Impact of Firefighting Sprays on the Fire Performance of Structural Steel Members

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Received: 21 December 2021/ Accepted: 12 April 2022/Published online: 7 May 2022

Abstract. The feasibility of coupled computational fluid dynamics (CFD) and finite element (FE) simulations to aid the planning of fire intervention tactics and the effectiveness of structural cooling during firefighting were investigated. Water sprays, generated using fire monitors, were characterized using bucket tests and the results were used for calibrating the CFD spray model. The cooling ability of the sprays was measured experimentally by applying them onto a fire exposed steel beam. The experimental results showed that water application produces a sudden drop in steel temperatures and after 10–15 s, no significant further reduction in temperature was observed. The CFD-FE coupling was performed using the adiabatic surface temperature method, extended here to include the cooling by water droplets. The coupled CFD-FE model was validated using the experimental data and applied to simulate the intervention to a developing warehouse fire, showing how an attempt to cool the structure reduces the temperatures but does not stall the fire-spread. In fact, the intervention-induced vapor generation was found to enhance the flow of hot gases and accelerate the fire-spread if the water resources are inadequate. Thermal and stress analyses of the cooled and uncooled truss beams were performed, showing how the spray cooling halted the truss mid-span deformation.

Keywords: Firefighting, Water monitors, Fire spread, FDS, Abaqus, FDS2FEM, Adiabatic surface temperature

1. Introduction

Water sprays are one of the most common means of firefighting and firefighters attempt to use the available water in the most efficient way possible. However, the practical efficiency of a water spray in suppressing a fire depends on several factors such as the ability of the spray to penetrate the flames [1], the droplet sizes and the actual amount of water that effectively contributes to the extinguishment process [2].
In many situations, the amount of water required to control or suppress the fire is not known. Grant et al. [3] and Rasbash [4] have reviewed the investigations of critical water flow rate required for the extinction of liquid and solid fuel fires. Many of the reported studies were conducted at laboratory scales and employed very small water flow rates. Grant et al. reported that the practical water flow rate required for extinction is usually one order of magnitude higher than the experimental values [3]. The water required can be estimated based on various empirical relations [5–8] which assume a steady state fire. The dynamic nature of real fires makes the estimation of necessary extinguishment resources difficult. Also, the firefighting approach adopted also affects the necessary amount of water.

In general, there are two firefighting approaches that can be adopted based on the fire severity and resource availability. An offensive approach aims at extinguishing the fire as quickly as possible by attacking the seat of the fire either directly (fuel cooling by water sprays aimed at the base of the fire) or indirectly (water is aimed at the ceiling or wall such that it rains down on the flames from above). A defensive approach focuses on the containment of fire by cooling of the hot gases [9], ceiling and internal structures to disrupt radiative heating cycle, or by cooling the unburnt fuel to prevent fire-spread. The choice of the appropriate tactic determines the success of the firefighting operation and reduces the stress and risk encountered by firefighters. However, only few studies have analysed the water requirements and firefighting tactics for realistic, large-scale fires [10, 11]. Even fire intervention recommendations have significant differences across the world regarding the amount of water required for different firefighting operations [10]. Most studies are focused on residential buildings and safe evacuation of the occupants [12–14]. Särdqvist et al. analysed the different tactical approaches and the water flow densities used based on datasets from the UK fire and rescue services. They reported that mostly the required water is not estimated accurately, and that the defensive approach is commonly adopted for fighting large fires as offense requires more resources than containment [15].

One of the defensive actions that can be adopted is structural cooling where the aim is to cool the load bearing elements to prevent structure failure. But as sudden cooling of structural elements at high temperatures (> 600°C) can result in significant change in material properties and load bearing capacity [16, 17], it is unclear how the structural cooling affects the safety of the building. In large open-frame buildings (floor area > 500 m²) with unprotected structural elements, both the fire and the fire intervention may contribute to the risk of structural failure. There is a lack of research into firefighting tactics for such buildings as undertaking real-scale experiments requires significant time and outlay.

One way of studying real-scale fire interventions is through coupled numerical simulations of manual fire suppression and their influence on structural behaviour. The current state-of-the-art Computational Fluid Dynamics (CFD) modelling has been shown to reproduce the fire behaviour observed in a large compartment [18], and the coupled fire-structure interaction methodology, validated in [19], can then be extended for studying the response of the heated and cooled structural elements. In between these two steps of simulation, a water spray model needs to be included.
Water monitor systems, with adjustable flow rates and range, can deliver large amounts of water into precise locations without continuous operator presence making them a safer and more efficient option than handheld sprays. The influence of discharge flow and pressure [20], air resistance [21] and mean vector-velocity fields [22] on performance of water monitor systems have been evaluated. Also, several other studies have investigated the non-linear dynamics of the jet sprays from water monitors [23–27]. However, there is a need for characterizing the water distribution patterns from water monitor sprays to aid the development of computational models of fire suppression. Existing CFD studies on the interaction of water spray and fire have mainly focused on a single, isolated fire plume [1, 2].

In this work, we investigate the feasibility of using numerical simulations for the tactical planning of firefighting operations by studying the effectiveness of structural cooling as an alternative to the commonly used fuel cooling and gas cooling [9] approaches. The water monitor spray used in the simulations was characterized using bucket test experiments and the cooling power of the generated sprays was studied in experiments where a steel beam was first heated up by a pool fire and then cooled using a water monitor spray. The efficacy of the structural cooling tactic was investigated for a truss beam exposed to a fire-spread scenario within a large warehouse structure. Different fire intervention tactics were defined based on the discussions with firefighters from the Emergency Services Academy in Finland. The fire development and the selected ways of using water monitors were simulated using the modelling methods developed in [18] and [19] in Fire Dynamics Simulator (FDS) [28, 30]. The fire performance of a truss exposed to fire and spray cooling is analysed using the Finite Element (FE) solver, Abaqus [29].

2. Modelling

2.1. CFD Simulations Using FDS

Fire Dynamics Simulator (FDS) [30] is a Large Eddy Simulation (LES) -based Computational Fluid Dynamics code which solves low Mach number combustion equations in a rectilinear grid over time. The mass, species and momentum transport equations are explained in detail in the FDS technical reference guide [30].

The simulations in this study were performed with FDS version 6.7.6 using the Very Large Eddy Simulation (VLES) mode and Deardoff turbulence model. The radiation was modelled using the Finite Volume Method -based solver with 104 solid angles, gray radiative properties for gas, soot and droplets, and a radiative fraction of the local heat release rate equal to 0.35. The heat transfer within solids was computed using the one-dimensional heat conduction equation with radiative and convective heat flux boundary conditions [30].

In the validation simulations of Sect. 4.2, the fire boundary condition was modelled as prescribed fuel inflow boundary. In the fire-spread simulations, the fire load was modelled as stacked wood cribs, the pyrolysis of which was modelled using a combined ignition temperature-based pyrolysis and reaction kinetics-based
pyrolysis model developed in [18]. Specific details regarding the simulations are described in their respective sections.

Two different water sprays—narrow and wide—were modelled using Lagrangian particles dispersed within a continuum gas phase medium. The details of the Lagrangian model are described in [28]. Due to the uncertainties associated with the parameters describing manually applied water sprays, the parameters were calibrated to reproduce the water distribution patterns observed in the bucket tests. The exit velocity of the spray was first estimated from the measured value of pressure behind the nozzle using Bernoulli’s law

$$\vartheta = \sqrt{\frac{2\Delta P}{\rho}}$$

where $\Delta P$ is the operating pressure and $\rho$ is the density of water and tuned further during calibration. The spray offset, which is the distance from the nozzle after which the uniform water sheet breaks apart and where the Lagrangian droplets are introduced to the domain, was visually estimated during the bucket tests. The spray pattern was mainly controlled by user specified spray angles which were calibrated based on the bucket test results. An elliptical spray pattern was specified for the narrow spray using a pair of spray angles with a gaussian distribution of droplets within the specified spray angle. A conical pattern with a uniform distribution of droplets was adopted for the wide spray. The droplet size of the spray was assumed to fit a combination of Lognormal and Rosin–Rammler distribution for computational convenience. In FDS, this requires the user to specify the droplet median volumetric diameter and the width of the Rosin–Rammler distribution. As there is very little information about the droplet size distribution of the actual water monitor spray, the size distribution parameters were calibrated based on the values reported for firefighting sprays in [10] and [31].

2.2. Unidirectional Coupling Using FDS2FEM

The transfer of boundary conditions between FDS and Abaqus was achieved using a Fortran based unidirectional coupling tool called FDS2FEM [32]. The adiabatic surface temperatures ($T_{AST}$) from the fire simulation is provided as the input boundary condition for the heat conduction solver in Abaqus. Within FDS, the adiabatic surface temperature is solved from the local net heat flux equilibrium equation $q''_{\text{net}} = 0$ at every point of the surface at different times $t$, using the analytical method proposed by Malendowski in [33]. In this work, we modified the method by including the convective cooling by water droplets in the equation:

$$q''_{\text{net}} = \varepsilon (q''_{\text{inc}} - \sigma T_s^4) + h_c (T_g - T_s) + h_{w,\text{eff}} (T_{w,\text{eff}} - T_s) = 0$$

where $q''_{\text{net}}$ is the net heat flux on a surface, $T_s$ is the surface temperature to be solved for, $q''_{\text{inc}}$ is the incident radiative heat flux, $T_g$ is gas temperature, $h_c$ is convective heat transfer coefficient between gas and solid, $\varepsilon$ is the surface emissivity, $\sigma$ is the Stefan-Boltzmann constant, $h_{w,\text{eff}}$ is the heat transfer coefficient between
the droplets and solid, and $T_{\text{w,eff}}$ is the effective temperature of the water droplets. After rearranging, the adiabatic surface temperature $T_{\text{AST}}$ can be solved from

$$\varepsilon \sigma T_{\text{AST}}^4 + (h_c + h_{\text{w,eff}}) T_{\text{AST}} + (-h_{\text{w,eff}} T_{\text{w,eff}} - h_c T_g - \varepsilon q_{\text{inc}}') = 0$$

using the same analytical method as before. The implementation of the water cooling into $T_{\text{AST}}$ was verified by comparing the predicted temperature and $T_{\text{AST}}$ of a fictitious thin steel plate with perfectly insulated backing [28]. A calibrated value of 10,500 W/m$^2$ K was specified for $h_{\text{w,eff}}$ in the validation simulation. Typically, the magnitude of $h_{\text{w,eff}}$ is much greater than $h_c$.

The $T_{\text{AST}}$ is transferred to the structural solver as a combination of convective and radiative terms. Typically, the convective heat transfer coefficient does not show significant variation and hence, for computational convenience the FDS2FEM tool only supports the transfer of a constant convective heat transfer coefficient to the FE solver as in [34]. However, the increase in turbulence due to the spray cooling enhances the heat and momentum transfer which increases the convective heat transfer coefficient. The influence of spray cooling must be captured by through the convective term. To address this time-dependent behaviour of convective terms, a new module was added to the FDS2FEM code which allows the user to specify a time-dependent value of convective heat transfer coefficient to the structural solver. This can be applied to the entire structure or a specific region of the structure. The convection heat transfer coefficient used during the cooling phase would essentially be a sum of the $h_c$ and $h_{\text{w,eff}}$ terms as shown in Eq. 3.

$T_{\text{AST}}$ is transferred from FDS to Abaqus using a procedure called node set-to-device (NSET-DEVC) mapping and is explained in detail in [19]. The $T_{\text{AST}}$ values in the fire simulation are recorded at several discrete locations along the structure and then assigned to specific node sets of the Abaqus model. The computational cost of this approach is low, as the amount of transferred data is small, but the accuracy of the transferred boundary condition is limited by the resolution of $T_{\text{AST}}$ data points. The sensitivity of the predicted structural response on the mapping resolution during flame heating was investigated in [19].

### 2.3. FE Analysis Using Abaqus

FE analysis was performed in two sequential stages, namely thermal analysis and mechanical analysis. This type of simulation is termed ‘sequentially coupled thermal-stress analysis’ [35]. FE models in the 3D domain were created and discretised in Abaqus using DS4 shell elements for transient thermal analysis and S4R shell elements for mechanical analysis. The density of mesh was selected based on a mesh sensitivity study for each case. An explicit solver was used to capture the highly non-linear behaviour of the structures, including both geometrical and material non-linearity. As the mass acceleration was assumed to be negligible, the dynamic effects created by the solution method were kept within acceptable limits. The thermal properties of steel were adopted from EN 1991-1-2 [36]. For the material modelling, the temperature-dependent stress–strain curves for steel
defined in EN 1993-1-2 [37] were used. The thermomechanical behaviour of steel which includes strain reversals due to loading and unloading of the material and heating–cooling cycles of temperature has been studied in [38]. The Abaqus implementation of the material model was validated using the existing theory in [38]. The Abaqus implementation of the material model was validated using the existing theory in [38], and was adopted for the current work, although different behaviours may appear under rapid cooling.

3. Experimental Methods

3.1. Spray Characterization

The spray characterization experiments were conducted in a 24 m × 8.4 m × 5.2 m hall at the Emergency Services Academy, Finland. The water spray was generated using a TFT Blitzfire water monitor (see Fig. 1). A fire truck at about 10 m distance served as a water source, and the water pressure was measured at the pump of the truck and at the nozzle. Two different settings of the spray width were used: a narrow spray with a long (37 m) throw and a wide spray with a short (21 m) throw. At a distance of 18 m and 10.5 m, the spray widths were about one and three meters, respectively. At 10 bar pressure, the measured flow rates through the monitor were 650 L/min and 1750 L/min for the narrow and wide sprays, respectively. The monitor was aligned 30° upwards from the horizontal direction.

The delivered water flux at both spray widths was measured using plastic buckets, arranged in different patterns to resolve the essential spray characteristics. Four different patterns were used for both spray widths, and the data from the four tests were combined into final water distribution. The distance between the buckets along the longitudinal direction was 1.0 m in the narrow spray tests and 1.5 m in the wide spray tests. The bucket covered a width of 1.0 m and 3.0 m in the lateral direction for the narrow and wide spray respectively. Figure 1 shows two of the bucket patterns used for the 1.0 m and 3.0 m sprays.

3.2. Burn Tests

To quantify the cooling capacity of the two water monitor sprays, a series of burn tests was carried out using a kerosene pool fire and a steel beam as the heating and cooling target. The tests were carried out in a 24 m × 8.4 m × 5.2 m hall. The kerosene pool and steel beam were placed under a steel sub-structure (shown in Fig. 2) to protect the hall structures. The dimensions of the sub-structure were 5.2 m × 3.0 m × 3.7 m, and the height of the straight edge was 2.6 m. The distance between the lower flange of the steel beam and the fuel surface was 1.6 m. The surface area of the kerosene pool was 1.0 m × 1.0 m, and pan height was 0.5 m. The hall was open to ambient through a 4.0 m × 2.6 m door. The hall ceiling had two 1.2 m × 2.2 m openings for smoke ventilation.
The beam used in the test was a 3.0 m long, S300 steel I-beam (IPE 200) and is shown in Fig. 3. The web height and thickness were 200 mm and 5.6 mm, respectively. The flange width and thickness were 200 mm and 8.5 mm, respectively.

In each test, 15 L of kerosene was poured on a water layer so that the lip height from pan rim to the liquid surface was 20 cm. The pool was ignited, and an uninterrupted burning was allowed for 6 min to heat up the steel beam. After the heating phase, water monitor was opened, and the pre-aimed spray started to cool down the beam. Tests were performed using 1.0 m and 3.0 m spray widths and 6 bar and 10 bar nozzle pressures with two repetitions for each test. However, the tests performed with nozzle pressure of 6 bar were not used for the validations as these water pressures are not generally used during such fire interventions. Table 1 lists the test details with the measured flow rates and spray widths.

The steel temperatures were measured using K-type thermocouples (TC) attached to the web of the beam on the side opposite to spraying. The steel temperatures were also recorded from behind the beam using an infrared camera.

Figure 1. The TFT Blitzfire series water monitor and flow meter (left), the bucket arrangement for the 1 m (middle), and 3 m wide sprays (right).

Figure 2. The burn test setup used to estimate the cooling ability of the water sprays. (a) The substructure in the test compartment, the steel beam and fuel pan. (b) The narrow water spray used for cooling the beam.
Figure 4 shows the steel temperature captured using the IR camera at different instances. Occasionally, the steel beam was shielded by the flames and the IR camera was unable to register the steel surface temperature. As the IR camera records 30 frames per second, the running minima of the IR temperature captured in the moments when the flame does not obstruct the steel beam were found to represent steel surface. The heat flux at the sub-structure ceiling, just above the beam was measured using water-cooled Hukseflux SBG01 heat flux meter.

4. FDS Model Calibration and Validation

4.1. Calibration of Water Spray Parameters

A simple rectangular domain was modelled with several devices placed on the floor to record the accumulated water. The domain was divided into six meshes with a grid size of 20 cm and assigned to separate parallel processes. The spray was computed for several minutes to collect sufficient water density, taking approximately 1 h on a personal workstation.

The spray input parameters were calibrated manually by carrying out multiple simulations, seeking for the best agreement between the measured and simulated water flux distributions. The droplet inlet velocity for the narrow spray was calculated as 45 m/s from Eq. (1) and then increased by 7% to 48.3 m/s. The inlet velocity of the wide spray was calibrated based on the throw of the spray. The

| Test | Pre-burn time (min) | Pressure (Pa) | Flow rate (L/min) | Spray width (m) |
|------|---------------------|---------------|-------------------|-----------------|
| 1    | 6                   | 10            | 1710              | 3               |
| 2    | 6                   | 10            | 1710              | 3               |
| 3    | 6                   | 10            | 610               | 1               |
| 4    | 6                   | 10            | 610               | 1               |
| 5    | 6                   | 6             | 1324              | 3               |
| 6    | 6                   | 6             | 1324              | 3               |
| 7    | 6                   | 6             | 472               | 1               |
| 8    | 6                   | 6             | 472               | 1               |

(FLIR A660c). The beam used during the fire tests along with the thermocouples used for measuring the steel temperatures.
number of droplets inserted into the simulation domain per second, i.e., the numerical parameter controlling the statistical accuracy of the method, was increased from the default values until the measured water distributions converged. The spray angles were calibrated based on the experimental results. For the wide spray, the spray pattern was controlled using an additional parameter called ‘SPRAY_PATTERN_BETA’ parameter which was set to a value of 45°. The calibrated modelling parameters for the two spray widths are shown in Table 2.

Table 2
Description of the Parameters Used to Model the Water Sprays

| Modelling parameters          | Narrow (1.0 m) spray | Wide (3.0 m) spray |
|------------------------------|----------------------|--------------------|
| Nozzle pressure (bar)        | 10                   | 10                 |
| Flow rate (L/min)            | 650                  | 1750               |
| Offset (m)                   | 0.7                  | 0.5                |
| Particles per second         | 50,000               | 60,000             |
| Particle velocity (m/s)      | 48.3                 | 20.8               |
| Spray angle (°)              | 0.0 to 1.6           | 0.0 to 8.7         |
| Droplet median diameter (μm) | 1600                 | 1000               |
| Horizontal particle velocity (m/s) | 0.1              | 0.1                |
| Vertical particle velocity (m/s) | 0.5               | 0.5                |

Figure 4. The steel beam and the flames as observed by the IR camera at 30 s (left), 30.2 s (middle) and at 230 s (right). The beam is occasionally obstructed from the view of the IR camera by the flames.

Figure 5. The FDS model of the narrow (1.0 m) (left) and wide (3.0 m) (right) sprays showing the throw [m] and the spray pattern.
The simulated spray patterns for the two spray widths are shown in Fig. 5. The comparison of experimental and simulated results is shown in Figs. 6 and 7. The maximum value of the delivered water flux was observed on the centreline for both sprays. Due to the long throw of the 1.0 m spray, the water monitor had to be placed outside the test hall in the experiments. As a result, wind was found to skew the spray to one side shown in the horizontal profiles of the water distribution. The simulation overpredicts the water flux by 12–18% until 8.0 m from the first measurement point along the centreline but underpredicts between 35 m and 37 m. This could be due to the wind transporting the water particles farther in the experiments.

For the 3.0 m spray, the effect of wind was not significant along the centreline as the monitor was placed inside the test hall. The water flux along the spray centreline is predicted accurately at the first and last two buckets but is overpredicted by a maximum of 52% between 11.5 m and 16.5 m. The horizontal profiles of water distribution, however, are underpredicted away from the centreline by about the same range. Overall, the simulated water distribution patterns capture the main features of the two firefighting sprays well.

4.2. Validation of the Water Spray Cooling Ability

The CFD model of the experimental hall was modelled as per dimensions with a door and two smoke vents. The sub-structure (shown in Fig. 2) inside the hall was simplified to fit the rectilinear grid employed in FDS. The fire was modelled as a burner with a specified, steady heat release rate per unit area of 1300 kW/m², and the gas-phase combustion reaction $C_{10}H_{20}$ was assumed with heat of combustion of 43,200 kJ/kg and soot yield of 0.042 [39].

The CFD simulations of were performed using multiple parallel meshes on the Puhti supercomputing cluster of the CSC IT Centre for 460 s and the computational time was approximately 15 h. The domain was divided into seven meshes with a 20 cm resolution in all the meshes except the one containing the fire, where the resolution was 10 cm.

For the validation, we used temperatures from tests 2 and 4 for the 1.0 m and 3.0 m sprays, respectively, as these were the most reliable. Also, in these tests the cooling started during the steady heating phase of the beam. It was not possible to use averages of several tests because the wind produced significant variations in burning rate between the tests.

For the FE analysis, the steel beam was discretized with 5000 DS4 elements (Fig. 8a). The DS4 in Abaqus is a 4-node quadrilateral shell. To facilitate the transfer of $T_{AST}$ from FDS, the beam was partitioned into 9 equal parts (Fig. 8b). The coloured sections of the beam in Fig. 8c and d represent the regions where cooling was applied in the simulation for the two widths of 1 m and 3 m of the water spray. The simulation accounted for the following three phases: heating phase, cooling phase and dissipation phase.

The temperature contours received from the FE thermal analysis at the end of the heating phase for the 1.0 m and 3.0 m spray widths are shown in Figs. 9a and 10a. The highest temperature was at the mid-span of the beam, indicative of the
position of the flame and temperatures decrease with a gradual gradient towards the end of the beam.

Figures 9b and 10b show the temperature distributions after the cooling spray of 1.0 m and 3.0 m was applied, respectively. In the beam exposed to 1.0 m spray, the blue contours at the mid-span indicate that the beam has cooled down to the range of 54°C to 139°C. In the beam exposed to 3.0 m spray, the light blue contours at the mid-span indicate that the beam has cooled down to the range of 118°C to 254°C. The mid-span temperatures of the flange edges are observed to be higher than the web temperatures in both tests, and the back side flange edges are seen to remain at higher temperature than the front side.

4.3. Comparison of Measured and Predicted Temperatures

The temperature curves and the normalized temperatures from the burn tests are reported in Appendix. The results show that the steel temperatures decrease only
during the initial 10–12 s of cooling and the rate of decrease is proportional to the quantity of water utilised as shown in Fig. 32 in the Appendix.

Figure 11 shows a comparison of the beam surface temperatures, measured using the central thermocouple and using the IR camera, for tests 2 and 4. During the heating phase, the thermocouple temperatures contain several fast fluctuations more representative of gas or boundary layer temperatures. The surface temperatures observed by the IR camera show lesser fluctuations and a gradual increase of temperature as expected for a steel beam with high thermal inertia. Once the cooling starts, the IR signal is masked by cool water vapor, and becomes unus-

Figure 8. Modelling of the beam in Abaqus a mesh discretisation using DS4 elements, b partition strategy of the beam, c coloured section covering 1 m spray, and d coloured section covering 3 m spray.

Figure 9. Temperature development of the steel beam in the narrow (1.0 m) spray validation simulation a at the end of heating phase and b at the end of cooling phase.
The IR camera temperature values are thus used for the validation of the heating phase results and the thermocouple values are used for the validating the cooling phase temperatures.

In Fig. 12a, the temperature development of the thermocouples is presented along with the results of the FE simulation for the 1.0 m spray case. The left and right positions are located 50 cm from the ends of the beam, as shown in Fig. 3. The predicted FE steel temperatures in the heating phase were on an average 5% above the measured values on the left, 26% and 30% below than the measured values for the central and right positions, respectively. However, the overall trends of temperature development are similar. Figure 12b shows the same data focussed on the cooling phase only. During this phase, the values obtained in FE analysis are on average 2% above the measured values for the left position, 12% below for the centre position, and 37% above at the right position. It seems that, in the test, wind caused spray fluctuations and increased cooling towards the right end of the beam, while the simulated spray was close to the original spray shape.
In Fig. 13a, the measured and predicted temperatures are presented for the 3.0 m spray. The FE result for the left, central and right positions are on average 37%, 8%, and 7% below the measured values, respectively. The cooling phase results in Fig. 13b show that the predicted temperatures were on average 50% below, 11% below, and within 1% of the measured temperatures for the left, central and right positions, respectively.

The lowest temperature during the cooling phase is underpredicted by 20°C in narrow spray test and overpredicted by 30°C in the wide spray test. These uncertainties are mainly due to the uncertainties in the burning rate during the heating phase, but also due to the flame moving away from the centre of the beam, as well as the necessary simplifications in modelling the complex spray cooling process. The steel temperature at the centre of the beam predicted by the 1D heat solver of FDS is also presented in Figs. 12a and 13a. During the first 40 s, the FDS predictions are lower than the FE values by a maximum of 75%, indicating the lack of heat conduction from the lower flange to the measurement point at web. After the initial period, FDS shows approximately 38% higher temperatures than FE, indicating the lack of longitudinal heat dissipation. Also, the rapid decrease of steel temperature during the cooling stage is not captured well by the 1D heat solver.

The predicted cooling performance was found to be fairly sensitive to the value of heat transfer coefficient between droplets and the steel surface, $h_{w,\text{eff}}$. Assuming a value of 5000 W/m² K (instead of 10,500 W/m² K) reduced the cooling effect (temperature drop) from about 220°C to 185°C, and reducing it further down to 1000 W/m² K led to as small as 90°C temperature reduction. On the other hand, the cooling results were not sensitive to the number of computational particles. This indicates that calibrating the spray parameters using a large-scale bucket tests leads to a robust spray model, when it comes to its cooling performance.
5. Application Case: Warehouse Made of Steel Trusses

The target of the application example is to simulate an intervention scenario for travelling fire case and investigate the feasibility of the simulation chain in producing detailed predictions of the structural behaviours. A typical warehouse composed of several truss beams as load bearing structures is considered (Fig. 14) as the fire compartment. To achieve only one predominant direction of fire-spread, only one opening was defined. The design of this structure was provided by Ruukki Building Systems Oy (now Nordec Oy).

5.1. FDS Model

The computational domain for the fire simulation was $38.0 \times 30.8 \times 9.6 \text{ m}^3$ with a ventilation opening of $24.0 \times 9.6 \text{ m}^2$. An additional domain of $4.0 \times 30.8 \times 9.6 \text{ m}$ was added in front of the ventilation opening to minimise the influence of the open boundaries and to account for the heat produced by the flaming outside the opening. The walls are made of steel—mineral wool sandwich elements. The frame is modelled as a combination of multiple members: columns, lateral bracings, and truss beams with their upper and lower chords, and vertical and diagonal braces as shown in Fig. 14. The columns are 9.2 m high and are modelled as 0.2 m thick hollow rectangular elements. A comparison between the actual warehouse design and the FDS model of the warehouse are shown in Fig. 14. A constant grid resolution of 0.2 m was used within the domain, as found adequate in [18].

The fire load distribution modelled a rack storage in warehouses. A total of 204 wood piles with a height of 4.2 m were arranged in the compartment with a spacing of 1.0 m in between each pile. Each wood stick in the pile was 0.2 m thick and 1.0 m long. Each layer of the pile consists of 3 sticks and each pile consisted of 21 layers of sticks. Spacing between the side walls and the fire load were 3.6 m and 4.2 m. The combustible energy of each pile was $7458 \text{ MJ/m}^2$, and the average
Fire load density was 1300 MJ/m$^2$. This is in line with the Finnish decree [40], which assumes that warehouses have a fire load density greater than 1200 MJ/m$^2$.

The initial fire was produced using a 1 m$^3$ volume of wood crib shown as a red coloured section of the wood pile in Fig. 14. The surface pyrolysis of the crib was modelled using an ignition temperature of 300°C and an assumed heat release rate of 320 kW/m$^2$, following the approach validated in [41]. An ignitor was placed under the crib to provide the required ignition energy. The fire-spread modelling procedure utilised in this simulation has been validated and reported in [18].

### 5.2. FE Model

For the transient thermal analysis, the selected truss beam was discretized using 31,572 DS4 type elements with an approximate global size of 50 mm each. For the mechanical analysis, the same discretization was used, but with S4R shell elements. All the members of the truss were hollow with varying thicknesses. Columns were 10 mm, top and bottom chords were 8 mm, diagonal elements were 5 mm, and vertical elements were 4 mm thick. The mesh discretization of the top-left part of the truss beam can be seen in Fig. 15. For mapping, the members of the truss beam were partitioned as can be seen in Fig. 16. The columns of the truss beam were partitioned at every 0.5 m distance, and the top and bottom chords were partitioned at every 1 m distance. The diagonal elements and the vertical chords were not partitioned.

The steel was assigned the material strength of 355 N/mm$^2$. The bottom of the columns was assigned as rigid supports. It was assumed that secondary beams constraining the transverse displacement of main girders also undergo material degradation and are unable to restrain the truss beam at high temperatures. Therefore, the transverse movement of the truss beam was unconstrained. The mechanical loads included self-weight of the steel, snow loads (2.28 kN/m$^2$) according to EN 1991-1-3 [42], and horizontal wind loads (0.623 kN/m$^2$) according to EN 1991-1-4 [43]. The wind loads were applied as pressure load to the left...
surface of the column of the truss beam when viewed from the warehouse opening in Fig. 16. The loads were combined according to EN 1990 [44].

5.3. Fire Development

Without any fire intervention, the temporal fire development occurs in three phases: 1. growth phase 2. rapid spread to the opening and burning at the opening and 3. backward travelling phase. The phases of the development in terms of oxygen concentrations and temperatures are shown in Fig. 17. A detailed description of the modelling and the structural analysis of the heating phase is reported in [19].

5.4. Fire Intervention

The fire intervention simulations were carried out by adding one, two or three narrow (1.0 wide) sprays on the doorway of the fire scenario described in the previous section. The narrow spray was selected because the targeted truss beam was situated 33.5 m away from the nozzle at a height of 7.0 m. The arrangement of sprays is shown in Fig. 18.
Three sets of simulations were carried out with simulation parameters listed in Table 3. In sets 1 and 2, we assumed that the fire intervention (structural cooling) starts at 900 s (15 min), which is the median fire service help delivery time in Finland. At this point, the fire has engulfed multiple stacks of the fire load over an area of approximately 12 m². The starting time of 900 s can be considered a critical point, as the transition from a localized fire to a spreading fire occurred around 1000 s, giving the fire brigade about 1.5 min to prevent fire-spread. A faster arrival was assumed in Set 3, where the start time is 600 s, and the water sprays are aimed at the base of the fire rather than structures. The influence of a combined attack tactic was studied in the last simulation of Set 2, with two sprays cooling the structure and middle-spray directly attacking the fire.

Suppression durations were 180 s in sets 1 and 3, corresponding to very limited water resources. In Set 2 we increased the duration to 360 s, based on the study by Särdqvist which reported this value as the minimum suppression period required for the extinction of large-scale fires [10]. During the suppression, the sprays were not fixed at one location, but swept from the center to the right and left by changing their orientation. The residence time (30 s) is the duration of the spray in each direction. The effect of the sweep angle was studied by carrying out simulations with two sweep angles: ± 5° (short) and ± 9° (long). These angles correspond to 3 m and 5 m shift of the impingement point along the truss, respectively.

**Figure 17.** Predicted gas temperatures [°C] (left) and oxygen concentrations [mol/mol] (right) along the centreline of the opening in Scenario 1.

**Figure 18.** Illustration of the different spray scenarios, their arrangement and region of impact.
The simulations were performed using FDS version 6.7.6 on Puhti supercomputer of CSC IT Centre of Scientific Computing Ltd. and Aalto supercomputing cluster Triton.

5.5. Results: Influence of Fire Intervention on Fire Development

Although the simulated sprays were validated in terms of their capability to cool the steel surface, the sprays also influence the fire-spread through fuel surface cooling and oxygen replacement. This effect is currently unvalidated, and the resulting uncertainty must be kept in mind when evaluating the simulated performance in structural cooling. The fire HRR could be, at least partially, decoupled from the sprays by prescribing the fire HRR, but this would lead to non-physical and unrealistic behaviour.

The potential of fire extinction by cooling the fire environment can be estimated by comparing the spray’s capability to absorb thermal energy against the HRR. Evaporating all the water from a single 650 L/min (10.8 kg/s) spray would require about 25 000 kW of power. The predicted HRR at 900 s is 165 000 kW. One spray can thus potentially absorb about 15% of the HRR, and this value becomes 45% when three sprays are used. Under laboratory conditions, the 45% level has been found to be sufficient for total extinction [10] but that was not found to be the case in this study. Särdqvist and Holmstedt [45] report that in real fires, the total water application per compartment area is about 30–120 kg/m² and the

| Set 1   | Start time (s) | Duration (s) | Residence time (s) | Sweeps | No. of monitors | Sweep type | Distance between monitors (m) |
|---------|----------------|--------------|-------------------|--------|----------------|-----------|-------------------------------|
| 900     | 180            | 30           | 2                 | 1      | Short          | –         | –                            |
| 900     | 180            | 30           | 2                 | 2      | Short          | 5         |                               |
| 900     | 180            | 30           | 2                 | 3      | Short          | 5         |                               |
| 900     | 180            | 30           | 2                 | 2      | Long           | 4         |                               |
| 900     | 180            | 30           | 2                 | 3      | Long           | 4         |                               |

| Set 2   | Start time (s) | Duration (s) | Residence time (s) | Sweeps | No. of monitors | Sweep type | Distance between monitors (m) |
|---------|----------------|--------------|-------------------|--------|----------------|-----------|-------------------------------|
| 900     | 360            | 30           | 4                 | 1      | Short          | –         | –                            |
| 900     | 360            | 30           | 4                 | 2      | Short          | 5         |                               |
| 900     | 360            | 30           | 4                 | 3      | Short          | 5         |                               |
| 900     | 360            | 30           | 4                 | 2      | Long           | 4         |                               |
| 900     | 360            | 30           | 4                 | 3      | Long           | 4         |                               |

| Set 3   | Start time (s) | Duration (s) | Residence time (s) | Sweeps | No. of monitors | Sweep type | Distance between monitors (m) |
|---------|----------------|--------------|-------------------|--------|----------------|-----------|-------------------------------|
| 600     | 180            | 30           | 2                 | 1      | Short          | –         | –                            |
| 600     | 180            | 30           | 2                 | 2      | Short          | 5         |                               |
| 600     | 180            | 30           | 2                 | 3      | Short          | 5         |                               |
application rates about 0.15–0.25 kg/m² s. In 360 s long simulations of three monitors, the total applied water mass was 10 kg/m² and the application rate 0.027 kg/m²s, far below the estimated real fire -values.

The influence of the water suppression on the fire development was monitored by following the heat release rate (HRR) of the fire. Figure 19 shows the predicted HRR in all simulations. In Set 1, the cases with three sprays show the most effective fire control, decreasing the HRR 20–30% from the level before suppression. The reductions were, however, temporary, and the HRR increased again after the 180 s suppression period. The sprays tend to displace hot gases close to the fire load, as shown in Fig. 20, either by water evaporation or by entraining fresh air. The same figure shows also that two or three sprays lead to more effective reduction of the hot gas layer temperatures.

In the simulation Set 2, the longer suppression duration led to lower HRR towards the end of the suppression period, but the HRR increased again once the sprays were turned off (Fig. 19). Higher number of monitors is found to enhance the HRR reduction, but the combined approach, where two sprays cool the structure and one spray is directed to the seat of the fire, appears most effective in controlling the HRR. In the cases focusing on structural cooling, most of the water is evaporated by the hot gases and little water actually reaches the burning region. Figure 21 shows the flame positions in the case with three monitors. Even with 360 s of suppression action, the flames are not entirely controlled and spread outside the region experiencing the cooling action. In the model, the aiming of the sprays did not respond to the spreading of the flames, but kept cooling the same region despite the significant changes in fire behaviour. In reality, the firefighters

![Figure 19. Fire heat release rates in the simulated suppression scenarios.](image-url)
controlling the water monitor could try to ensure that the spray follows the burning zone.

Figure 22 shows the values of the horizontal velocity component at two different vertical planes of the compartment before (900 s), during (924 s) and after (1300 s) the fire intervention with three sprays. With respect to the fire-driven flow field of Fig. 22a, the intervention flow field at 924 s shows strongly enhanced entrainment, i.e., negative values of the velocity. When combined with the water evaporation, hot gases flow away from the centre and towards the sides of the compartment, increasing the hot gas layer thickness at side of the compartment. After the intervention, a rapid fire-spread along the surface of the wood stacks towards the opening is observed. This results in an increase of temperature around the truss beam, particularly towards one end, as shown in Fig. 23.

The results reaffirm the idea that the efficiency of the suppression action is proportional to the number of sprays, i.e. the amount of water used for suppression, and the suppression duration. Other parameters, such as the sweep angle and residence time, had little influence on the HRR. We also learned that using low flow rates of water might actually accelerate the fire-spread instead of decreasing it. In fires like the current case study, the aim of the suppression actions must be to control, contain and extinguish the fire within the initial burning region. With too few resources, this may not be possible, and the aim should be to decrease the rate of fire-spread.

Figure 24 compares the uncooled $T_{\text{AST}}$ at the left, right and centre sections of the truss beam’s bottom chord front face against the cooled results from Set 2. Without suppression, $T_{\text{AST}}$ starts to reduce after 1200 s as the fire moves away from it. With one or two monitors, the heat exposure to the structure increases immediately after the spray moves away, producing a fluctuating $T_{\text{AST}}$. With three sprays, $T_{\text{AST}}$ also fluctuates initially but a longer period of reduction of about 600–800°C is observed at the centre. The $T_{\text{AST}}$ at the left end is reduced by 200°C. At the right end, after an initial reduction of 200°C the $T_{\text{AST}}$ slowly increases and
Figure 21. Suppression using three water sprays for 360 s starting at 900 s. The flames move away from the cooled region and spread along the region unaffected by the sprays.

Figure 22. U-velocity slice along the centreline of the opening and at x = 25 m showing the velocity field before, during and after the fire intervention.

Figure 23. Temperature slice at y = 5 m showing the temperature field near the analysed truss beam before, during and after the fire intervention.
remains the same until the end of cooling. This change in $T_{\text{AST}}$ is also due to decreasing HRR as the fire moves away from the initial burning region, and not structural cooling alone.

5.6. Results: Structural Analysis

For the thermo-mechanical analysis, we selected the truss beam underneath which the fire started in the FDS simulation. In all the figures of this section, the truss beam is viewed from the opening of the warehouse, which is also the direction from where the water is sprayed. The 31 m long truss beam was exposed to fire for 900 s, then the centre of the truss beam was sprayed with three sprays for 360 s, and then left without cooling for 140 s. The $T_{\text{AST}}$ fields were mapped on the truss using FDS2FEM, and the temperature distribution and deformation of the truss beam were analysed and compared against the truss beam without fire-intervention.

The temperature contours received from the FE thermal analysis at the end of the heating phase (900 s) can be seen in Fig. 25a. The temperature was highest at the mid-span (about 1100°C) and it reduced gradually towards the two ends. The columns were relatively cooler with temperatures between 22°C and 300°C. In Fig. 25b are shown the temperature distribution at the end of the cooling period (1260 s). The green contours at the mid-span indicate that the truss beam cooled

Figure 24. Adiabatic surface temperatures at the front face of the bottom chord without any cooling (top left) and with 1, 2 and 3 simultaneous water monitors in the simulation Set 2. The positions 1 m and 29 m correspond to the ends, and position 15 m is the centre of the truss beam.
down to 700°C and below. However, the temperature towards the right of the cooled region gradually rose to 1100°C approximately by the end of the cooling period.

In Fig. 26, the steel temperatures recorded at 11 locations on the front face of the truss beam are presented for the different times of the FE thermal analysis without suppression. Figure 26a shows that the top chord mid-span temperature exceeds 400°C from 600 s onwards and peaks at 810°C between 900 s and 1260 s from ignition. The maximum temperature difference between the mid-span and the ends of the truss beam occurs at 900 s. The highest overall temperature of the top chord is observed at 1260 s, after which the temperature gradually comes down towards the end of simulation at 2100s. Similar trends of temperature can be observed for the bottom chord in Fig. 26b. In the early stages of fire, the differences between the mid-span and the ends are noticeably higher in the bottom chord than in the top chord. This can be due to direct flame impingement of the bottom chord. Conversely, the formation of a hot smoke layer at the ceiling contributes to a relatively uniform heating of the top chord resulting in smoother temperature difference between the mid-span and the ends.

Figure 27 presents the adiabatic surface temperatures from the fire simulation and the steel temperatures from the FE thermal analysis along the top and bottom chords of the cooled truss beam at different times. The temperatures are taken from the front face where the water spray impinges on the truss beam. The \( T_{\text{AST}} \) along the bottom chord shows a significant non-uniformity at the start of
cooling (900 s) unlike the top chord which is exposed to the smoke layer under the ceiling. The hottest region prior to cooling is at the mid-span. After the cooling period (at 1260 s), the hottest region is towards the right end of the truss beam. The water sprays reduce the burning rate below the truss beam mid-span, but do not completely suppress the fire. Instead, the fire moves away towards the uncooled region on the right resulting in higher $T_{AST}$ values post suppression.

Until 900 s, the temperature development is akin to a localized fire. At 1260 s, the temperatures at the mid-span decreased by 200°C for the top chord and by 400°C for the bottom chord. However, the temperature reduction is not consistent across the span of the truss beam, as the sprays pushed the flames and hot gases from the initial burning region towards the sides of the compartment, as seen in

**Figure 26.** Steel temperature along the span of the truss beam for the different times of simulation without suppression a top chord and b bottom chord.

**Figure 27.** Steel temperatures and adiabatic surface temperatures along the span of the truss beam with cooling for the different times of the simulation a top chord, b bottom chord.
Figs. 21 and 22, resulting in higher temperatures at positions between 20 m and 25 m. The bottom chord, in particular, is heated by the flames directly. Post cooling, the truss beam temperature increases as the fire spreads across the entire fire load resulting in higher temperatures across the top chord especially towards the ends. Overall, the trends of the steel temperatures and adiabatic surface temperatures are similar with some observable differences due to the 3-dimensional heat conduction of the FE solver.

In Fig. 28, the temperature development at the mid-span of the truss beams is presented for the top and the bottom chords, with and without cooling. Without cooling, the temperature of the top chord increased faster than the bottom chord up to 1000 s, after which the bottom chord recorded up to 200°C higher temperatures. The higher temperature of the top chord is opposite to the scenario which would normally result from an analysis assuming a local fire plume. For the cooled truss beam, both top and bottom chord temperatures start to decrease after 900 s at equal rates, achieving 450°C and 200°C, respectively, in about 300 s. This shows the efficiency of the cooling effect of the water sprays at mid-span. Soon after the cooling period, the temperatures increased again by about 500°C.

In Fig. 29, the transverse and vertical displacements of the bottom chord of the truss beam at the mid-span are presented. The transverse displacement starts to develop at 300 s to the direction away from the warehouse opening, due to the temperature difference between the front and back sides of the truss and the resulting thermal bowing. The direction of development is seen to reverse at 650 s, indicating a reduction in temperature difference as the fire starts to engulf more of the truss, and the hot smoke layer develops. At 850 s, the direction of bowing becomes positive, i.e., towards the opening, and just before the cooling (900 s), the transverse displacement is about 650 mm. The uncooled truss beam reaches its maximum transverse displacement of 800 mm at 1000 s, reducing to 550 mm at 1260 s, while the displacement of the cooled truss gets down to 200 mm at 1260 s.

The total vertical displacements in the direction of the ceiling (i.e., positive values) both before and after cooling suggest that the whole truss beam expanded upwards due to overall temperature increase. According to Fig. 29, the vertical displacement of the uncooled truss beam continues to increase from 900 s to 1050 s and then gradually reduces afterwards while the displacement of the cooled truss beam stays almost at the same level 900 s onwards. The vertical displacement of the cooled truss beam is 40 mm lower than the uncooled truss beam during the cooling period, and at the end of the cooling both have similar vertical displacements. The increase of temperature in the post-suppression period does not seem to affect the deformation significantly.

In Fig. 30, the deformed truss beam is superimposed on the undeformed shapes at 650 s and 900 s respectively to demonstrate the change of deformation with time. In Fig. 30a, the truss beam transversely deforms in the direction opposite to the opening of the warehouse, while in Fig. 30b, the truss beam deforms in the direction of the warehouse opening. This behaviour as explained earlier can be attributed to the changing temperature difference between the front and the back face of the truss beam leading to thermal bowing in one or the other directions. In Fig. 29, it can be seen that after the application of water from 900 s to 1260 s,
the direction of the transverse displacement reverses again because of the cooler front face of the truss beam.

For evaluating the global failure of the truss beams, a vertical displacement limit of span/20 \[46\] can be considered. For the 31 m long truss beam, the span/20 limit is 1550 mm. The truss beams had local temperatures as high as 1100°C.

Figure 28. Temperature development at the mid-span of the studied truss beam without and with cooling.

Figure 29. Lateral and vertical displacement at the mid-span of the truss beam bottom chord without and with cooling.

the direction of the transverse displacement reverses again because of the cooler front face of the truss beam.

For evaluating the global failure of the truss beams, a vertical displacement limit of span/20 \[46\] can be considered. For the 31 m long truss beam, the span/20 limit is 1550 mm. The truss beams had local temperatures as high as 1100°C and
the cooled down temperatures as low as 200°C, but the vertical displacement did not exceed the displacement limit criterion demonstrating an efficient load redistribution in the truss beam system. The material tests in [16] showed that, when the steel was cooled from 700°C to 900°C to room temperature, it had about 40% and 20% retention in strength, respectively. Therefore, the effect of the reduced strength on the structural response after cooling stage as well as the effects of different heating–cooling-heating cycles on the strength reserve needs further research.

The relatively greater transverse displacement compared to the vertical displacement is due to the absence of out-of-plane restraints. As mentioned previously, we did not model the transverse restraints because we assumed that they lose their functions at elevated temperatures. However, Figs. 26 and 27 show that the temperatures close to the ends of the truss remained below 400°C before 900 s. In reality, some of the restraints can still support the truss beams and should be studied in the future.

6. Discussion—General Applicability of the Models

This work was undertaken to study the cooling ability of firefighting water sprays and to evaluate the efficiency of structural cooling as a firefighting tactic. The FDS model of the firefighting spray was calibrated based on the bucket test data, measured in this work. The experimental data from the pool fire scenario was used to validate the coupled CFD-FE analysis methodology, which was then applied to study the influence of structural cooling on the response of a steel truss beam exposed to a fire-spread scenario.

The current validation base for the structural cooling predictions consists of previous validation studies regarding the FDS’ capability to simulate spray...
dynamics [47], structures’ thermal [19, 47] and mechanical [19] responses, as well as fire spread within wood cribs [18]. These results were supplemented with the current validation regarding the reduction of steel beam temperature by a firefighting spray. The cooling effect is a result of both direct cooling and the reduction of the flame heat flux due to the flame bending and fluctuation, as well as HRR reduction. Based on the current experimental data, it is not possible to quantify the share of each cooling mechanism, and thus validate the contributing processes separately. However, turning off the different cooling mechanisms in the numerical model revealed that a purely natural cooling of the beam would take roughly 100 times longer than what was observed with spray cooling. The direct cooling by the spray was thus much more important that the reduction of heating.

The flame heat flux reduction may, however, be important in more realistic fire scenarios, including the case study of this work. The current evidence of the model performance can be used as a basis for judging the model’s applicability in scenarios where the flame heat flux reduction is a consequence of relatively simple fluid dynamics and heat transfer interactions between the spray and the flame. This should be true for liquid and gas pool fires, as well as solid fuels with simple geometries. In large wood cribs, geometrical details and physical processes are obviously much more complex, and the predictions of the spray’s influence on HRR must be considered unvalidated. The capability of FDS to predict the wood crib HRR reduction by water sprinklers has been briefly demonstrated in [47], but since such cases are not included in the FDS validation database, the validity of the wood crib HRR reduction cannot be assumed. From this viewpoint, the full-scale case study mainly serves as a demonstration of CFD’s feasibility to investigate demanding fire-intervention operations. Quantitative conclusions will require further validation.

7. Conclusions

The work attempts to evaluate the feasibility of using numerical simulations for tactical planning of fire intervention operations by investigating the efficiency of structural cooling as a fire intervention strategy. Fire experiments were conducted to quantify the cooling ability of water monitors, i.e. heavy-duty firefighting sprays, and the results were used for validating the FDS water spray and cooling model. The validated method was then used to perform fire intervention simulations in a large structure with exposed steel frames.

The experimental results showed that the cooling power of the spray was proportional to the amount of water used. A cooling period of 10–15 s was sufficient to produce 50 -60% reduction in steel surface temperatures in the hottest region. The spray modelling showed that the water distribution pattern is highly sensitive to all the modelling parameters and should be carefully calibrated. The CFD-FEM coupled analysis procedure was enabled by extending the adiabatic surface temperature method for spray cooling and validated by reproducing the cooling experiments. The simulations of the burn tests showed that the temperature field is highly influenced by the sub-structure around the beam. The simulated steel
temperatures at the mid-section during the cooling phase of the beam were under-
predicted by 11% and 12% in the narrow and wide spray respectively.

The validated model was used to simulate fire intervention in a fictitious ware-
house with a single large opening. The simulation results indicate that the cooling
effect in this scenario was lower than in the beam experiment due to the longer
distance and higher overall temperature. This implies that the pure structural cool-
ing tactic might not be efficient in such buildings as it is extremely difficult to aim
the sprays precisely onto the truss beams. However, a significant reduction in the
adiabatic surface temperature around the truss beam was observed when the num-
ber of water sprays were increased.

In addition to the cooling, the water application also affected the simulated fire-
spread and power, although this effect has not been sufficiently validated. The
results showed that using too small suppression resources can lead to an acceler-
ation of fire development, as the evaporation of the water spray enhances mass
flow and turbulence inside the compartment. An important tactical lesson is to
ensure that sufficient resources are available before attempting a direct extinguish-
ment of such a strong fire. The most effective control of the fire was obtained
when a combined suppression approach was used, with two sprays cooling the
structure and one spray attacking the base of the fire. In the current simulations,
we did not investigate a situation where the sprays would have been used solely
for fire extinction.

The FE analysis of the studied truss beam showed that 360 s of water applica-
tion resulted in the reduction of the steel temperatures to the range of 200°C to
450°C. The cooling of the truss beam at the mid-section successfully prevented
further transverse deformation of the mid-span by 350 mm and reduced the ver-
tical deformation of the mid-span by 40 mm when compared to the uncooled truss
beam. The temperature rise in the post-suppression period did not increase the
deforation at the mid-span significantly. It was assumed in this study that the
transverse restraints would lose their functions in the high temperature environ-
ment of the warehouse. However, the relatively large transverse displacements of
the truss beams suggest that the transverse restraints are critical in fire-safety
design. The vertical deformation of the truss beams was in the upward direction
until 1100 s after which the deformation reverted in the direction of the unde-
formed shape. The wind and snow load along with the self-weight of the structure
did not challenge the robustness of the truss beams enough in the current study.
Based on the results obtained from the investigated scenario, the structural cool-
ing approach does produce a reduction in temperatures in the cooled region but
does not limit the fire-spread. Its effectiveness in real scenarios is uncertain espe-
cially in large structures. The results also show that the numerical simulations of
fire development and structural response provide a safe and powerful methodol-
ogy for studying and planning fire intervention tactics.
8. Future Research

The current work shows that the fire behaviour and structural cooling problems are strongly coupled. The method of spray application, its duration and the amount of water affect both the fire and the structural response. Further experiments with several repetitions for each scenario are needed to reduce the uncertainty associated with structural cooling. Separate measurements of the gas and steel temperature would help in decoupling the influence of the flames and water spray. Also, experiments on the cooling response of fire-exposed load bearing structures would significantly contribute towards understanding the actual performance of structures. Furthermore, the model development and validation are needed for the interaction between the water sprays, fuel, and flames.

Since the truss beams had large transverse displacements in the current study, the effect of the use of the transverse restraints for the truss beams should be investigated in the future. The vertical mid-span deformation was in the upward direction for the studied truss beams, therefore, the effect of imposed loads like overhead cranes supported by the truss beam which would challenge the utilisation of the steel strength should also be investigated. The mechanical properties leading to the material modelling of steels involving rapid cooling, especially high strength steels, and the reusability of the structures exposed to fires need further research.

Acknowledgements

This work was funded by the Academy of Finland Project No: 289037 and the Finnish Fire Protection Fund (Palosuojelurahasto). The authors would also like to acknowledge CSC—IT Center for Science Ltd. and Aalto Science-IT project for the computational resources provided for the study. Mr. Kimmo Partanen of Emergency Services Academy helped with the experimental work, which is greatly acknowledged. We would also like to thank Dr Jyrki Kesti of Ruukki Construction Oy and Mr Dan Pada of Nordec Oy for their support in the definition of the case study structure.

Funding

Open Access funding provided by Aalto University.

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Appendix: Burn Test Results

The appendix provides further details about the cooling tests performed in this work. The measured thermocouple temperatures from the burn tests are shown in Fig. 31. The peak steel temperature reached at the mid-point of the beam was close to 600°C in 6 min of heating. The influence of the water spray is seen as a sudden drop in the steel temperatures when the spray is applied.

Figure 32 shows the normalized steel temperature results during the cooling phase. The values are normalized against the steel temperature at the beginning of the cooling phase for each test. The green horizontal line represents the 60% threshold of the normalized temperatures and the vertical lines represent the time to reach the 60% threshold. The results show that the cooling rate was proportional to the amount of water used, as a higher cooling rate is observed with higher flow rates. Also, the reduction in steel temperatures occurs during the first 10–12 s of the cooling period. With the wide spray, the cooling rate was similar across the two tests with the same flow rate and the 60% threshold is attained faster with higher water flow rates. With the narrow spray, a larger variation in the cooling rate across the four tests (particularly Test 4) was observed because the temperatures at the start of cooling are different. This is possibly due to experiment variability and it produces a skewed result as the cooling power of the spray is either too low or too high.
Figure 33 shows the comparison between the average heat flux from the experiments and the gauge heat flux measured from the simulations. The heat flux in the simulations is overpredicted between 50 s and 200 s. The sub-structure geometry contributed to the heating of the beam through radiation and by facilitating the circulation of hot gases. This effect was partially missing in the simulations due to the limitations in simulating slant surfaces in FDS.

**Figure 32.** The normalized steel temperature rise ($\theta = \text{temperature relative to the ambient}$) of the cooling phase from the thermocouple 6 for the narrow (1.0 m) and wide (3.0 m) spray at different flow rates.

**Figure 33.** The comparison of the average heat flux from the experiments against the simulated heat flux.

Figure 33 shows the comparison between the average heat flux from the experiments and the gauge heat flux measured from the simulations. The heat flux in the simulations is overpredicted between 50 s and 200 s. The sub-structure geometry contributed to the heating of the beam through radiation and by facilitating the circulation of hot gases. This effect was partially missing in the simulations due to the limitations in simulating slant surfaces in FDS.

**References**

1. Schwille JA, Lueptow RM (2006) The reaction of a fire plume to a droplet spray. Fire Saf J 41(5):390–398. [https://doi.org/10.1016/j.firesaf.2006.02.005](https://doi.org/10.1016/j.firesaf.2006.02.005)
2. Hua J, Kumar K, Khoo BC, Xue H (2002) A numerical study of the interaction of water spray with a fire plume. Fire Saf J 37(7):631–657. https://doi.org/10.1016/S0379-7112(02)00026-7
3. Grant G, Brenton J, Drysdale D (2000) Fire suppression by water sprays. Prog Energy Combust Sci 26:79–130. https://doi.org/10.1016/S0360-1285(99)00012-X
4. Rashbash DJ (1986) The extinction of fire with plain water—a review. Fire Saf Sci 1:1145–1163
5. Rashbash DJ (1976) Theory in the evaluation of fire properties of combustible materials. In: Proceedings of the fifth international fire protection seminar, Karlsruhe, Germany
6. Beyler C (1992) A unified model of fire suppression. J Fire Prot Eng 4(1):5–16. https://doi.org/10.1177/104239159200400102
7. Torvi D, Hadjisophocleous G, Guenther MB, Thomas G (2001) Estimating water requirements for firefighting operations using FIERAsystem. Fire Technol 37:235–262. https://doi.org/10.1023/A:1012487619577
8. Chang C-H, Huang H-C (2005) A water requirements estimation model for fire suppression: a study based on integrated uncertainty analysis. Fire Technol 41:5–24. https://doi.org/10.1007/s10694-005-4627-5
9. Svensson S, Van de Veire M (2019) Experimental study of gas cooling during firefighting operations. Fire Technol 55:285–305. https://doi.org/10.1007/s10694-018-0790-3
10. Särdqvist S, Svensson S (2001) Fire tests in a large hall using manually applied high and low-pressure water sprays. Fire Sci Technol 21(1):1–17. https://doi.org/10.1007/fst.21.1
11. Weinschenk C, Zevotek R (2020) Exploratory analysis of the impact of ventilation on strip mall fires. UL Firefighter Safety Research Institute, Columbia. 10.54206/102376/YJZP6358
12. Kerber S (2012) Analysis of one and two-story single family home fire dynamics and the impact of firefighter horizontal ventilation. Fire Technol 49:857–889. https://doi.org/10.1007/s10694-012-0294-5
13. Kerber S, Regan JW, Horn GP, Fent KW, Smith DL (2019) Effect of firefighting intervention on occupant tenability during a residential fire. Fire Technol 55:2289–2316. https://doi.org/10.1007/s10694-019-00864-2
14. Lambert K, Merci B (2013) Experimental study on the use of positive pressure ventilation for fire service interventions in buildings with staircases. Fire Technol 50:1517–1534. https://doi.org/10.1007/s10694-013-0359-0
15. Särdqvist S, Jonsson A, Grimwood P (2019) Three different fire suppression approaches used by fire and rescue services. Fire Technol 55:837–852. https://doi.org/10.1007/s10694-018-0797-9
16. Abebe Z, Shakil S, Lu W, Puttonen J (2021) Post-fire mechanical properties of steel S900. In: Proceedings of the 9th European conference on steel and composite structures, Sheffield, U.K, 1–3 September 2021. https://doi.org/10.1002/CEPA.1467
17. Zhang C, Wang R, Zhu L (2021) Mechanical properties of Q345 structural steel after artificial cooling from elevated temperatures. J Constr Steel Res. https://doi.org/10.1016/j.jcsr.2020.106432
18. Kallada Janardhan R, Hostikka S (2021) When is the fire spreading and when it travels?—Numerical simulations of compartments with wood crib fire loads. Fire Saf J 126:103485. https://doi.org/10.1016/j.firesaf.2021.103485
19. Kallada Janardhan R, Shakil S, Lu W, Hostikka S, Puttonen J (2022) Coupled CFD-FE analysis of a long-span truss beam exposed to spreading fires. Eng Struct 259:114150. https://doi.org/10.1016/j.engstruct.2022.114150
20. Miyashita T, Sugawa O, Imamura T, Kamiya K, Kawaguchi Y (2014) Modeling and analysis of water discharge trajectory with large capacity monitor. Fire Saf J 63:1–8. https://doi.org/10.1016/j.firesaf.2013.09.028

21. Zhu J, Li W, Da L, Zhao G (2019) Study on water jet trajectory model of fire monitor based on simulation and experiment. Fire Technol 55:773–787. https://doi.org/10.1007/s10694-018-0804-1

22. Dai H-Y, Hasegawa M, Kawabata N, Sieke M, Chien S-W, Shen T-S (2021) Applying large-scale PIV to water monitor discharge experiment. Fire Saf J 120:103110. https://doi.org/10.1016/j.firesaf.2020.103110

23. Liu X, Wang J, Li B, Li W (2019) Experimental study on jet flow characteristics of fire water monitor. J Eng 120:150–154. https://doi.org/10.1049/joe.2018.8950

24. Yuan X, Zhu X, Wang C, Zhang L, Zhu Y (2019) Research on the dynamic behaviors of the jet system of adaptive fire-fighting monitors. Processes 7(12):952. https://doi.org/10.3390/pr7120952

25. Yuan X, Zhu X, Wang C, Zhu L (2020) Nonlinear dynamics of adaptive gun head jet system of fire-fighting monitor. IEEE Access 8:75210–75222. https://doi.org/10.1109/ACCESS.2020.2988912

26. Guha A, Barron RM, Balachandar R (2010) Numerical simulation of high-speed turbulent water jets in air. J Hydraul Res 48(1):119–124. https://doi.org/10.1080/00221680903568667

27. Sun J, Li W, He M (2019) Analysis of fire water monitor jet reaction forces and their influences on the roll stabilities of urban firefighting vehicle. Fire Technol 55:2547–2566. https://doi.org/10.1007/s10694-019-00879-9

28. McGrattan K, Hostikka S, Floyd J, McDermott R, Vanella M (2020) Fire dynamics simulator technical reference guide volume 2: verification, 6th edn. National Institute of Standards and Technology Special Publication 1018-2, 324 p

29. Abaqus A (2013) Analysis user guide version 6.13. Dassault Systèmes, Providence

30. McGrattan KB, McDermott RJ, Vanella M, Hostikka S, Floyd J (2020) Fire dynamics simulator technical reference guide volume 1: mathematical model, 6th edn. National Institute of Standards and Technology Special Publication 1018-1

31. Grimwood P, Barnett C (2005) Firefighting flow rate. https://highrisefire.co.uk/docs/FLFLOW-RATE%202002004.pdf. Accessed 29 Apr 2022

32. Paajanen A, Korhonen T, Sippola M, Hostikka S, Malendowski M, Gutkin R (2013) FDS2FEM—A tool for coupling fire and structural analyses. IABSE Symp Rep. https://doi.org/10.2749/222137813807054629

33. Malendowski M (2016) Analytical solution for adiabatic surface temperature (AST). Fire Technol 53:413–420. https://doi.org/10.1007/s10694-016-0585-3

34. Zhang C, Silva JG, Weinschenk C, Kamikawa D, Hasemi Y (2016) Simulation methodology for coupled fire-structure analysis: Modelling localized fire tests on a steel column. Fire Technol 52:239–262. https://doi.org/10.1007/s10694-015-0495-9

35. Segura G, Pournaghshband A, Afshan S, Mirambell E (2021) Numerical simulation and analysis of stainless steel frames at high temperature. Eng Struct. https://doi.org/10.1016/j.engstruct.2020.111446

36. EN 1991-1-2 Actions on structures—Part 1–2: General actions—actions on structures exposed to fire (2002) CEN, Brussels

37. EN 1993-1-2 Design of steel structures—Part 1–2: general rules—structural fire design (2005) CEN, Brussels

38. Shakil S, Lu W, Puttonen J (2019) Repeated loading and unloading of steel material in fire. Proc Nordic Steel. https://doi.org/10.1002/cepa.1122
39. Hurley MJ, Gottuk DTH, Hall JR, Harada KK, Kuligowski EDP, Puchovsky MW, Watts JM, Więczerzak CJ (2016) SFPE handbook of fire protection engineering, 5th edn. Springer, New York.

40. 848/2017—Decree of the Ministry of the Environment on Fire Safety of Buildings (2017) https://ym.fi/en/the-national-building-code-of-finland. (Ympäristöministeriön ase-tus rakennusten paloturvallisuudesta https://finlex.fi/fi/laki/ajantasa/2017/20170848). Accessed 29 Apr 2022.

41. Janardhan RK, Hostikka S (2019) Predictive computational fluid dynamics simulation of fire spread on wood cribs. Fire Technol 55(6):2245–2268. https://doi.org/10.1007/s10694-019-00855-3

42. EN 1991-1-3 Actions on structures—Part 1–3: General actions—snow loads (2003) CEN, Brussels.

43. EN 1991-1-4 Actions on structures—Part 1–4: General actions—Wind actions (2005) CEN, Brussels.

44. EN 1990 Basis of structural design (2002) CEN, Brussels.

45. Särdqvist S, Holmstedt G (2001) Water for manual fire suppression. Environ Behav 11(4):209–231. https://doi.org/10.1177/104239101400934333

46. Xi F (2016) Large deflection response of restrained steel beams under fire and explosion loads. Springerplus 5:752. https://doi.org/10.1186/s40064-016-2509-6

47. Vaari J, Hostikka S, Sikanen T, Paajanen A (2012) Numerical simulations on the performance of water-based fire suppression systems. Espoo, VTT. 144 p. VTT Technology; 54. http://www.vtt.fi/inf/pdf/technology/2012/T54.pdf. Accessed 29 Apr 2022.

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