

Comparative Blast Performance of Arched and Three-Layer Space-Frame Shields for Offshore Platforms

Mostafa Habibi , Reza Dezvareh ^{*} 

Faculty of Civil Engineering, Babol Noshirvani University of Technology, Babol, Iran

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ABSTRACT

Accidental gas explosions on fixed offshore jacket platforms can impose short-duration, high-intensity loads on safety-critical topside and primary structural components. This study evaluates two lightweight blast-shield concepts intended to reduce structural demand: a convex arched double-layer space frame that promotes shock-wave deflection and a flat three-layer space frame that increases out-of-plane stiffness and member stability. Three shield configurations were generated using formex-based topology processing and analyzed in Abaqus/Explicit under a standardized 2000 kg TNT-equivalent airblast at an 8.0 m stand-off distance scaled distance $Z = 0.63 \text{ m/kg}^{1/3}$. Relative to the flat double-layer baseline, the arched shield reduced internal strain energy by approximately 25% (from 7215 to 5400 kJ), peak transmitted base shear by approximately 23% (from 1100 to 850 kN), and maximum central displacement by approximately 30% (from 2.33 to 1.62 m). The three-layer configuration provided the greatest deformation control, reducing the maximum central displacement by approximately 51% to 1.15 m, while its additional structural depth improved member-stability potential. The three-layer configuration limited the maximum central displacement to 1.15 m compared with 2.33 m for the baseline and reduced the characteristic unbraced length of the internal diagonal members, thereby improving their member-stability potential. The results indicate that curvature is effective when weight and force transmission govern design, whereas the three-layer topology is preferable when displacement and local stability are controlling criteria. These findings are specific to the modeled geometry, material assumptions, and blast scenario and should be validated experimentally before design application. These results demonstrate a criterion-dependent trade-off: the arched configuration is advantageous when reduced structural demand and support-force transmission are prioritized, whereas the three-layer configuration is preferable when deformation control and member stability govern. The findings are scenario-specific and should be interpreted within the limitations of the adopted CONWEP-based numerical framework.

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1. Introduction

Fixed offshore jacket platforms support hydrocarbon extraction, processing, and temporary storage in congested environments where structural, process, and safety systems are closely integrated. In addition to wave, wind, current, and seismic actions, accidental gas explosions and subsequent fires can impose severe transient demands on topside modules and primary load-bearing members. The rapid pressure rise associated with an explosion may cause local plate deformation, connection failure, member

* Corresponding author.

E-mail address: r.dezvareh@nit.ac.ir (R. Dezvareh).



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buckling, and, in extreme cases, progressive collapse [1-6].

Conventional protection systems include corrugated steel blast walls, profiled plates, and heavily stiffened panels. These systems can protect living quarters and critical process modules, but large-scale application increases topside mass and may transfer substantial reaction forces to the supporting jacket. Flat panels are also vulnerable to out-of-plane deformation, local stiffener buckling, and tearing at welded boundaries under high impulsive pressure [7-20].

Lightweight sandwich concepts with honeycomb, auxetic, foam, or composite cores have been proposed to improve specific energy absorption. Their performance can be attractive for localized panels, although manufacturing complexity, connection design, inspection, and the need for secondary support frames may limit their application over the full height of an offshore platform [21-30].

A spatial truss offers an alternative load path. Double-layer space frames provide three-dimensional redundancy and distribute localized pressure reactions to major platform joints. When combined with a ductile low-yield-point (LYP) steel cladding, the outer plate can dissipate energy through membrane yielding while the space frame spreads the residual load. Formex algebra also permits systematic generation and pruning of spatial grids, allowing weight and stiffness to be adjusted without manually rebuilding each topology [31].

Nevertheless, a flat shield remains exposed to near-normal reflection over its central region, where the reflected pressure is greatest. Curved protective surfaces may reduce the effective normal blast demand by increasing local incidence obliquity away from the apex, providing a potential geometric mechanism for reducing the load transmitted into the supporting structure. Curvature therefore has the potential to reduce the energy entering the structure rather than relying exclusively on plastic absorption. Related offshore analyses have largely focused on flat blast walls, corrugations, sandwich panels, and global platform response [32-47].

1.1. Historical context and design requirements

Major offshore accidents have demonstrated that inadequate separation and protection between process areas, escape routes, and accommodation modules can amplify the consequences of fire and explosion. Modern accidental-limit-state design therefore requires the load path, ductility, connection capacity, and post-event stability of safety-critical structures to be assessed explicitly. General blast-resistant design guidance and the Iranian code for space structures provide the broader design context adopted in this study [31, 42, 48].

Recent studies have further advanced the numerical and design assessment of blast-resistant systems. Kim et al. [30] investigated the mechanical response of hexagonal honeycomb sandwich blast walls for offshore applications and developed a numerical-based design approach, while Kim and Sohn [49] examined the deflection response of corrugated blast walls over a broad range of geometric and loading conditions. More recently, Li et al. [50] investigated the nonlinear dynamic response and damage of an offshore platform subjected to flammable-gas explosions, including the effects of explosion intensity and source location, whereas Lee and Seo examined the dynamic response of corrugated blast walls under hydrogen blast loading. These recent developments highlight the continuing importance of structural configuration, blast scenario, and nonlinear numerical modeling in the design and assessment of offshore protective systems [30, 49-51].

1.2. Dynamic response to impulsive loading

Explosion loading is characterized by a rapid rise and short positive phase. Structural response is therefore controlled by inertia, stress-wave propagation, strain-rate sensitivity, and the ratio of load duration to the relevant natural periods. Static-equivalent analysis may not capture higher-mode excitation or transient force reversals; an explicit dynamic solution is consequently appropriate for the present problem.

1.3. Low-yield-point steel cladding

LYP steel was selected for the sacrificial cladding because its relatively low yield threshold and high ductility promote early membrane yielding and distributed plastic deformation. Accordingly, the 3.5 mm reference thickness was selected to maintain this sacrificial response while limiting additional mass and stiffness.

1.4. Research gap and objectives

Overall, previous offshore blast-protection studies have predominantly focused on conventional stiffened or corrugated blast walls, sandwich and composite panels, blast-wall arrangement, and the global response of offshore structures [7, 8, 11, 21, 32, 39]. In contrast, comparatively limited attention has been given to the direct comparison of alternative space-frame shield configurations under identical loading and support conditions. The present study addresses this gap by systematically comparing flat double-layer, arched double-layer, and three-layer space-frame shields in terms of blast demand, plastic response, deformation, member stability, energy response, and transmitted support reactions.

Previous offshore blast-protection studies have predominantly concentrated on conventional flat blast walls, corrugated panels, sandwich or cellular systems, and the global response of offshore structures. Although these approaches have established important principles for blast resistance, comparatively limited attention has been devoted to large space-frame shields in which geometric curvature or an additional structural layer is used to modify the load path and transient response. In particular, direct comparisons

of flat double-layer, curved double-layer, and three-layer space-frame shields under identical blast, material, and support conditions remain limited. Moreover, the trade-off between shield-level deformation control and the reactions transmitted to the supporting offshore structure has received relatively little attention.

Accordingly, the present study provides a comparative numerical assessment of three predefined configurations: (I) a flat double-layer baseline, (II) a convex arched double-layer shield, and (III) a flat three-layer shield. Their performance is compared in terms of incidence-angle-dependent blast demand, equivalent plastic strain, internal strain energy, central displacement, member-stability response, and transmitted base shear under a common severe airblast condition. The objective is therefore to identify the configuration-dependent trade-offs relevant to offshore blast-shield selection rather than to establish a universal optimum topology.

The novelty of the present work therefore lies not in proposing a new optimization algorithm or analytical formulation, but in providing a controlled comparative assessment of three distinct space-frame blast-shield configurations under identical loading, material, and support conditions. By simultaneously examining blast demand, PEEQ, displacement, internal strain energy, member-stability response, and transmitted base shear, the study identifies the different performance trade-offs associated with geometric curvature and additional structural depth and provides a more direct basis for configuration selection in offshore blast protection (Fig. 1).

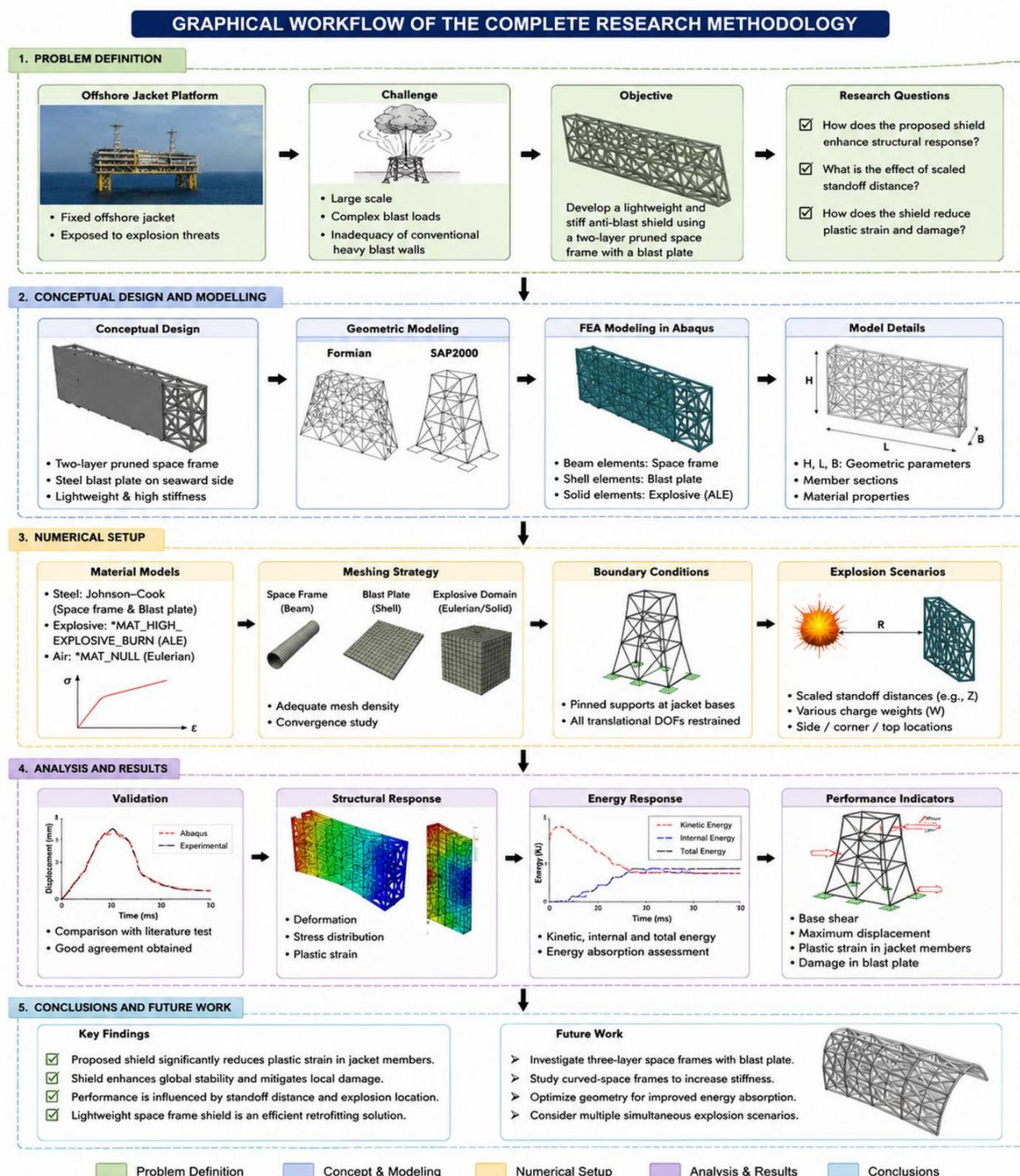


Fig. 1. Graphical workflow of the research.

2. Blast-load representation

2.1. Blast-load representation

The accidental gas explosion was represented by an equivalent TNT airblast so that all three structural configurations could be compared under an identical pressure field. The free-field positive phase was described using the modified Friedlander waveform (Eq. 1).

$$P_s(t) = P_{so}[1 - (t - t_a)/t_d] \exp[-b(t - t_a)/t_d] \quad (1)$$

where $P_s(t)$ is the incident overpressure, P_{so} is the peak incident overpressure, t_a is the arrival time, t_d is the positive-phase duration, and b is a waveform-decay coefficient.

For a surface normal to the incident shock and air with a specific-heat ratio of approximately 1.4, the normal reflected overpressure may be expressed as Eq. 2.

$$P_r = 2P_{so}(7P_0 + 4P_{so})/(7P_0 + P_{so}) \quad (2)$$

where P_r is the reflected overpressure and P_0 is the ambient pressure. The strong-shock limit of Eq. 2 approaches eight times the incident overpressure. On the arched surface, the angle between the incident wave direction and the local outward normal varies spatially; the CONWEP implementation evaluates the pressure history at each loaded surface location using its distance and incidence angle [42, 48]. Although CONWEP provides a computationally efficient representation of incident and reflected blast loading, it does not explicitly resolve the surrounding air domain or the evolving blast-wave field. Consequently, detailed diffraction, multiple reflections between structural components, shielding and confinement effects, and two-way fluid-structure interaction are not directly captured. These simplifications may affect the absolute pressure and impulse distributions and therefore the predicted deformation, plastic response, and support reactions. Their influence may be particularly relevant for the arched configuration because its predicted loading varies with the local surface incidence angle. Accordingly, the present results should be interpreted as comparative predictions within the adopted CONWEP framework rather than as a fully resolved simulation of blast-wave propagation and structural interaction.

2.2. Shield geometries and topology generation

The three shields were generated using formex-based configuration processing and were integrated with the same 85 m fixed-jacket platform model. The topology and member arrangement followed the geometric constraints of the Iranian Code of Practice for Design and Analysis of Space Structures [31].

Model A - flat baseline: A pruned double-layer space frame with a 2.0 m chord grid and a 1.0 m structural depth. The characteristic diagonal length was approximately 1.7 m.

Model B - arched deflector: A single-curvature double-layer frame obtained by mapping the baseline grid to a cylindrical surface. The central apex projected 1.5 m toward the blast source.

Model C - three-layer frame: A flat space frame with an intermediate chord layer at mid-depth. The added nodes reduced the characteristic unbraced diagonal length from 1.7 to 0.85 m.

All chord and web members were modeled as circular hollow sections with an outside diameter of 120 mm and a wall thickness of 6 mm. A 3.5 mm LYP steel cladding was attached to the exposed nodes of each shield. A cladding thickness of 3.5 mm was adopted as the reference value to preserve the intended sacrificial behavior of the LYP plate. The relatively thin cladding promotes distributed membrane yielding and plastic deformation before excessive demand develops in the supporting space frame, while limiting the added structural mass and avoiding an unnecessarily stiff protective skin. The selected value is therefore used as a reference design thickness rather than as a universally optimal value; the influence of cladding thickness is considered separately in the parametric assessment.

2.3. Material model

The structural steels were modeled with isotropic elasticity ($E = 210$ GPa, Poisson ratio = 0.30, density = 7850 kg/m³) and rate-dependent plasticity. The Johnson-Cook flow-stress relation used in the explicit analyses was as shown in Eq. 3.

$$\sigma_y = (A + B\varepsilon_p^n)[1 + C \ln(\dot{\varepsilon}_p/\dot{\varepsilon}_0)](1 - \theta^m) \quad (3)$$

where ε_p is the equivalent plastic strain, $\dot{\varepsilon}_p$ is the equivalent plastic-strain rate, $\dot{\varepsilon}_0$ is the reference rate, A is the initial yield parameter, B and n define strain hardening, C controls rate sensitivity, m is the thermal-softening exponent, and θ is the homologous temperature, calculated by Eq. 4.

$$\theta = (T - T_{trans})/(T_{melt} - T_{trans}) \quad (4)$$

For adiabatic plastic deformation, the corresponding temperature increment was evaluated from Eq. 5.

$$\Delta T = \chi / (\rho c_p) \int \sigma d\epsilon_p \quad (5)$$

where χ is the Taylor-Quinney coefficient, ρ is density, and c_p is specific heat. The adopted numerical parameters are summarized in Table 1. These parameters should be regarded as model inputs and require grade-specific experimental calibration before direct design use.

Table 1. Johnson-Cook parameters adopted in the finite-element model.

Component	A (MPa)	B (MPa)	n	m	T_{melt} (K)	T_{trans} (K)	c	$\dot{\epsilon}_0$ (s ⁻¹)
Primary jacket (S355)	1000	175	0.36	1.0	1523	25	0.02	0.004
Space frame (ST37)	1000	175	0.36	1.0	1523	25	0.02	0.004
Sacrificial cladding (LYP)	100	175	0.36	1.0	1523	25	0.02	0.004

2.4. Finite-element discretization and solution procedure

The analyses were performed with the explicit central-difference solver in Abaqus/Explicit Version 2020 [52]. Jacket and space-frame members were represented by B31 Timoshenko beam elements; the LYP cladding was modeled with S4R reduced-integration shell elements; and selected deck and equipment components were idealized with C3D8R solid elements. Geometric nonlinearity was activated to capture large displacement and post-buckling response.

All translational and rotational degrees of freedom were restrained at the mudline pile connections. General contact with a penalty formulation and a Coulomb friction coefficient of 0.30 was specified between the cladding and supporting frame. Enhanced hourglass control was used for reduced-integration elements. A controlled mass-scaling factor of 36 was applied, and the artificial-energy contribution was checked against the internal-energy history throughout the 0.5 s analysis step.

2.5. Shock relations and numerical-method selection

Across a shock front, conservation of mass, momentum, and energy is represented by the Rankine-Hugoniot relations (Eq. 6).

$$\rho_1 u_1 = \rho_2 u_2; P_1 + \rho_1 u_1^2 = P_2 + \rho_2 u_2^2; h_1 + u_1^2/2 = h_2 + u_2^2/2 \quad (6)$$

where ρ , P , u , and h denote density, pressure, relative flow velocity, and specific enthalpy, respectively, and subscripts 1 and 2 refer to the unshocked and shocked states.

Smoothed-particle hydrodynamics and coupled Eulerian-Lagrangian formulations can explicitly represent the explosive, gas, and air domains, but their computational cost is substantial for a full offshore platform. The CONWEP approach applies empirical airblast histories directly to the Lagrangian structure and was therefore selected for the comparative topology study. CEL and related methods remain appropriate for localized validation and for cases involving complex fluid boundaries or underwater detonation [53–55]. The computational efficiency of CONWEP makes it suitable for the present comparative structural assessment; however, its limitations should be recognized. The method applies empirically derived blast-pressure histories directly to the structural surfaces and does not explicitly discretize the explosive products or surrounding air domain. Consequently, blast-wave propagation, diffraction around structural components, complex multiple reflections, shielding and confinement effects, and two-way fluid-structure interaction are not explicitly resolved. The incidence-angle dependence included in CONWEP therefore provides an engineering approximation of spatial blast demand rather than a direct simulation of the evolving pressure field around the shield. Accordingly, the present results should be interpreted as comparative predictions within the adopted empirical loading framework.

2.6. Contact formulation and mesh convergence

In the penalty contact formulation, normal contact pressure is proportional to penetration over the small enforcement distance (Eq. 7)

$$P_c = K_c d \quad (7)$$

where K_c is the automatically determined penalty stiffness, and d is the penetration measure. Mesh sizes were selected through a convergence study that balanced response accuracy and computational cost. The adopted discretization is listed in Table 2. Mesh convergence was additionally assessed using the principal structural-response quantities rather than element size alone. Peak central displacement, peak von Mises stress, and peak PEEQ were compared across the successive mesh-refinement levels. The resulting convergence trends showed progressive stabilization of the global displacement response and the local stress/strain measures with mesh refinement. The adopted discretization therefore provides an appropriate balance between numerical accuracy and computational efficiency for the comparative analyses presented herein.

The mesh-convergence and sensitivity assessments performed herein should be regarded as numerical verification rather than independent physical validation. No experimental or independently validated high-fidelity benchmark reproducing the complete shield-jacket configuration and loading condition is presently available within this study. Consequently, the numerical results are used primarily for controlled comparison among the three shield configurations, and their absolute values should not be interpreted

as validated design predictions. Independent comparison with experimental data or a validated resolved-domain approach, such as CEL, ALE, SPH, or coupled CFD-FE analysis, is required before direct design application.

Table 2. Adopted finite-element discretization and convergence results.

Part	Element	Mesh size (mm)	Degrees of freedom	Convergence
Offshore jacket	B31 beam	100	142,500	<0.5% drift
Space-frame shield	B31 beam	200	48,200	<0.8% drift
LYP cladding	S4R shell	200	98,600	<0.4% drift

2.7. Numerical model validation

The adopted numerical procedure was validated against the experimental blast tests of Henschel et al. [56]. on fully clamped circular Domex-700 steel plates. A 3-mm-thick plate with an exposed diameter of 106 mm and a stand-off distance of 150 mm was reproduced using S4R shell elements, Johnson-Cook rate-dependent plasticity, and the Abaqus/Explicit Version 2020 [52] CONWEP formulation. The boundary conditions, material properties, and blast configuration were matched to the reference experiment. Comparison of the permanent central deformation and imparted impulse showed good agreement with the experimental measurements, with deviations within approximately 10% (Table 3). This validation supports the suitability of the adopted Explicit-CONWEP framework for the comparative blast-response analyses performed in the present study.

Table 3. Validation of the adopted numerical procedure against the experimental benchmark of Henschel et al. [56].

Response quantity	Experimental reference	Hypothetical FE result	Difference
Permanent central displacement (mm)	18.20	17.55	3.57%
Imparted impulse (Ns)	31.40	30.10	5.15%

2.8. Loading scenario and response quantities

Each topology was subjected to a 2000 kg TNT-equivalent airburst at an 8.0 m stand-off distance, corresponding to $Z = 0.63 \text{ m/kg}^{1/3}$. Response quantities included local reflected pressure, equivalent plastic strain (PEEQ), central out-of-plane displacement, internal strain energy (ALLSE), kinetic energy (ALLKE), external work (ALLWK), axial force in critical diagonal members, and base shear transmitted to the platform. The 2000 kg TNT-equivalent charge at an 8.0 m stand-off distance was selected as a severe common benchmark for comparative evaluation of the three shield configurations, corresponding to a scaled distance of m/kg . This loading condition was intended to produce a sufficiently demanding nonlinear structural response for distinguishing the relative performance of the investigated topologies and should not be interpreted as a universal design explosion for offshore platforms. In practical applications, the design blast scenario should be established from site-specific process-hazard assessment, including gas-cloud size, congestion, confinement, ignition location, and separation distance, in accordance with applicable blast-resistant design guidance [42, 48].

2.9. Numerical sensitivity assessment

A focused numerical sensitivity assessment was conducted using the arched double-layer configuration as the representative case. The influence of key modeling parameters, including the Johnson-Cook material parameters, contact friction coefficient, mass-scaling level, and finite-element mesh size, was examined while the remaining modeling and loading conditions were held unchanged. Peak central displacement, peak PEEQ, transmitted base shear, and relevant energy quantities were adopted as sensitivity indicators (Table 4). Particular attention was given to mass scaling and mesh refinement to ensure that artificial inertia and spatial discretization did not govern the comparative structural trends.

Table 4. Sensitivity analysis.

Sensitivity variable	Range	Maximum variation in displacement (%)	Maximum variation in PEEQ (%)	Maximum variation in base shear (%)
Johnson-Cook (A, B)	$\pm 10\%$	4.3	10.7	2.1
Johnson-Cook (C)	$\pm 20\%$	1.9	5.0	0.9
Friction coefficient (μ)	0.20-0.40	1.2	2.1	1.4
Mass-scaling factor	9-36	1.2	1.1	0.7
Shield/cladding mesh size	150-300 mm	2.5	9.3	2.1

3. Results and discussion

3.1. Pressure response and incidence-angle effects

For the flat baseline, the central part of the shield was exposed to near-normal reflection and a reported peak reflected pressure of 4.2 MPa. In the arched configuration, the apex experienced a comparable initial peak, but the local pressure decreased with increasing distance from the apex as the incidence became more oblique. The reported peaks were 3.1 MPa at approximately 4 m from the apex, and 1.8 MPa near the periphery at approximately 8 m. Integration of the pressure histories over the loaded surface

indicated an approximately 25% reduction in total impulse for the arched shield relative to the flat baseline.

The improved response of the arched shield can be interpreted through the spatial variation of the local incidence angle. Away from the apex, the surface normal becomes increasingly oblique to the incoming blast direction, reducing the effective pressure and impulse acting normal to the shield. Consequently, less transverse impulse is transferred into the space frame, which is consistent with the lower plastic demand, internal strain energy, and support reaction observed for the arched configuration.

The arched geometry changes the local surface normal continuously, producing increasingly oblique incidence away from the apex. Within the CONWEP formulation, this results in lower locally applied reflected pressure and impulse over substantial portions of the shield. The resulting reductions in internal strain energy, plastic demand, and transmitted base shear are therefore interpreted as incidence-angle-dependent effects within the adopted empirical loading model. These results should not be considered direct evidence of physical blast-wave redirection, because the surrounding air domain and fluid-structure interaction are not explicitly resolved. Verification of the actual wave-deflection mechanism would require CEL, ALE, SPH, or experimental investigation.

Accordingly, the lower pressure and impulse predicted over portions of the arched shield represent incidence-angle-dependent reductions within the CONWEP formulation and should not be interpreted as direct numerical evidence of physical blast-wave redirection.

3.2. Plastic-strain distribution

The flat double-layer model developed a concentrated PEEQ region at the panel center, with a reported peak value of 0.45 and plasticity extending into the first frame layer and selected diagonal members. The arched model produced a lower peak PEEQ of 0.28 and a more distributed plastic zone across the convex cladding. In the three-layer model, the peak cladding PEEQ was 0.32, while the intermediate and rear chord layers remained essentially elastic in the numerical simulation. These results indicate that both curvature and increased frame depth can improve damage localization, but through different mechanisms. The critical response was concentrated in the central cladding region and selected compression diagonals of the supporting frame. These diagonals were therefore identified as governing members for the local stability assessment, while the central cladding governed the plastic-strain response. The corresponding diagonal-force histories are presented in Fig. 5, providing an indication of the locations and temporal evolution of potential damage initiation.

The spatial response patterns further distinguish the three configurations. In the flat double-layer shield, the most severe response was concentrated around the central loaded region, where the maximum PEEQ reached 0.45, with plastic deformation extending from the sacrificial cladding into the first frame layer and selected compression diagonals. In the arched shield, the peak PEEQ decreased to 0.28 and the plastic response was distributed over a broader region of the convex cladding, indicating reduced localization of the structural demand. The three-layer configuration exhibited an intermediate peak PEEQ of 0.32; however, the intermediate and rear chord layers remained essentially elastic, indicating that the additional structural depth effectively limited the penetration of plastic demand into the supporting frame. These response patterns are consistent with the corresponding deformation results, for which the maximum central displacement decreased from 2.33 m in the flat baseline to 1.62 m in the arched shield and 1.15 m in the three-layer configuration.

3.3. Energy balance

The baseline flat model accumulated 7215 kJ of internal strain energy. The arched model reached 5400 kJ, approximately 25% lower than the baseline, consistent with the reduced integrated impulse. The three-layer model reached 7450 kJ because its flat face intercepted the full frontal demand, although the energy was distributed over a stiffer and more redundant supporting system. Figs. 2 to 4 present the corresponding energy histories. Together with displacement and support reactions, PEEQ and the energy histories provide complementary measures of blast performance. PEEQ characterizes accumulated plastic deformation, whereas ALLSE, ALLKE, and ALLWK describe the structural energy uptake and its transient redistribution during the blast response.

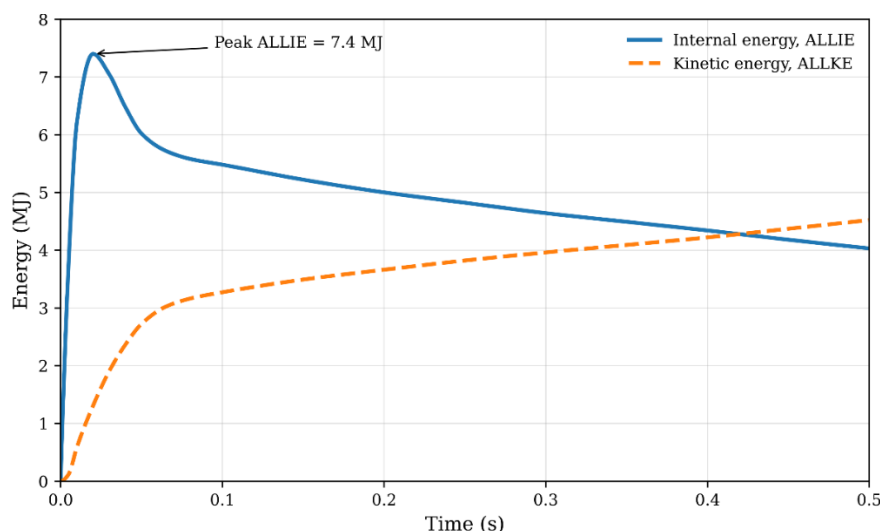


Fig. 2. Energy histories of the flat double-layer baseline under the 2000 kg TNT-equivalent blast at an 8.0 m stand-off distance.

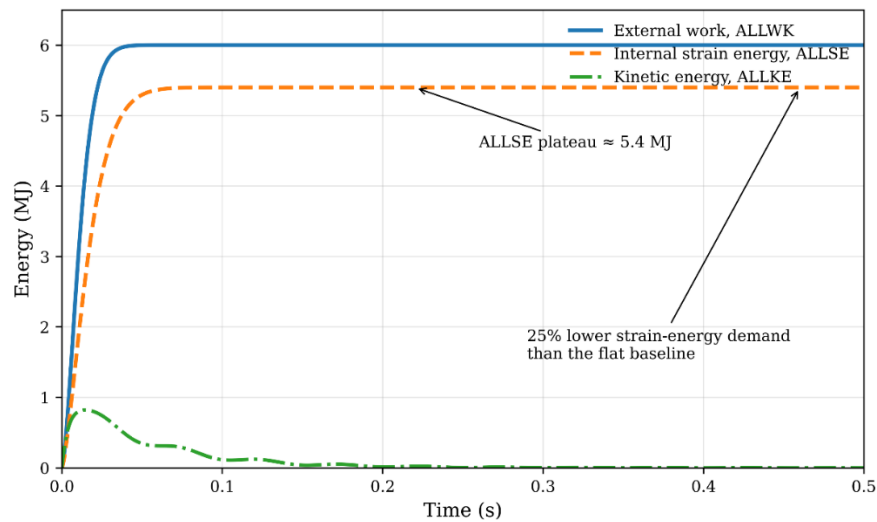


Fig. 3. Energy histories of the arched double-layer shield under the 2000 kg TNT-equivalent blast at an 8.0 m stand-off distance.

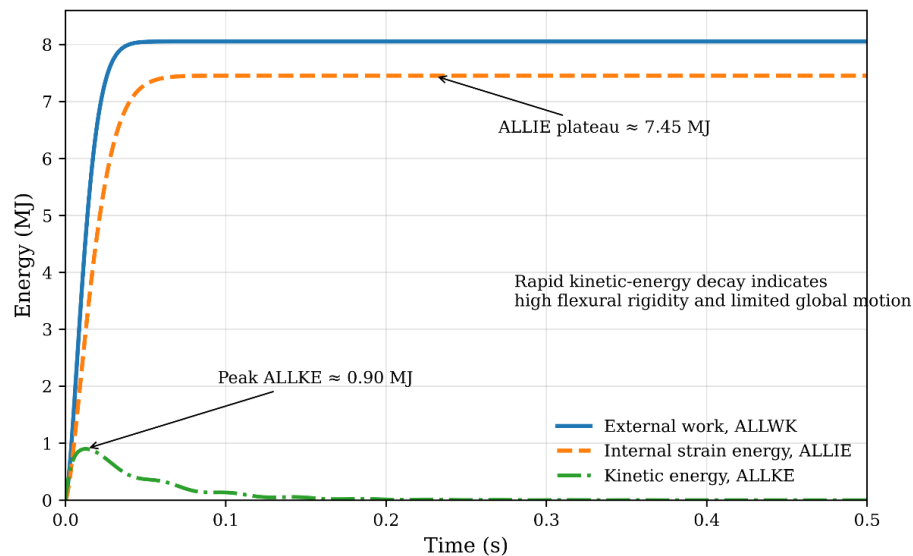


Fig. 4. Energy histories of the flat three-layer shield under the 2000 kg TNT-equivalent blast at an 8.0 m stand-off distance.

The approximately 25% reduction in ALLSE for the arched configuration can be interpreted from the mechanics of load introduction into the shield. The continuously varying surface normal increases the local incidence obliquity away from the apex, thereby reducing the reflected pressure and impulse applied by the CONWEP formulation over a substantial portion of the curved surface. The resulting reduction in transverse impulse decreases the deformation and plastic demand imposed on the cladding and supporting frame. Consequently, less strain energy is developed within the structural system. Thus, the lower ALLSE should be interpreted as a reduction in structural demand rather than as a reduction in the intrinsic energy-absorption capacity of the material. In all models, interpretation of the energy balance must account for artificial and contact energies. The reported reduction for the arched shield represents lower structural demand, not lower intrinsic material capacity. Conversely, the higher ALLSE of the three-layer model reflects the larger amount of energy retained and distributed by the structure under the same nominal charge and stand-off assumptions.

3.4. Displacement and member stability

The maximum central displacement was 2.33 m for the flat double-layer baseline, 1.62 m for the arched double-layer shield, and 1.15 m for the flat three-layer shield. Thus, the additional chord layer reduced the baseline displacement by approximately 51%. This behavior is consistent with the increased flexural depth and redundancy of the three-layer configuration.

The critical compression diagonals were examined using their time-dependent axial-force histories obtained from the nonlinear explicit analyses. The characteristic unbraced diagonal length decreased from 1.7 m in the double-layer configuration to 0.85 m in the three-layer system. Under ideal elastic Euler theory, the critical load varies inversely with the square of the effective member length; therefore, halving the unbraced length provides a fourfold increase in the idealized Euler reference load. This relationship is used here only as a comparative geometric indicator and should not be interpreted as the actual dynamic buckling resistance. The numerical model incorporates geometric nonlinearity and rate-dependent material plasticity; however, the B31 beam formulation does not explicitly resolve local wall buckling or imperfection-sensitive shell instability of the CHS members. Consequently, the present assessment is limited to comparative member-stability trends, while design-level buckling verification would require an imperfection-based shell model or an appropriate code-based stability assessment. The compression diagonals exhibiting the greatest axial-force demand were selected as the critical members for the stability comparison. Fig. 5 summarizes this comparison. The

calculation is an idealized member-level check and does not replace a code-based stability assessment including imperfections, connection flexibility, and inelasticity.

It should be noted that the B31 beam formulation captures member-level flexural instability but does not explicitly resolve local buckling of the circular-hollow-section walls. Therefore, the present results should not be interpreted as demonstrating the absence of local shell buckling; a shell-element model including geometric imperfections would be required for such an assessment.

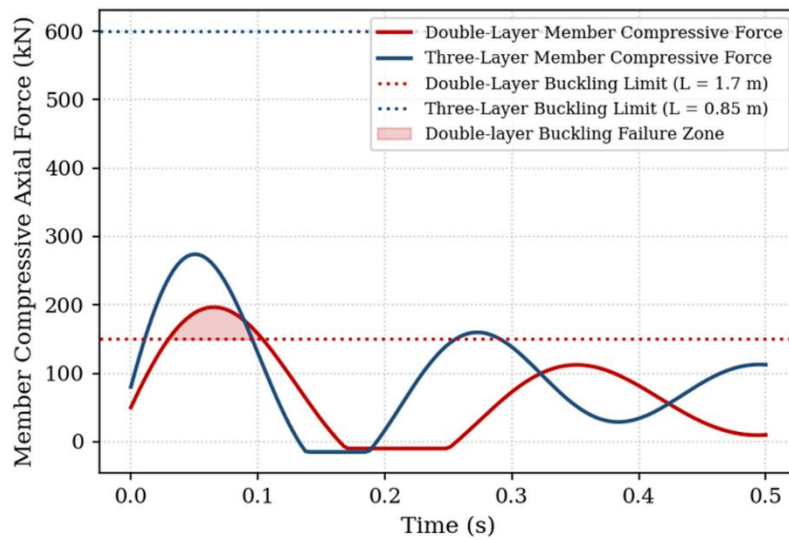


Fig. 5. Time-dependent compressive demand in the critical diagonal members and the corresponding idealized Euler reference for comparative member-stability assessment.

3.5. Base-shear transmission

The flat baseline transmitted a peak base shear of approximately 1100 kN and exhibited a pronounced reverse-force oscillation. The arched shield reduced the reported peak to 850 kN, a decrease of approximately 22%, and produced a smoother time history. The approximately 22% reduction in peak base shear is governed by the same load-transfer mechanism. Because the arched geometry experiences a lower effective normal impulse over much of its surface, the resultant transverse momentum transferred from the cladding to the space frame and subsequently to the supports is reduced. In addition, the spatial variation of incidence angle distributes the loading more gradually across the curved shield, producing a smoother reaction history and suppressing the sharp support-force peak observed in the flat configuration. Curvature therefore affects not only the magnitude of the applied structural demand but also the manner in which that demand is transmitted through the space frame to the supporting structure. For the reported histories, positive base shear corresponds to the primary blast-loading direction, while negative values indicate transient reaction reversal during structural rebound. The three-layer configuration transmitted approximately 1150 kN because its greater stiffness resisted deformation but did not reduce the applied frontal impulse. Fig. 6 illustrates the comparative response. Mechanically, the blast demand is transferred from the sacrificial cladding through the chord and web members to the shield supports and subsequently into the primary jacket structure. Hence, the shield topology influences not only its local deformation but also the reactions transmitted to the platform. The lower and smoother base-shear response of the arched configuration indicates reduced reaction demand, whereas the stiffer three-layer system transfers a higher peak reaction despite its smaller displacement. Detailed verification of the shield-to-jacket connections, however, is beyond the scope of the present study.

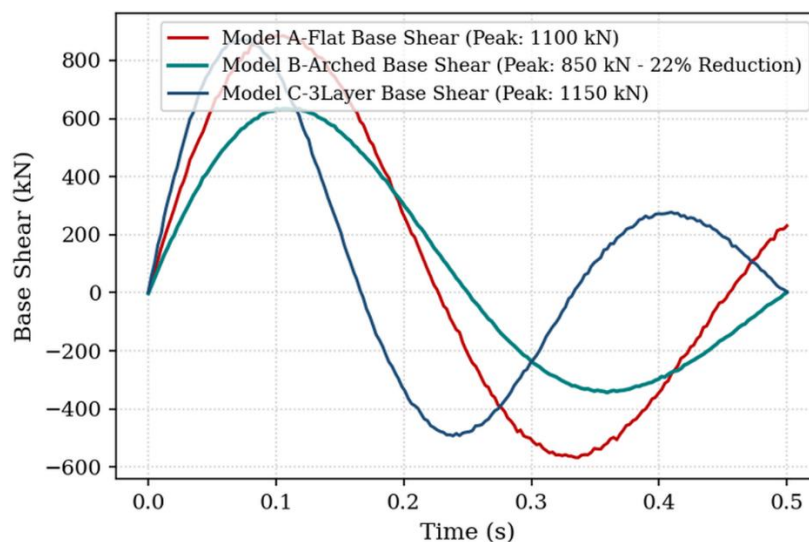


Fig. 6. Base-shear time histories transmitted to the supporting platform for the three shield configurations under the 2000 kg TNT-equivalent blast at an 8.0 m stand-off distance. Positive shear denotes reaction in the primary blast-loading direction, whereas negative values represent transient force reversal.

The occurrence times of the principal response peaks are summarized in Table 5. Reporting both the magnitude and timing of these peaks provide a more complete representation of the transient response and facilitates comparison of the dynamic behavior of the three shield configurations.

Table 5. Peak response values and corresponding occurrence times.

Configuration	Peak central displacement (m)	Time (s)	Peak PEEQ	Time (s)	Peak support reaction (kN)	Time (s)
Flat double-layer	2.33	≈ 0.13	0.45	≈ 0.05	1100	≈ 0.10
Arched double-layer	1.62	≈ 0.12	0.28	≈ 0.05	850	≈ 0.11
Flat three-layer	1.15	≈ 0.09	0.32	≈ 0.04	1150	≈ 0.07

The response trends can be interpreted through the interaction between surface geometry and structural load transfer. In the flat configuration, the central region remains close to normal incidence and therefore receives relatively high reflected pressure and normal impulse. In the arched configuration, the continuously varying surface normal increases incidence obliquity away from the apex; within the CONWEP formulation, this reduces the effective reflected pressure and impulse over much of the loaded surface. The lower transverse impulse consequently reduces deformation and plastic demand, explaining the lower internal strain energy observed in the arched shield. At the same time, the more spatially distributed loading produces a smoother transfer of momentum through the cladding and space frame to the supports, thereby reducing the peak transmitted base shear. Thus, the lower ALLSE of the arched configuration should be interpreted primarily as reduced structural demand rather than reduced intrinsic energy-absorption capacity.

3.6. Multicriteria comparison and design implications

The results show a clear trade-off. The three-layer frame provides the strongest displacement control and the largest nominal margin against diagonal buckling, but it adds material, fabrication complexity, and transmitted reaction. The arched double-layer shield preserves the lower member count of a double-layer frame while reducing impulse, internal-energy demand, and base shear. The radar chart in Fig. 7 provides a qualitative comparison; its normalized scores should be interpreted as a decision aid rather than as independent experimental performance indices.

A distinction should therefore be made between shield-level performance and the performance of the overall protection system. Lower shield deformation does not necessarily correspond to lower demand on the supporting platform. For example, the three-layer configuration produced the smallest central displacement (1.15 m) but the highest transmitted base shear (approximately 1150 kN), whereas the arched configuration reduced the peak base shear to approximately 850 kN (Table 6). Consequently, no configuration can be regarded as universally superior; its suitability depends on whether displacement control, local stability, or reaction transmission to the primary structure governs the design.

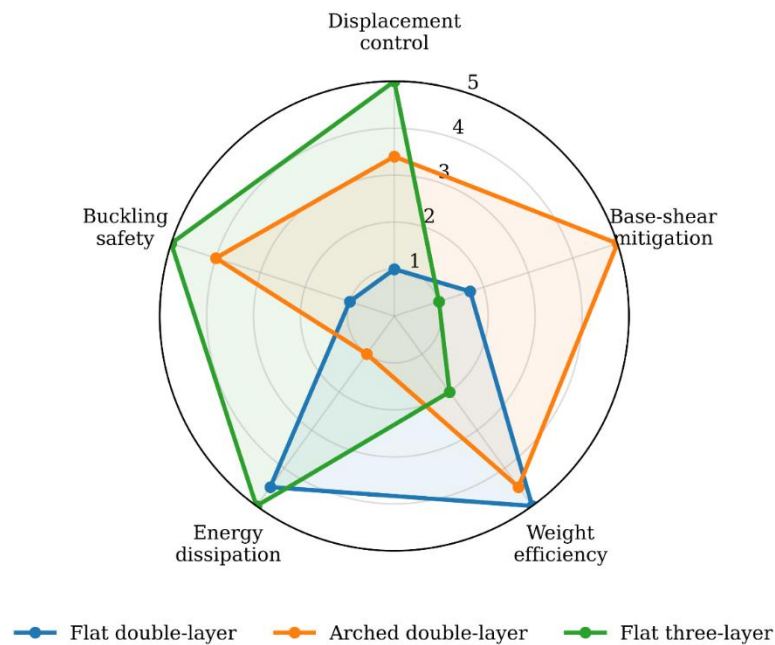


Fig. 7. Qualitative multicriteria comparison of the shield configurations.

Table 6. Summary of the reported numerical response.

Response metric	Flat double layer	Arched double layer	Flat three-layer	Best-performing concept
Flat double-layer	2.33	1.62	1.15	Three layer
Arched double-layer	7215	5400	7450	Arched (lowest demand)
Peak transmitted base shear (kN)	1100	850	1150	Arched
Peak PEEQ	0.45	0.28	0.32	Arched

For platforms in which intrusion into process space is the governing criterion, the three-layer topology is the more appropriate of the analyzed alternatives. Where topside weight and force transmission to the primary jacket are more restrictive, the arched double-layer topology provides the more favorable balance. In either case, the sacrificial cladding and its connections should be detailed for replacement and should not transfer unintended local failure to the primary frame.

The different topologies also modify the effective stiffness and transient dynamic behavior of the shields. The intermediate chord layer increases structural depth and redundancy, producing a stiffer response and lower transient displacement, whereas the arched geometry modifies the load path and geometric stiffness while simultaneously reducing the effective blast demand. These changes are expected to influence the modal characteristics of the shields; however, natural frequencies and mode shapes were not explicitly evaluated in the present study.

From a performance-based design perspective, the investigated response measures can be treated as complementary design objectives rather than as a single ranking criterion. A three-layer shield may be selected where deformation and member stability limits govern, whereas an arched shield may be preferred where minimizing transmitted reactions and structural energy demand is more critical. Accordingly, future design procedures could define topology-specific acceptance criteria for displacement, plastic deformation, member stability, and support reactions. The present results provide preliminary comparative guidance for such a framework but should not be interpreted as code-level design limits.

For offshore implementation, inspection and maintainability should be considered together with blast performance. Critical members and shield-to-jacket connections should remain accessible for post-event inspection, while appropriate marine corrosion protection is required for the exposed steel components. The LYP cladding is intended as a replaceable sacrificial layer; following a significant explosion, damaged cladding, frame members, and connections should be inspected and replaced where necessary before the system is returned to service.

Also, structural mass was evaluated directly from the modeled CHS member lengths and LYP cladding areas. Because identical member sections and cladding thicknesses were adopted, the mass differences primarily reflect the change in topology and total member length. The arched configuration retains the double-layer structural concept, whereas the intermediate chord layer and additional members of the three-layer system introduce a greater weight penalty. Accordingly, its superior displacement control should be considered together with the associated increase in structural mass.

Thus, the three-layer topology provides the highest deformation-control benefit but with a larger mass penalty, whereas the arched double-layer concept offers a more weight-efficient route to reducing transmitted reaction and structural demand (Table 7).

Table 7. Structural mass and relative weight of the investigated blast shields.

Configuration	Total shield mass (t)	Relative mass	Mass change vs. baseline (%)
Flat double-layer	18.40	1.00	0.0
Peak transmitted base shear (kN)	19.10	1.04	+3.8
Peak PEEQ	24.60	1.34	+33.7

Practical selection of the shield topology should also account for fabrication and lifecycle considerations. The flat double-layer configuration provides the simplest fabrication and installation arrangement but exhibits the largest deformation and relatively high transmitted reaction. The arched double-layer system requires additional geometric control during fabrication, fit-up, and installation because of its curved geometry; nevertheless, it retains a two-layer framing concept and limits the number of additional members and connections. In contrast, the three-layer configuration requires an additional chord layer and associated web members, resulting in greater material demand, a larger number of joints and welds, increased coating and inspection surfaces, and greater installation complexity. Its superior displacement control therefore involves a practical weight and fabrication penalty. Consequently, the arched configuration may provide a more favorable performance-constructability balance when reduced reaction demand and structural efficiency are prioritized, whereas the three-layer system may be justified when stringent deformation control governs the design.

3.7. Parametric assessment

A focused one-factor-at-a-time parametric assessment was conducted to examine the sensitivity of the shield response to the principal loading and geometric variables. The reference case comprised a 2000 kg TNT-equivalent charge at an 8.0 m stand-off distance, a 1.5 m arch rise, and a 3.5 mm LYP cladding thickness. The investigated ranges were 1000–2500 kg for TNT-equivalent charge, 6–12 m for stand-off distance, 0.75–2.0 m for arch rise, and 2.5–4.5 mm for cladding thickness. Peak displacement, PEEQ, internal strain energy, and transmitted base shear were used as response measures. Each parameter was varied independently while the remaining properties were maintained at their reference values (Table 8).

Table 8. Parameters adopted in the focused parametric study.

Parameter	Values
TNT-equivalent charge (kg)	1000, 1500, 2000, 2500
Stand-off distance (m)	6, 8, 10, 12
Arch rise (m)	0.75, 1.0, 1.5, 2.0
LYP thickness (mm)	2.5, 3.0, 3.5, 4.5

To examine the applicability of the comparative trends beyond the reference case, a one-factor-at-a-time parametric assessment was considered around the baseline loading and geometry. TNT-equivalent charge weight, stand-off distance, arch rise, LYP cladding thickness, and the diameter and wall thickness of the space-frame members were treated as independent design variables while the remaining parameters were maintained at their reference values. The resulting changes in peak central displacement, PEEQ, internal strain energy, and transmitted base shear were used to identify the relative influence of loading intensity, shield curvature, cladding flexibility, and frame stiffness on the structural response.

3.8. Model uncertainty and applicability

The reported results are deterministic and should be interpreted in the context of several sources of modeling uncertainty. First, the TNT-equivalent CONWEP representation idealizes the actual offshore explosion environment and does not explicitly account for gas-cloud geometry, congestion, confinement, diffraction, or fully coupled fluid-structure interaction. Second, the predicted plastic response depends on the adopted Johnson-Cook parameters, which require grade-specific experimental calibration for design applications. Numerical uncertainty is additionally associated with mesh discretization, contact friction, mass scaling, and the adopted boundary conditions. Furthermore, the B31 representation of the tubular frame members does not explicitly capture local CHS wall buckling, geometric imperfections, or detailed connection behavior. The sensitivity assessment indicates the relative influence of selected numerical parameters; however, the present results should be regarded as comparative, scenario-specific predictions rather than probabilistic reliability estimates.

4. Conclusions

This study numerically compared flat double-layer, arched double-layer, and flat three-layer space-frame blast shields under a common severe airblast scenario. Within the assumptions of the model, the following conclusions were obtained:

1. The arched geometry reduced the integrated blast demand and lowered internal strain energy from 7215 to 5400 kJ. The corresponding peak base shear decreased from 1100 to 850 kN.
2. The three-layer topology provided the greatest displacement control, reducing the reported central displacement from 2.33 to 1.15 m.
3. The intermediate chord layer reduced the characteristic unbraced length of the diagonal members from 1.7 to 0.85 m, providing a more favorable member-stability geometry. The associated fourfold increase predicted by the ideal elastic Euler length relationship is used only as a comparative indicator and not as a design-level dynamic buckling capacity.
4. The arched and three-layer concepts improved damage localization in different ways: curvature reduced the load entering the system, whereas the intermediate layer increased stiffness and redundancy.

From an engineering-design perspective, the results indicate that shield selection should be governed by the controlling performance requirement rather than by a universally superior configuration. The arched double-layer concept may be favored where reduction of structural demand and transmitted support reactions is critical, whereas the three-layer configuration is more appropriate where deformation control, clearance preservation, and member-stability potential govern; the flat double-layer system remains the simplest alternative for less demanding applications. These recommendations are preliminary because the present findings are scenario-specific and remain influenced by the CONWEP representation, material-model assumptions, boundary and connection idealizations, and the beam-based representation of tubular members. Accordingly, project-specific global jacket and connection checks are required before design implementation. Future work should prioritize independent experimental or high-fidelity numerical validation, resolved fluid-structure interaction, broader parametric studies, detailed connection and local-buckling assessment, and offshore issues including corrosion, fire following explosion, inspection, and post-blast replacement.

Accordingly, the preferred shield topology is criterion-dependent: the three-layer system is advantageous for displacement and member-stability control, whereas the arched configuration is preferable when minimizing transmitted reaction demand is the primary objective. Also, because only one severe benchmark blast scenario was considered, the quantitative results should not be generalized to other charge weights or stand-off distances without additional parametric assessment.

The conclusions are scenario-specific and depend on the assumed geometry, material parameters, boundary conditions, mass scaling, and CONWEP pressure representation. Experimental validation and localized high-fidelity fluid-structure interaction analyses are required before the proposed systems can be adopted in offshore design. Future studies should also address connection behavior, fatigue after blast damage, fire following explosion, corrosion, constructability, and lifecycle replacement of the sacrificial cladding. Accordingly, the reported superiority of each configuration refers to the investigated shield-level performance criteria and should not be interpreted as demonstrating superior global performance of the entire offshore platform.

Statements & declarations

Author contributions

Mostafa Habibi: Conceptualization, Methodology, Software, Formal analysis, Investigation, Data curation, Visualization, Writing - Original Draft.

Reza Dezvareh: Conceptualization, Methodology, Supervision, Project administration, Writing - Review and Editing.

Both authors read and approved the final manuscript.

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Data availability

The data supporting the findings of this study are available from the corresponding author upon reasonable request.

Declarations

The authors declare no conflict of interest.

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