Synergistic Response of Steel Columns to Explosive Thermal and Long Duration Blast Loading

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Abstract
This paper investigates the response of steel columns to combined pre-cursor thermal and long duration blast loads. Blast waves are the primary damage mechanism from explosive events. However, explosive events also emit thermal loads. Depending upon the explosive size and standoff distance, the effective thermal load can reach a structure prior to the arrival of the blast. An effective thermal load can damage steel structures such that total damage from the combined thermal and blast load is different to, or greater than the blast load alone. There is limited research to date investigating the synergistic response of steel structures to explosive thermal and blast loading. This paper specifically focuses on the numerical and computational methods appropriate to predict the response of steel columns subject to focused thermal and long duration blast loads (positive pressure phase exceeding 100msec). Results from a substantive study are presented, showing a synergistic response within a sensitivity envelope of load cases. Computational methods are verified with a novel series of explosive fireball trials and a series of combined thermal, compression and long duration blast trials.

Keywords: Thermal, Long Duration Blast, Steel Column, Dynamic, High Strain Rate

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1. Introduction

The protection of civilians and critical infrastructure from explosive events is of high national and international importance. Whether terrorist, state sponsored or accidental, explosive events can have highly damaging effects on buildings and structures causing partial or total collapse.

The primary damage mechanism from explosions is the blast wave caused by a region of highly compressed air forced out from the explosive centre. However, explosive events also emit high thermal radiation prior to the blast wave. This thermal radiation can lead to thermo-mechanical changes in a structure, altering the response to a subsequent blast load to be greater than or different to the response to a blast load alone. Thermal load parameters including intensity, duration and arrival time will affect the probability, nature and magnitude of such a response.

This combined response is synergistic, referring to synchronous structural response to the separate loads, not necessarily the separate loads acting at the same time. Explosive events with highly damaging thermal loads are typically accompanied by long duration blast loads (positive pressure phase exceeding 100msec), for example the Buncefield oil refinery disaster - a hydrocarbon explosive event with a positive phase duration of up to 500msec duration, of 250 tonnes TNT equivalence (eq.), Burgan, 2009 [1], Atkinson, 2011 [2]. Further examples include boiling liquid expanding vapour explosions (BLEVE) [3], the West Texas fertiliser factory explosion (2013), Tianjin warehouse disaster (2015), Yancheng fertiliser explosion (2019) and near Earth object detonations (asteroids etc.). Higher emitted thermal energy increases the probability of a structural response analogous to thermo-mechanical phenomena from explosive thermal loads.

There is limited existing research regarding the response of structures to combined thermal (fire or explosive) and blast loading. In Pearson, 1981 [4] the combined effect of blast with focused thermal loads on aluminium cylinders was investigated through a series of experimental trials exposing cylinders to a thermal load of 25 cal.cm$^{-2}$ as a precursor to a 42kPa
shock (200msec approximate duration). The magnitude and response of the results from the combined tests differed considerably to the de-coupled tests. An adaptive method of analysing steel frame structures to explosions and subsequent fire loads was detailed by Song et al, 2000 [5] and validated by Izzudin et. al, 2000 [6]. The analysis used elastic elements, which were re-meshed into elasto-plastic elements when yield stresses were reached. A mixed element approach to modelling large structure’s response to explosive and subsequent fire loading was discussed in Liew, 2007 [7]. The paper describes how the methods of performing detailed finite element modelling on structural elements under both blast and fire loads leads to an improved accuracy to predict structural behaviour. Despite successful use of the adaptive method in [5] and [6] and the mixed element approach in [7], the papers focus on combining the response to explosive blast loads and subsequent fires rather than a synergistic response of explosive precursor thermal and subsequent blast loads, which is the premise of this paper. There exist negligible research towards the synergistic response of steel structures to thermal and blast loading from singular explosive events.

A computational study using non-linear, coupled (thermal and structural) finite element analysis (FEA) has been developed to predict the response of primary structural steel columns under highly damaging thermal loads and long duration blast loads from singular explosive events. This paper discusses the numerical and computational methods used in the study including algorithms to predict blast and thermal loading, FEA procedures and steel material models predicting effects of high strain, strain rate and thermal softening. Full results from the study are presented, showing a synergistic response occurring within a sensitivity envelope of load cases. Verification of computational methods is demonstrated holistically through a series of arena trials investigating the response of steel plates subject to conventional (41kg TNT (eq.)) explosive thermal and blast loads [8] and a companion series of experimental trials
investigating the response of structural steel columns to combined thermal and long duration blast loads (150 – 190msec) inside an Air Blast Tunnel (ABT).

2. Numerical Method

Progressive collapse of buildings and structures subject to explosive loading is often dictated by the resistance of key elements to the blast pressures produced by detonated explosives. The simple approach of designing structures to resist explosive load (with reference to internal gas pressure explosions), according to Eurocode 1 [9] is to apply 34kN/m² to any key or critical elements in a building. For all other explosive types (external or internal chemical), specialist input is suggested. Given the highly complex nature of blast loading, coupled with the structural damage from thermal load, a more rigorous approach is required and, as such, this is the guidance advocated in the approved codes of practice. A key element in a multi-storey building construction is typically a primary column under axial loading from floors above or a heavily loaded transfer beam. This study investigates the response of structural columns under combined compressive, thermal and long duration blast loads. A circular hollow section (CHS) 193.7x5mm, was selected for investigation in the study as it is both a typical column used in building construction and has relatively thin (5mm) walls facilitating rapid conduction of thermal load. Columns were 4m in length, representative of an average structural floor height used in multi-storey buildings.

The FEA software LUSAS, [10] was used in the development of full predictive column models subject to compressive, thermal and blast loads from explosive events. The Johnson Cook constitutive material model [11] was defined for S355 high yield steel. Temperature (Figure 1) and pressure versus time (Figure 2) histories were calculated using empirical data detailed in Glasstone, 1977 [12] and Dolan, 1972 [13]. Compressive axial loads were derived based on a one or two storey steel building with typical 7.5m x 7.5m (on plan) structural bays.
A static analysis was initially performed on the column model geometry under self-weight and static compressive loads. An eigenvalue analysis was subsequently undertaken to determine appropriate Rayleigh damping parameters (Equation 1) for full dynamic response.

A damping ratio of 3% [14], (normal building use) was used to calculate the mass constant ($\alpha = 301/s$) and stiffness constant ($\beta = 1.0 \times 10^{-6} s$) using Equation 1.

$$C = \alpha M + \beta K$$  \hspace{1cm} (1)

A non-linear transient dynamic analysis was undertaken with manual time step control without automatic step reduction in the solution strategy. This allowed specification of small steps at peak load points and numerical coupling of separate thermal and structural steps throughout the analysis. Implicit dynamic analysis was defined for compatibility with Rayleigh damping coefficients, ensuring alignment of thermal and structural time steps and to enable mapping of varying stress distributions through column walls due to thermal gradients. Column geometry was defined with a singular solid element thickness. A continuum quadratic solid element (HX20) LUSAS, 2011 [15], was used to maintain compatibility with geometric and material non-linear analyses within the coupled solution. A Total Lagrangian formulation, capable of calculating large rotations, displacements and strains was implemented for the geometric non-linear analysis.

A key stage in the numerical method was the determination of accurate and synchronous temperature loading. Thermal emittance data calculated using Glasstone, 1977 [12] and Dolan, 1972 [13] was used to develop an algorithm predicting thermal loads from explosive events. The thermal loads calculated in this research represent similar loading profiles to accidental events. Different accidental explosive and thermal events will have differing thermal profiles. It is the researcher’s intention to investigate differing thermal load profiles beyond this research project. Input parameters included explosive charge size (W, kg
TNT (eq.), standoff distance (m), air temperature (°K), explosive thermal to total energy ratio (f), the Stefan-Boltzmann constant (σ, J.sec⁻¹.m².K⁻⁴), and visual distance (m). The following describes the calculation procedure implemented within the algorithm. Firstly, the maximum thermal power (Pₘₐₓ, cal.sec⁻¹) and time to reach maximum power (Tₘₐₓ, sec) were calculated using Equations 2 and 3 [12]. The maximum temperature (°K) at a given location outside a hemi-spherical fireball (Equation 4) is calculated by rearranging the Stefan-Boltzmann black body radiation equation (Equation 5), where J = thermal power per area (cal⁻¹.sec⁻¹.m⁻²), σ = 5.6704 × 10⁻⁸ J.sec⁻¹.m⁻⁴ [12].

\[ P_{\text{max}} = 3.18W^{0.56} \]  
\[ T_{\text{max}} = 0.0417W^{0.44} \]  
\[ \text{Max Temp} = \left(\frac{P_{\text{max}}}{(4\pi\sigma R^2)}\right)^{\frac{1}{4}} \]

\[ J = \sigma T^4 \]  
\[ R = \text{radial distance from centre}, \]

Explosive fireballs are assumed to radiate as black bodies (100% absorption versus emittance of radiated energy). Equations 2 to 5 provide base values for maximum thermal energy (power), time to maximum power and maximum temperature without the factors thermal to total energy ratio \( f \) and transmittance \( T \), taken into account. The thermal to total energy ratio is a factor describing the emitted thermal energy as a percentage of the explosive size (Equation 6).

\[ f = \frac{E_{\text{tot}}}{W} \]
This numerical study focused on surface explosions therefore, in accordance with Glasstone, 1977 [12], \( f = 0.19 \), taking account of ground dust and debris which may impede and reduce the overall thermal radiation. Transmittance (calculated using visual distance, \( V(m) \) and radial distance, \( R (m) \)), is a factor describing the proportion of total thermal energy to reach a specified standoff distance (Equation 7).

\[
T = \exp^{-2.9R/V}(1 + 1.9R/V)
\]  

(7)

Visual distance depends on atmospheric (weather) conditions; if clear the thermal energy will radiate further than if cloud and/or fog is present. If cloud or fog is present a lower visual range is assumed. For surface explosions, the radial distance is equal to the ground distance. A visual distance of 20km (clear conditions) was assumed for this study to observe the effect of thermal radiation at furthest practical distances. The maximum power per area or peak flux (cal.cm.\(^{-2}\).sec\(^{-1}\)) at any specified radial distance can be evaluated using Equation 8.

The peak flux is calculated by dividing the product of the maximum power \( P_{max} \), transmittance \( T \) and thermal to total energy ratio \( f \) by the hemispherical area of radiated energy at the specified distance:

\[
Peak \ Flux = \frac{fTP_{max}}{(4\pi R^2)}
\]  

(8)

The peak flux (Equation 8) is converted to a flux-time history using a normalised power versus time curve (Figure 3). Gaussian distribution (Equation 9) is used as a best fit curve of the normalised power versus time curve within the algorithm. In this study the Gaussian distribution parameters were \( \mu = 0.7 \), \( \sigma = 0.05 \), \( \lambda = 0.7 \), and \( \text{erfc} \) is the complementary Error Function. The Gaussian distribution was adopted and the shown parameters selected as the best fit function after analysis of multiple polynomial functions and parameters.
Equation 9 directly relates the maximum power to the varying rate of flux up to $10T_{\text{max}}$. A flux-time profile is the product of the peak flux at a specified distance and the Gaussian distribution (Figure 4). The flux-time profile is converted to a temperature versus time profile (Figure 1) using the re-arranged Stefan-Boltzmann black body radiation equation (Equation 4). The theoretical framework of the algorithm defines the evolution of an explosive fireball as discussed by Brode, 1964 [16]. The philosophy of fireball development and radiation is fundamentally empirically based.

During the initial phase of explosive thermal loading (detonation to peak flux), the dominant thermal transfer mechanism is radiation. This radiated thermal energy is applied to the surface of the studied steel column using an absorption coefficient of 0.79 (oxidised steel) [17]. This absorption coefficient is used within Equation 4 to calculate surface temperature. After peak thermal loading, the steel column surface will start to cool via convection, conduction and radiation. Thermal energy is conducted through the steel column thus cooling the external surface and creating a thermal gradient through the thickness of the steel. The surface of the steel will also start to cool coincidentally due to convection and radiation. Surface cooling temperature can be calculated using Newton’s Law of Cooling [18] (Equation 10), assuming a free convective flow prior to blast. Radiative cooling is ignored for the duration of the explosive event due to a negligible temperature drop.

$$\frac{dQ}{dt} = hA(T(t) - T_{\text{env}}) = hA\Delta T(t)$$ (10)
Q = thermal energy (J), h = heat transfer coefficient (W.m⁻².K), A = heat transfer surface area (m²), T = surface temperature, T_{env} = environmental temperature, ΔT(t) = time-dependent thermal gradient between the environment and the object.

Peak overpressures, P are calculated from Glasstone, 1977 [12] initially assuming a standardised and notional 1000 tonne TNT (eq.) explosive event. The standardised pressures are subsequently scaled according to the yield and distance of a range of blast events. Blast parameters and the application of blast loads upon structural columns were calculated in accordance with the respective works of Glasstone, 1977 [12] and UFC 3-340, 2008 [19]. Peak dynamic pressures (q, Equation 11 [20]), and blast wavefront velocities (U, Equation 12 [20]) were initially calculated based on the Rankine-Hugoniot equations.

\[
q = \frac{5p_s^2}{2(p_s + 7p_0)} \tag{11}
\]

\[
U_s = a_0 \sqrt{\frac{6p_s + 7p_0}{7p_0}} \tag{12}
\]

p₀ = ambient pressure at sea level, p_s = static overpressure, a₀ = speed of sound

Blast arrival times and commensurate rates of decay for the positive phase static and dynamic overpressures were calculated for a 1000 tonne TNT (eq.) explosive event [12]. These parameters were subsequently converted to equivalent pressures for a range of explosive events (Table 1) varying size and distance using scaling laws in accordance with Brode, 1954 [21].

Applied pressure-time histories on the front and side faces of the columns were derived using interaction theory for blast waves with columns as detailed in van Netten, 1997 [22].

The columns investigated in this study were assumed to be cladded. If a column was not cladded (standalone), rapid equalisation of the incident pressure around the element would occur due to the narrow dimensions of the column cross-section in comparison to the positive
phase wavelength. Rapid equalisation results in a low (negligible) net load in the direction of the blast. The following assumptions define the mechanical interaction of the building cladding with the column:

- Cladding does not inhibit direct thermal radiation to the column face.
- Cladding adjacent to the column is attached to horizontal beams or slabs at top and bottom edges.
- Cladding is assumed to remain initially intact (during incident pressure phase) therefore preventing early blast load equalisation around the column.

Based upon these assumptions, a combination of dynamic and incident blast loads were applied to the front face of a 4m structural column height with drag coefficients of 0.8 for the front and 0.9 for the side elevations [12]. To predict the behaviour of an in-service steel column within a multi-storey building, axial loads and equivalent spring (top) support stiffness were calculated assuming a regular steel beam layout (plan area of 7.5m²) and a typical office loading scenario [23] for an external facing wall column. The column top support stiffness varied from pinned (roller), to fully pinned with two levels of vertical stiffness between, calculated from the number of theoretical floors above the column. The support stiffness ($K_s$) was calculated using Equations 13 to 15 [24] for one storey (1.319kN/mm) and two storey (2.627kN/mm).

Compressive loads from one (249kN, SLS) and two (498kN, SLS) storeys were applied to the top face of the columns. The base supports were pinned.

$$K_r = K_{floor \, n} + \frac{1}{\frac{1}{K_{c \, n+1}} + \frac{1}{K_{floor \, n+1}}}$$  (13)

$$K_{floor \, n} = N \frac{12EI}{I^3}$$  (14)

$$K_c = \frac{EA}{L}$$  (15)
$K_{\text{floor}} = \text{stiffness of floor at support, } K_{c\ n+1} = \text{stiffness of column above support, } K_c = \text{column stiffness, } E = \text{Young’s Modulus (beam or column), } A = \text{sectional area of a column, } l = \text{length of beam, } I = \text{second moment of area (beam), } L = \text{length of column, } N = \text{number of beams supported at top of column.}$

The Johnson Cook (J-C) material model was used in the FEA to predict the yield stress (MPa) and strain hardening behaviour of structural steel due to high strains (>0.025ε), high strain rates (>50εs⁻¹) typical of blast loads and thermal softening from explosive thermal loads (Equation 16 [11]). Parameters for the J-C model (Table 2) were calculated assuming a constant strain rate of 80εs⁻¹. This constant strain rate was assumed to as it is typical in structures under long duration blast loading events, van Netten, 1997 [22] and Clubley, 2014 [25], and for adaptability within the computational material model. The effect of varying strain rates can be investigated beyond the scope of this research.

Strain and strain hardening parameters, C and N are derived from Vedantam [26], who undertook Split-Hopkinson bar tests on steel at 20°C. Material parameter M is derived from Schwer [27], through investigating the effect of high temperature and strain rates on U.S structural steel (A36). The homologous temperature $T_H$ is calculated using Equation 17. Figure 5 shows the J-C material model for the post-elastic behaviour after reaching yield (first point on each line).

$$\sigma_y = [A + B(\varepsilon_{eff}^P)^N] \left(1 + C \ln \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\right) \left[1 - \left(\frac{T_H}{T_m - T_R}\right)^M\right]$$

Equation 16

$$\sigma_y = \text{yield stress, } \varepsilon_{eff}^P = \text{effective plastic strain, } \dot{\varepsilon} = \text{strain rate, } \dot{\varepsilon}_0 = \text{reference strain rate } A = \text{elastic yield stress, } B = \text{ultimate tensile stress, }$$

$$T_H = \frac{T - T_R}{T_m - T_R}$$

Equation 17

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\[ T_m = \text{melt temperature (1500}^\circ\text{C)}, T_R = \text{reference temperature (20}^\circ\text{C), } T = \text{applied temperature } (\circ\text{C}). \]

Factors taken from Table 3.1 in BS EN 1993-1-2-2005 [28] describing the reduction of Young’s Modulus with increasing temperature were adopted within the FE steel model. Conductivity and specific heat were \(45\text{W.m}^{-1}\text{.K and } 4.68\text{mJ.mm}^{-3}\text{.K}^{-1}\) and constant values of 0.3 for Poisson’s ratio and \(12 \times 10^{-6}\circ\text{C}^{-1}\) for the coefficient of thermal expansion were used [29].

3. Numerical Results & Discussion

Using the numerical procedure described, a 193.7mm x 5mm steel circular hollow section was analysed subject to varying combined thermal and blast load cases from explosive events (peak temperatures and pressures shown in Table 1). Results from these load cases are presented in the form of individual column deflection and stress profiles, individual deflection versus time histories and collated peak and final deflections versus peak pressures, temperatures and scaled distance. Scaled distance \((Z)\) is an explosive parameter derived from radial distance \((R)\) and explosive size \((W)\) (Equation 18), regularly used by industry practitioners and researchers in the field of explosives to quantify blast parameters [20]. For long duration events, a low scaled distance \((0 – 20\text{m.}T^{-1/3} \text{ TNT eq.) indicates relatively high blast and thermal loads at close proximity to the centre of an explosive event.}\)

\[ Z = \frac{R}{W^{1/3}} \text{ (m.}T^{-1/3}) \quad (18) \]

Figures 6 and 7 show the collated peak and final deflections versus scaled distance \((Z)\), with exponential decay regression curves overlaid on the scatter plots. The best-fit curves highlight a clear difference in response between combined blast and thermal and blast only load sets. With increasing scaled distance \((20\text{m.}T^{-1/3} \text{ to } 43\text{m.}T^{-1/3})\) the blast only cases exhibit small decreases (relative to combined cases) of peak deflections (106mm to 7mm). Combined
blast and thermal cases exhibit a higher drop in peak deflections (678mm to 34mm) over the
same scaled distance range. At low scaled distances (<20m T^{1/3}) the peak and final deflections
exceed 1000mm (beyond complete failure) for blast only cases. These high deflections are
representative of columns at close proximity to an explosive source, where blast loads reach a
structure before the thermal load has significantly softened and weakened the structure.
Structural response at these locations is blast dominant. At high scaled distances (>43m T^{1/3}),
peak deflections for combined blast and thermal load cases are reduced (17mm) and of a similar
magnitude to the blast only cases (4mm) (Figure 6). There was no difference noted in final
deflections above 43m T^{1/3} between the blast only and combined blast and thermal cases,
indicating no reduction in yield strength for these cases. A clear sensitivity response envelope
of load cases from 20m T^{1/3} to 35m T^{1/3} can be seen indicating columns in this range are
susceptible to a synergistic response. Equations for best fit exponential decay regressions are
detailed in [30] with standard errors and parameters. Data trends also indicate increases in peak
and final deflections with peak pressures and temperatures.

The deflection and stress histories of the 193.7 x 5mm CHS column subject to a set of
load cases within the sensitivity response envelope (peak pressure 198.8kPa) are shown in
Figure 8. The combined blast with thermal load case shows a prominent synergistic response
which takes the form of an initial negative thermal deflection (towards the explosive centre)
followed by a sudden large deflection (away from the explosive centre) at blast arrival. A peak
deflection of 182mm is reached prior to settling at a final deflection of 138mm, indicating a
plastic response. The blast only model displays an elastic response, reaching a peak of 9.14mm,
before returning to zero. The deformed column shape with plastic hinge formation is shown in
Figure 8 with von Mises stress overlaid. Locations of maximum stress (~670N.mm^{-2}) occur
adjacent to the plastic hinge and at end supports.
The deflection histories of a column with varying top support conditions (pinned, roller-pinned top, one storey and two storey compressive load) under combined blast with thermal loads (198.8kPa peak pressure load case) are shown in Figure 9. Peak deflections of columns under one and two storey compressive loads are similar to the pinned top connection (1 storey = 53.9mm, 2 storey = 48.9mm, pinned = 33.8mm) whereas, peak deflection exhibited by the roller-pinned top connection is increased to 187.4mm. Higher lateral deflection exhibited by columns with a roller-pinned top connection can be attributed to the top of the column lowering as lateral deflection increases, therefore allowing higher rotation at the plastic hinge. Peak deflection of the column with a one storey compressive load was greater than that exhibited by the column with a two storey compressive load. This was due to a higher top support stiffness used for the two storey column (2.627kN.mm\(^{-1}\)) compared to one storey (1.319kN.mm\(^{-1}\)). The higher compressive load (2 storeys - 730kN, 1 storey - 365kN) was not a critical parameter in this case. For increasing vertical support stiffness, the support is comparable to fully pinned therefore the lowering of the column head is reduced.

A mesh sensitivity study was undertaken to investigate any potential difference in the response of columns with double element versus single element definition through the plate thickness. Deflection variations under blast with thermal loading were 3.8% between one and two element thickness models - a negligible difference for increased computational power requirements (double elements: 65hr, single element: 7hr running time). Therefore, the use of single element thickness was justified for the full study.

4. Experimental Verification

4.1 High Explosive Fireball Trials

Two benchmark experimental trials were undertaken to record thermal and blast loads and in addition investigate the synergistic response of steel plates inside a fireball using a
representative 41kg TNT charge [31]. Thermal load (temperature and flux) and reflected pressures were recorded using bespoke Heavy Structural Boxes (HSB), designed to withstand focused thermal and blast loads at close range (4m to 8m) to an explosive centre. Steel plates (2mm thick x 150mm diameter), were fixed circumferentially to the front face of the HSBs with bolted connections. Figure 10 shows the experimental set up of the arena trials with deformed plate and explosive fireball inset. The HSBs were welded to Circular Hollow Sections providing protected conduits for electrical cables. Two sets of three HSBs were positioned at 4m, 6m and 8m on two radial arms from the charge centre. One set of HSBs were instrumented with reflected pressure gauges (Endevco 8510c-100) at 4m and 8m, and thermocouples (internal and external, K-Type - RS), thermal flux gauges (Seqouia) and strain gauges (Rosette) at 4m, 6m and 8m. A high speed camera (Phantom, 2000 frames per second), recorded the propagation of the explosive fireball. Post shot deflections were recorded using Vernier Caliper after removal from the HSBs.

Figure 11 shows the recorded reflected pressure history during the two trials at 4m and 8m radial distance. The peak reflected pressures at 4m were 4.05MPa (Trial 1) and 4.3MPa (Trial 2). A noticeable double peak pressure was recorded at the 4m location (between 3 to 4msec) indicating two distinct charge effects. The first peak represents explosive products reaching the HSB, the second peak can be attributed to the expanding shock wave. A double spike in peak pressure was not recorded at 8m, indicating the explosive products did not reach this distance. This observation corresponds with high speed photography showing the explosive fireball reaching a maximum diameter of 6.25m. The two trials demonstrated large peak pressure variations at such close distances to an explosive centre. There are many factors affecting the accuracy of recording pressures at such close ranges, including the explosive product, fireball volatility, atmospheric pressure and temperature variations and the pressure gauge response.
Figure 12 shows the recorded temperatures on the external face of the HSBs at 4m, 6m and 8m during both trials. Peak temperatures of 480°C (Trial 1) and 465°C (Trial 2) were measured at the 4m location. Figure 13 shows the post-shot deflections of the steel plate at 4m (Trial 1 and 2) and 6m (Trial 1). Peak midpoint deflections were 10.5mm (4m, Trial 1), 7.5mm (4m, Trial 2) and 5.25mm (6m, Trial 1). There were no visible deformations on the plates at 6m (Trial 2) and 8m (Trial 1 and 2). The difference in plate deflection between the two trials can be attributed to the volatility of the explosive fireball. Table 3 summarises the results from the two arena trials (peak temperature, peak reflected pressures and maximum final deflection).

FEA models were constructed to analyse plate response within each explosive fireball using recorded pressures and temperatures. The core computational modelling procedure used to develop the predictive column models as described in Section 2 was adopted for modelling of the plates in the fireball trials. Figure 14 shows the modelled plate deformation and deflection versus time histories for plates at 4m, during Trials 1 and 2 under separate thermal and combined blast and thermal loads. Table 4 summarises the results (peak deflection, peak stress and maximum final deflection) from the numerical modelling of the steel plates. There is reasonable correlation (within 20%), between trial final deflection results in Table 3 (10.5mm) and numerical final deflection results in Table 4 (8.6mm), providing verification for the core modelling procedure. Variability of key parameters (temperature, peak pressure) between the two trials is due to the high volatility of the explosive fireball at such close distances to the explosive centre.

4.2 Air Blast Tunnel & Thermal Shock Trials

A series of trials investigating the response of steel columns to thermal loading akin to a fire, precursor axial compression and long duration blast loading inside an Air Blast Tunnel (ABT) were undertaken. Six ABT trials were performed with nine, 3m length columns per trial. Three
different section sizes were tested (50x25x2 RHS, 25x25x2 SHS, 33.7x3 CHS) during the six trials to provide data for a range of sectional geometry. Columns were subject to a range of temperatures: environmental (~17°C), low (340°C - 400°C), medium (400°C - 500°C) and high (500°C – 572°C) combined with compressive loads equal to 50%, 75% or 90% of each column’s buckling capacity (50x25x2 RHS: 5.72kN, 25x25x2 SHS:3.08kN, 33.7x3 CHS:6.96kN). Columns were heated using ceramic heating elements [32] inside a thermally insulated box. The columns were heated prior to the blast until target temperatures were reached and stabilised. Figure 15 shows the heating of the columns in rig 2 during trial 2, the heating was stopped after 1150sec (~19min 10sec), 8 seconds prior to the blast. (e.g. Axial compressive load was applied using heavy duty springs (IST “Closed and Ground” [33]) in BS5216 patented carbon [34], positioned at the base of each column. A novel structural rig supporting a “drop down frame” was designed and constructed for the ABT trials. The rig enabled ceramic heating elements to be held in a vertical position around the columns during an initial heating phase then released backwards prior to the shock wave arrival. Figure 16 shows a plan of the ABT trial set up and Figure 17 shows the heavy structural rig design with drop down frame, springs and inset image of Trial 2 configuration. 

Instrumentation employed at each rig in the ABT included incident (Endevco – 8510), dynamic (Kulite – 20D) and reflected (Endevco) pressure gauges. Figure 18 shows the recorded reflected pressures for all trials. Compressive loads were recorded using Zemic BM23-C3-1T-3B load cells, positioned at the base of each column. Temperatures were recorded using thermocouples (K-type, RS) positioned along the external length of each column at 1250mm, 1500mm and 1750mm locations. Midpoint deflection versus time histories were recorded during each trial using high speed Phantom cameras positioned adjacent to each rig. The post-shot final deflected shape of each column was measured in laboratory conditions using Vernier callipers against a reference line following removal from the test rig. Table 5 shows the final
deflections of each column from the ABT trials.

The recorded temperatures, pressures and compressive loads were adopted within FEA models of the columns using the identical core modelling procedure for column models detailed in Section 2. Figure 19 shows the deflection versus time history of Trial 2, rig 3, (RHS). The final midpoint deflections were 44.3mm (trial) and 45.7mm (model), a difference of 3%, providing further validation of the computational methods. The midpoint deflection of 44.3mm is the column in-situ deflection recording during the trial via high speed photography. The midpoint deflection shown in Table 5 for Trial 2, rig 3 (RHS) is 33mm. The deflections in Table 5 were recorded after removal from the supports and rig, the removal of restrained supports accounts for the difference between the two values. The sharp decrease at 1sec is due to an instrumental failure towards the end of the trial. Other key similarities between the model and trial deflection response include a high peak followed by a semi-periodic motion which reduces to the final deflected position.

5. Conclusions

The synergistic response of structural steel columns to focused pre-cursor thermal with long duration explosive loads has been investigated in this paper. Numerical algorithms predicting the thermal and blast loads from explosive events were developed and adopted within a non-linear, coupled FEA solution for steel columns. The study investigated a range of blast pressures and pre-cursor temperatures from explosive events. The effect of secondary parameters including axial compressive load and support stiffness were also investigated.

Results from the study indicate that within a critical response envelope of load cases a synergistic response does occur under combined thermal and long duration blast loading regimes. Within this envelope the thermal load degrades steel material properties prior to blast
arrival which, in turn, alters the mechanical behaviour of the column subject to a subsequent blast load. The formation of a plastic hinge and large permanent deformation marked a reduction in yield and ultimate strength characteristics in line with the Johnson-Cook material model at high temperatures.

Importantly, two series of benchmark experimental trials were conducted to observe the response of steel plates inside an explosive fireball and investigate the response of steel columns to combined thermal, compression and long duration blast loading. Both trials were computationally modelled using the same core modelling procedure used for predicting column response to focused explosive thermal and long duration blast loads. Good correlations were shown between tunnel trial and computational results (within 3%) providing verification for the computational methods. These outcomes will be of direct importance to both practitioners and researchers in the field.

Acknowledgement

The author acknowledges that all results and data reported herein are the property of the UK Ministry of Defence. The assistance of Spurpark staff at MOD Shoeburyness is gratefully acknowledged with respect to the preparation and undertaking of the trials and for their support during the project. The author would also like to express thanks to the sponsor. With support and contribution from AWE Plc, Aldermaston, UK.

References


[30] Clough LG. Synergistic Reponse of Steel Structures to Thermal and Blast Loading: University of Southampton; 2017.

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Table Captions

Table 1: Peak pressure and temperature load cases

Table 2: Johnson-Cook material parameters

Table 3: Fireball trials: results summary (temperature, pressure and plate deflection)

Table 4: Fireball plate trials: plate model results (stress and deflection)

Table 5: ABT trials: post-trial column deflections (along length maximum)

Figure Captions

Figure 1: Temperature versus time profile for application to CHS

Figure 2: Front and side-on pressure on CHS (99.4kPa peak incident pressure)

Figure 3: Normalised power versus time profile, reproduced from [14]

Figure 4: Thermal flux history

Figure 5: Johnson-Cook material model at increasing temperatures

Figure 6: Peak deflection versus scaled distance

Figure 7: Final deflection versus scaled distance

Figure 8: Mid-point displacement & stress versus time (peak pressure = 198.8kPa)

Figure 9: Horizontal displacement versus time: pinned, roller, 1 storey & 2 storey

Figure 10: Plan of arena trials with explosive fireball and heavy structural boxes

Figure 11: Reflected pressure gauge data at 4m and 8m radial positions

Figure 12: Recorded external temperatures at 4m, 6m and 8m radial positions

Figure 13: Average maximum plate deflections at 4m, 6m, and 8m radial positions

Figure 14: Displacement versus time of modelled plate at 4m radial position facing blast
Figure 15: Pre-shot recorded temperature: Trial 2, Rig 2

Figure 16: ABT trial design: plan view with rig configurations

Figure 17: Heavy structural rig with drop down frame and tunnel overview

Figure 18: Reflected pressure all trials

Figure 19: Trial and model horizontal deflection versus time: Trial 2, Rig 3
**Figure 16**

- **CHS** = Circular Hollow Section (Light)
- **SHS** = Square Hollow Section (Light)
- **RHS** = Rectangular Hollow Section
- **Column Section with Heating Elements & Compression**
- **PL: L,M,H** = Compressive Pre-Load: Low, Medium or High
- **HL: L,M,H** = Heat Load: Low, Medium or High
- **High Speed Cameras (wide angle lens)**
- **Incident & Dynamic Pressure Gauge Locations**

The diagram shows a layout with labels indicating different sections and pre-load conditions. The Blast Direction is shown, along with markers for Rig 1, Rig 2, and Rig 3, each indicating specific loading conditions.

- **Rig 1**
  - **Column Fixing Rig**
  - **Non Pre-Loaded Column Sections (Pinned Ends)**
- **Rig 2**
  - **Thermal & Compression Pre-Loaded Column Sections**
  - **PL: L, HL: H**
- **Rig 3**
  - **Rarefaction Wave Eliminator**
  - **Rig 3**
  - **Tunnel Wall**
  - **Thermal & Compression Pre-Loaded Column Sections**
Table 1:

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<tr>
<th>Case</th>
<th>Peak Pressure ($2p$) (kPa)</th>
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