additional limitation. Because the measured fuel mass-loss-rate history was prescribed through the UDF, the simulations did not independently predict the burning-rate evolution or the self-extinction time. Moreover, the mixing-controlled, fast-chemistry assumption of the ED model does not resolve finite-rate or unsteady extinction chemistry and is not suitable for predicting intermediate species such as CO. Accordingly, the CFD results are used only to support interpretation of the macroscopic flow and temperature fields, rather than to confirm the chemical extinction mechanism or gas-species evolution.

The thermal boundary treatment represents a further limitation. The CFD model employed uniform zero-thickness external-convection boundaries and did not separately resolve the 5 mm glass–ceramic observation doors, wall conduction, thermal inertia, insulation, or radiative heat transfer. The simplified treatment may introduce compensating biases: neglecting the thermal resistance of the insulated wall may overestimate heat removal, whereas omitting radiation may underestimate energy transfer from the hot gases and flame to the boundaries. Their partial compensation may help explain the overall agreement of the centerline temperature profiles, but this interpretation remains unverified because no radiation-enabled or material-resolved sensitivity analysis was performed. Consequently, the quantitative temperature predictions should be interpreted within the present reduced-scale configuration, particularly near the ceiling and for shallow-fire conditions. Future work will incorporate material-resolved wall conduction, insulation, and radiation models, together with targeted measurements of wall temperature and radiative heat flux, to separate and quantify these effects.

In addition, uncertainties associated with the heat-release-rate input should be noted. The HRR values reported in Table 2 were computed from the condition-specific fuel mass-loss-rate histories using an effective heat of combustion and an assumed combustion efficiency χ . In the present work, χ was taken as 1.0 due to the lack of time-resolved gas composition data or oxygen-consumption calorimetry to quantify incomplete combustion under oxygen-limited burning. As a result, the calculated Q may overestimate the effective heat release in sealed cases, introducing uncertainty into the Q -dependent correlations and characteristic-time predictions. Future work should incorporate gas-species measurements and/or oxygen-consumption calorimetry to constrain $\chi(t)$ and thereby refine the HRR input and related quantitative correlations.

The applicability of the empirical correlations is also limited by their calibration datasets. Equations (7) and (8) were obtained from 12 decay coefficient values after excluding the shallow $h = 0.45$ m cases, and Equation (10) was derived from the 14 cases in which self-extinction was observed. All tests used ethanol pool fires in the same 0.6 m diameter, 3.0 m depth, and 1:26 reduced-scale shaft under a sealed ceiling. The correlations should therefore be regarded as configuration-specific descriptions rather than universal plume or extinction laws.

The present study considered only naturally buoyancy-driven open conditions and unventilated sealed conditions. The effects of mechanically controlled supply and exhaust airflow were not investigated. Future work should examine different ventilation rates and supply/exhaust arrangements before the present findings are applied to mechanical smoke-control design.

The experimental program was conducted in a single 1:26 reduced-scale facility and did not include larger-scale validation. Consequently, the measured temperatures, self-extinction times, and empirical correlations should not be transferred directly to full-scale smoke-control design. Larger-scale experiments incorporating representative wall thermal inertia and controlled supply/exhaust or smoke-extraction conditions are required before engineering application.

5. Conclusions

This paper investigated the influence of a sealing boundary on smoke/heat transport and flame self-extinction in a 1:26 reduced-scale deep vertical shaft using a combination of reduced-scale experiments and CFD simulations. The effects of fire source depth h and heat release rate Q (represented by the quasi-steady HRR level for each test condition) were analyzed. The main conclusions are summarized as follows:

- (1) Sealing modifies the dominant transport mode from free exhaust to ceiling-impingement-induced return flow and global recirculation. Across the 20 matched open-sealed pairs, the characteristic upper-shaft centerline temperature rise increased in every sealed case, with enhancement values ranging from 43.2% to 374.9%. The detailed $h = 2.85$ m histories show that the sealed cases continue heating before extinction, consistent with the recirculation-driven heat accumulation observed in the flow and temperature fields.
- (2) Under sealed conditions, the plume centerline temperature rise exhibits a segmented vertical structure: a smoke-free region below the fire source, a plume-rise region within approximately 0.75 m above the source where a power-law decay holds after virtual-origin correction, and (when present) an upper well-stratified hot-smoke layer. The decay slopes in the plume-rise region are systematically less negative than the ideal unconfined value ($-5/3$), consistent with reduced effective entrainment caused by confinement-driven accumulation/recirculation. A unit-consistent empirical correlation for the decay coefficient k was established as a function of Q and h within the tested range.
- (3) Under sealing, flame self-extinction was preceded by a distinct “ghosting” stage and occurred with fuel remaining. The observed flame behavior, temperature evolution, and simulated recirculation patterns are consistent with a recirculation-buffer-volume interpretation in which reduced fresh-air entrainment may contribute to extinction. However, because time-resolved O_2 , CO , and CO_2 concentrations were not measured, local oxygen depletion at the flame base was not directly demonstrated; the oxygen-limited explanation should therefore be regarded as a plausible interpretation rather than a confirmed mechanism.
- (4) To connect the above mechanism to the observed parametric trends, an order-of-magnitude characteristic timescale t_{o_2} was introduced and recalculated using the same representative HRR levels used in the self-extinction correlation. The original 14-point nonlinear regression of t_{ext} yielded $R^2 = 0.9542$ and an adjusted $R^2 = 0.9458$, and it was recast as the dimensionless ratio in Equation (10) without refitting. The remaining discrepancy between t_{o_2} and t_{ext} reflects the simplified well-mixed assumption, spatially non-uniform vitiation, plume entrainment, and non-constant HRR. Equation (10) should be applied only within the calibrated reduced-scale sealed conditions; larger-scale validation is required before transfer to full-scale engineering or mechanical smoke-control design.

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