

Research Article

Numerical Investigations of a Novel Offshore Platform Blast Wall Design Using Energy Absorption Devices at Supports

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ABSTRACT

Offshore blast walls are critical safety barriers designed to protect personnel and equipment from hydrocarbon explosions. Traditional designs often struggle to simultaneously satisfy code requirements for deflection limits and weld integrity under high-intensity blasts. This study proposes a novel offshore blast wall concept incorporating energy-absorbing support devices comprising PMI foam-filled sliding cores and rotational springs. The devices provide flexible support with adjustable translational and rotational stiffness, enabling controlled energy dissipation through foam crushing and spring mechanisms. Finite element analyses were performed to compare the proposed design with traditional Type 1 and Type 2 configurations under strength-level (SLB, 1 bar) and ductility-level (DLB, 3 bar) blast events. Parametric studies further investigated the influences of translational stiffness in the foam elastic range (K_{tl}),

hardening stiffness (K_{t2}), rotational stiffness (K_r), and blast duration. Results demonstrate that the proposed design significantly outperforms traditional schemes: under SLB, it exhibits negligible plastic deformation (~1 mm vs. 7 mm and 4 mm for traditional designs); under DLB, it limits panel deflection to 167.7 mm (44% reduction) while maintaining support plastic strains at 4.54% (below the 5% rupture limit), whereas traditional designs fail both criteria (303.3 mm deflection and 26.68% PEEQ). Parametric findings reveal that K_{tl} and K_r critically influence deflection and strain responses, while K_{t2} affects the safety margin against densification. Long-duration blasts are identified as more onerous due to t_d/T_n ratios. The proposed design offers a balanced solution for enhanced blast wall resilience in offshore applications.

KEY WORDS

Offshore blast wall, Flexible support, Energy absorption, Finite element analysis, PMI foam, Support connection.

1. Introduction

Blast-resistant structures find widespread application across both industrial and defence sectors for explosion hazard mitigation. Within offshore oil and gas platforms, blast walls serve as essential safety barriers, ensuring the safety of personnel and the protection of critical assets in the event of hydrocarbon blasts. Traditional concrete blast walls depend primarily on high rigidity and substantial mass to withstand blast loads. In contrast, offshore environments impose more stringent requirements, necessitating lightweight yet resilient solutions capable of preserving structural integrity under extreme blast events.

Stainless steel profiled panels are well-suited for offshore blast wall applications, offering advantages in manufacturability, high ductility, and exceptional resistance to both corrosion and fire. Current design of stainless-steel profiled blast wall is typically performed according to FABIG TN5 guidelines^[1], which utilise the single-degree-of-freedom (SDOF) method. However, the inherent simplifications of the SDOF approach often yield inaccurate predictions for complex structural configurations. Consequently, advanced computational modelling has been employed to more effectively assess the dynamic performance of these walls. Among three profiled sections investigated by Boh et al.^[2], the intermediate profile was found to exhibit the greatest ductile

capacity, surpassing both the deep and shallow sections. Louca et al.^[3] performed numerical studies on panel deformations and demonstrated that the use of SS316L material facilitated better energy dissipation. The influence of end connection details was investigated by Schleyer et al.^[4] through field tests, which found that more flexible supports led to larger displacements but could also delay the onset of beneficial membrane action.

In engineering practice, blast overpressure and duration are determined via detailed explosion risk analyses, with results often presented as exceedance curves (annual frequency vs. overpressure). Current design codes, such as those from Oil and Gas UK^[5], mandate the design for two distinct events: strength level blast (SLB, with a 10^{-3} per annum exceedance) and the ductility level blast (DLB, with a 10^{-4} to 10^{-5} per annum exceedance). Under SLB, the wall must remain essentially elastic, whereas limited plasticity is permissible under DLB, provided overall integrity is maintained. Deformations are typically capped at 300 mm or the clearance to adjacent equipment, whichever is smaller, and plastic strains at welded connections must not exceed 5% to prevent tearing. Consequently, the design of blast walls for extreme loads must address three major challenges: large plastic deformations, potential clash with adjacent structures, and the risk of weld rupture at supports.

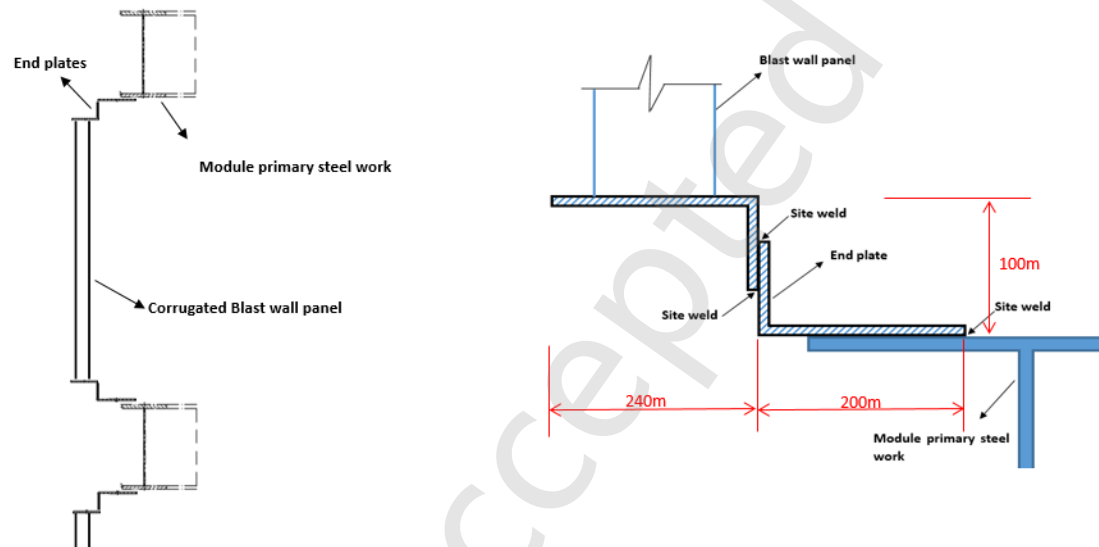
Traditionally, two types of structural configurations are utilised (Figure 1). The Type 1 design features a cantilevered wall connected via welded end plates extended from the primary module steelwork flanges. This configuration may suffice for weak blasts (0.5–1 bar), as shallow, light sections can remain elastic. However, recent large-scale tests indicate that overpressures in typical offshore topside modules can reach up to 4 bar^[6]. For such high overpressures (2–4 bar), the Type 1 design becomes inadequate due to large plastic deformations, necessitating larger sections and improved support details. The Type 2 design addresses this by incorporating additional supporting steelwork cantilevered from the module perimeter beams. Unlike Type 1, which has low axial stiffness, Type 2 provides greater resistance through panel membrane action, delaying buckling and reducing deflections. Nevertheless, ensuring the requisite boundary restraint in offshore environments is challenging. Connections are particularly vulnerable to rupture under severe loading, as they must accommodate translational shear, in-plane membrane forces, and extensive rotations for energy absorption. Gusset plate strengthening (Figure 1b) may be employed for

intense blasts to curtail deflections; however, this measure intensifies membrane forces at welded joints, where local stress concentrations can precipitate progressive failure. The resulting failure mode may transform from a ductile flexural failure of the wall itself to brittle shear failure or tensile tearing at the support connections, consistent with failure modes II and III defined by Menkes and Opat^[7]. Thus, traditional designs often struggle to achieve a compromised balance between controlling wall deflections and preventing weld failure.

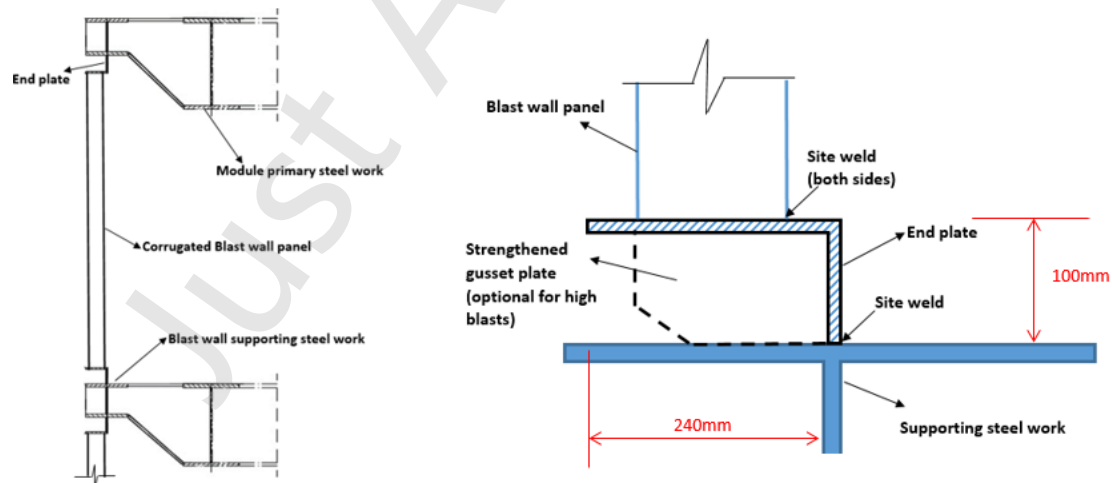
This study proposes a new design concept for blast walls adopted in oil and gas platforms through the application of energy absorption supports, aiming to simultaneously satisfy code requirements for both deflection limits and weld integrity^[8,9]. The design energy absorption device is shown in Figure 1c, conventional endplates are replaced by these devices, which are installed at the interface of the module edge girders and blast wall panels to create a flexible and energy consumption support. Each device, comprising a sliding core, a roller core, and an outer steel shell, is designed with internal voids to allow simultaneous sliding and rotation, specifically targeting the mitigation of mode II and III failures^[7]. Bridge supports commonly incorporate similar devices to dissipate energy from seismic events or thermal expansion. To boost its energy absorption capability in this design, the sliding cavities are filled with PMI foam, and the roller core fitted with linear springs. Under blast loading, the sliding core, coupled with the blast wall panel, can retract behind the plate girder edge to circumvent the peak blast overpressure, whilst blast energy is absorbed and dissipated through the deformation of the foam material. The significant crushing plateau of the PMI foam acts as a cushion, preventing a sudden spike in reaction shear forces and thereby greatly minimizing the likelihood of shear failure in the welds. Meanwhile, the function of linear springs positioned about the roller core begin to manifest, for they can provide significant rotational resistance, particularly during intense blast events. In summary, the main novelty of the proposed system lies in the integration of independently controllable translational and rotational support mechanisms. The PMI foam-filled sliding component dissipates blast energy through controlled crushing deformation, while the rotational spring provides adjustable rotational restraint. Their combined action enables simultaneous regulation of panel deflection, membrane-force development, and local connection demand, thereby providing a balanced design approach that is difficult to achieve using conventional rigid or semi-rigid blast-wall supports. In this investigation,

numerical comparison studies between traditional and the proposed designs using corrugated blast wall as an example are carried out to demonstrate the feasibility and effectiveness of the proposed design concept. Afterwards, the influences of some key parameters on the structural performance and behaviour of the proposed design are numerically investigated to optimise the proposed design.

(a) Traditional Type 1 Design and Support Detail



(b) Traditional Type 2 Design and Support Detail



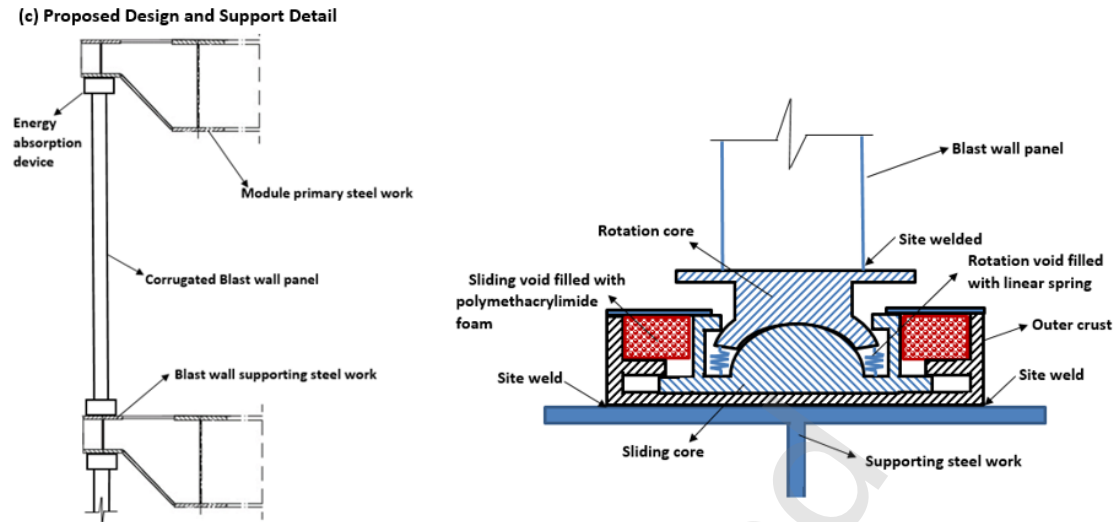


Fig. 1 Comparisons of traditional design and proposed design configurations.

2. Numerical modelling

2.1 Finite element models and boundary conditions

Finite element (FE) simulations were performed using ABAQUS. A single strip of the corrugated blast wall panel, with section dimensions shown in Figure 2a, was modelled using S4R shell elements. The panel length was 3 m. Three FE models were developed for comparative study: the traditional Type 1, traditional Type 2, and the design in this study.

For the sake of simplicity and illustrative purpose, the model comprised only the endplates to capture the support rigidity, with the adjoining structures assumed as rigid supports in both design cases. The endplates for Type 1 and Type 2 designs are typically angle sections (Figure 1), with thicknesses of 10 mm for SLB and 15 mm for DLB events. Symmetry boundary conditions were imposed by restraining the corrugated panel edges in the global UX, RY, and RZ directions, simulating continuous panel behaviour. For the two traditional designs, all edge nodes of the endplates were fully fixed, as illustrated in Figures 3a and 3b.

For the proposed design, two pieces of energy-absorbing foam, each has a height of 62.5 mm $\times$ a depth of 100 mm, were positioned across the entire width of the panel strip, oriented in the direction of blast loading. The mechanical behaviour of the PMI foam is depicted in Figure 2b. Its compressive Young's modulus was determined at a compressive strain of 5%, following Arezoo et al.^[10], while other properties were computed according to Gibson and Ashby^[11]. Two PMI foam

densities were selected for SLB and DLB events, with relative densities of 10% and 23%, respectively. The corresponding compressive Young's moduli are 50 MPa and 125 MPa, with plastic collapse stresses of 2.5 MPa and 6.25 MPa. Both foams exhibit an elastic unloading strain of 5%, while the densification strains are 86% and 69%, respectively. Rotational stiffness is provided by the push-pull mechanism formed by the linear springs positioned around the rotation core, with values of $K_r = 400 \text{ kN}\cdot\text{m/rad}$ for SLB and $300 \text{ kN}\cdot\text{m/rad}$ for DLB. To ensure computational stability, the crushing plateau stiffness of the foam (K_{t2}) was set to 0.01 of its initial elastic stiffness (K_{t1}). Table 1 provides a comprehensive summary of the translational and rotational spring properties.

Consistent with Chen and Hao^[12], the behaviours of energy absorbing supports were represented by nonlinear connector elements in ABAQUS. These springs were defined using the keyword *CONNECTOR SECTION, AXIAL, ROTATION, which permits independent translational and rotational movements. The spring force-displacement relationships (Table 1) were implemented via the *CONNECTOR BEHAVIOR keyword, which can simulate elastic and plastic deformation, as well as unloading and arresting actions. The connector elements link the centroid node of the end panel section (where global UZ is restrained) to a boundary node with all degrees of freedom fixed (Figure 3c). Since the endplate height for both traditional cases is 100 mm, the connector elements for the proposed design were similarly positioned at an equivalent 100 mm offset for consistency. The connector-based representation is intended to reproduce the equivalent support-level force-displacement and moment-rotation relationships rather than the detailed local behaviour of individual device components. This simplification is considered appropriate for the present global response and parametric studies. Local contact, friction, component deformation, and manufacturing tolerances are not explicitly represented and should be examined in future detailed numerical and experimental investigations.

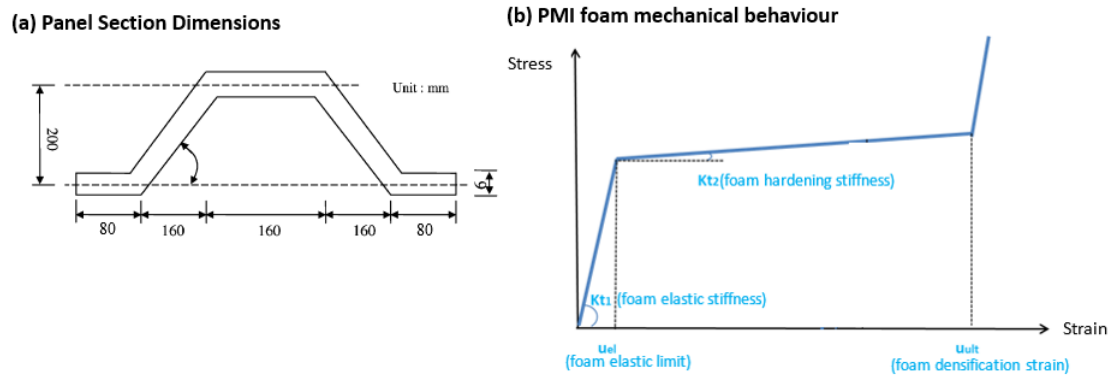


Fig. 2 Panel section view and PMI foam mechanical behaviour.

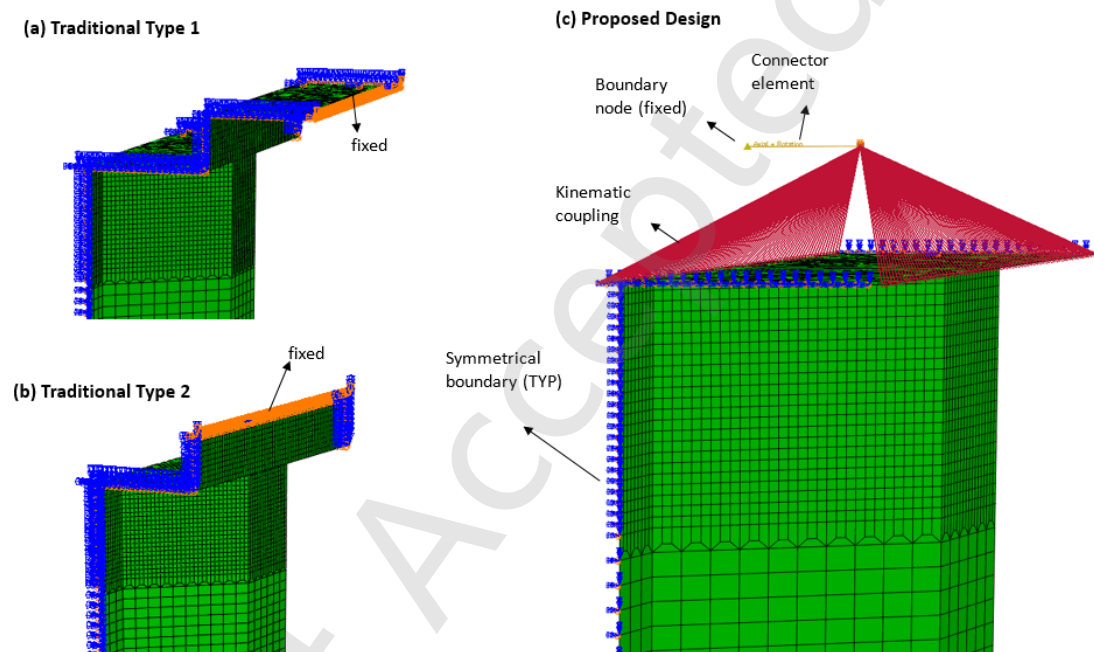


Fig. 3 FE model boundary conditions.

Table 1 PMI foam mechanical properties.

Spring name	Translational spring K_t				Rotational spring, K_r (kN·m/rad)
	K_{t1} (kN/m)	$K_{t2}=0.01 K_{t1}$ (kN/m)	Foam elastic limit, u_{el} (mm)	Densification displacement, u_{ult} (mm)	
Springs for SLB	20000	200	5	86	400

Springs for DLB	50000	500	5	69	300
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2.2 Mesh sensitivity tests

Mesh sensitivity test is performed with element size of 30 mm, 20 mm and 10 mm by applying pinned boundary conditions to both panel ends directly (i.e. without endplates). The results as seen in Figure 4 indicate that the mid-span time-history deflections for different mesh sizes are in close agreement and different mesh size has little impact on computational accuracy. A baseline mesh size of 20 mm was chosen to optimise computational efficiency while maintaining accuracy. Element counts for the Type 1, Type 2, and proposed design FE models are 17,204, 14,324, and 12,886, respectively.

Since equivalent plastic strain is more sensitive to mesh density than global displacement, an 8 mm local mesh was adopted around the support connection regions to improve the resolution of strain concentrations (Figure 3). The reported local equivalent plastic strain (PEEQ) values are therefore used primarily as indicators of connection demand. A fully mesh-independent prediction of local fracture would require a dedicated fracture model and additional local mesh-sensitivity assessment, which is beyond the scope of the present study.

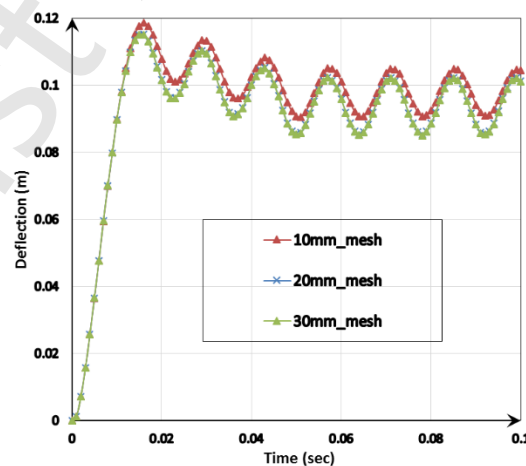


Fig. 4 Mesh sensitivity test results.

2.3 Material property

Stainless steel AISI316L is commonly adopted in offshore blast wall design. The full-range

stress-strain curves for stainless steel were derived by Rasmussen^[14], who found that traditional curves based on the Ramberg-Osgood expressions were inaccurate for stresses beyond the 0.2% proof stress. Consequently, the formulations were modified based on experimental testing results. The modified Ramberg-Osgood expressions are presented in Eq. (1) in terms of nominal stress-strain relationships, with material parameters given in Table 2.

$$\varepsilon = \begin{cases} \frac{\sigma}{E_0} - 0.002 \cdot \left(\frac{\sigma}{\sigma_{0.2}} \right)^n, & (\sigma \leq \sigma_{0.2}) \\ \frac{(\sigma - \sigma_{0.2})}{E_{0.2}} + \varepsilon_u \left(\frac{\sigma - \sigma_{0.2}}{\sigma_u - \sigma_{0.2}} \right)^m, & (\sigma > \sigma_{0.2}) \end{cases} \quad (1)$$

The Cowper–Symonds relationship was employed to capture strain rate effects, as follows:

$$\frac{\sigma_{dy}}{\sigma_y} = 1 + \left(\frac{\dot{\varepsilon}}{D} \right)^{1/q} \quad (2)$$

Here, σ_{dy} and σ_y are the dynamic and static yield stresses, respectively, with $\dot{\varepsilon}$ the plastic strain rate. The Cowper-Symonds constants for AISI316L are $D = 240 \text{ s}^{-1}$ and $q = 4.74$ ^[1]. With a representative strain rate of 0.422 s^{-1} ^[13], the dynamic yield stress was found to be 316 MPa. For the ABAQUS analyses, the engineering stress-strain data were transformed into true stress-strain curves, as indicated in Figure 5.

Table 2 Material parameters for AIS316L.

Parameters	Values
Initial Young's modulus E	200 GPa
Tangent modulus at 0.2% strain $E_{0.2}$	23.541 GPa
0.2% proof stress $\sigma_{0.2}$	316 MPa
Ultimate plastic stress σ_u	616 MPa
Ultimate plastic strain ε_u	0.487
Strain hardening constant n	5.88

Strain hardening constant m	2.8
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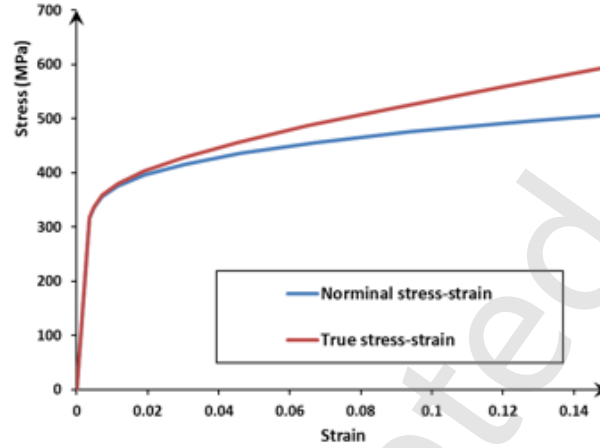


Fig. 5 AISI316L material stress-strain curve.

2.4 Loading and analysis

The blast load was idealised as a triangular pulse with zero rise time to represent a far-field vapor cloud explosion. A blast duration of $t_d = 50$ ms was adopted, which is typical for hydrocarbon explosions. Two peak overpressures, $p_0 = 1$ bar and 3 bar (1 bar = 100 kPa), were considered to represent SLB and DLB events, respectively. The blast pressure was applied as a distributed normal load on all shell elements of the panel using the keyword *DLOAD.

To reflect realistic design conditions, both global and local geometric imperfections were introduced into the model. Following DNV-RP-C208^[15], global and local imperfections of $L/300 = 10$ mm and $s/200 = 3$ mm were seeded via the *BUCKLE and *IMPERFECTION keywords, respectively, where L is the panel length and s is the panel width. All analyses were conducted in the nonlinear dynamic time domain using the implicit solver in ABAQUS/STANDARD.

3. Results and discussion

3.1 Comparisons between traditional and proposed design

To demonstrate the effectiveness of the proposed design scheme, a comparative assessment of the numerical results is conducted between the traditional designs (Type 1 and Type 2) and the

proposed design, with respect to panel peak deflections, permanent deflections, peak von-Mises stresses, and equivalent plastic strains (PEEQ) at the supports.

3.1.1 SLB event

Figure 6 presents the von-Mises stress contour plots at the instant of peak deflection for the SLB event (1 bar). The maximum stresses are reported as the envelope stress of the top and bottom surfaces, with a deformation amplification factor of 1.0. As shown, the maximum stresses are 406.3 MPa, 450.2 MPa, and 345.9 MPa for Type 1, Type 2, and the proposed design, respectively. Notably, the proposed design exhibits the lowest peak stress among the three configurations, and this value only marginally exceeds the material yield stress, indicating a predominantly elastic response.

Figure 7a compares the mid-span deflection time-histories for all three designs. The peak deflections are 26.2 mm, 15.3 mm, and 20.0 mm for Type 1, Type 2, and the proposed design, respectively. In the absence of damping, the panels continue to vibrate after the blast load subsides. The average permanent deflections are approximately 7 mm for Type 1 and 4 mm for Type 2; in contrast, the proposed design exhibits a permanent deflection of virtually zero (approximately 1 mm), demonstrating excellent elastic recovery.

These results indicate that both Type 1 and Type 2 configurations fail to comply with the code requirement for SLB events, as relatively large plastic deformations have developed due to over-stressing at the endplate connections. In the proposed design, however, the high stresses at the supports are effectively dissipated through the displacements and rotations of the flexible support devices. Consequently, a smaller proportion of blast energy is transferred to the panel itself. Under identical loading conditions, the proposed design therefore yields almost negligible plastic deflections, satisfying the SLB design criteria.

3.1.2 DLB event

For the DLB event (3 bar), the advantages of the proposed design become substantially more pronounced. As previously discussed, the primary concerns for DLB events are excessive panel deflections and the risk of weld rupture at the supports. In the present numerical study, the welds were omitted from the explicit FE model; instead, weld failure was assessed by a strain-based criterion, capping plastic strains at 5%, which is the limit prescribed by ISO 19902^[16] and other

design standards. Figure 8 presents a comparison of the deformed geometries and PEEQ contours at the panel-endplate interfaces for the three designs. For Type 1, due to the low axial stiffness provided by the endplate bending, significant local buckling of the panel is observed at mid-span, accompanied by a maximum PEEQ of 15.92% at the mid-span and 12.6% at the welded supports. Correspondingly, the mid-span deflection is substantial, reaching 303.3 mm (Figure 7b), which exceeds the typical 300 mm code limit. These results demonstrate that the Type 1 configuration is inadequate for DLB events. It should be noted that the welds were not explicitly modelled in the present FE analyses. Therefore, the 5% plastic-strain limit is used as an engineering surrogate for assessing excessive deformation demand in the support region rather than as a direct numerical prediction of weld fracture. Explicit simulation of weld tearing would require detailed modelling of the weld geometry, heat-affected zone, and appropriate damage and fracture criteria.

In contrast, the Type 2 design exhibits a significantly reduced panel deflection of 111.1 mm compared to Type 1. This improvement is attributed to the endplates being positioned close to the compression flange of the panel, which effectively mobilises membrane action that counteracts panel compression. Consequently, no local buckling is observed in the Type 2 panel (Figure 8b). However, this enhanced performance comes at the cost of exceedingly high PEEQ at the supports, reaching 26.68%, which far exceeds the 5% allowable limit and poses a severe risk of weld tearing.

Neither traditional design configuration is therefore capable of simultaneously satisfying both deflection and strain criteria under DLB conditions. By contrast, the proposed design achieves a PEEQ of only 4.54%, which is below the 5% limit. Minor local buckling is observed at the compression flange of the panel, primarily attributable to the seeded local imperfections, but this does not compromise structural integrity. The peak total displacement, comprising panel deflection (167.7 mm) and foam deformation (67 mm), amounts to 234.7 mm, which is well within the 300 mm code requirement.

Two principal mechanisms account for the improved performance of the proposed design. First, the semi-fix connection formed by the spring system provides rotational flexibility that delays or controls the build-up of axial forces, thereby limiting the development of membrane forces at the supports. Second, the PMI foam's crushing plateau cushions against sudden shear force spikes,

reducing weld shear failure risk. By integrating adjustable rotations with controlled displacements, the proposed design effectively exploits membrane action to restrain panel deflection, while concurrently keeping support forces within a non-destructive range. This balanced approach ensures compliance with both deflection limits and weld integrity requirements, thereby enhancing the overall robustness and reliability of the blast wall under extreme loading conditions.

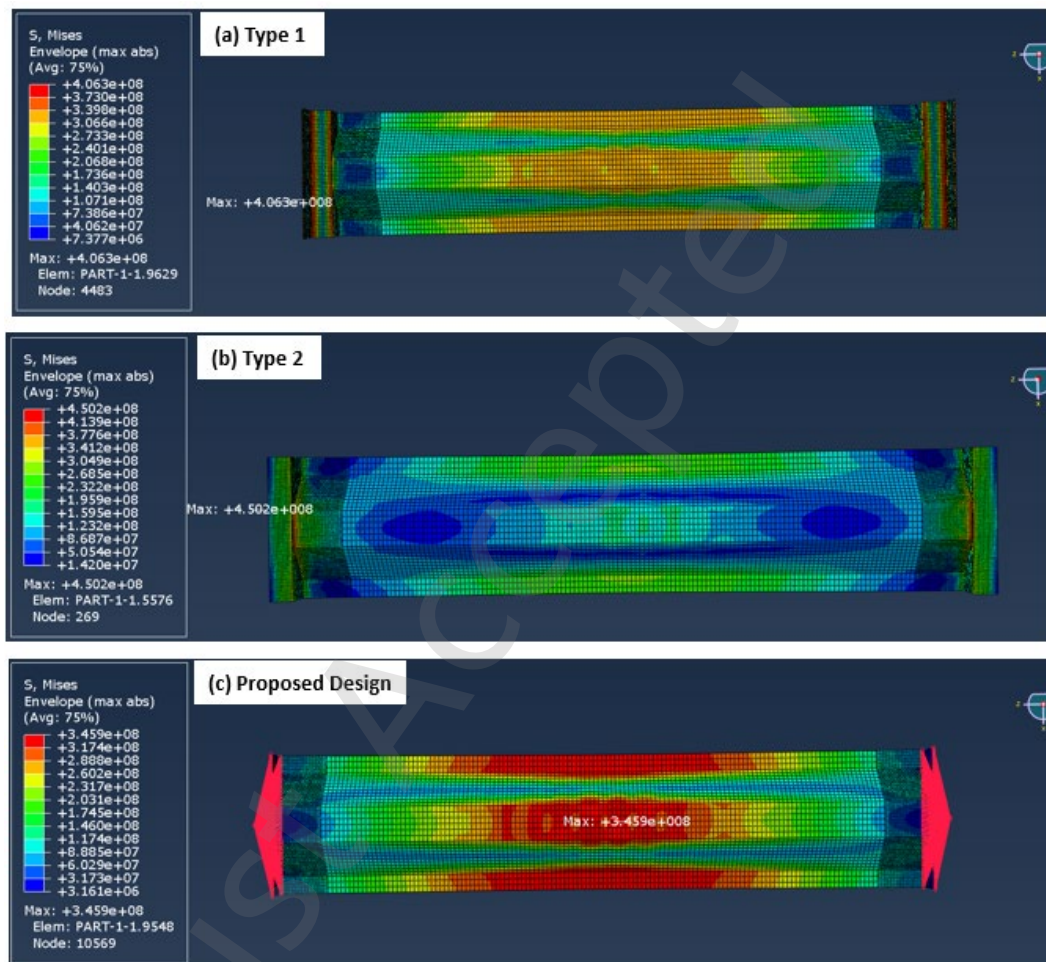


Fig. 6 Stress contour plots at peak panel deflection for SLB event.

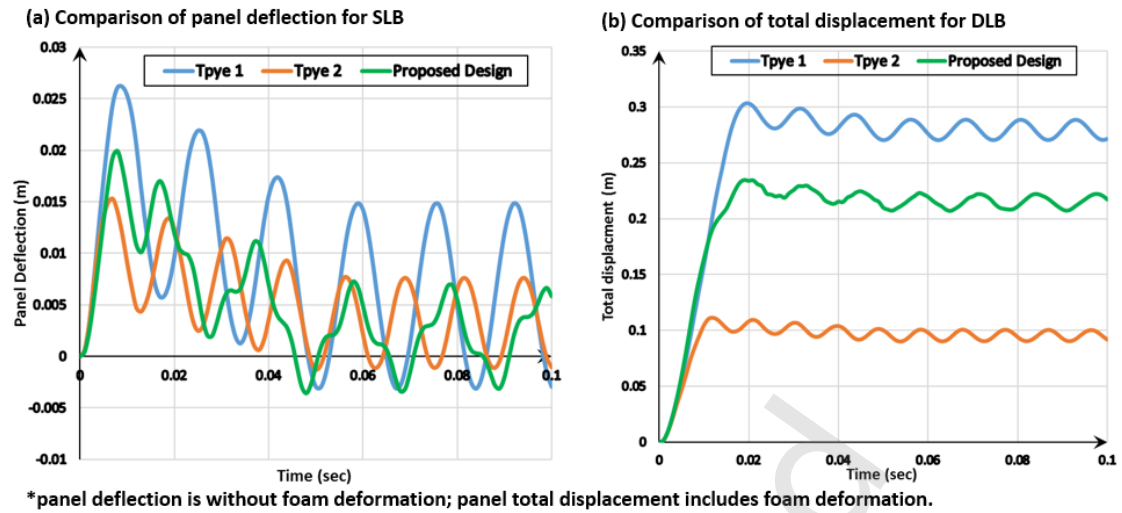


Fig. 7 Comparisons of panel deflections for SLB and panel total displacements for DLB.

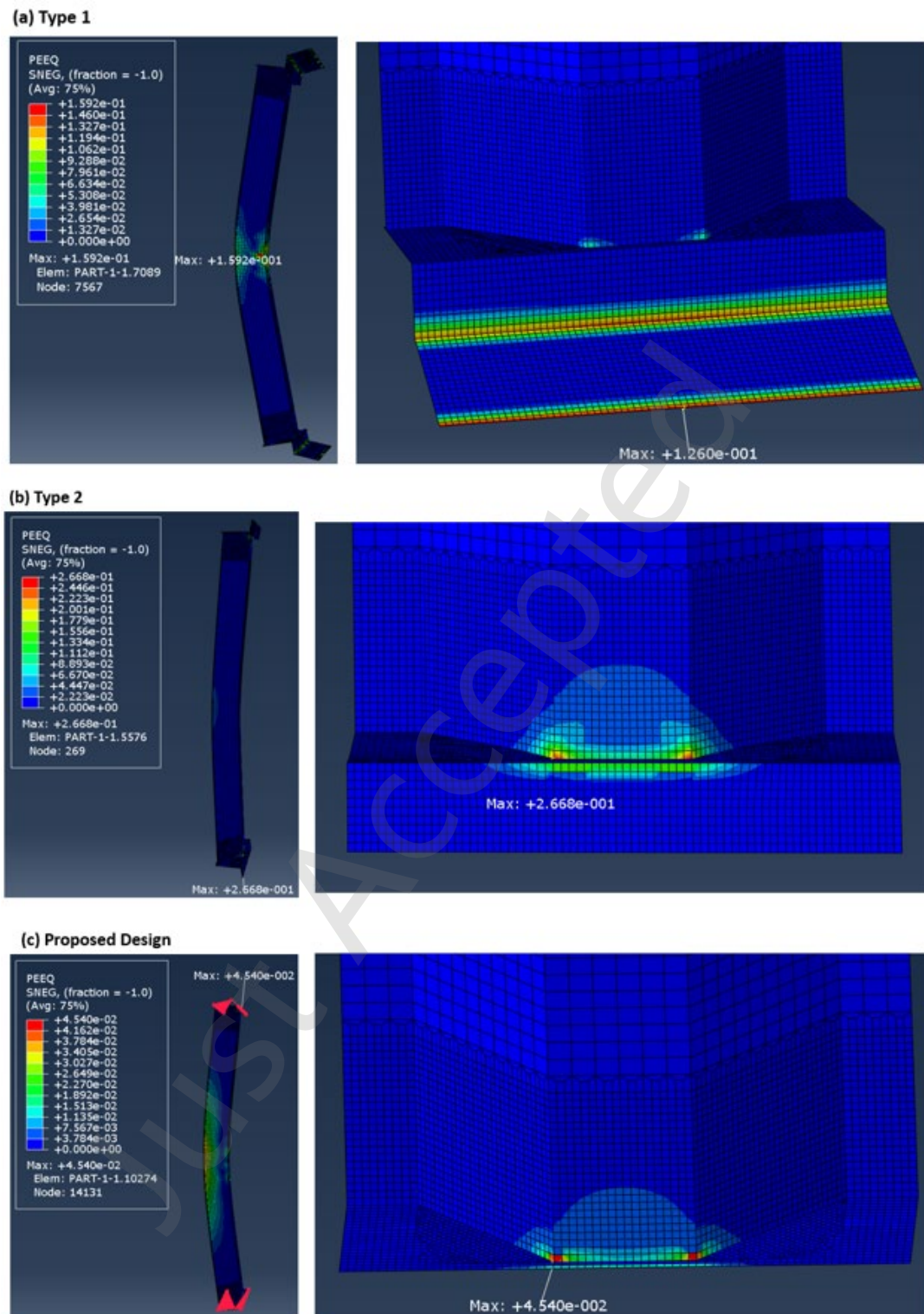


Fig. 8 Equivalent plastic strain (PEEQ) contour plots for DLB event.

3.2 Parametric studies

Having demonstrated the feasibility and effectiveness of the proposed design through above numerical studies, this section presents a parametric investigation into the influence of key design parameters on structural performance. Four parameters are examined: foam elastic stiffness (K_{II}),

foam hardening stiffness (K_{t2}), rotational spring stiffness (K_r), and blast duration (t_d). The results are discussed to provide guidance for optimal design.

3.2.1 Foam elastic stiffness K_{t1}

According to Gibson and Ashby^[11], the linear elastic stiffness of foam increases with relative density (ρ^*/ρ_s), though densification occurs earlier as porosity decreases. Microscopically, the deformation mechanisms differ between open-cell and closed-cell foams^[11]. In open-cell foams at low relative densities, deformation is governed by cell wall bending; as density increases, tension or compression of the cell walls becomes dominant, resulting in higher stiffness. For closed-cell foams, such as the PMI foam used in this study, the behaviour is more complex and depends on the fraction of solids in the cell edges (ϕ). When excessive material is concentrated in the edges, leaving thin cell faces, early rupture occurs and the foam subsequently behaves like an open-cell foam. Conversely, when a substantial fraction of solids resides in the cell faces, stiffness increases markedly with relative density. Through curve fitting of extensive experimental data, Gibson and Ashby^[11] derived two empirical ϕ values, $\phi = 0.8$ and 0.6 , corresponding to 20% and 40% of solids in the cell faces, respectively. The empirical formulae are given below^[11].

$$\frac{E^*}{E_s} = \left(\frac{\rho^*}{\rho_s} \right)^2 \quad (\text{open-cell}) \quad (3)$$

$$\frac{E^*}{E_s} = \phi^2 \left(\frac{\rho^*}{\rho_s} \right)^2 + (1 - \phi) \left(\frac{\rho^*}{\rho_s} \right) + \frac{p_{at}(1 - 2\nu^*)}{E_s(1 - \rho^*/\rho_s)} \quad (\text{closed-cell}) \quad (4)$$

$$\varepsilon_D = 1 - 1.4 \frac{\rho^*}{\rho_s} \quad (5)$$

where E^* is Young's modulus of the foam; E_s is Young's modulus of the foam solids; p_{at} is atmospheric pressure (0.1 MPa); ν^* is Poisson's ratio of the foam, taken as 1/3.

This section investigates the influence of K_{t1} under a 2 bar overpressure. Three stiffness values—small, medium, and large—were selected, denoted by Case 1, 2 and 3, respectively. The rotational spring stiffness and all other structural properties were kept consistent at 400 kN·m/rad. Panel deflection and foam displacement results are presented in Figure 9, with corresponding data summarised in Table 3.

For the large K_{tl} stiffness case (Case 3 in Figure 9), which is similar to the case of the DLB event in Section 3.1, the foams reach a maximum displacement of 4.96 mm, which is below the foam elastic limit ($u_{el} = 5$ mm), and subsequently rebound. This indicates that the large K_{tl} stiffness is excessively rigid in this context. Consequently, the foams behave like linear springs without energy absorption, resulting in a maximum panel deflection of 90.1 mm. For the low K_{tl} stiffness case (Case 1 in Figure 9), the panel deflection increases sharply from 16.8 mm to 117.8 mm over a period of 10 ms (highlighted in Figure 9). This excessive deflection is attributed to an additional impact load caused by the sudden stopping mechanism when the foams reach full densification strain, suggesting that a low K_{tl} stiffness may limit energy absorption capacity.

A comparison of these cases clearly demonstrates that the selection of K_{tl} has a significant influence on structural performance. To achieve an optimal design, K_{tl} should be selected such that the foam deformation terminates within the plastic crushing plateau, which represents the ideal energy absorption zone.

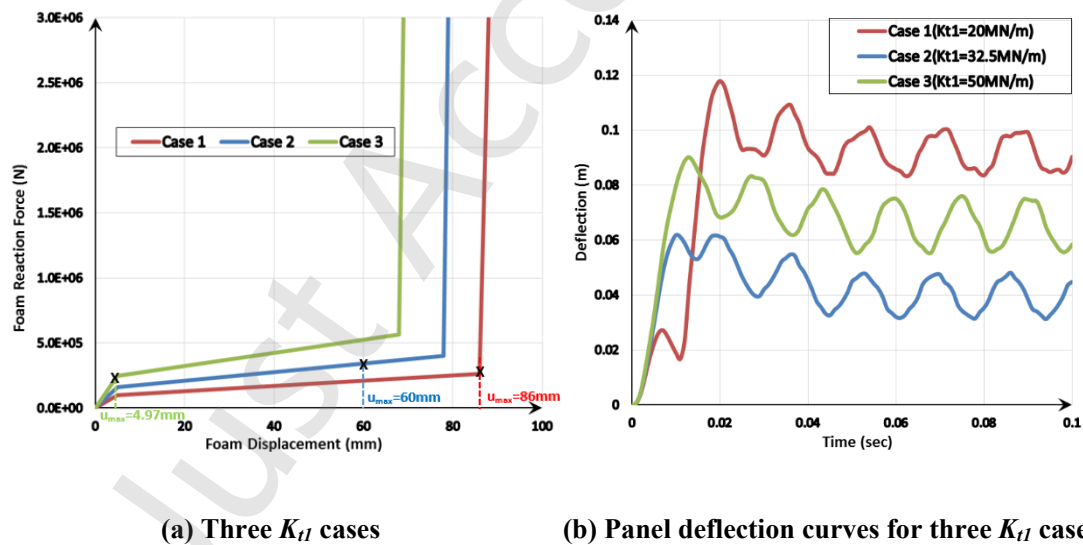


Fig. 9 Parametric study results for K_{tl} .

Table 3 Result summary for parametric study of K_{tl} .

Case	K_{tl} (kN/m)	K_{t2} (kN/m)	Densification displacement u_{ult} (mm)	K_r (kN·m/rad)	Panel max. deflection (mm)	Foam max. displacement (mm)
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Case 1	20000	$1/100K_{tl}$	86	400	117.8	86
Case 2	32500	$1/100K_{tl}$	78	400	61.9	60
Case 3	50000	$1/100K_{tl}$	69	400	90.1	4.97

3.2.2 Foam hardening stiffness K_{t2}

Generally, the stiffness in the foam crushing stage (referred to as the foam hardening stiffness, K_{t2} , in this study) is relatively low, often approximating a flat plateau. However, since the energy absorbed by the foam is equivalent to the area beneath its force-displacement curve, researchers have endeavoured to increase K_{t2} to enhance energy absorption. This can be achieved by employing alternative foam materials with suitable relative densities or by developing hybrid energy absorption devices. For example, Fan et al.^[17] conducted both experimental and numerical investigations into the dynamic lateral crushing behaviour of short sandwich circular tubes filled with aluminum foam. Their results indicated that, due to interaction effects, the force-displacement curve tended to increase beyond the plastic limit. Therefore, the parametric study in this section investigates the effect of hardening stiffness K_{t2} using the Case 2 spring properties listed in Table 4, with the corresponding foam behaviour shown in Figure 9a, as an example, the testing overpressure is 2 bar. Three cases with K_{t2} equals to 1/500, 1/100 and 1/50 of K_{tl} are studied, rotational spring stiffness and support ultimate displacement are unchanged. The panel deflections are illustrated in Figure 10 and corresponding results are summarised in Table 4. As shown, the deflections for the three cases are generally around 61–62 mm, with differences due to the variation in K_{t2} being minor (within 2 mm). In all three cases, the supports stop within the ideal energy absorption zones. A larger K_{t2} (Case 3 in Figure 10) tends to cause the foams to stop earlier, at a smaller displacement (51.8 mm), whereas in the low K_{t2} case (Case 1 in Figure 10), the foams are nearly compressed to full densification strain at a displacement of 76.1 mm. It is therefore concluded that, under the same blast loading, the effects of the hardening stiffness K_{t2} are relatively minor. Although foams with lower K_{t2} may undergo greater deformation and consequently produce less panel deflection, adopting a medium K_{t2} stiffness in design is more conservative, as it provides a larger safety margin.

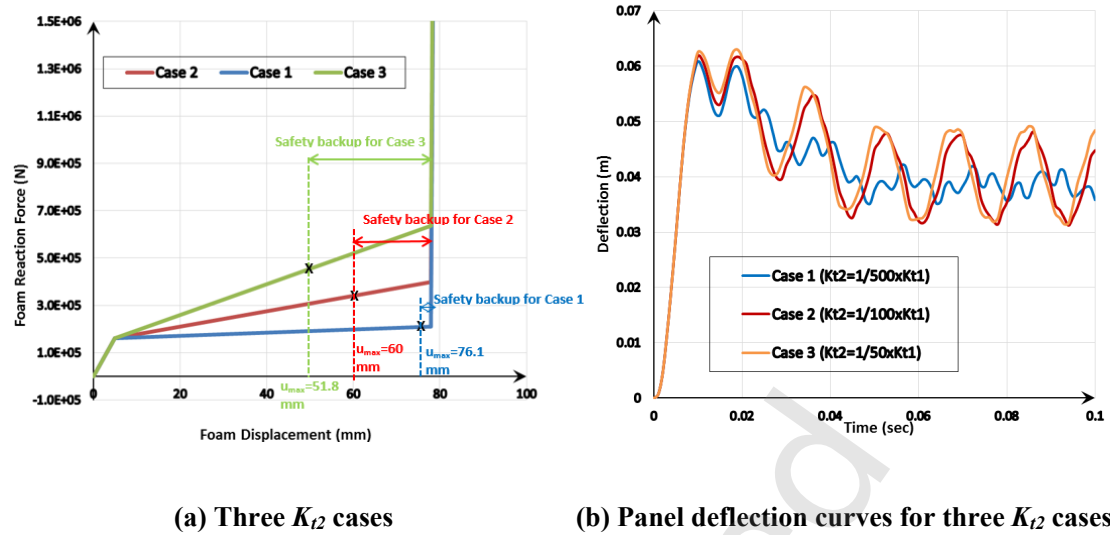


Fig. 10 Parametric study results for K_{t2} .

Table 4 Result summary for parametric study of K_{t2} .

Case	K_{t1} (kN/m)	K_{t2} (kN/m)	K_r (kN·m/rad)	Panel max. deflection (mm)	Foam max. displacement (mm)
Case 1	32500	$1/500K_{t1}$	400	60.9	76.1
Case 2	32500	$1/100K_{t1}$	400	61.9	60.0
Case 3	32500	$1/50K_{t1}$	400	63.0	51.8

3.2.3 Rotational spring stiffness K_r

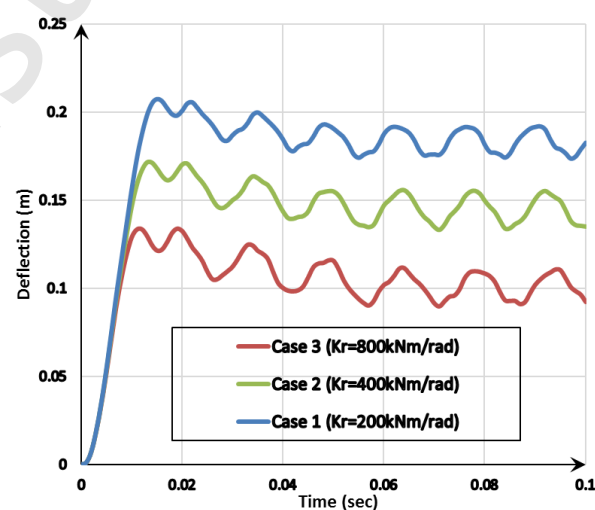
As stated earlier, the function of the rotational spring is to provide a semi-fixed connection with a certain degree of bending stiffness and rotational flexibility, enabling simultaneous control of panel membrane forces and end moments. Previous studies concluded that the use of linear rotational springs at supports offers advantages over nonlinear springs, as linear springs act as energy absorbers in the beam plastic stage and provide greater restraint to support rotations^[7,8]. However, their influence on plastic strains at the support connections cannot be overlooked.

This parametric study evaluates the effects of rotational spring stiffness K_r on both panel

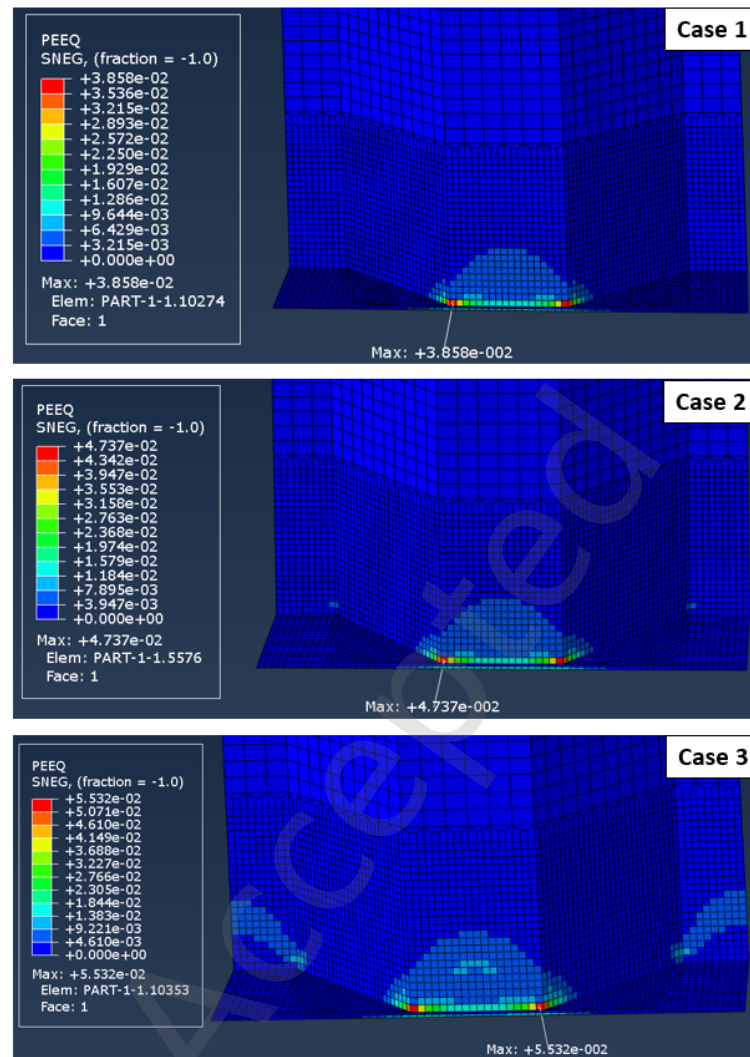
deflection and plastic strains at connections. The Case 3 in Table 3 and Figure 9 is selected as the base case for investigation. Three cases with K_r values of 200, 400, and 800 kN·m/rad are examined, while other structural properties, including translational spring stiffness and support ultimate displacement, are kept consistent across all cases (i.e., same as Case 3 in Table 3). The testing overpressure is 3 bar in this section. The mid-span deflection and support plastic strain contour plots are presented in Figure 11, with corresponding results summarised in Table 5.

As shown, the effect of rotational spring stiffness on panel behaviour is significant. The maximum panel deflections for $K_r = 200$ (Case 1 in Figure 11), 400 (Case 2 in Figure 11), and 800 kN·m/rad (Case 3 in Figure 11) are 207.2 mm, 171.5 mm, and 133.8 mm, respectively. A clear trend is observed that increasing K_r substantially reduces panel deflection. However, an opposite trend is noted for plastic strains at the supports, which increase with increasing K_r , reaching 3.86% for Case 1, 4.74% for Case 2, and 5.53% for Case 3. As previously mentioned, plastic strains must not exceed the 5% rupture limit for design; therefore, Case 3 is considered invalid despite yielding the lowest panel deflection.

In summary, while a larger K_r is beneficial for reducing panel deflection, it also attracts larger end forces at the supports. An optimal design should therefore balance both aspects according to the actual design conditions.



(a) Panel deflection curves for three K_r cases



(b) Support plastic strain contour plots for three K_r cases

Fig. 11 Parametric study results for K_r .

Table 5 Result summary for parametric study of K_r .

Case	K_{tl} (kN/m)	K_{t2} (kN/m)	K_r (kN·m/rad)	Panel max. deflection (mm)	Plastic strain at support connection
Case 1	50000	$1/100K_{tl}$	200	207.2	3.86%
Case 2	50000	$1/100K_{tl}$	400	171.5	4.74%

Case 3	50000	$1/100K_{tl}$	800	133.8	5.53%
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3.2.4 Blast duration t_d

In engineering design, blast overpressure and duration are determined by detailed explosion risk analysis, the results are usually provided in the form of exceedance curves with Y-axis representing event occurrence annual frequency and X-axis as explosion overpressure. Each design overpressure is normally associated with three blast durations: short, mean and long. Due to the dynamic nature of an explosion, all three durations shall be taken into account for blast wall design.

According to the Fire and Explosion Guidance^[5], the response of a structure to a dynamic load is commonly characterised by the ratio of the load duration (t_d) to the natural period (T_n) of the structure. Based on this ratio, three response regimes are defined: impulsive ($t_d/T_n < 0.3$), dynamic ($0.3 < t_d/T_n < 3.0$), and quasi-static ($t_d/T_n > 3.0$). Accordingly, prior to the parametric study, a natural frequency analysis was performed in ABAQUS using the keyword *FREQUENCY with the EIGENSOLVER = SUBSPACE option. The first six modes are illustrated in Figure 12; the first mode corresponds to out-of-plane bending with a natural period of 14.8 ms. Given that most explosion durations range from 20 ms to 200 ms^[13], the responses of this blast wall panel are expected to fall within the dynamic or quasi-static regimes, in which the blast duration can significantly influence structural behaviour, particularly when the response is plastic.

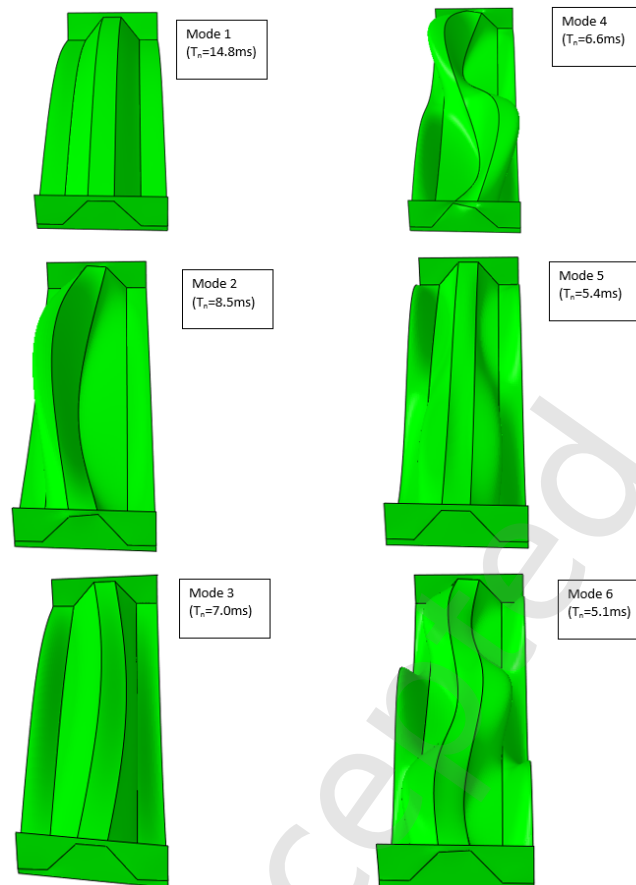


Fig. 12 Fundamental mode shapes and natural periods of the blast wall panel.

The parametric study in this section assesses the responses of the blast wall panel under three blast durations: 30 ms (short), 50 ms (mean), and 100 ms (long), using a 2 bar overpressure and the spring properties for Case 2 in Table 4. Both traditional and proposed design schemes were studied with the blast profiles. The results are summarised in Table 6, and the corresponding panel mid-span deflection time-histories are presented in Figure 13.

For the traditional design, the peak deflections are predicted to be 89.4 mm for the 30 ms duration (Case 1), 110.5 mm for the 50 ms duration (Case 2), and 134.4 mm for the 100 ms duration (Case 3). A clear trend is observed: as blast duration increases, the panel deforms more due to greater input blast energy. In all three cases, the time steps at which the blast wall panels reach their peak deflections (t_{peak}) are relatively concentrated, ranging from 13 ms to 16 ms, which appears to be independent from t_d . Compared to the traditional design, the peak deflections obtained from the proposed design are substantially reduced. They are 58.2 mm, 61.9 mm, and 88.6 mm for the short, mean, and duration cases, respectively. These values are at least 34% lower than their counterparts from the traditional design, once again demonstrating the excellent energy absorption capability of

the proposed design. Notably, for short and mean duration results, greater deflection reductions are achieved with increasing blast duration (44% reduction for mean duration, while 35% for short duration), implying more effective energy dissipation under higher input energy. However, this trend does not been extended to long duration result. Examination of the deflection history for the long duration case reveals two peaks with a climbing pattern in between, occurring between 10 ms and 23 ms. This is because the safety backup zone has been completely consumed, and the supports have been compressed to the full densification limit of 78 mm at 18 ms. Consequently, the panel deflection rises again to absorb the remaining input blast energy. Therefore, the deviation of the long duration result from the trend is because the energy-absorption potentials of the supports are fully exploited, so that the panel has to deform more to dissipate the remaining energy.

In summary, based on the t_d/T_n ratios, blasts with long durations are more onerous in design as they possess greater energy and the panel tends to respond in a quasi-static manner. Furthermore, the results in this section emphasise the importance of providing a sufficient safety backup zone, which can be achieved by employing materials with a longer stroke or a larger K_{I2} stiffness.

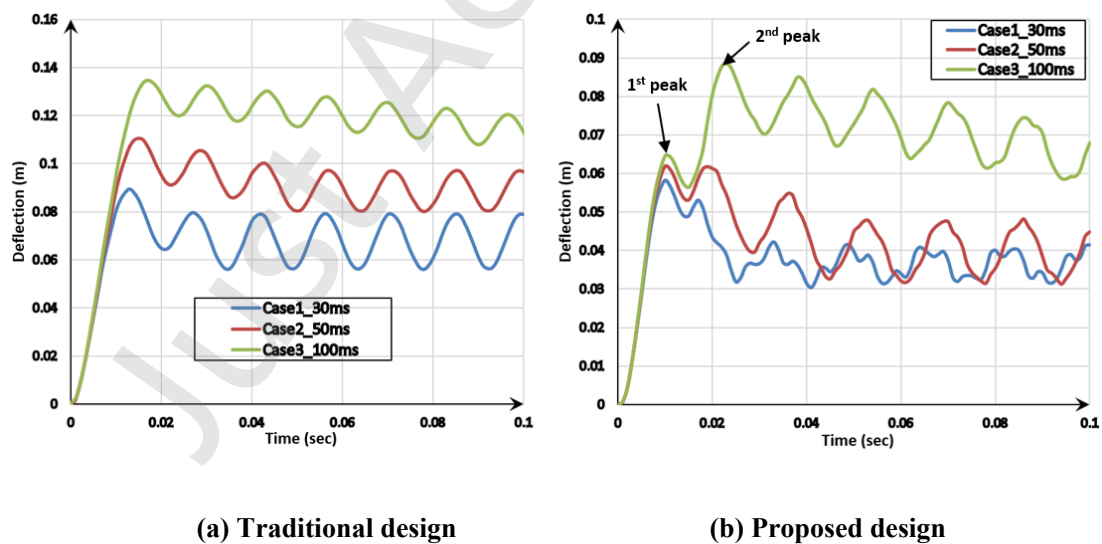


Fig. 13 Parametric study results for t_d .

Table 6 Result summary for parametric study of t_d .

Case	t_d (ms)	Traditional design (Type 2)		Proposed design			Deflection reduction
		Max.	t_{peak} (ms)	Max.	t_{peak}	u_{max}	

		deflection (mm)		deflection (mm)	(ms)	(mm)	
Case 1	30	89.4	13	58.2	10	36.7	-35.0%
Case 2	50	110.5	15	61.9	10	60	-44.0%
Case 3	100	134.4	16.5	88.6	23	78	-34.1%

4 Conclusions

The present study proposes a novel offshore blast wall design concept that incorporates energy absorption devices at the supports, aiming to achieve code compliance in terms of maintaining operational function, reducing panel deflection, and minimizing weld rupture risk. Comparative studies between traditional designs and the proposed design using the finite element method have demonstrated the feasibility and effectiveness of the new concept, as exemplified with a typical stainless steel corrugated blast wall. Meanwhile, parametric investigations into the influences of four key design parameters on structural performance are conducted to provide guidance for optimal design. The key findings are summarised below:

- The proposed energy-absorbing support design outperforms traditional schemes: under SLB, it exhibits negligible plastic deformation (~1 mm vs. 7 mm and 4 mm for traditional schemes); under DLB, it limits panel deflection to 167.7 mm (44% reduction vs. traditional schemes) and maintains support plastic strains at 4.54% (below the 5% limit), whereas traditional designs fail to satisfy both deflection and strain criteria (303.3 mm deflection and 26.68% PEEQ).
- Foam elastic stiffness K_{t1} is important for energy absorption and reducing panel deflection. In an optimal design, the selection of K_{t1} should allow the supports to stop within the foam plastic crushing zone, which is ideal for energy absorption. The value of K_{t1} can be controlled by adjusting the foam relative density.
- Foam hardening stiffness K_{t2} is less influential than K_{t1} in reducing panel deflections. Under the same blast overpressure and duration, a relatively steeper K_{t2} tends to make the support stop earlier and farther from the foam densification limit, thereby leaving a larger safety backup zone. The value of K_{t2} can be controlled by using hybrid energy absorption devices.

- Increasing rotation spring stiffness K_r can efficiently reduce blast wall panel deflections, but it also causes membrane force build-up at support connections and consequently higher plastic strains. Therefore, a balanced consideration is required in engineering design.
- Since the natural periods of blast wall panels are relatively short, long-duration blasts are more onerous in blast wall design due to the t_d/T_n ratios. A sufficient safety backup zone may be required to prevent the supports from being compressed to densification. This can be achieved by using a material with a long stroke or a larger K_{t2} stiffness.

It should be noted that the findings of this study are based solely on numerical simulations. Future work should include experimental testing and analytical validation to confirm the accuracy and reliability of the proposed design under realistic blast conditions

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Author contribution statement

Xuanchi Liu is responsible for Writing – original draft, Writing – review & editing, Formal analysis. Siraj Hussain SYED is responsible for Resources, Software. Muhammadh Khalid is responsible for Software, Validation. Haiming Li is responsible for Methodology, Data curation. All the authors have approved the final manuscript.

Data availability

The data is available from the corresponding author upon reasonable request.

Declaration of competing interest

The authors (including those from industries) declare that ave no competing interests relevant to the content of this article.

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