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Fire Ignition and Propagation in Hidden Zones of Aircrafts: A Novel Confined Fire Apparatus (CFA) for Flame Spreading Investigation

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Abstract: This research investigated potential fire hazards originating in hidden areas of pressurized sections of aircrafts. The objective was to establish a laboratory-scale flammability test method to predict the behavior of fire propagation under real fire conditions. A confined fire apparatus (CFA) was designed and constructed, and several tests were conducted to better understand the involved mechanisms and their consequences and to estimate flame spreading in hidden-zone fires. The experimental facility and flame-spreading results obtained for a typical material involved in hidden fires, specifically a ceiling panel, were presented and discussed. The experimental facility consisted of a narrow passage where a fire was initiated using a burner on a specimen exposed to a controlled heat flux. Experiments were conducted in the absence of forced airflow. Flame spreading was estimated through visual monitoring of fire development or temperature measurements at specific locations in the specimen. Both methods yielded similar results. The flame spread velocity in relation to the imposed heat flux allowed for the estimation of the critical heat flux for spreading $\dot{q}_{sp,cr}''$ and for ignition $\dot{q}_{ig,cr}''$; the corresponding temperatures, $T_{s,min}$ and T_{ig} ; and the flame spread parameter Φ .

Keywords: cone calorimeter; flame spread; external radiation; flammability properties; carbon/Nomex honeycomb composite



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1. Introduction

In-flight fires in pressurized zones are relatively common, with most being rather trivial. However, they all have the potential to lead to the catastrophic loss of the aircraft and the loss of lives. Fires originating in hidden areas can quickly become uncontrollable, resulting in the loss of life, as seen in the case of Swissair Flight 111 on 2 September 1998. In that incident, a fire started above the ceiling and quickly spread, degrading aircraft systems and the cockpit environment, ultimately leading to the loss of control of the aircraft [1]. In-flight fire incidents are typically caused by electrical failures, overheated equipment, cabin layout, power plant installations, or improper cargo [2].

Serious in-flight fires usually occur in hidden areas such as above the cabin ceiling, behind side walls, beneath the floor, or in electronics and electrical bays, where access is difficult, and fire suppression is challenging. Airbus currently follows ABD 0031 (replacing ATS 1000.001) specifications, which outline fire-worthiness design-criteria for materials used in the pressurized section of the fuselage, in Airbus commercial aircraft. The Federal Aviation Administration (FAA) has conducted research to develop more stringent fire test methods for materials located in hidden areas. The focus is on upgrading materials such as thermal-acoustic insulation, ducts, wires, and panel closeouts to enhance their fire resistance to reduce the likelihood of in-flight fires occurring in hidden areas. Previous flammability test methods, such as the 12-second vertical Bunsen-burner test (12-VBB) [3], were deemed inadequate to accurately predict fire propagation behavior under realistic fire

conditions. They were unable to differentiate materials that would allow or prevent fire spread when exposed to a typical fire source.

The FAA has been researching and updating tests related to the flammability properties of materials in hidden areas, including thermal/acoustic insulation [4,5], ducting [3], and electrical wiring [6,7]. Full-scale or intermediate-scale fire (ISF) tests in the overhead area are often used to study material behavior using actual specimens exposed to a block-of-foam fire source [3,8]. Supplementary tests such as smoke, heat release, and microscale combustion calorimeter tests are also employed to expand knowledge of the flammability properties of materials. While full-scale or intermediate-scale tests are considered reliable for assessing material flammability characteristics for use in aircraft, they are not suitable for certification due to their size, time-consuming nature, and cost. Therefore, there is a need to define laboratory-scale tests that provide similar assessment capabilities but are more efficient to perform.

The FAA has developed a lab-scale configuration, known as the radiant heat panel (RHP) apparatus with an impinging burner, for certification purposes to determine flammability characteristics such as fire propagation and after-flame time [3,7,8]. Specific test conditions and procedures were defined for each material family to correlate with the results of the corresponding ISF tests. Geometry characteristics such as material thickness and thermal properties such as thermal conductivity are important variables in these tests. Therefore, the test parameters were adjusted to account for composite materials with varying properties and characteristics (warm-up time, flame-exposure time, radiant-heat energy, etc.).

Quintiere et al. [9] investigated the flammability of carbon-fiber composite materials for aircraft structures. They measured flame spreading using a flame spread apparatus [10], which involved promoting spread through preheating by a radiant panel. The FAA also employed an intermediate-scale test to develop a flame propagation test method for structural composite materials in inaccessible areas [8], and the results obtained were correlated with corresponding tests using the RHP apparatus. In the past decade, the FAA has developed the vertical flame propagation apparatus [7,11,12] with the goal of establishing a flame propagation test method for inaccessible area materials. Current regulations do not require flammability testing for fuselage skins or structures in traditional designs, as they are inherently non-flammable.

Flame spread over solid combustibles has been a subject of significant attention, with numerous experiments and models exploring theoretical approaches and practical applications. The influence of various factors on flame spread rate has been extensively studied, including environmental conditions [13–18] (such as air velocity, direction, oxygen concentration, pressure, and incident radiation intensity) and geometrical characteristics [13,19–23] (such as surface orientation, initial temperature, thickness, and width). In real fire scenarios, an external heat flux is typically present during the flame spread process. Many studies have focused on investigating the impact of the imposed external heat flux on flame spread rate. Williams [24], Fernandez-Pello [16,25], Hirano [26], Kashiwagi [27], and Magee [13] conducted significant experimental studies to explore the influence of radiation intensity on flame spread rate over solid fuels. They showed that the effect of external radiation is primarily integrated into the initial temperature increase. Kashiwagi [28], Fernandez-Pello [29], and Quintiere [30] also examined the effect of the duration of exposure to a constant heat flux and found that flame spread rate over the surface of a thermally thick material increases with longer preheating time. Quintiere [30] investigated the rate of lateral flame spread and piloted ignition under externally imposed heat fluxes and concluded that increasing the imposed radiation enhances the flame spread rate and decreases the time to ignition.

Preventing and controlling flame spread in real fire scenarios remains an active area of research. While significant progress has been made, the development of laboratory-scale apparatuses capable of simulating different fire scenarios in aircraft, thus representing real conditions, could greatly reduce the time and equipment required for

assessing potential fire hazards during the design phase. This would also facilitate the development of new products with improved fire performance. Additionally, as different factors can significantly impact material behavior and performance in fire, research in this direction has the potential to enable both material characterization and the evaluation of their behaviors under varying conditions.

The objective of this research was to establish a laboratory-scale flammability test method to predict fire propagation behavior in the hidden and inaccessible zones of aircraft under real fire conditions. Flame spread over solid combustibles significantly contributes to fire growth. While the fail/pass nature of FAA/EASA tests is suitable for certification purposes, their contribution to understanding the fire behavior of materials and structures is limited. To address this, a confined fire apparatus (CFA) was designed and constructed to experimentally identify the involved mechanisms and their consequences and to estimate flame spread rates for specific fire scenarios in the hidden zones of aircraft at an early stage. The CFA setup allows for the control of airflow velocity and composition, the monitoring of incident heat flux and preheating time, the adjustment of impinging burner power and duration, and specimen orientation in both horizontal and vertical directions at different inclinations. Flame spread is monitored using a digital camera and appropriately positioned thermocouples. The CFA, with support from cone calorimeter-type instrumentation, enables the direct measurement of the fire-reaction properties of materials involved in specific fire scenarios, including heat release rate, smoke, and CO₂ and CO production. Flue gases can also be directed through a heated tube to an FTIR analyzer to identify gases released during the combustion process.

This study describes the CFA and presents and discusses the results obtained for a composite material used as a ceiling panel in aircraft cabins. Piloted ignition tests were conducted under different incident heat fluxes and preheating times, with a horizontal facing-up orientation. Flame front, v_f , and flame tip, v_t , spread rates were estimated using optical and thermal probing measurements. Flame spread properties including critical surface heat flux, $\dot{q}_{sp,cr}''$, minimum surface temperature, $T_{s,min}$, and the flame spread parameter, Φ , were calculated in accordance with the theoretical analysis of the flame spread process to assess the predictive capability of available models and correlations.

2. Materials and Methods

2.1. Methods

Confined Fire Apparatus

Figures 1 and 2 illustrate the schematic of the confined fire apparatus (CFA). This closed and airtight system enabled the control and monitoring of the inlet and outlet conditions. Similar to the forced-ignition and flame spread test [17,18], the CFA consisted of a small-scale metallic combustion wind tunnel with thermally insulated walls. The test section measured 1 m in length, 0.40 m in width, and 0.10 m in height. The inlet and outlet air flows were monitored under both free- and forced-flow conditions. To generate a uniform incident heat flux over the specimen surface and facilitate flame spread, a radiant heating source was positioned on one of the large walls of the test section. An electric radiant panel was selected for its stable operation within a wide range of heat fluxes. The test specimen, measuring 0.295 m in length and 0.195 m in width, was mounted flush on the wall opposite to the radiant panel. An impinging burner was utilized for pilot or sustained ignition. When the material was exposed to heat flux from the radiant panel, the surface temperature gradually increased, initiating pyrolysis and ignition when the burner was positioned on the specimen. Flame spreading was deemed to occur if the combined effect of heating from the panel and the flame raised the surface temperature of the area adjacent to the flame above the ignition temperature. Transparent, heat-resistant side windows allowed for visualization and video recording of the phenomena. A digital video camera (Nikon D7200) with a data acquisition frequency of 60 frames per second was chosen as a non-intrusive method to measure the flame spread rate. Flame development was recorded during the tests, and the visual images were subsequently analyzed. Four type-K thermocouples,

positioned at a spacing of 4 cm, were employed to measure temperature–time series and to estimate the flame spread rate using the second time derivative. Thermocouple leads were inserted from the non-exposed surface at a depth of 9 mm, to avoid uncertainties related to differences between surface temperature and temperature measured by thermocouples placed on the specimen surface, due to different emissivity.

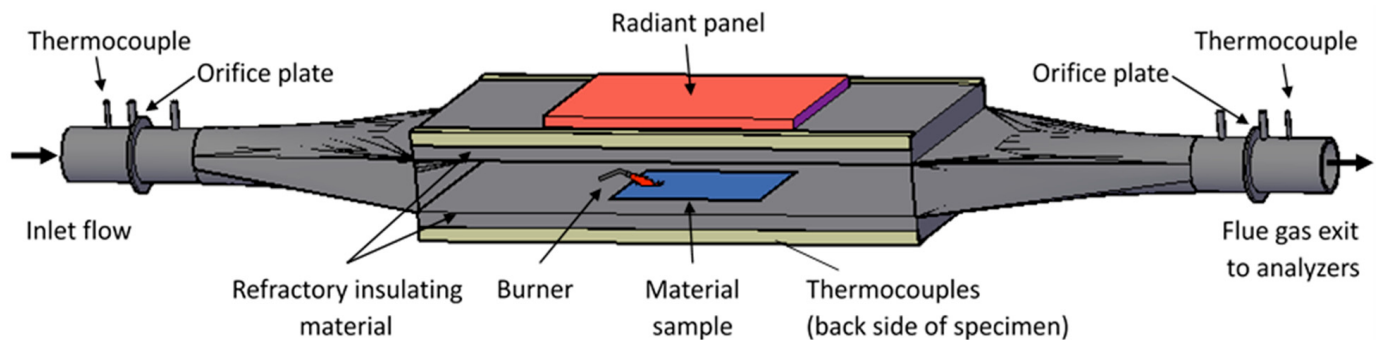


Figure 1. Simplified schematic view of the experimental apparatus.

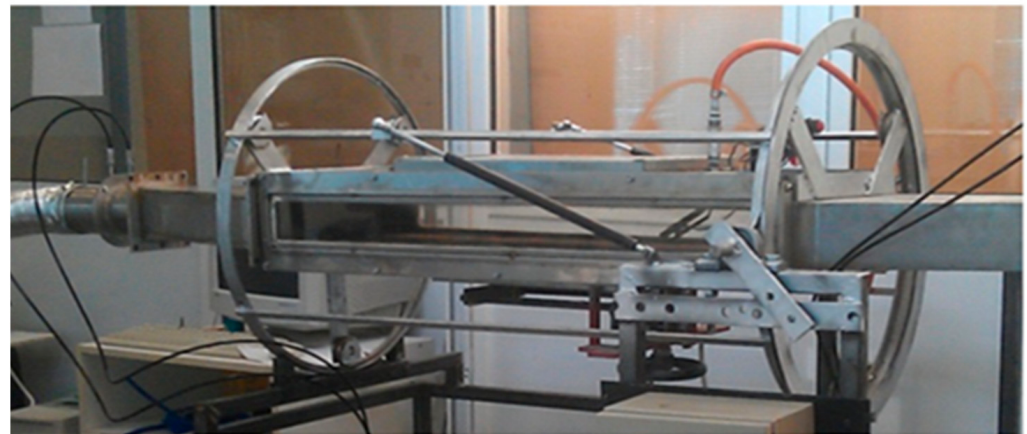


Figure 2. Confined fire apparatus (CFA).

A suitably designed mechanism allowed us to position the test section at any inclination relative to the vertical, either along the long or narrow side. This enabled opposed or concurrent flame spread tests in various orientations, including horizontal (facing up or down), upward, downward, lateral, and inclined flame spread. Mass loss was estimated by weighing the sample before and after each test. After-flame time, flame spread, and, if reached, burn-through were monitored through visual observation during experiments, and burn length and width were directly measured after the test.

The flue gases were directed to the hood of an FTT cone calorimeter, and its instrumentation was used to monitor several variables during tests, including heat release rate, carbon monoxide and dioxide production, and smoke production. A portion of the flue gases could also be directed to an FTIR analyzer to identify gases released during the combustion process. However, these results were not included in the present work.

2.2. Materials

The experiments were conducted using a multilayer composite material used as a ceiling panel in aircraft cabins. It is a composite material with a Nomex honeycomb core sandwiched between two carbon-fiber face sheets. A white layer is also adhered to the side of the panel facing the interior of the cabin, as depicted in Figure 3. The sample size was $29.5 \times 19.5 \text{ cm}^2$ ($\pm 2 \text{ mm}$), and the measured density was 158 kg/m^3 , with an average mass of 149 g ($\pm 0.2 \text{ g}$) and a thickness of 17.4 mm ($\pm 0.2 \text{ mm}$). Due to confidentiality requirements, as the material was provided by the aviation industry, specific details about

the material composition are not included. All samples were pre-conditioned for over 72 h in a controlled environment at a temperature of 23 ± 2 °C and relative humidity of $50\% \pm 2\%$, following ISO 554:1976 [31].

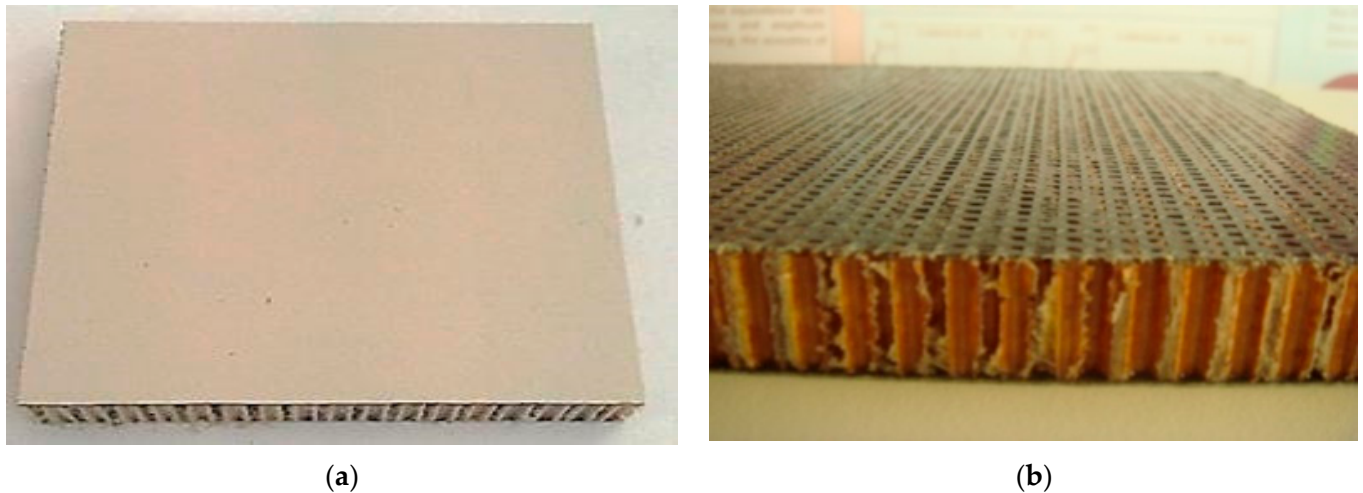


Figure 3. Test specimen front view (a) and back view (b).

3. Experimental Setup

Fire scenarios describe probable occurrences of aircraft fire incidents/accidents and are particularly suited for studying the impact of new materials on the fireworthiness of modern aircraft. Aircraft fire scenarios aim to protect vital components of the aircraft and contain the fire to prevent its propagation to the cabin, ensuring the survivability of passengers and crew until a safe landing can be made. From the perspective of the FAA, the scenario examined in this study is one of the most important in identifying risks associated with the use of new materials in aircraft. This scenario involved in-flight fires above ceiling panels within the aircraft, falling under the category of fires in hidden zones, where areas of the aircraft not directly accessible to the crew make it challenging to identify a fire source at an early stage, thus rendering extinguishing more difficult. The scenario envisioned a hidden fire occurring in the void above the cabin ceiling, resulting in the leakage of gases and smoke into the cabin.

In this study, the experiments were performed under free-flow conditions, with natural circulation due to buoyant forces, creating an approximate longitudinal air flow speed of 0.1 m/s in the chamber. The absence of forced flow and the flow pattern generated by the flame development itself qualified the type of flame spread as opposed flow rather than wind-aided flow.

Piloted ignition tests were conducted in the CFA, at different radiant heat fluxes and preheating times, to examine the effects of these parameters on heat transfer, pyrolysis, and flame spread mechanisms. Preheating time t^* is defined as the time elapsed from when the sample is placed in the chamber to the moment the burner is activated. The range of imposed heat fluxes for the experiments in the CFA apparatus was selected based on cone calorimeter measurements, which have been presented elsewhere [32]. Cone calorimeter tests revealed that combustion occurred between 40 to 70 kW/m², while at 30 kW/m², no ignition was observed. The material exhibited complex thermal decomposition and combustion processes at different heat fluxes in the cone calorimeter. Below 50 kW/m², one-stage combustion was observed, as the detachment of the upper layer of fibers during the fire inhibited heat transfer to the underlying material and the release of flammable volatiles in the fire zone. Burning behavior was significantly affected by the structure of the surface layer and the honeycomb core, as discussed in [33]. Thermal and flammability properties of the tested material, including thermal inertia $k\rho c$ and ignition temperature T_{ig} , were deduced from cone calorimeter measurements according to ISO 5660 and calculations based

on the correlation of ignition data, as described in [34–37]. These properties, estimated using the analytical procedure presented in [38,39], are shown in Table 1.

Table 1. Thermal and flammability properties of the ceiling panel [32].

ρ (kg/m ³)	k (W/mK)	c_p (J/kgK)	$k\rho c$ ((kW/m ² K) ² s)	T_{ig} (°C)
158	1.05	690	0.1136	596

Triple experiments were conducted in the CFA apparatus with a horizontal orientation using radiant heat fluxes of 25, 28, 30, 32, and 35 kW/m² for preheating times of 30 and 180 s. Flame spread on the backside of the sample (the side facing the overhead area of the aircraft) was investigated in a facing-up configuration, and flame propagation was monitored with a video camera. Flame spread can be considered as successive piloted ignitions with the flame itself as pilot. The pyrolysis area adjacent to the flame front is heated under the effect of the flame heat flux, heat transfer through conduction from the already burning material, or any other external heat source such as the incident heat flux of a radiant panel, leading to the process of progressive ignitions. These successive piloted ignitions appear as a moving flame front that can be measured visually. The flame front location is the progressing edge of the flame on the specimen surface, as shown in Figures 4 and 5, where x_f is its horizontal distance from the burner's impinging point. The flame tip is the corresponding upper edge of the luminous flame, where x_t is its distance from the impinging point. In the case of wind-aided flame spread, the flame tip extends over the unburned area, ahead of the flame front, intensifying the pre-heating of the material due to flame radiation, while in the case of opposed flame, the tip lags behind the flame front. The horizontal distance of the flame front, x_f , and the flame tip, x_t , were digitized on sequential frames and used to estimate the respective spread rates.

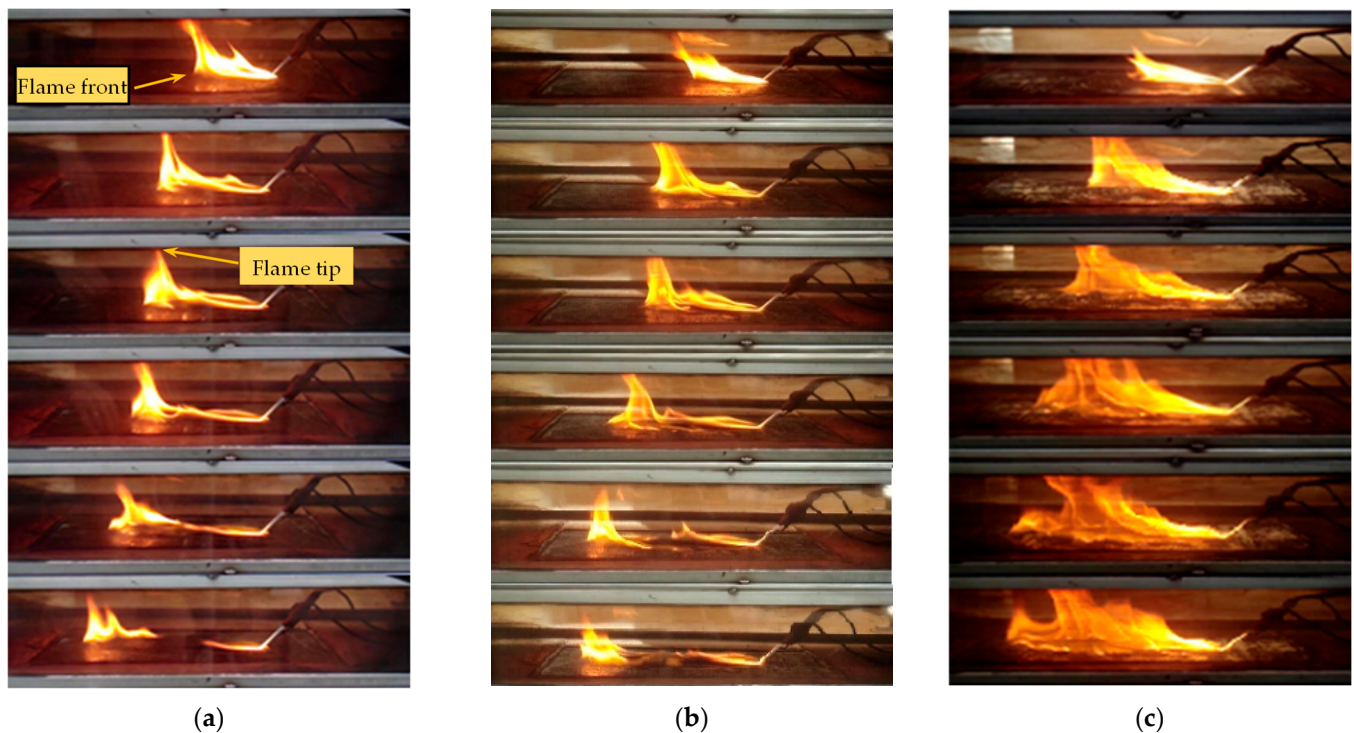


Figure 4. Frames recorded by high-definition camera at 30 kW/m² (a), 32 kW/m² (b), and 35 kW/m² (c).

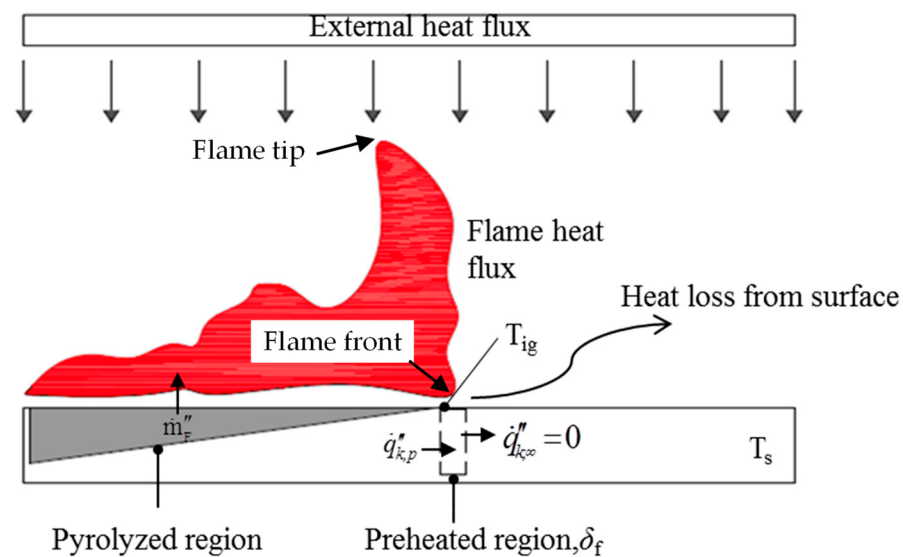


Figure 5. Flame spread model.

4. Results and Discussion

The effect of preheating time was examined by varying the duration of exposure to a constant radiant heat flux, and the results are presented in Table 2. Increasing the preheating time was expected to raise the surface temperature prior to ignition. Consequently, when the burner was activated, less time was required for the flame's heat input to raise the already elevated temperature of the unburned region to the ignition temperature. Therefore, preheating time was expected to impact flame spread, except when the material surface reached thermal equilibrium. However, the analysis of the experimental data revealed that the effect of preheating time on flame spread within the considered panel heat flux range was rather limited, with only small differences observed in flame spread velocity. As a result, only the cases with a preheating time of $t^* = 30$ s were considered. The analysis of flame spread theory in the subsequent section provides further insight into this matter.

Table 2. Experimental test cases and fire-spreading velocity values.

Heat Flux (kW/m ²)	Pre-Heating Time (s)	Flame Spread Rate, v_f (mm/s) (Visual)	Flame Tip Spread Rate, v_t (mm/s) (Visual)	Flame Spread Rate, $v_{f,T}$ (mm/s) (Thermocouples)
25	30/180	n.s. *		
28	30/180	n.s. *		
30	30/180	1.09/1.11	0.96/–	1.17/–
32	30/–	1.72/–	1.61/–	1.70/–
35	30/180	3.48/4.05	2.43/–	3.63/–

* no spread.

4.1. Flame Spread

Figure 4 illustrates flame development over the specimen at representative successive time intervals for the three incident heat fluxes. As the heat flux increased, the flames became richer and longer. There was no flame impingement on the unpyrolyzed area, and the flames seldom extended over it. The positions of the flame front, x_f , and the flame tip, x_t , at numerous time intervals for incident heat fluxes of 30, 32, and 35 kW/m² are shown in Figure 6. The data were reasonably approximated with linear fits, with R^2 values higher than 0.97 for the flame-front and over 0.88 for the flame-tip locations. Therefore, a steady flame spread rate was assumed for the different radiant heat fluxes, and the corresponding values obtained from the slope of the linear fit are summarized in Table 2.

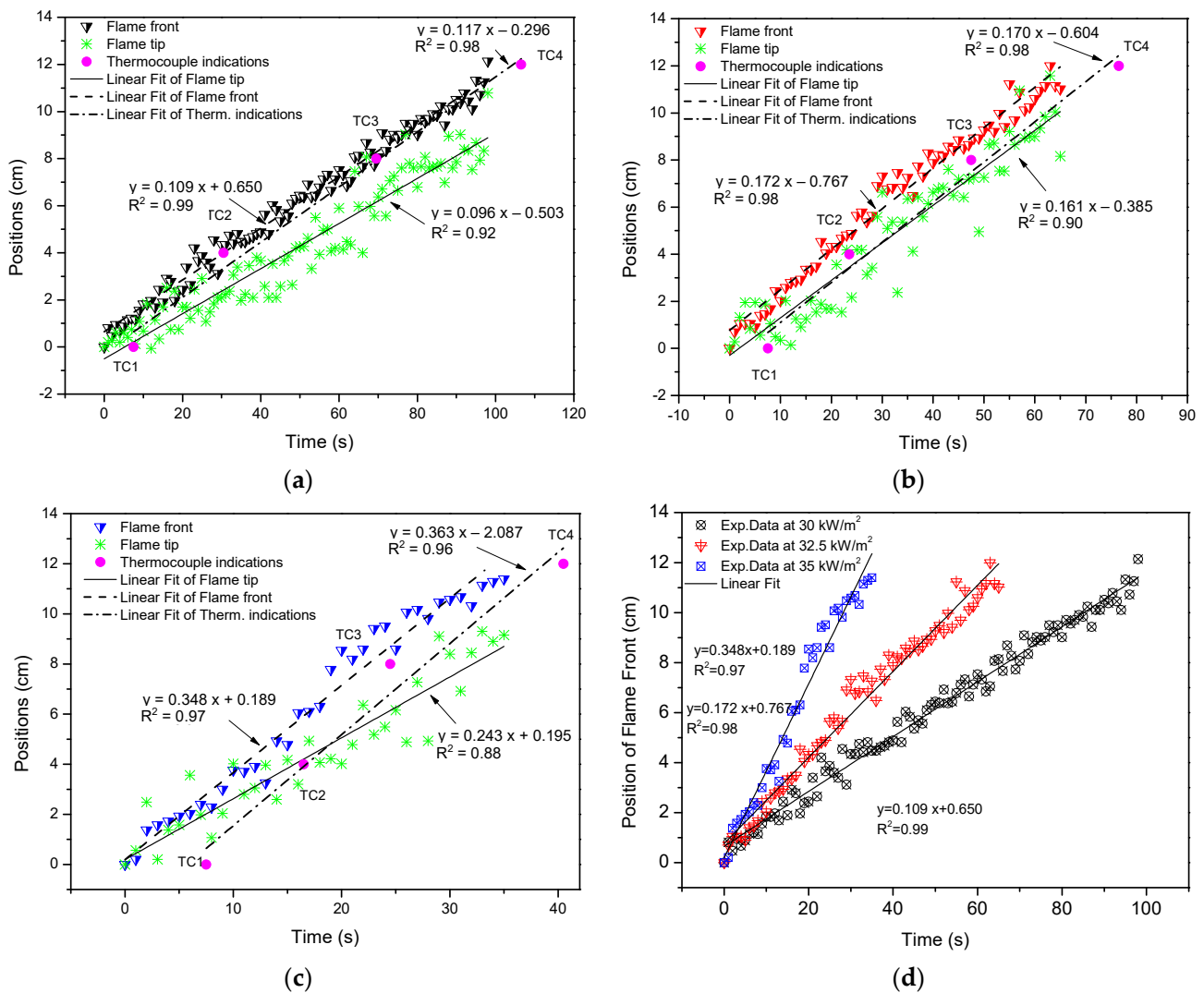


Figure 6. Flame front and tip locations in relation to time at (a) 30 kW/m², (b) 32 kW/m², and (c) 35 kW/m². (d) Flame front locations for all heat fluxes.

As previously discussed, when material is exposed to a radiant heat flux, it heats up, and its surface temperature increases at a rate determined by the balance between the imposed heating and the heat losses from the surface. If the surface temperature, due to the imposed irradiance level and the exposure time, is sufficiently high, the additional heat provided by a burner or a sustained flame is adequate to raise the temperature to the ignition temperature. Subsequently, a critical mixture of pyrolysis gases is generated and mixed with ambient air, and when this mixture falls within the flammability limits, ignition is initiated, either by the pilot burner or the advancing flame front. Therefore, for sustainable flame spread to occur, the surface must have a sufficiently high temperature, $T_{s,min}$, such that the heating provided by the flame itself to the adjacent pyrolysis area is sufficient to further raise the temperature to the ignition temperature. Thus, the minimum critical heat flux for flame spread, $\dot{q}_{sp,cr}''$, should balance energy losses due to radiation and convection for the surface temperature, $T_{s,min}$, given by the following equation [40]:

$$\dot{q}_{sp,cr}'' = \sigma(T_{s,min}^4 - T_{\infty}^4) + h_c(T_{s,min} - T_{\infty}) \quad (1)$$

Here, σ is the Stefan–Boltzmann constant, h_c is the convection heat transfer coefficient, and T_{∞} is the ambient temperature. This equation is obtained by considering a steady state energy balance of the surface heated by $\dot{q}_e''$ (the incident heat flux of the radiant panel)

outside the area heated by the flame. It is apparent that there is a critical heat flux $\dot{q}_{sp,cr}''$ or a $T_{s,min}$ for flame spread below which flame spread cannot occur. Experimental results demonstrated that flame did not spread for incident heat fluxes below 28 kW/m^2 , whereas it did for over 30 kW/m^2 , indicating that the minimum critical heat flux for flame spread, $\dot{q}_{sp,cr}''$, was between these two values. Values of $\dot{q}_e''$ lower than $\dot{q}_{sp,cr}''$ did not result in ignition and therefore flame spread.

The results presented in Table 2 and Figure 6 indicate that the flame spread rate, v_f , increased with an increase in panel radiation. Higher incident heat fluxes resulted in higher surface temperature, T_s , and the additional heat flux provided by the flame prior to ignition rapidly raised the surface temperature to the ignition temperature. Moreover, the larger overall heat input raised the surface temperature under the flame faster and pushed the pyrolysis front (at T_{ig}) deeper into the specimen. Pyrolysis proceeded more rapidly, resulting in a larger amount of pyrolysis gases being produced and burned. Therefore, the heat flux of the flame also increased, further promoting an increase in flame spread rate. As the incident heat flux increased, the sustainability and intensity of the flames, as depicted in Figure 4, clearly demonstrated an increase in pyrolysis depth. At a later stage, the production of pyrolysis gases from the lower layers was gradually diminished due to the inadequacy of the flame heat transfer to push the pyrolysis front deeper. Consequently, the flame heat transfer was further diminished, and the flame was extinguished over this region.

Figures 4 and 6 show that the flame front generally preceded the flame tip. Flame front v_f , and tip v_t spread rates yielded similar values at 30 and 32 kW/m^2 , whereas at 35 kW/m^2 , the flame front spread rate shifted to a higher value. These phenomena were probably associated with the flow conditions established in the combustion tunnel during these experiments, which were related to the absence of forced flow and the formation of free convection flow. The delay of the flame tip in relation to the flame front indicated that a backward-inclined flame was sufficient to provide the required pre-ignition heat flux for flame spread on the specimen surface. The larger amount of pyrolysis gases and the higher flame heat release, in the case of high-incident heat flux, appeared to be capable of supplying the required pre-ignition heat with the lower radiation form factor associated with a larger flame inclination.

The larger scattering of flame tip locations compared to flame front locations, as shown in Figure 6, and the corresponding R^2 factors, could be attributed to the turbulence induced by the flow and the resulting flame pulsation. As the imposed heat flux increased, the larger and quicker production of pyrolysis gases and the associated increase in heat release contributed to higher turbulent levels and increased scattering.

4.2. Temperature Profile Analysis

An array of four thermocouples placed at 4 cm spacing along the centerline, with their beads located 9 mm below the exposed surface, was employed. The temperature histories measured by the thermocouples were used to calculate the flame spread or “pyrolysis” rate as follows: Assuming the flame begins spreading at a location when the surface temperature reaches the ignition temperature [41,42], and the pyrolyzed gases ignite with the flame front considered as the source of pilot ignition, ignition at a location results in a step change in the heat transfer balance at the surface, increasing the heat input into the specimen.

The heat wave generated by this change propagated within the specimen and altered the trends of the transient temperature increase, which was established due to the pre-flame balance. Assuming one-dimensional heat transfer, the time lag for this wave to reach a penetration depth equal to the depth where the thermocouple beads were located was expected to be proportional to δ^2/α and therefore the same for all thermocouples.

The time difference between the arrivals of the wave at successive thermocouples, sensed by the peak in the second time derivative of the measured temperature history, was used to estimate the flame spread rate. Temperature plots with respect to time and their corresponding time derivatives for the three incident heat fluxes are presented on the left

side of Figure 7, while the right side shows the second time derivatives of the series. In the early stages, the temperature increased due to incident heating from the panel. The leftmost shaded area, referred to as the preheating stage, corresponded to the time interval during which the temperature increase was solely due to the imposed heat flux. In this period, the temperature curves for the four thermocouples overlapped for each imposed heat flux, indicating uniform heating of the specimen. As expected, higher heat fluxes resulted in steeper temperature increases.

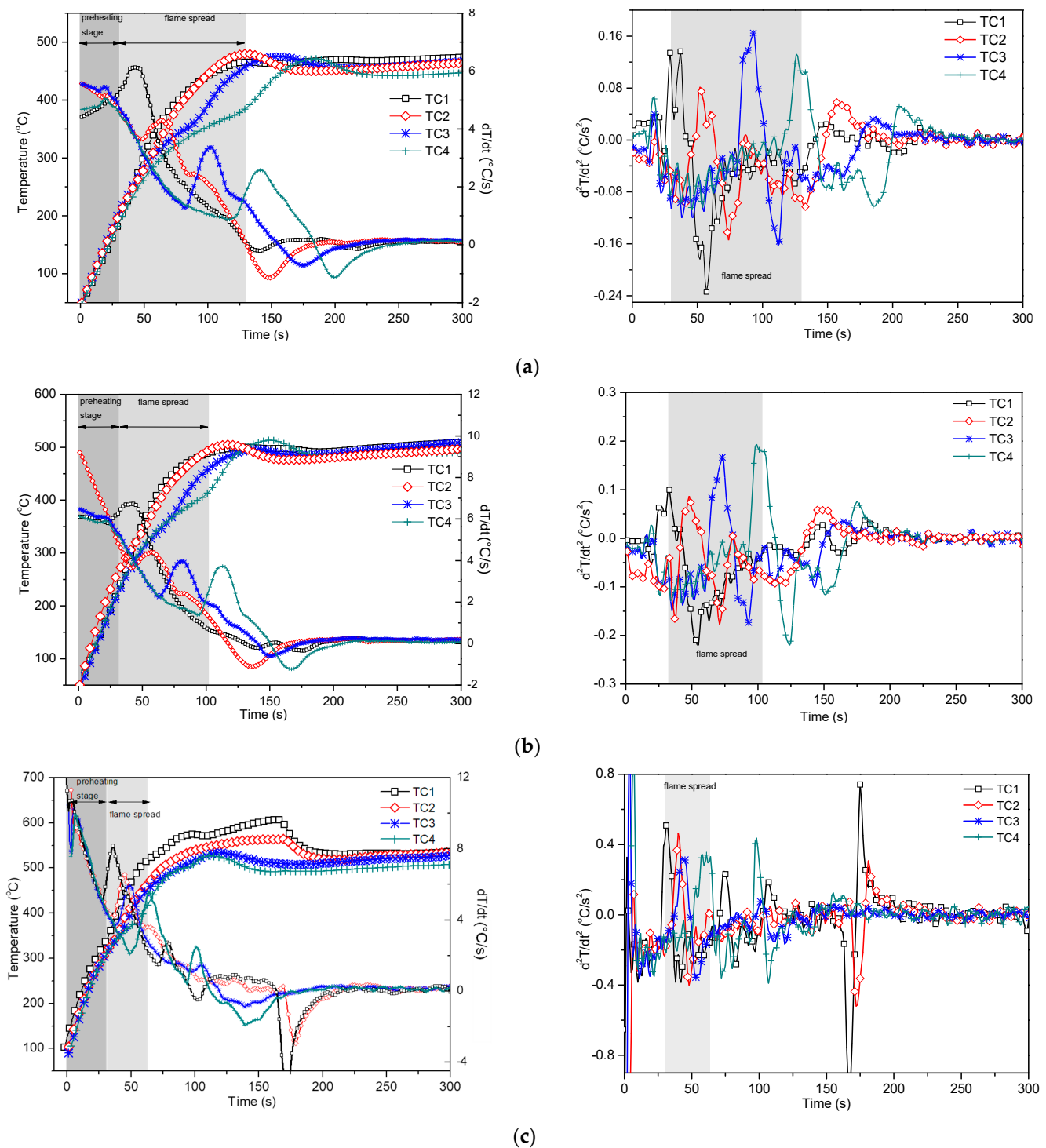


Figure 7. Temperature profiles and second time derivatives of temperature at 30 (a), 32 (b), and 35 kW/m² (c).

The second shaded area, denoted as the flame spread range, began when the first thermocouple detected the change in surface heat transfer due to flame ignition, as indicated by the peak in the corresponding second-derivative distribution. This shaded area ended when the last thermocouple was also affected. The data points indicating the time of the second-derivative peak for the four thermocouples, in relation to their positions, are included in Figure 6a–c, for the different incident heat fluxes. The data points were approximated with linear fitting curves, with R^2 values higher than 0.91, denoting a constant flame spread rate equal to the slope of the fitting curves. The flame-spread-rate values, $v_{f,T}$, measured in this manner, are included in Table 2.

At the end of the preheating stage, the inner temperature (at depth $\delta = 9$ mm) reached similar values for all four thermocouples at each heat flux, specifically 236 ± 9 °C, 277 ± 15 °C, and 336 ± 12 °C for 30, 32, and 35 kW/m² incident heat fluxes, respectively. These observations converged on the assumption that the surface temperature was almost constant or varied slowly along the surface ($\partial T / \partial x \approx 0$) just before the initiation of flame spread. The estimation of the minimum surface temperature, T_s , for each heat flux was obtained by employing energy conservation for a control volume that surrounded the preheated area in the solid phase around the TC4 thermocouple, fixed at the surface and extending in depth $\delta = 9$ mm at a distance far enough ahead of the region unaffected by flame heat transfer and heat conduction from the pyrolysis region, $\dot{q}_{k,p}''$. Thus, flame heat flux and conduction from the virgin material were considered negligible ($\dot{q}_f'' \approx 0$ and $\dot{q}_{k,p}'' \approx 0$, respectively). Additionally, since T_s was assumed to be constant or vary slowly, conduction to the virgin material was neglected ($\dot{q}_{k,\infty}'' \approx 0$), and at $y = \delta_T$, $\dot{q}_{loss}'' \approx 0$ for thick materials [36]. With these assumptions, the net incident heat flux was approximated using the energy balance equation with the surface temperature T_s :

$$\dot{q}_{net,o}'' = \dot{q}_e'' - \sigma(T_s^4 - T_\infty^4) \quad (2)$$

In the above equation, the convection heat losses were neglected since radiation heat losses dominated at elevated temperatures. The heat conduction in the solid phase was obtained from the following equation:

$$\dot{q}_{net,o}'' = -\left(k \frac{\partial T}{\partial y}\right)_{y=0} \approx k \frac{T_s - T_{TC}}{d} \quad (3)$$

To estimate the surface temperature, the average temperature values from the thermocouple measurements at each heat flux were used. The experimentally calculated results are shown in Table 3, compared with the predicted values obtained by employing the flame spread theory discussed in the next section.

Table 3. Surface temperature based on temperature experiments and flame spread theory.

$\dot{q}_e''$ (kW/m ²)	T_s (°C, Exp. Calculated)	T_s (°C, Predicted)
30	478	531
32	514	546
35	572	564

4.3. Flame Spread and Heat Transfer Analysis

Figure 5 illustrates the heat-transfer mechanisms occurring during the flame spread process over the ceiling panel subjected to external heating. The control volume surrounds the preheated area ahead of the flame front, is fixed at the pyrolysis front point, spans the flame heated length δ_f , and extends to depth δ_T . T_s represents the surface temperature of the solid that is not affected by flame heating. It is assumed that heat transfer from the surroundings to the control volume includes radiation from the already burning material, the external heat flux from the radiant panel, and conduction through the solid [36,43].

Considering that T_s is constant or varies slowly with x ($\partial T / \partial x \approx 0$), as demonstrated in temperature measurements and suggested in Quintiere's model [30,36], conduction from the control volume to the virgin material can be neglected ($\dot{q}_{k,\infty}'' = 0$). Additionally, although the importance of $\dot{q}_{k,p}''$ can be significant, the dominant heat transfer is the flame heat flux, $\dot{q}_f''$, even under slow opposed spread conditions [36]. Assuming a constant pyrolysis rate (as approximately derived from our experimental results) due to the absence of flame impingement on the unpyrolyzed area, the following energy balance can be derived for the control volume:

$$\rho c_p v_f \int_0^{\delta_T} (T - T_\infty) dy = \int_0^{\delta_f} (\dot{q}_f'' - \dot{q}_e'') dx \quad (4)$$

where the incident heat flux from the radiant heater, $\dot{q}_e''$, is considered constant due to a long radiant pre-heating period. Thus, following Quintiere's approximation [36] for the thermal gradient in the y -direction, the temperature integration is given as

$$\int_0^{\delta_T} (T - T_\infty) dy = \delta_T (T_{ig} - T_\infty) / 3 \quad \text{where} \quad \frac{T - T_\infty}{T_{ig} - T_\infty} = \left(1 - \frac{y}{\delta_T}\right)^2 \quad (5)$$

A rational approximation for the thermal penetration depth is $\delta_T = C\sqrt{\alpha t}$, where α is the thermal diffusivity, $\alpha = k / \rho c_p$, and the C factor is approximately 2.7 [36]. Substituting this into Equation (2) gives

$$v_f \approx \frac{\left[\int_0^{\delta_f} (\dot{q}_f'' + \dot{q}_e'') dx\right]^2}{0.81 k \rho c_p \delta_f (T_{ig} - T_\infty)^2} \quad (6)$$

The heat transfer analysis based on Quintiere's flame spread model for thermally thick materials [36] proposes the following equation, which is applicable to the LIFT apparatus [44] where external heating of the material is also applied:

$$v_f \approx \frac{\Phi}{k \rho c (T_{ig} - T_s)^2}, \quad T_s > T_{\min} \quad (7)$$

where Φ is the flame spread parameter $\Phi = \frac{4}{\pi} (\dot{q}_f'') \delta_f$, and T_∞ is generalized to a heated material, originally at T_s . The assumptions implied in this model include the following: (1) $\dot{q}_f''$ is constant over the preheated region, δ_f ; (2) v_f is constant; (3) $\dot{q}_{loss}'' = 0$ from the rear surface since the back face is insulated; (4) there is negligible surface heat loss, as $\dot{q}_f'' \gg \sigma T_{ig}^4$. For h_t approximated as a constant and assuming that T_s is the result of long-time heating of the material, that is, $\dot{q}_e'' = h_t (T_s - T_\infty)$, then the spreading velocity equation can be alternatively given in the following form [36]:

$$v_f^{-\frac{1}{2}} = \left(\frac{k \rho c_p}{h_t^2 \Phi}\right)^{1/2} (\dot{q}_{ig,cr}'' - \dot{q}_e'') \quad (8)$$

where $\dot{q}_{ig,cr}''$ is the critical heat flux of ignition, and h_t is the combined heat transfer coefficient. As the heat flux increases, T_s achieves higher values. In the limited case where the imposed heat flux, $\dot{q}_e''$, reaches the ignition critical heat flux, $\dot{q}_{ig,cr}''$, and therefore the surface temperature, T_s , reaches the ignition temperature, T_{ig} , Equation (8) implies a very high velocity, v_f , and in the presence of a pilot flame, combustion on the entire surface of material will occur, as in cone calorimeter experiments [32]. The suitability of Quintiere's flame spread model with the present results was examined next.

Flame spread rate as a function of the external heat flux was plotted, as shown in Figure 8, based on the experimental results for both 30 s and 180 s preheating times. The intercept of the linear fitted line with the x -axis, which corresponded to zero flame spread

rate, gave a value of approximately 28 kW/m^2 for both lines, indicating no flame spread below this threshold. This result agreed with the experimental tests, which showed that $\dot{q}_{sp,cr}''$ was expected to lie between 28 and 30 kW/m^2 .

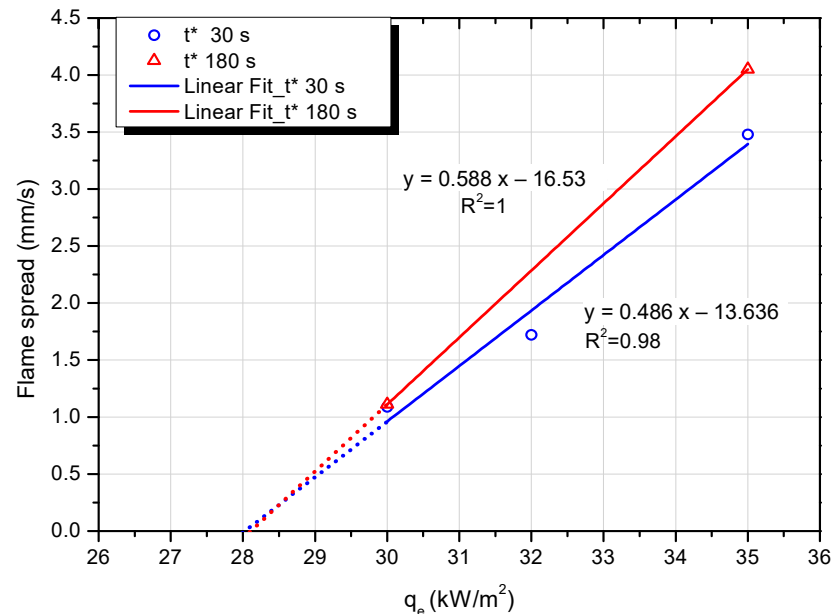


Figure 8. Flame spread rate in relation to incident heat flux.

Next, the calculation of the heat transfer coefficient was deduced to estimate the minimum surface temperature for spreading according to Equation (1). It was expected that free-flow natural conditions prevailed in the combustion chamber. Therefore, for laminar natural convection over a hot, flat plate, the average Nusselt number was calculated based on the following equation [21,45]:

$$\text{Nu}_L = hl/k = 0.54(\text{Ra})^{1/4} \quad (9)$$

where the Rayleigh number, Ra , is given as $\text{Ra} = \text{Gr Pr} = \frac{gl^3\beta\Delta T}{\nu\alpha}$, and Gr and Pr are the Grashof and Prandtl numbers, respectively. The parameters l , β , ν , and α are the characteristic length, thermal expansion coefficient, kinematic viscosity, and thermal diffusivity, respectively. Air properties were evaluated at the arithmetic mean or film temperature, $T_m = (T_{ig} + T_\infty)/2 \approx 310^\circ\text{C}$, and the characteristic length was given as the ratio of area to perimeter, $l = A/P \approx 0.059 \text{ m}$. The heat transfer coefficient was obtained from Equation (9) as $h_c = 13.29 \text{ W/m}^2 \text{ K}$. Then, for $\dot{q}_{sp,cr}'' = 28 \text{ kW/m}^2$ and $T_\infty = 30^\circ\text{C}$, the minimum surface temperature for spreading was estimated as $T_{s,min} = 514^\circ\text{C}$, while the combined heat transfer coefficient for convection and radiation was calculated as $h_t = 57.9 \text{ W/m}^2 \text{ K}$. The corresponding values of surface temperature T_s at different radiant heat fluxes were also calculated and are presented in Table 3 along with the respective experimental values based on the temperature method. The values obtained by the temperature method yielded lower values at lower incident heat fluxes (30 kW/m^2), where the temperature difference was higher (approximately 50°C), whereas at higher heat fluxes, the difference was reduced to below 32°C .

The pseudo-property Φ , introduced in Quintiere's model [30,36], represents flame spread behavior and can be calculated, in accordance with Equation (8), from the slope of the linear regression of the plot of $v_f^{-1/2}$ vs. $\dot{q}_e''$, as shown in Figure 9, for both preheating times. A negative linear relationship was illustrated, consistent with Equation (8) and steady velocity data. The intercept with the abscissa provided $\dot{q}_{ig,cr}''$, which had an average

value of 40.9 kW/m^2 , and the critical temperature for ignition was estimated as follows, similar to Equation (1):

$$\dot{q}_{ig,cr}'' = \sigma(T_{ig}^4 - T_{\infty}^4) + h_c(T_{ig} - T_{\infty}) \quad \dot{q}_{ig,cr}'' = \sigma(T_{ign}^4 - T_{\infty}^4) + h_c(T_{ign} - T_{\infty}) \quad (10)$$

For $\dot{q}_{ig,cr}'' = 40.09 \text{ kW/m}^2$ and $T_{\infty} = 30 \text{ }^{\circ}\text{C}$, and for the estimated heat transfer coefficient, $h_c = 13.29 \text{ W/m}^2 \text{ K}$, for free convection, the ignition surface temperature based on Equation (8) was calculated as $T_{ig} = 604 \text{ }^{\circ}\text{C}$, which closely matched the ignition temperature of $596 \text{ }^{\circ}\text{C}$, estimated from cone calorimeter experiments. The combined heat transfer coefficient for convection and radiation in this case was $h_t = 71.3 \text{ W/m}^2 \text{ K}$. Using this value as a constant, as suggested in [35], along with the value of the thermal inertia value, $k\rho c_p$, provided in Table 1, Φ was estimated, in accordance with Equation (8), from the slope, C , of the line fit at 30 s of preheating time, as shown in Figure 9:

$$\Phi = k\rho c_p / (h_t C)^2 \approx 2.79 \text{ kW}^2/\text{m}^3 \quad (11)$$

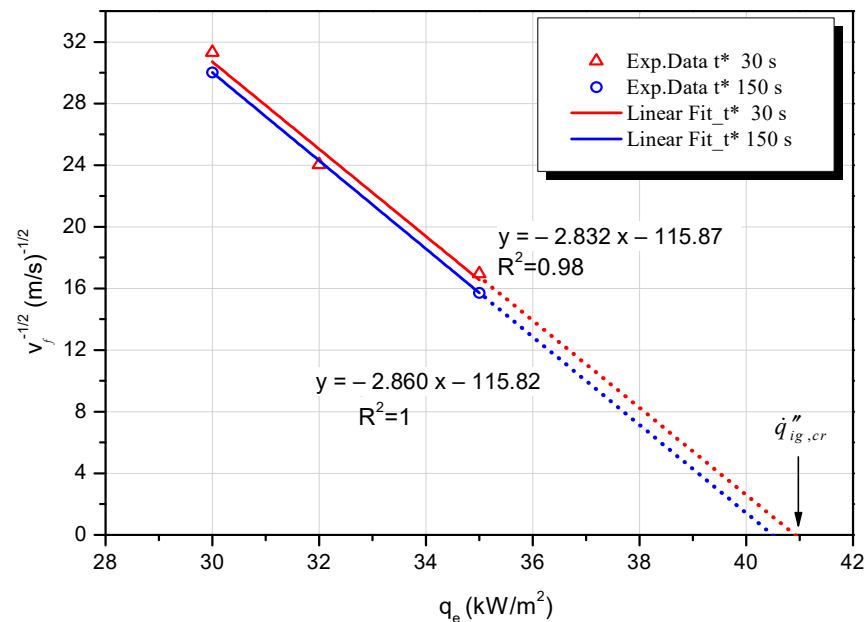


Figure 9. Inverse of the square of flame spread at different irradiant heat fluxes.

5. Conclusions

In this study, a novel confined fire apparatus (CFA) was designed and constructed to investigate flame spread in confined spaces, such as the hidden zones of aircraft. Flame spread on a carbon/Nomex honeycomb composite was studied in the CFA apparatus, in free convection conditions, under the influence of different incident heat fluxes (30, 32, and 35 kW/m^2) and preheating times (30 and 180 s) and analyzed in terms of pyrolysis, flame-spread, and heat-transfer mechanisms.

It was observed that the increase in external heat flux affected the flame spread rate, influencing the initial temperature (T_s) of the solid. Time to ignition decreased as the heat flux increased, and as the time to reach the ignition temperature of the already elevated material temperature decreased. An increase in pyrolysis depth with the heat flux was also observed through the sustainability and intensity of the flames. Pyrolysis proceeded more rapidly, resulting in a larger amount of pyrolysis gases being produced and burned, leading to a higher flame heat flux and an increased flame spread rate.

In these experiments, the preheating time had a limited effect on the flame spread rate, except for 35 kW/m^2 with 180 s preheating, where a slightly higher value was observed. The calculation of flame spread rate based on image analysis and temperature–time series yielded similar values and demonstrated a linear behavior. The low values of flame spread

velocity indicated that this material exhibited good fire performance. Surface-temperature estimation using temperature data was also achieved, and the results closely matched the corresponding estimations based on heat transfer analysis, except for lower heat fluxes (30 kW/m^2).

The heat transfer analysis of flame spread was approximated using the theory of opposed flame spread over a thick solid, considering that free-flow natural conditions prevailed in the combustion chamber, and the flame front generally preceded the flame tip. The predictive capability of the models and correlations adopted for the analysis was satisfactory, providing reasonable values for the ignition temperature, T_{ig} , minimum surface temperature, $T_{s,min}$, and critical heat flux for flame spread, $\dot{q}_{sp,cr}''$, in comparison with the experimental values.

The confined fire apparatus (CFA) and the implementation of the adopted methods and techniques successfully predicted the fire behavior of the tested material. The experimental results are currently used for the early-stage assessment of fire hazards related to specific materials used in the hidden zones of aircraft. This approach can be extended to materials generally located in confined spaces. Additionally, the flame spread parameters and properties calculated in this study can serve as input data for evaluating computational tools and models to predict the flame spread behaviors of materials in the case of a fire event in confined spaces in general.

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Nomenclature

c	Specific heat (kJ/kg K)	Greek	
k_{pc}	Thermal inertia ($\text{kW/m}^2 \text{K}^2 \text{s}$)	α	Thermal diffusivity (m^2/s)
k	Thermal conductivity (kW/m K)	δ	Sample thickness (m)
$\dot{q}_{ig,cr}''$	Critical heat flux for ignition (kW/m^2)	δ_{th}	Thermal depth (m)
$\dot{q}_e''$	Incident heat flux (kW/m^2)	ε	Emissivity (-)
T_o	Initial temperature (K)	ρ	Density (kg/m^3)
$T_{s,min}$	Minimum surface temperature for flame spread	σ	Stefan–Boltzmann constant ($\text{W/m}^2 \text{K}^4$)
T_{ig}	Ignition temperature (K)	δ_f	Flame heated length (m)
t_{ig}	Ignition time (s)	Φ	Flame spread parameter (kW^2/m^3)
b	Ignition parameter ($\text{s}^{-1/2}$)		
h_t	Average heat transfer coefficient ($\text{kW/m}^2 \text{K}$)		
$\dot{q}_{sp,cr}''$	Critical heat flux for spreading (kW/m^2)		
t	Time (s)		
t^*	Preheating time (s)	Subscripts	
v_f	Flame (front) spread rate (m/s)	ig	ignition
v_t	Flame (tip) spread rate (m/s)		

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