

Article

Parametric Analysis and Simplified Design Method for Multi-Layer Composite Confinement Structures Under Internal Blast Loading

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Abstract

Multi-layer composite confinement structures (MLCCSs) are critical for containing accidental internal explosions, yet conventional design methods cannot capture their three-layer load-redistribution mechanism. Based on a validated fluid–structure interaction (FSI) model, this study investigates four key parameters (the foam-concrete yield strength, interlayer thickness, inner-layer reinforcement ratio, and inner-layer thickness) under 3 and 5 kg TNT charges and develops a simplified design method for MLCCSs with a foamed-concrete interlayer under internal blast. The foam yield strength is found to be the most critical parameter controlling the Mode I/II transition: increasing it reduces the inner-layer displacement but increases the risk of premature outer-layer yielding, challenging the conventional “stronger-is-better” design philosophy and establishing both upper and lower bounds for the foam yield strength. The method is derived from a rigid–plastic inner-layer model and the load-controllability mechanism of the foam interlayer, yielding closed-form expressions for the inner-layer maximum displacement, the minimum required interlayer thickness, and the outer-layer peak hoop strain. The inner-layer maximum displacement is quantitatively validated, with a mean absolute error of 9.50%, while the three remaining outputs are checked for consistency with the numerical results; because the Mode I/II threshold is calibrated from the same numerical dataset, this consistency check is an internal self-consistency test rather than an independent validation of the strain prediction. A step-by-step design procedure incorporating the Mode I/II criterion is proposed and demonstrated on a prospective design example whose configuration is not among the calibration cases. The study provides a rapid screening tool for the preliminary design of MLCCSs.



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Keywords: multi-layer composite confinement structure; foamed concrete; internal blast; parametric analysis; simplified design method; Mode I/II failure

1. Introduction

Reinforced concrete (RC) containment structures are widely adopted in hazardous material storage, ammunition depots, and industrial facilities to confine accidental internal explosions [1–4]. Conventional single-layer RC walls, however, are prone to severe spalling, cracking, or even perforation under intense internal blast loads, compromising airtightness

and inducing secondary hazards [1,2]. To mitigate this, multi-layer composite confinement structures (MLCCSs)—typically comprising an RC inner layer, an energy-absorbing interlayer (e.g., foamed concrete or aluminum foam), and an RC outer layer—have been proposed, in which the outer layer can be maintained elastic while the inner layer undergoes controlled plastic deformation, thereby preserving overall integrity and airtightness [5]. The energy-absorbing interlayer is the key component that enables this dual-mode behavior, and its material selection has attracted extensive research attention.

Cellular materials, including aluminum foam [6–8], foamed concrete [9–11], and polyurethane foam [12,13], have been extensively studied as energy-absorbing interlayers owing to their low density and prolonged yield plateau. For aluminum-foam-cored sandwiches, core energy absorption generally scales with density and thickness [6–8], and foam interlayers notably enhance the blast resistance of steel–concrete composite walls [8]. Related studies on foam- or vermiculite-filled double-wall containers [14,15] and XPS-foam-cored aluminum-skin sandwiches under shock-tube blast [16] further confirm the effectiveness of cellular materials in mitigating blast effects. In the concrete-based direction, Fang et al. [10,11] proposed the “load-controllability” philosophy for composite protective structures, whereby the peak load transmitted to the primary RC member can be capped at the foam’s yield stress by tailoring the foamed concrete strength and thickness. Yang et al. [17] derived a minimum-thickness formula for foamed-concrete distribution layers based on one-dimensional wave propagation, and Shi et al. [18] developed a plastic-damage constitutive model for foamed concrete under high-pressure blast, later applied to gradient-foam composite protective structures [19]. Graded-core optimization has also been explored for sandwich panels, with core density gradient and plateau-stress ratio shown to noticeably affect the back-face response [20,21]. These studies consistently indicate that interlayer parameters exert a discernible influence on the energy absorption and blast resistance of sandwich/composite systems [22–24].

Internal blast loads differ fundamentally from free-field scenarios due to confinement-induced pressure build-up and multiple wave reflections, which intensify structural damage [25–27]. Full-scale internal-blast tests on RC single-room structures have revealed five damage-aggravating modes (Model I to Model V), which were quantitatively classified using a dimensionless weighted parameter [28]. Liu et al. [28] conducted full-scale internal-blast tests on an RC single room combined with extended numerical simulations, providing a reference for damage assessment of shear-wall-type rooms. Bai et al. [29] performed reduced-scale internal-blast tests on an RC frame–masonry infill model combined with numerical simulation, and derived an empirical formula for the deflection response of plate components. On the RC box-type and concrete–steel composite side, Guo et al. [25] analyzed the internal-blast load characteristics and dynamic behavior of RC box-type structures, and Cui et al. [2] examined the deformation and failure of concrete–steel tubular structures under internal explosion, both indicating that local damage at the inner face is a key concern. Despite these advances, investigations on multi-layer composite confinement structures under internal blast—where the presence of an energy-absorbing interlayer fundamentally alters the load-transmission path—remain scarce, and the dynamic response characteristics of such systems are still not fully elucidated.

On the design-methodology side, simplified blast-resistant design is routinely performed via single-degree-of-freedom (SDOF) approaches or equivalent-static-load procedures, as codified in UFC 3-340-02 [30] and its predecessor manuals [31]. These methods are calibrated for single-layer RC members under small-to-moderate deformation, where the load–displacement relation is idealized as elastic–perfectly plastic and the blast impulse is directly applied to the bearing member. Extensions of the SDOF framework to RC beam–columns and to sandwich-type configurations have also been reported [32]. For MLCCSs

under internal blast, however, the configuration is fundamentally different: the inner RC layer undergoes large plastic deformation while the foamed concrete interlayer exhibits a plateau-then-hardening behavior, reshaping the impulse transmitted to the outer layer which is designed to remain elastic. The three-layer load path—inner plasticity → interlayer compaction → outer elastic—cannot be captured by directly applying UFC 3-340-02 SDOF formulae [33–35].

In our recent work, experimental tests on MLCCSs with two types of energy-absorbing interlayers (foamed concrete and sandy soil) under internal blast were reported [5], revealing the macroscopic differences in damage evolution and energy distribution between the two materials. The foamed-concrete specimens exhibited a more stable progressive deformation pattern, with the outer layer remaining elastic under moderate charges. A follow-up numerical study [36] established a validated fluid–structure interaction (FSI) model using LS-DYNA, calibrated against the experimental data [5]. That study identified two distinct failure modes: Mode I (inner-layer-dominated, with the outer layer remaining elastic) and Mode II (outer-layer yielding before the inner layer reaches its deformation limit). However, that study did not conduct a systematic parametric sweep, nor did it propose a design-oriented simplified method—both of which are addressed in the present paper.

The above review reveals two interrelated gaps. First, existing studies on energy-absorbing interlayers and sandwich/composite structures under blast have predominantly focused on planar configurations or external blast scenarios, with parametric analyses calibrated accordingly [37,38]. Systematic parametric investigations on cylindrical reinforced-concrete–foam–reinforced-concrete (RC-foam-RC) confinement structures under internal blast—where the geometry, loading, and failure mechanisms are fundamentally different—are lacking. Second, although simplified design methods (e.g., SDOF approaches) are well established for conventional RC members, no dedicated design method exists for MLCCSs that accounts for the three-layer load redistribution and the Mode I/II failure criterion to guarantee the desired Mode I and enable parameter optimization.

This paper aims to fill this gap by developing a simplified design method for MLCCSs under internal blast, built upon systematic parametric analysis. To the authors' knowledge, this is the first dedicated simplified formula for RC-foam-RC cylindrical confinement under internal blast with the Mode I/II criterion. The novelties are threefold: (i) a systematic parametric analysis revealing the sensitivity ranking of four key parameters (foam yield strength, interlayer thickness, inner-layer reinforcement ratio, and inner-layer thickness) and the conditions triggering Mode II failure; (ii) a closed-form simplified method based on a rigid–plastic inner layer, load-controllability foam model, and elastic outer shell, yielding explicit expressions for the inner-layer maximum displacement, minimum required interlayer thickness, and outer-layer peak hoop strain; and (iii) the identification of a bounded, cost-optimal interlayer strength together with the minimum thickness it requires, which challenges the conventional “stronger-is-better” design philosophy, demonstrating that excessive foam strength can paradoxically induce premature outer-layer yielding.

It is important to clarify the distinction between the present work and the authors' preceding studies [5,36]. While [5] provided experimental observations of MLCCSs damage evolution and [36] established a validated FSI model and identified Mode I/II failure, neither study offered a predictive design tool. The present work transcends these descriptive and numerical frameworks by deriving a closed-form, design-oriented simplified method. This method enables engineers to rapidly predict the inner-layer displacement, determine the minimum required interlayer thickness, and verify the Mode I criterion without performing computationally expensive FSI simulations. Hence, the novelty lies in translating previously understood physical phenomena into a practical, quantitative design methodology.

The remainder of this paper is organized as follows. Section 2 briefly recalls the validated numerical model and presents the parametric analysis results. Section 3 derives the simplified design method, assesses it against numerical simulations with attention to the strength of the evidence that each output can support, and demonstrates it on a prospective design example. Section 4 discusses the applicability, limitations, and engineering implications of the proposed method. Section 5 concludes the paper.

2. Parametric Analysis

2.1. Numerical Model Overview

The fluid–structure interaction (FSI) numerical model employed in this study was established and rigorously validated in our companion paper [36]. That work detailed the model geometry, material constitutive laws, mesh convergence study, and benchmark validation against the internal-blast test on the MLCCS prototype with a foamed-concrete interlayer [5]. For completeness and self-contained reading, a condensed summary of the model is given below, and representative validation results are presented separately in Appendix A; the full validation matrix and the mesh-convergence study remain in [36].

The computation uses LS-DYNA explicit dynamics with a 1/8 symmetry model of the cylindrical MLCCS (inner radius $R_i = 0.5$ m, total length $L = 5$ m, inner and outer RC layer thicknesses $h_i = h_o = 175$ mm, and interlayer thickness $h_f = 150$ mm at the baseline). The explosive charge is placed at the geometric center; TNT (trinitrotoluene) is modeled with the JWL (Jones–Wilkins–Lee) equation of state (EOS) (parameters as given in [30]), and the surrounding air domain uses a linear polynomial EOS. The FSI coupling is realized through the CONSTRAINED_LAGRANGE_IN_SOLID algorithm, with an Eulerian multi-material formulation for air and explosive and a Lagrangian formulation for the RC layers and foam interlayer. Radial mesh size is 5 mm near the blast center, with progressive coarsening along the axis (200 axial elements in total), as verified for both shock propagation and structural response accuracy [36].

Material models are: RC layers use MAT_072R3 (*MAT_CONCRETE_DAMAGE_REL3) with strain-rate enhancement per Malvar–Ross [39] and a reinforcement ratio of 1% (baseline) introduced via the built-in rebar option; the foamed-concrete interlayer uses *MAT_CRUSHABLE_FOAM with the yield-stress–volumetric-strain relation as detailed in [36]; TNT uses *MAT_HIGH_EXPLOSIVE_BURN + JWL; air uses *MAT_NULL + linear polynomial. Boundary conditions: the RC layers and interlayer are connected by automatic surface-to-surface contact, while the end plates share nodes with the RC layers.

Table 1 summarizes the key modeling features. The model predicts the inner-wall reflected pressure, interlayer contact pressures, and the inner/outer radial displacements within 16.4% of the test data for 0.8 kg TNT, with an average absolute error of approximately 9.3% across five charge masses (0.15–0.80 kg) [36], and reproduces the Mode I/II transition observed in the companion experiment [5,36]. The hoop strain at four measurement points agrees within 11%, and the interlayer contact pressures are captured with errors ranging from −24.5% to 9.6% [36]. These quantitative agreements confirm that the numerical framework provides a reliable basis for the subsequent parametric study and simplified design method.

Representative graphical validation evidence for the numerical framework is collected in Appendix A (Figure A1), so that the numerical basis of the present study can be assessed without recourse to the companion paper.

With the model thus recalled, the parametric study in the following sections varies four independent parameters—foam-concrete yield strength σ_p , interlayer thickness h_f , inner-layer reinforcement ratio ϕ , and inner-layer thickness h_i —under 3 kg and 5 kg TNT, to quantify their influence on the Mode I/II transition and to provide the empirical basis for the simplified design method in Section 3.

Table 1. Summary of numerical model settings (detailed in [36]).

Item	Setting
Solver	LS-DYNA explicit, 1/8 symmetry
Structure	Cylindrical MLCCS, $R_i = 0.5$ m, $L = 5$ m
RC layers	C40 concrete, MAT_072R3, rebar option (reinforcement ratio 1%), $h_i = h_o = 175$ mm
Interlayer	Foam concrete, MAT_CRUSHABLE_FOAM, baseline $\sigma_p = 1.0$ MPa, $h_f = 150$ mm
Explosive	TNT at center, JWL EOS
Air	Linear polynomial EOS
FSI	CONSTRAINED_LAGRANGE_IN_SOLID, ALE–Lagrange
Mesh	Radial 5 mm near center, 200 axial elements, progressive coarsening

Note: validation against experimental data [36] shows: peak overpressure error $\leq 16.4\%$ (avg. 9.3%); hoop strain error $\leq 11\%$; interlayer contact pressure error -24.5% to 9.6% .

2.2. Effect of Foam-Concrete Yield Strength

To examine the influence of the energy-absorbing interlayer strength on the structural response, four values of foam-concrete yield strength were considered: 1.0, 1.5, 2.0, and 2.5 MPa. The baseline value of 1.0 MPa corresponds to the foamed concrete tested in the companion experiment [5]. The upper bound of 2.5 MPa represents a practical upper limit for structural-grade foamed concrete [17,40], while the intermediate values (1.5 and 2.0 MPa) allow interpolation of the trend. The corresponding density and elastic modulus were assigned according to the experimentally characterized relations [17,40,41], as listed in Table 2. All other geometric and material parameters remained at the baseline values (Section 2.1). Two charge masses, $W = 3$ kg and 5 kg TNT, were simulated for each value, covering both the small-charge regime where the inner-layer self-resistance dominates and the large-charge regime where the interlayer force becomes decisive.

Table 2. Foam-concrete material parameters for different yield strengths.

σ_p (MPa)	Density (kg/m ³)	Elastic Modulus (MPa)
1.0	610	654
1.5	660	716
2.0	710	792
2.5	750	844

2.2.1. Effect of Foam-Concrete Yield Strength on Radial Displacement

Figure 1 presents the time histories of the inner-layer radial displacement at the blast-center cross-section for different values under 3 kg and 5 kg TNT. For both charge masses, the peak inner-layer displacement decreases with increasing σ_p , and the time to reach the peak is shortened. However, the reduction is marginal under 3 kg TNT (the peak at $\sigma_p = 2.5$ MPa is 94.4% of that at $\sigma_p = 1.0$ MPa), whereas it becomes substantial under 5 kg TNT (71.6%). This trend reflects the shift in the governing resistance mechanism, which is closely related to the load-bearing mode of the RC-foam-RC composite structure. After the inner layer acquires an initial velocity from the blast impulse, its outward motion is resisted by two forces: the self-resistance of the inner RC layer and the reaction force from the foam interlayer. The latter is directly proportional to the foam yield strength σ_p . When the charge mass is small, the self-resistance of the inner layer plays a dominant role in limiting the radial expansion, and the contribution of the foam reaction force is relatively minor. As the charge mass increases, the self-resistance of the inner layer becomes less effective in restraining the deformation, while the foam reaction force accounts for an increasing proportion of the total resistance. Consequently, the inner-layer displacement becomes more sensitive to the foam yield strength under larger charges.

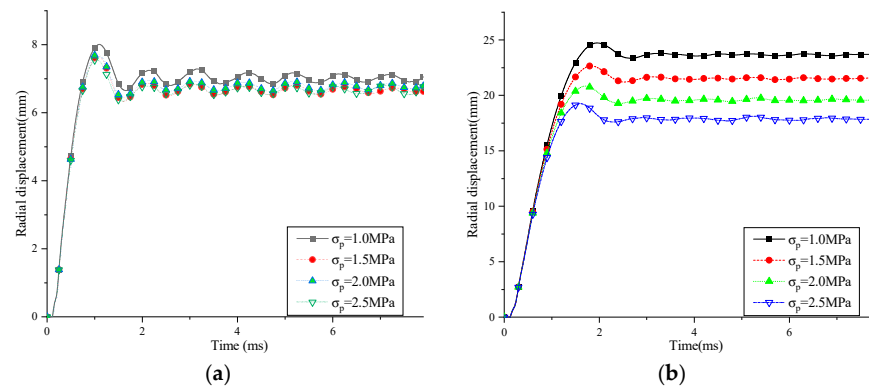


Figure 1. Time-history curves of the structural inner-layer displacement for different energy-absorbing materials: (a) under 3 kg TNT; (b) under 5 kg TNT.

For $\sigma_p = 1.0$ MPa under 5 kg TNT, the peak inner-layer displacement reaches 24.78 mm, at which point the hoop strain of the innermost reinforcing bar exceeds 5%—the commonly accepted ultimate hoop strain for cylindrical RC structures under internal blast [36]. At this moment, the outer layer remains elastic, representing the desired Mode I failure (inner-layer collapse before outer-layer yielding).

Figure 2 shows the corresponding outer-layer displacement time histories. In contrast to the inner layer, the outer-layer peak displacement increases with σ_p . Under 3 kg TNT, the peak at $\sigma_p = 2.5$ MPa is 1.33 times that at $\sigma_p = 1.0$ MPa; under 5 kg TNT, this factor rises to 3.71. More importantly, for $\sigma_p = 2.5$ MPa under 5 kg TNT, the outer layer develops effective plastic strain before the inner layer reaches its deformation limit, i.e., the structure transitions to Mode II failure (premature outer-layer yielding). Thus, while a stronger interlayer reduces inner-layer deformation, it may inadvertently cause loss of airtightness by inducing premature plastic deformation in the outer layer.

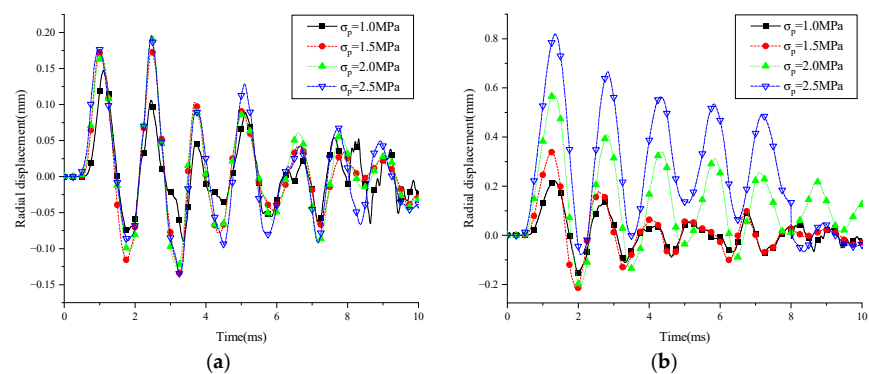


Figure 2. Time-history curves of the structural outer-layer displacement for different energy-absorbing materials: (a) under 3 kg TNT; (b) under 5 kg TNT.

2.2.2. Effect of Foam-Concrete Yield Strength on Interlayer Contact Pressure

Tables 3 and 4 list the peak contact pressures at the inner interface (between the inner RC layer and the foam interlayer) and the outer interface (between the foam interlayer and the outer RC layer), respectively, for all combinations of σ_p and charge mass.

Table 3. Peak contact pressure at the inner interface (MPa).

σ_p (MPa)	3 kg TNT	5 kg TNT
1.0	1.150	1.254
1.5	1.591	1.795
2.0	2.102	2.289
2.5	2.557	2.831

Table 4. Peak contact pressure at the outer interface (MPa).

σ_p (MPa)	3 kg TNT	5 kg TNT
1.0	1.044	1.042
1.5	1.552	1.576
2.0	1.966	2.077
2.5	2.617	2.603

Before the foam enters the hardening stage, the contact pressures at both interfaces are approximately equal to the foam yield strength σ_p , confirming the “load-controllability” concept [10,11]. Higher σ_p leads to earlier attainment of the peak pressure but does not significantly alter the total pressure duration. The transmitted pressure to the outer layer increases almost linearly with σ_p , which explains the sharp rise in outer-layer displacement observed in Figure 2.

2.2.3. Upper Bound of Foam Yield Strength

The above results reveal a critical trade-off in the selection of σ_p : a higher σ_p reduces inner-layer displacement but increases the transmitted pressure to the outer layer. When σ_p exceeds a certain threshold, the outer layer develops plastic deformation before the inner layer reaches its deformation limit, i.e., the structure transitions from the desired Mode I to the undesirable Mode II. In the present parametric study, this transition occurs at $\sigma_p \approx 2.5$ MPa under 5 kg TNT (Figure 2). The threshold value is governed by the elastic capacity of the outer RC layer. Therefore, for a given charge mass and structural geometry, the foam yield strength must be kept below this upper bound to ensure outer-layer elasticity. The determination of this bound is embedded in the iterative design procedure presented in Section 3.3 (Step 10), where σ_p is adjusted until the Mode I criterion is satisfied.

2.3. Effect of Interlayer Thickness

To investigate the influence of the foam-concrete interlayer thickness on the structural response, three values of interlayer thickness h_f were considered: 150 mm (baseline), 175 mm, and 200 mm. The baseline thickness of 150 mm was adopted from the experimental prototype [5]. The range of 150–200 mm covers the typical design space for foamed concrete interlayers in containment structures [10,17], where thinner layers risk densification and thicker layers yield diminishing returns. The foam-concrete yield strength was fixed at $\sigma_p = 1.0$ MPa (baseline), and all other geometric and material parameters remained unchanged (Section 2.1). Two charge masses, $W = 3$ kg and 5 kg TNT, were simulated for each thickness.

2.3.1. Effect of Interlayer Thickness on Radial Displacement

Figure 3 presents the time histories of the inner-layer radial displacement at the blast-center cross-section for different h_f values under 3 kg and 5 kg TNT. For both charge masses, the inner-layer displacement curves for the three thicknesses nearly coincide, indicating that increasing the interlayer thickness has negligible influence on the inner-layer peak displacement or the time to reach the peak. This is because the inner-layer motion is governed primarily by the blast impulse and its own structural resistance, rather than by the interlayer stiffness or thickness.

Figure 4 shows the corresponding outer-layer displacement time histories. The peak outer-layer displacement decreases only slightly with increasing h_f : under 3 kg TNT, the peak at $h_f = 200$ mm is 92.6% of that at $h_f = 150$ mm; under 5 kg TNT, this ratio is 95.9%. However, the time to reach the peak displacement is noticeably delayed as h_f increases, due to the longer stress-wave propagation path through the thicker interlayer.

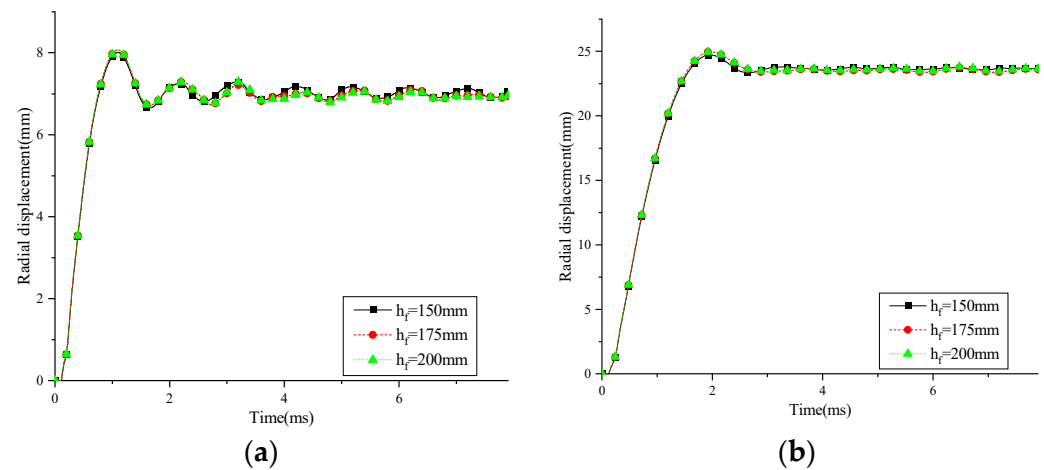


Figure 3. Time-history curves of the structural inner-layer displacement for different thicknesses of the energy-absorbing layer: (a) under 3 kg TNT; (b) under 5 kg TNT.

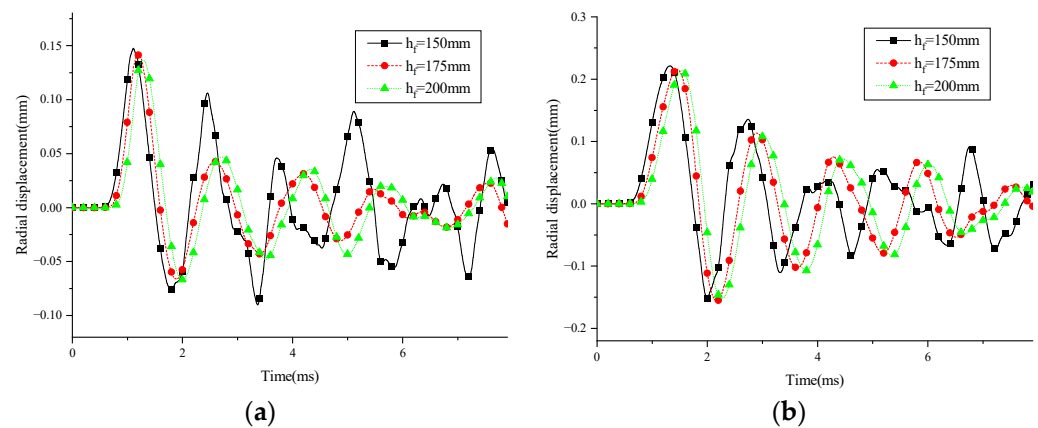


Figure 4. Time-history curves of the structural outer-layer displacement for different thicknesses of the energy-absorbing layer: (a) under 3 kg TNT; (b) under 5 kg TNT.

2.3.2. Effect of Interlayer Thickness on Interlayer Contact Pressure

Figure 5 compares the contact pressure time histories at the inner and outer interfaces for different h_f values under 3 kg and 5 kg TNT. The peak contact pressures at both interfaces are essentially unaffected by the interlayer thickness. However, the onset time, the time to peak, and the termination time of the outer-interface pressure are all delayed with increasing h_f , consistent with the extended wave travel distance through the foam layer. The total pressure duration at the outer interface remains largely unchanged.

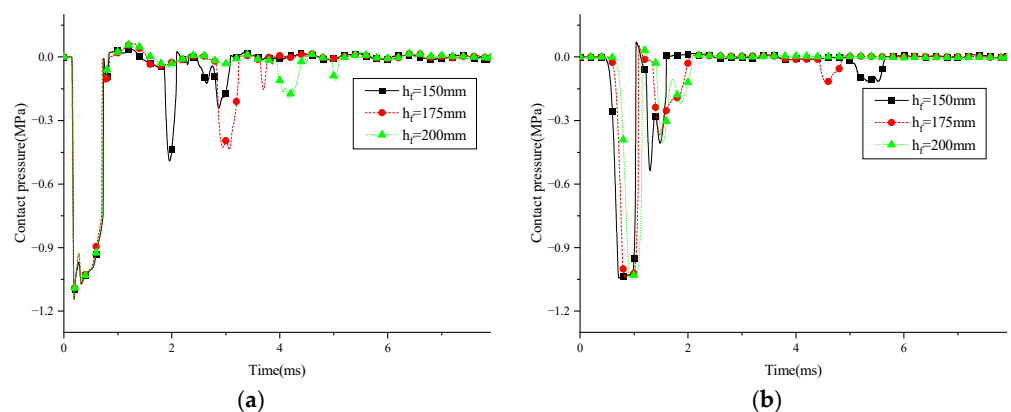


Figure 5. Cont.

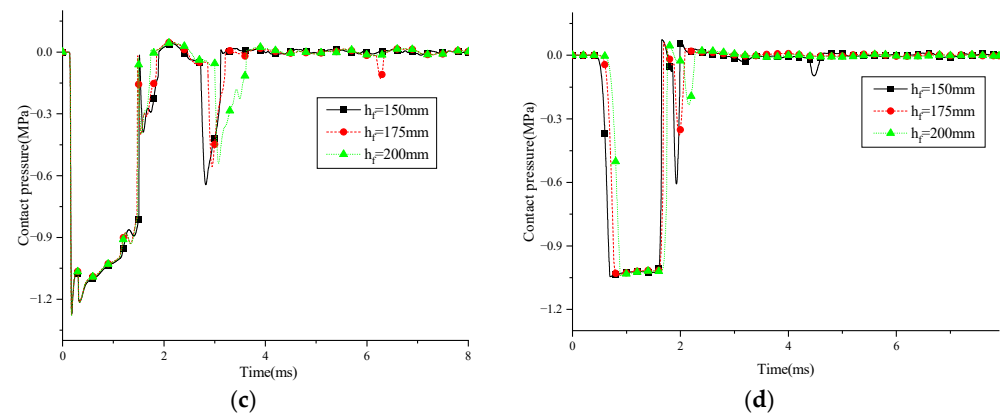


Figure 5. Time-history curves of contact pressure for different thicknesses of the energy-absorbing layer. (a) Inner interface under 3 kg TNT. (b) Outer interface under 3 kg TNT. (c) Inner interface under 5 kg TNT. (d) Outer interface under 5 kg TNT.

2.3.3. Minimum Required Interlayer Thickness

The above results demonstrate that, for a given charge mass and inner-layer configuration, increasing the interlayer thickness beyond a certain value yields diminishing returns in reducing the outer-layer response. Conversely, if the interlayer is too thin, the foam may enter the compaction-hardening stage during the inner-layer deceleration, causing the transmitted pressure to exceed σ_p and potentially triggering premature outer-layer yielding. Therefore, for specified resistance requirements and structural dimensions, there exists a minimum interlayer thickness that provides sufficient energy absorption and impulse attenuation while avoiding foam densification; increasing the thickness beyond this minimum does not further reduce the outer-layer response but does increase material cost. This concept will be revisited in the derivation of the simplified design method in Section 3.

2.4. Effect of Inner-Layer Reinforcement Ratio

To evaluate the influence of the inner-layer reinforcement ratio φ on the structural response, three values of reinforcement ratio were considered: 1% (baseline), 2%, and 3%. The baseline ratio of 1% corresponds to the reinforcement layout in the companion experiment [5]. Ratios of 2% and 3% represent common upper bounds for RC containment structures [41], beyond which steel congestion may hinder construction. The reinforcement was implemented via the built-in rebar option in MAT_072R3, with all other parameters kept at the baseline values (Section 2.1). Two charge masses, $W = 3$ kg and 5 kg TNT, were simulated for each value.

2.4.1. Effect of Inner-Layer Reinforcement Ratio on Radial Displacement

Figure 6 presents the time histories of the inner-layer radial displacement for different φ values. The peak inner-layer displacement decreases markedly with increasing reinforcement ratio. Under 3 kg TNT, the peak displacement at $\varphi = 3\%$ is 74.4% of that at $\varphi = 1\%$; under 5 kg TNT, this ratio is 69.9%. The time to reach the peak displacement is also shortened as φ increases, reflecting the enhanced flexural stiffness and energy dissipation capacity of the inner layer.

Figure 7 shows the corresponding outer-layer displacement time histories. The outer-layer peak displacement also decreases with increasing φ , though the effect is less pronounced than for the inner layer. Under 5 kg TNT, the peak outer-layer displacement at $\varphi = 3\%$ is 82.4% of that at $\varphi = 1\%$. The reduction is attributed to the decreased inner-layer deformation, which in turn reduces the compression demand on the foam interlayer and the impulse transmitted to the outer layer.

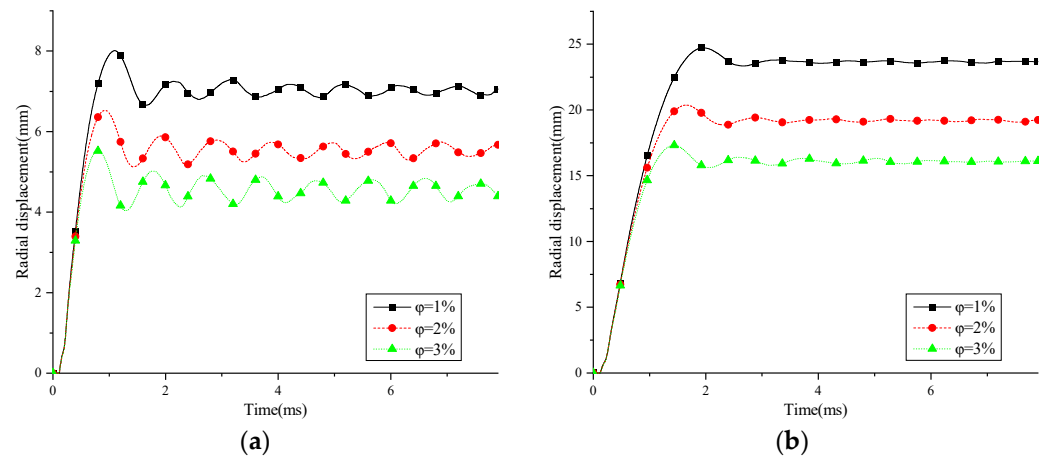


Figure 6. Time-history curves of inner-layer displacement for different reinforcement ratios: (a) under 3 kg TNT; (b) under 5 kg TNT.

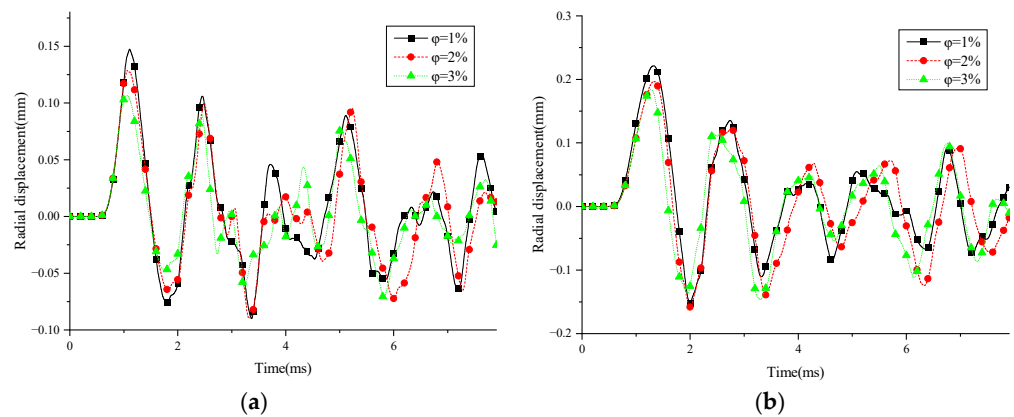


Figure 7. Time-history curves of outer-layer displacement for different reinforcement ratios: (a) under 3 kg TNT; (b) under 5 kg TNT.

2.4.2. Effect of Inner-Layer Reinforcement Ratio on Interlayer Contact Pressure

Figure 8 compares the contact pressure time histories at the inner and outer interfaces for different ϕ values. The peak contact pressures at both interfaces show little variation with the reinforcement ratio. However, the duration of the contact pressure is noticeably shortened as ϕ increases, because the stiffer inner layer decelerates more rapidly, reducing the time over which the foam is compressed. This shortening of the pressure pulse contributes to the reduction in outer-layer displacement observed in Figure 7.

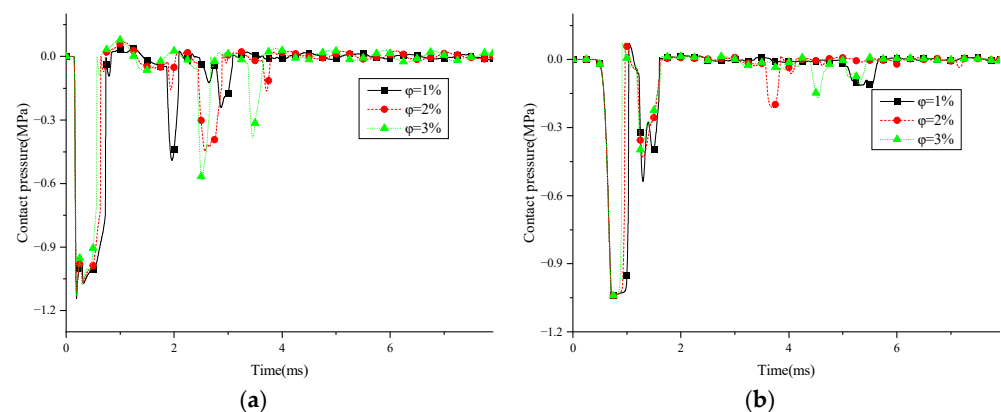


Figure 8. Cont.

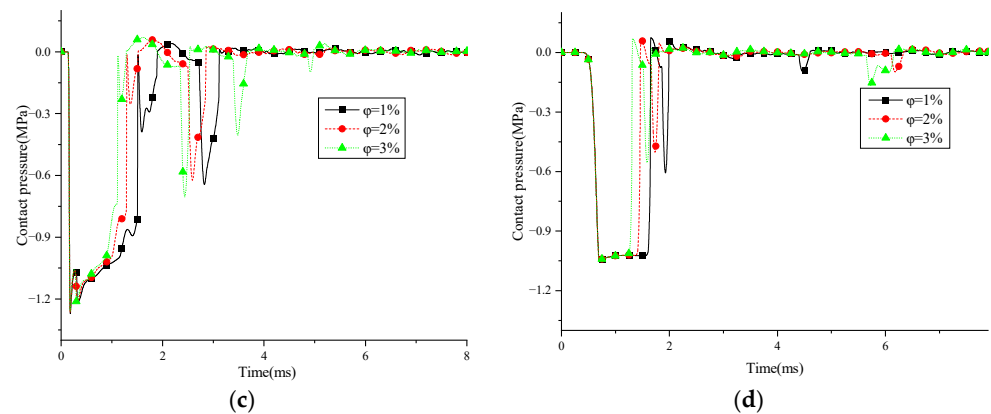


Figure 8. Time-history curves of contact pressure for different reinforcement ratios. (a) Inner interface under 3 kg TNT. (b) Outer interface under 3 kg TNT. (c) Inner interface under 5 kg TNT. (d) Outer interface under 5 kg TNT.

2.5. Effect of Inner-Layer Thickness

To investigate the influence of the inner RC layer thickness on the structural response, three values of inner-layer thickness h_i were considered: 175 mm (baseline), 200 mm, and 225 mm. The baseline thickness of 175 mm matches the experimental prototype [5]. The range of 175–225 mm spans typical design practice for RC containment walls [42], where thicker sections increase self-weight and cost. The outer RC layer thickness $h_o = 175$ mm and the interlayer thickness $h_f = 150$ mm was held constant; the foam-concrete yield strength was $\sigma_p = 1.0$ MPa and the reinforcement ratio was $\phi = 1\%$. Two charge masses, $W = 3$ kg and 5 kg TNT, were simulated for each h_i value.

2.5.1. Effect of Inner-Layer Thickness on Radial Displacement

Figure 9 presents the time histories of the inner-layer radial displacement for different h_i values. The peak inner-layer displacement decreases substantially with increasing inner-layer thickness. Under 3 kg TNT, the peak displacement at $h_i = 225$ mm is 69.6% of that at $h_i = 175$ mm; under 5 kg TNT, this ratio is 61.2%. The time to reach the peak displacement is also shortened as h_i increases, due to the greater bending stiffness and mass of the thicker inner layer.

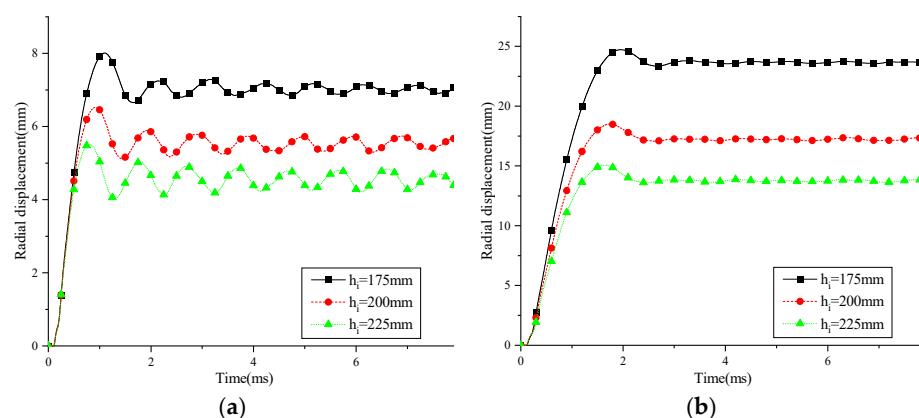


Figure 9. Time-history curves of inner-layer displacement for different h_i values: (a) under 3 kg TNT; (b) under 5 kg TNT.

Figure 10 shows the corresponding outer-layer displacement time histories. The outer-layer peak displacement also decreases with increasing h_i , though the effect is more pronounced under the smaller charge. Under 3 kg TNT, the peak outer-layer displacement at $h_i = 225$ mm is 50.7% of that at $h_i = 175$ mm; under 5 kg TNT, this ratio is 86.1%. The weaker influence under the larger charge suggests that when the inner layer undergoes

large plastic deformation, the additional stiffness gained from increasing its thickness becomes less effective in limiting the impulse transmitted to the outer layer.

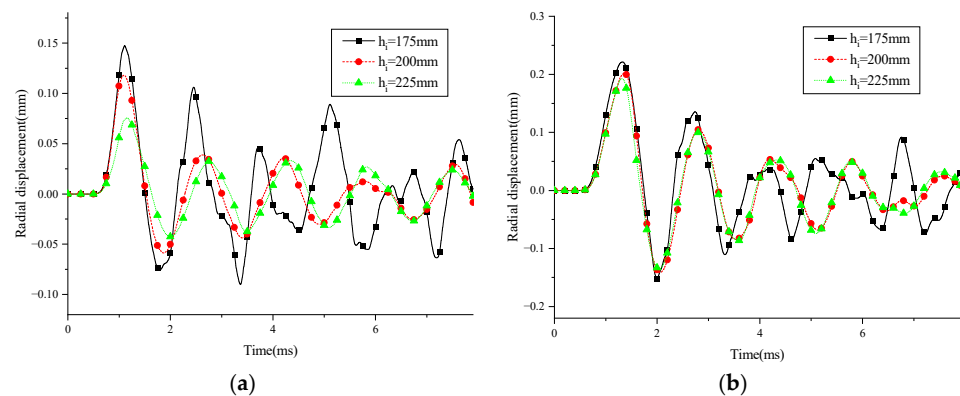


Figure 10. Time-history curves of outer-layer displacement for different h_i values: (a) under 3 kg TNT; (b) under 5 kg TNT.

2.5.2. Effect of Inner-Layer Thickness on Interlayer Contact Pressure

Figure 11 compares the contact pressure time histories at the inner and outer interfaces for different h_i values. Under 3 kg TNT, increasing h_i reduces both the peak contact pressures and their durations at both interfaces. Under 5 kg TNT, the peak contact pressures remain largely unchanged, but the pressure duration is noticeably shortened as h_i increases. This difference arises because, under the larger charge, the foam quickly reaches its yield plateau regardless of the inner-layer stiffness, so the peak pressure is governed by σ_p rather than by the inner-layer response; however, the stiffer inner layer decelerates faster, reducing the time over which the foam is compressed.

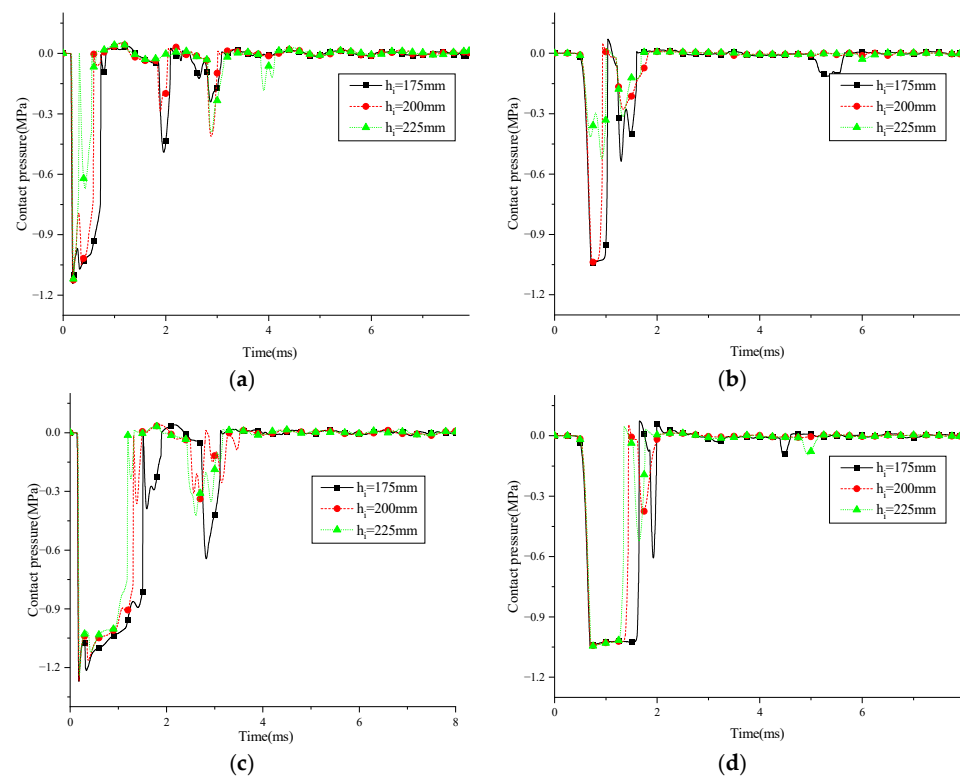


Figure 11. Time-history curves of contact pressure for different h_i values. (a) Inner interface under 3 kg TNT. (b) Outer interface under 3 kg TNT. (c) Inner interface under 5 kg TNT. (d) Outer interface under 5 kg TNT.

2.6. Summary of Parametric Results

The parametric study presented in Sections 2.2–2.5 examined the influence of four key parameters on the response of MLCCSs under internal blast. The main findings are summarized as follows:

- Foam-concrete yield strength σ_p (Section 2.2): Increasing σ_p reduces the inner-layer displacement but increases the outer-layer displacement and contact pressure. At sufficiently high σ_p and large charge mass, the failure mode transitions from the desired Mode I (inner-layer collapse before outer-layer yielding) to the undesirable Mode II (premature outer-layer yielding). The critical σ_p for Mode II initiation depends on the charge mass and the outer-layer elastic capacity.
- Interlayer thickness h_f (Section 2.3): Increasing h_f has negligible influence on the inner-layer displacement and only a minor effect on the outer-layer peak displacement, but delays the outer-layer response. For given structural dimensions and resistance requirements, a minimum h_f exists below which foam densification is expected, while further increases in the thickness do not reduce the outer-layer response.
- Inner-layer reinforcement ratio φ (Section 2.4): Increasing φ effectively reduces both inner- and outer-layer displacements and shortens the contact pressure duration, without altering the peak contact pressure.
- Inner-layer thickness h_i (Section 2.5): Increasing h_i substantially reduces inner-layer displacement and moderately reduces outer-layer displacement, especially under smaller charges.

Among the four parameters, σ_p is the most critical for controlling the Mode I/II transition. Its selection is bounded by a dual criterion: an upper bound dictated by the outer layer's elastic capacity (to avoid Mode II), and a lower bound set by the minimum required interlayer thickness (to avoid foam densification). Interlayer thickness h_f governs the margin against foam densification that could also trigger Mode II. The inner-layer reinforcement ratio φ and thickness h_i serve as secondary adjustment variables. These observations form the basis for the simplified design method developed in Section 3.

3. Simplified Design Method and Validation

3.1. Structural Response Characteristics

Based on the experimental observations reported in [5], the numerical simulations in [36], and the parametric analysis presented in Section 2, the response of MLCCSs under internal blast can be characterized by the following features:

1. Sequential layer motion: The inner RC layer is first accelerated by the blast impulse and moves radially outward, compressing the foam interlayer. Only after the foam is sufficiently compacted does the outer RC layer begin to deform appreciably. The motions of the three layers are therefore temporally separated.
2. Outer-layer elasticity requirement: To maintain structural airtightness, the outer RC layer must remain elastic throughout the event. Once the outer layer develops plastic deformation, the structure is deemed to have lost its containment function—this defines the Mode II failure criterion.
3. Load-controlling behavior of the foam interlayer: Before the foam enters the compaction-hardening stage, the contact pressure at both the inner and outer interfaces remains approximately equal to the foam yield strength σ_p regardless of the incident blast intensity. This confirms the “load-controllability” concept [10,11] and implies that the outer layer experiences a pressure pulse whose magnitude is capped at σ_p .

4. Plane-strain dominance: The hoop strain in the inner layer far exceeds the axial strain at the blast-center cross-section, justifying a plane-strain idealization for the inner-layer deformation analysis.
5. Large-area foam plasticization: Under typical design charges, the foam interlayer undergoes full plasticization over a significant length along the structural axis. Consequently, the pressure acting on the outer layer can be approximated as a uniformly distributed pulse of magnitude σ_p and duration t_y .

These characteristics motivate the simplifications adopted in the following derivation.

3.2. Derivation of Simplified Method

3.2.1. Basic Assumptions

The following assumptions are made:

1. The inner RC layer is treated as a rigid–perfectly plastic ring under plane-strain conditions. Its elastic deformation is neglected compared with the large plastic deformation at the ultimate state.
2. The foam interlayer is idealized as a rigid–perfectly plastic material with a constant yield stress σ_p . Strain-hardening is neglected until the densification strain γ is reached. This idealization is consistent with the load-controllability concept [10,11] and is supported by our previous experimental [5] and numerical [36] investigations, as well as the parametric analysis presented in this paper.
3. The outer RC layer remains elastic and is modeled as a thin elastic cylindrical shell under uniform internal pressure.
4. The blast impulse I_R (per unit area of the inner wall) is instantaneously transferred to the inner layer, imparting an initial radial velocity V_0 . The subsequent motion of the inner layer is resisted by its own plastic membrane resistance, the foam reaction stress σ_p , and the quasi-static gas pressure P_g .
5. The inner and outer layers are mechanically decoupled by the foam interlayer; the outer layer responds only to the pressure transmitted through the foam.

3.2.2. Initial Velocity of the Inner Layer

Consider a unit-length ring segment of the inner layer subtending a small angle $d\alpha$. The mass per unit circumferential length μ_i is given by

$$\mu_i = \frac{(2R_i + h_i)h_i\rho_i}{2} \quad (1)$$

where R_i is the inner radius, h_i is the thickness, and ρ_i is the density of the inner RC layer. The mass per unit circumferential length μ_i is given by Equation (1). The total impulse acting on this segment is $I_R R_i d\alpha$. Equating the impulse to the change in momentum gives the initial radial velocity V_0 :

$$V_0 = \frac{I_R R_i d\alpha}{\mu_i d\alpha} = \frac{2I_R R_i}{(2R_i + h_i)h_i\rho_i} \quad (2)$$

3.2.3. Equation of Motion for the Inner Layer

After acquiring the initial velocity V_0 , the inner layer moves outward under the action of three forces per unit area: (i) the plastic membrane resistance σ_h of the RC ring; (ii) the foam reaction stress σ_p ; and (iii) the quasi-static gas pressure P_g inside the containment. The radial equation of motion is

$$\frac{d^2w}{dt^2} + \frac{\sigma_h}{\rho_i \left(R_i + \frac{h_i}{2}\right)} + \frac{\sigma_p(R_i + h_i)}{\mu_i} = \frac{P_g R_i}{\mu_i} \quad (3)$$

where w is the radial displacement of the inner layer.

For brevity, define the constant deceleration term:

$$K = \frac{P_g R_i}{\mu_i} - \frac{\sigma_h}{\rho_i \left(R_i + \frac{h_i}{2}\right)} - \frac{\sigma_p(R_i + h_i)}{\mu_i} \quad (4)$$

so that Equation (3) simplifies to

$$\frac{d^2w}{dt^2} = K \quad (5)$$

3.2.4. Maximum Inner-Layer Displacement

Integrating Equation (5) with the initial conditions $w(0) = 0$ and $\dot{w}(0) = V_0$ gives:

$$w(t) = V_0 t + \frac{1}{2} K t^2 \quad (6)$$

The inner layer comes to rest when $\dot{w}(t_f) = 0$, i.e.,:

$$t_f = -\frac{V_0}{K} \quad (7)$$

Substituting Equation (7) into Equation (6) yields the maximum radial displacement of the inner layer:

$$w_{max} = -\frac{V_0^2}{2K} \quad (8)$$

3.2.5. Plastic Membrane Resistance of the Inner Layer

The dynamic hoop tensile strength of a reinforced concrete ring can be expressed as [42]

$$\sigma_h = \frac{R_c}{k_h} \left(1 + \frac{\varphi R_s}{R_c}\right) \quad (9)$$

where R_c and R_s are the dynamic tensile strengths of concrete and steel, respectively; φ is the reinforcement ratio; and k_h is a reduction factor accounting for bond degradation at large deformation (typically taken as 1.2). Following the recommendations for blast-loaded RC cylinders [42], the dynamic strength enhancement factors are taken as 2.0 for concrete and the dynamic tensile strength of the reinforcement steel is taken as 500 MPa.

3.2.6. Minimum Required Interlayer Thickness for Densification Avoidance

To prevent the foam from entering the compaction-hardening stage (which would cause the transmitted pressure to exceed σ_p), the interlayer must be thick enough to accommodate the inner-layer displacement without full densification. Let γ be the densification strain of the foam. The required minimum interlayer thickness is

$$h_{f,min} = k_e \frac{w_{max}}{\gamma} \quad (10)$$

Here, k_e is a conservative safety factor accounting for practical uncertainties such as local strain concentration, material variability, and three-dimensional effects. It provides a design margin so that the selected interlayer thickness exceeds the theoretical minimum; the value may be adjusted based on material characterization and project risk. For a given charge mass and inner-layer configuration, any interlayer thickness greater than $h_{f,min}$

provides no additional benefit in reducing the outer-layer response, while a thickness smaller than $h_{f,min}$ risks triggering Mode II failure. The value $k_e = 1.2$ adopted in the present study corresponds to a 20% thickness margin, and its effect can be read directly from Equation (10), since $h_{f,min}$ is linear in k_e : increasing k_e from 1.2 to 1.5 raises the computed minimum thickness by 25%, while decreasing it to 1.0 lowers it by 17%. The effect of k_e on the design outcome is therefore limited to a proportional rescaling of the thickness requirement; it is applied only to the interlayer thickness, never to the Mode I strain criterion of Equation (13), and the margin it provides in the assessment cases and in the design example is quantified in Sections 3.4 and 3.5.

3.2.7. Outer-Layer Elastic Response

The outer RC layer is treated as a thin elastic cylindrical shell. Under the transmitted pressure pulse (idealized as a rectangular pulse of magnitude σ_p and duration t_y), the radial displacement is obtained from the Duhamel integral:

$$u(t) = \frac{1}{\rho_o h_o \omega} \int_0^t p_i(\tau) \sin[\omega(t - \tau)] d\tau \quad (11)$$

where ρ_o and h_o are the density and thickness of the outer layer, $R_o = R_i + h_i + h_f + h_o/2$ is the mid-surface radius of the outer shell, and $\omega = \sqrt{E/(\rho_o R_o^2)}$ is the natural frequency of the ring (E being the elastic modulus of concrete). The pressure duration t_y can be approximated as

$$t_y = t_f - t_c \approx t_f \text{ (when } t_f \gg t_c \text{)} \quad (12)$$

where $t_c = h_f/c_f$ is the stress-wave transit time through the foam interlayer (negligible for typical thicknesses).

The peak outer-layer hoop strain is then:

$$\varepsilon_{\theta,peak} = u_{max}/R_o \quad (13)$$

The structure satisfies the Mode I design criterion if $\varepsilon_{\theta,peak} \leq \varepsilon_{allowable}$, where $\varepsilon_{allowable}$ is the allowable hoop strain of the outer RC layer. A single value, $\varepsilon_{allowable} = 8.0 \times 10^{-4}$, is adopted here for all configurations, calibrated on the Mode I/II transition observed in the parametric study of Section 2.2.3. Being a calibrated quantity rather than a first-principles material constant, it is stated explicitly so that the criterion can be re-anchored if the outer-layer material or the required performance level changes.

3.3. Step-by-Step Design Procedure

Based on the simplified method derived in Section 3.2, a step-by-step design procedure for MLCCSs under internal blast is proposed. The procedure ensures that the structure operates in the desired Mode I (inner-layer collapse before outer-layer yielding) while avoiding unnecessary conservatism in the interlayer design.

Step 1: Define design inputs

- Design charge mass W (kg TNT);
- Inner radius of the containment R_i (m);
- Total internal volume V (m³);
- Material properties: concrete tensile strength f_t , steel yield strength f_y , foam-concrete yield strength σ_p (initial trial value);
- Geometric parameters: inner-layer thickness h_i , outer-layer thickness h_o , interlayer thickness h_f (initial trial values);
- Reinforcement ratio of the inner layer φ .

Step 2: Calculate blast impulse

Determine the specific impulse I_R (Pa·s) acting on the inner wall. The following sources are recommended in order of priority:

1. Experimental data or validated numerical simulations—Direct measurement or high-fidelity FSI simulation results for the specific geometry and charge mass should be used whenever available.
2. Empirical formulas—When test or simulation data are unavailable, the impulse can be estimated using empirical formulas. For cylindrical confined explosions, the following formula is calibrated based on numerical simulations validated by experimental data [43]:

$$I_R = 652 \left(R_i / W^{1/3} \right)^{-1.791} W^{1/3}$$

This formula is calibrated for $0.1 \leq R_i / W^{1/3} \leq 0.97$. Its applicability outside this range should be verified. Alternatively, general-purpose empirical formulas such as UFC 3-340-02 [30] may be used, with due consideration of their underlying assumptions and applicable ranges.

3. Simplified relations—Other simplified relations calibrated for specific geometries may also be employed, provided their validity for the target configuration is confirmed.

For the present geometry ($R_i = 0.5$ m, $L = 5$ m), the impulse value for 5 kg TNT is 8264 Pa·s obtained from the validated FSI model [36].

Step 3: Compute inner-layer initial velocity

Using Equation (2), compute the inner-layer initial velocity V_0 .

Step 4: Estimate quasi-static gas pressure

For a fully contained explosion, the quasi-static gas pressure can be estimated as [44]

$$P_g = 1.34 W / V \text{ (MPa)}$$

where W is in kg and V is in m^3 . This formula assumes a fully sealed containment vessel with no venting or pressure relief. For structures with openings, vents, or blow-off panels, the actual quasi-static gas pressure will be lower, and the above formula will overestimate P_g , leading to a conservative (safe-side) prediction of inner-layer displacement.

Step 5: Compute plastic membrane resistance of the inner layer

Using Equation (9), compute the plastic membrane resistance σ_h , with $R_c = 2.0f_t$ (dynamic tensile strength of concrete), $R_s = 500$ MPa (dynamic yield strength of steel), and $k_h = 1.2$.

Step 6: Compute deceleration parameter and maximum inner-layer displacement

Using Equations (4) and (8), compute the deceleration parameter K and the maximum inner-layer displacement w_{max} .

Step 7: Check inner-layer failure criterion

If w_{max} exceeds the allowable inner-layer displacement w_{allow} (corresponding to the ultimate hoop strain of the reinforcement, typically 5% strain), the inner layer fails in Mode I as intended. If $w_{max} < w_{allow}$, the inner layer is underutilized and the design may be overly conservative—consider reducing h_i or ϕ . Note that an underutilized inner layer ($w_{max} < w_{allow}$) does not violate the Mode I criterion; it merely indicates potential for design optimization (e.g., reducing h_i or ϕ).

Step 8: Determine the minimum required interlayer thickness

Using Equation (10), determine the minimum required interlayer thickness $h_{f,min}$.

Select $h_f \geq h_{f,min}$, since any thickness beyond $h_{f,min}$ provides no additional reduction in the outer-layer response (Section 3.2.6), the smallest available thickness satisfying this constraint is the economical choice.

Step 9: Compute outer-layer peak hoop strain

Using Equations (11)–(13), compute the peak outer-layer hoop strain $\varepsilon_{\theta,peak}$. The pressure pulse on the outer layer is idealized as a rectangular pulse of magnitude σ_p and duration t_y .

Step 10: Verify Mode I criterion

Check:

$$\varepsilon_{\theta,peak} < \varepsilon_{allowable}$$

where $\varepsilon_{allowable}$ is the allowable hoop strain of the outer RC layer (Section 3.2.7). If satisfied, the design achieves Mode I. If not, adjust σ_p or h_f and return to Step 6.

Selection criterion for σ_p : The foam yield strength σ_p must satisfy a dual-bound criterion:

- Lower bound ($\sigma_{p,min}$): Determined by the minimum interlayer thickness. A lower σ_p leads to larger w_{max} , requiring a thicker interlayer $h_{f,min}$ to avoid densification. If $h_{f,min}$ exceeds the practical or cost-effective limit, σ_p should be increased.
- Upper bound ($\sigma_{p,max}$): Governed by the elastic capacity of the outer layer. It is the maximum σ_p for which $\varepsilon_{\theta,peak} < \varepsilon_{allowable}$ is satisfied in Step 10. Exceeding this bound triggers Mode II failure.

The designer should iterate σ_p within the range $[\sigma_{p,min}, \sigma_{p,max}]$ to optimize for cost, weight, or constructability; the utilization requirement of Step 7 may further restrict this range from above, as demonstrated in the design example (see Section 3.5).

Step 11: Output design parameters

Finalize σ_p , h_f , h_i , and φ that satisfy all criteria.

3.4. Numerical Assessment

To assess the accuracy of the simplified method, the predicted inner-layer maximum displacements are compared with the numerical results from the parametric study for five representative cases covering a range of σ_p and φ values under 5 kg TNT. The comparison is presented in Table 5.

Table 5. Comparison of inner-layer maximum displacement: simplified method vs. numerical simulation (5 kg TNT).

Case	σ_p (MPa)	φ (%)	$w_{max,num}$ (mm)	$w_{max,sim}$ (mm)	Error (%)
1	1.0	0.9	25.33	28.28	+11.64
2	1.0	1.0	24.78	26.64	+7.51
3	1.5	1.0	22.17	21.79	−1.71
4	2.0	1.0	19.73	17.29	−12.37
5	2.5	1.0	17.75	15.22	−14.25

The displacement predicted by the simplified method remains within the $\pm 15\%$ working tolerance of the numerical results in all five cases. The error exhibits a clear trend: the method overpredicts w_{max} when the displacement is large (Cases 1 and 2, where the inner layer undergoes significant plastic deformation) and underpredicts w_{max} when the displacement is small (Cases 4 and 5, where the elastic deformation of the inner layer and the finite stress-wave transit time through the foam become non-negligible relative to the total displacement).

The mean absolute error across the five cases is 9.50%, with a maximum of 14.3%. Only this displacement output is compared quantitatively; the remaining outputs of the method are assessed in Section 3.4 for consistency with the same numerical framework, as described below. An error band of $\pm 15\%$ is adopted internally as a working tolerance for preliminary screening, not as an established acceptance criterion that the method is

claimed to satisfy; no such external criterion is available, and the band is stated explicitly only so that the reader can judge the method against it. Its justification is one of purpose rather than of precedent: the method is offered as a screening tool, to be used in place of repeated FSI simulation during option screening. Within this purpose, the simplified method provides a practical tool for the preliminary sizing of MLCCSs under internal blast, with the understanding that final designs should be verified by numerical simulation, particularly when operating near the Mode I/II boundary.

The comparison above addresses only the inner-layer maximum displacement. The simplified method also delivers three further design-critical outputs (the minimum required interlayer thickness, the outer-layer peak hoop strain, and the Mode I/II classification, the latter returned by the Step 10 comparison of the outer-layer peak hoop strain with the allowable hoop strain), and these must be checked for consistency with the numerical results before the method can be used with confidence as a design tool. Table 6 therefore extends the comparison of Table 5 to all four outputs, using the same set of FSI results.

Table 6. Comparison of the four design-critical outputs of the simplified method with the FSI results (5 kg TNT; $h_i = h_o = 175$ mm and $h_f = 150$ mm in all cases).

Case	σ_p (MPa)	φ (%)	$h_{f,min}$, Equation (10) (mm)	$h_{f,min}/h_f$, Adopted	$\varepsilon_{\theta,peak}$, Equation (13) ($\mu\varepsilon$)	$\varepsilon_{\theta,peak}$, FSI ($\mu\varepsilon$)	Mode, Step 10	Mode, FSI
1	1.0	0.9	56.4	0.38	321	256	I	I
2	1.0	1.0	53.1	0.35	321	243	I	I
3	1.5	1.0	41.8	0.28	481	371	I	I
4	2.0	1.0	34.5	0.23	642	621	I	I
5	2.5	1.0	29.3	0.20	802	903	II	II

The minimum required interlayer thickness computed from Equation (10), evaluated with the safety factor $k_e = 1.2$, a densification strain $\gamma = 0.60$, and w_{max} taken from Equation (8), lies between 29.3 mm and 56.4 mm over the five cases. The thickness actually adopted in the numerical model, $h_f = 150$ mm, therefore exceeds the computed minimum by a factor of 2.6 or more in every case, and no densification of the interlayer is expected. This is consistent with the interlayer volumetric strain extracted from the FSI results, which remains below the densification strain in all five cases.

The outer-layer peak hoop strain obtained from Equations (11)–(13), evaluated with $E = 32.5$ GPa and $\rho_o = 2500$ kg/m³ for the outer RC layer, is compared in Table 6 with the peak hoop strain extracted from the outer RC layer at the blast-center cross-section of the FSI model. The closed-form strain exceeds the FSI value by 25–32% at the three lowest foam grades (Cases 1–3), agrees with it within 4% at $\sigma_p = 2.0$ MPa (Case 4), and falls 11% below it at $\sigma_p = 2.5$ MPa (Case 5), where both values exceed the threshold and the classification is unaffected. The mean absolute deviation is about 20%, considerably larger than that of the displacement output; the $\pm 15\%$ working tolerance stated above therefore applies to the displacement, whereas the strain output is used through the Mode I/II classification. The overprediction at low foam grades is conservative for the Mode I criterion. It is also noted that the closed-form strain is identical for Cases 1 and 2: under the load-controllability idealization, the transmitted pressure is capped at σ_p and the pulse duration enters the evaluation only through the conservative rectangular-pulse amplification, so the dependence on the inner-layer reinforcement ratio is not resolved. The small difference present in the FSI results (256 versus 243 $\mu\varepsilon$) is of second order and lies well inside the conservative bias of the prediction.

The Mode I/II classification returned by the Step 10 criterion, $\varepsilon_{\theta,peak} \leq \varepsilon_{allowable} = 8.0 \times 10^{-4}$, reproduces the mode actually observed in the FSI results in all five cases: Cases 1–4 are classified as Mode I and Case 5 ($\sigma_p = 2.5$ MPa) as Mode II, in agreement with the

transition identified independently from the parametric study in Section 2.2.3. Because $\varepsilon_{allowable}$ is anchored on that transition (Section 3.2.7), this agreement is not by itself an independent validation of the numerical value of the threshold; what the comparison establishes is that a single calibrated threshold, applied through the closed-form Equation (13), reproduces the observed classification over the whole σ_p range examined, and that the closed-form strain follows the same ordering as the FSI response.

It should be noted that the FSI results used for the comparison in Table 6 are obtained from the same modeling framework as the parametric study, so the comparison establishes consistency between the simplified method and the numerical model rather than independent physical validity. The outer-layer hoop strain and the densification state were obtained by post-processing the existing FSI results; no additional numerical simulations were performed for this purpose.

3.5. Prospective Design Example and Numerical Assessment

3.5.1. Design Brief and Optimization Criterion

The procedure of Section 3.3 is now applied prospectively to a design brief that does not coincide with any of the five cases used for assessment in Section 3.4. The design charge is $W = 5$ kg TNT; the containment geometry is $R_i = 0.5$ m and $L = 5$ m, for which the specific impulse on the inner wall is $I_R = 8264$ Pa·s (obtained from the validated FSI model, Step 2); the inner layer is $h_i = 175$ mm with reinforcement ratio $\varphi = 1\%$ as fixed by the prototype of [5]; and the interlayer thickness is restricted to the practical range 150–200 mm. To make the example prospective by construction, the brief further requires the adopted thickness to differ from the 150 mm used in all five assessment cases of Section 3.4 and to coincide with one of the other thicknesses analyzed in Section 2.3 (175 or 200 mm), so that the assessment of Section 3.5.3 can use an existing FSI result without new simulations. The design objective is to select the foam-concrete grade σ_p and the interlayer thickness h_f that satisfy all three design constraints of Section 3.3 while minimizing material cost. The optimization criterion is therefore stated explicitly: minimize the foam grade, subject to (i) the inner layer remaining fully utilized ($w_{max} \geq w_{allow}$, Step 7), (ii) the interlayer not densifying ($h_f \geq h_{f,min}$, Step 8), and (iii) the outer RC layer remaining elastic ($\varepsilon_{\theta,peak} \leq \varepsilon_{allowable}$, Step 10). Within the practical envelope, the densification constraint is non-binding and the structural response is only weakly sensitive to h_f (Section 2.3), so the thickness is fixed by the material-cost criterion within the admissible set defined above (Step 8).

3.5.2. Execution of the Design Procedure

Steps 1–11 of Section 3.3 are executed in order; the intermediate quantities are collected in Table 7.

Steps 3–6 proceed exactly as set out in Section 3.3: $\mu_i = 257.0$ kg/m from Equation (1); $V_0 = 16.08$ m/s from Equation (2); $P_g = 1.706$ MPa for the internal volume $V = 3.927$ m³, and $\sigma_h = 8.17$ MPa from Equation (9); with $R_c = 2.0f_t$ ($f_t = 2.4$ MPa, the tensile strength of the prototype concrete [5]), $R_s = 500$ MPa and $k_h = 1.2$; Equations (4) and (8) then give $K = -4.85 \times 10^3$ m/s² and $w_{max} = 26.64$ mm at $\sigma_p = 1.0$ MPa.

Step 7 gives $w_{allow} = 0.05 R_i = 25.0$ mm (the 5% hoop-strain limit of the reinforcement). Since $w_{max} = 26.64$ mm exceeds w_{allow} , the inner layer reaches its deformation limit and the design attains the intended Mode I collapse of the inner layer rather than an under-utilized configuration. Solving $w_{max} = w_{allow}$ gives $\sigma_p = 1.11$ MPa, so any foam grade above this value leaves the inner layer under-utilized.

Table 7. Step-by-step execution of the design procedure for the prospective design example ($W = 5$ kg TNT, $R_i = 0.5$ m, $L = 5$ m, $h_i = h_o = 175$ mm, $\varphi = 1\%$).

Step	Quantity	Value
1	$W; R_i, L$	5 kg TNT; 0.5 m, 5 m
1	$h_i, h_o, \varphi; f_t, E, \rho_i = \rho_o$	175 mm, 175 mm, 1.0%; 2.4 MPa, 32.5 GPa, 2500 kg/m ³
2	I_R	8264 Pa·s
3	μ_i , Equation (1); V_o , Equation (2)	257.0 kg/m; 16.08 m/s
4	P_g ($V = 3.927$ m ³)	1.706 MPa
5	σ_h , Equation (9)	8.17 MPa
6	K , Equation (4); w_{max} , Equation (8); t_f , Equation (7)	-4.85×10^3 m/s ² ; 26.64 mm; 3.30 ms
7	$w_{allow} = 0.05R_i$; check	25.0 mm; $w_{max} = 26.64$ mm $> w_{allow} \rightarrow$ inner layer fully utilized
8	$h_{f,min}$, Equation (10) ($k_e = 1.2, \gamma = 0.60$)	53.1 mm ≤ 150 –200 mm $\rightarrow$ satisfied
9	u_{max} , Equation (11); $\varepsilon_{\theta,peak}$, Equation (13)	0.309 mm; 330 $\mu\varepsilon$
10	Mode I check	330 $\mu\varepsilon < \varepsilon_{allowable} = 800$ $\mu\varepsilon \rightarrow$ Mode I; $\sigma_{p,max} = 2.43$ MPa
11	Design output	$\sigma_p = 1.0$ MPa; $h_f = 175$ mm

Step 8 gives $h_{f,min} = 53.1$ mm, comfortably below the available 150–200 mm range, so the densification constraint is satisfied throughout the practical envelope. Since the structural response is only weakly sensitive to h_f within this range (Section 2.3), the material-cost criterion favors the smallest admissible thickness. With 150 mm excluded by the design brief, the procedure returns $h_f = 175$ mm, the smaller of the two remaining thicknesses for which an FSI result exists (Section 2.3). The adopted value provides a factor of 3.3 over the computed minimum and therefore a substantial margin against densification, and because it differs from the thickness of all five validation cases, the resulting design constitutes a prospective prediction rather than a repetition of an already assessed configuration (Section 3.5.3).

Step 9 gives $u_{max} = 0.309$ mm and $\varepsilon_{\theta,peak} = 330$ $\mu\varepsilon$, evaluated at $R_o = 0.9375$ m for the adopted configuration ($h_f = 175$ mm). In Step 10, this satisfies $\varepsilon_{\theta,peak} < \varepsilon_{allowable} = 800$ $\mu\varepsilon$, so the outer RC layer remains elastic and the Mode I criterion is met. Solving the same criterion for σ_p returns the upper bound of the dual-bound window for this geometry, $\sigma_{p,max} = 2.43$ MPa.

For Step 11, the dual-bound window is evaluated as follows. The upper bound, $\sigma_{p,max} = 2.43$ MPa, is governed by the elastic capacity of the outer layer: above this value, the transmitted pressure drives the outer-layer hoop strain beyond $\varepsilon_{allowable}$ and Mode II ensues. The lower bound would be set by the densification requirement of Equation (10): a lower foam grade enlarges the inner-layer displacement and, hence, the computed minimum thickness. For this geometry, the thickness requirement never binds: the computed minimum of 53.1 mm is only about one third of the smallest available thickness (150 mm), and it would remain below half of that value even at $k_e = 1.5$. The available thickness therefore imposes no lower bound on σ_p . Within this window, the utilization requirement of Step 7 imposes a further ceiling: $w_{max} \geq w_{allow} = 25.0$ mm is satisfied only for $\sigma_p \leq 1.11$ MPa, tighter than the Mode II bound. The design point is therefore selected at $\sigma_p = 1.0$ MPa with $h_f = 175$ mm, the lowest foam grade for which the constitutive behavior has been characterized experimentally [5], combined with the smallest thickness admitted by the design brief, as determined in Step 8. The resulting design sits at $\sigma_p/\sigma_{p,max} = 0.41$ of the upper bound.

3.5.3. Numerical Assessment of the Resulting Design

The configuration returned by the procedure ($\sigma_p = 1.0$ MPa, $h_f = 175$ mm, $h_i = 175$ mm, $\varphi = 1\%$, $W = 5$ kg TNT) is not one of the five cases used for assessment in Section 3.4, all

of which have $h_f = 150$ mm; this difference is by construction, as required by the design brief. A numerical result for this configuration is nevertheless available from the parametric study of Section 2.3, and is used here to check the design prediction. Since this FSI result is produced by the same modeling framework used for calibration and validation, the check is independent of the five assessment cases in terms of configuration, though not of the numerical model itself. Four design-critical outputs are compared with the FSI results: the inner-layer maximum displacement, the occurrence of foam densification, the outer-layer peak hoop strain, and the resulting failure mode.

The predicted inner-layer maximum displacement is 26.64 mm. The FSI peak inner-layer displacements computed for $h_f = 150, 175$ and 200 mm under 5 kg TNT are $24.78, 24.96$ and 24.97 mm, respectively, within 0.8% of one another, confirming that the inner-layer response is essentially insensitive to h_f (Figure 3). The numerical value for the adopted configuration is therefore 24.96 mm, and the prediction is about 7% higher, the same order as the corresponding entry of Table 5 (Case 2); this is expected, because the inner-layer displacement is governed by the blast impulse and by the inner-layer resistance rather than by h_f . No densification of the interlayer occurred, consistent with the adopted thickness being about 3.3 times the computed $h_{f,min} = 53.1$ mm. The outer-layer peak radial displacement in the FSI result is 0.219 mm, corresponding to a peak hoop strain of about $234 \mu\epsilon$ at the mid-surface radius $R_o = 0.9375$ m through the same kinematic relation as Equation (13); the closed-form prediction of $330 \mu\epsilon$ is therefore conservative. The outer layer remained elastic, i.e., Mode I, in agreement with the Step 10 prediction.

3.5.4. Remarks on the Design Outcome

Applied prospectively, the design procedure selected the lowest-cost foam grade satisfying all three design constraints, and it did so within the dual-bound window rather than by trial and error. Because Table 7 lists every intermediate quantity, the design can be audited step by step, and the sensitivity of the outcome to σ_p , k_e , γ or $\epsilon_{allowable}$ can be read directly from the corresponding equation (for example, the linear dependence of $h_{f,min}$ on k_e and its inverse dependence on γ).

For this geometry, the design is governed by the requirement to utilize the inner layer (Step 7) rather than by the elastic capacity of the outer layer, which retains a margin of a factor of 2.4 . Since the inner layer is the sacrificial component, a larger deformation would by itself dissipate more energy; the deformation that can be credited in design is nevertheless limited by the ultimate hoop strain of the reinforcement, commonly taken as about 5% for cylindrical RC structures under internal blast (Section 2.2.1), beyond which the reinforcement approaches rupture and the layer can no longer be relied upon to remain a coherent membrane. This requirement imposes a utilization ceiling within the dual-bound window ($\sigma_p \leq 1.11$ MPa, Step 7), tighter than the Mode II bound of 2.43 MPa, and the selected grade $\sigma_p = 1.0$ MPa places $w_{max} = 26.64$ mm marginally above $w_{allow} = 25.0$ mm: the inner layer is fully but not excessively utilized.

4. Discussion

4.1. Applicability of the Simplified Method

The simplified design method proposed in Section 3 is derived from the fundamental physics of MLCCSs under internal blast—sequential layer motion, load controllability of the foam interlayer, and elastic outer-layer requirement—rather than from empirical curve-fitting to a specific dataset. As such, the formulation contains no coefficient fitted to a particular geometry or charge mass. Whether this property is sufficient for the equations to remain applicable to other cylindrical MLCCS geometries and material parameters cannot be established within the present study: the method has been examined numerically only

through variations in one parameter at a time at $R_i = 0.5$ m and 3–5 kg TNT, and the coupled-parameter conditions under which the idealizations of Section 3.2.1 break down have not been mapped. Accordingly, no applicability beyond the ranges examined here and listed below is claimed in this work; the broader applicability suggested by the structure of the equations is put forward as a hypothesis to be tested, and the coupled-parameter assessment required for this purpose is identified as future work (Sections 4.2 and 4.6).

In the present study, the method has been assessed against numerical simulations, within which the inner-layer displacement is quantitatively reproduced and the remaining outputs are consistent with the numerical model, for the following ranges: inner radius $R_i = 0.5$ m, charge mass $W = 3\sim 5$ kg TNT, foam-concrete yield strength $\sigma_p = 1.0\sim 2.5$ MPa, interlayer thickness $h_f = 150\sim 200$ mm, inner-layer reinforcement ratio $\varphi = 1\%\sim 3\%$, and inner-layer thickness $h_i = 175\sim 225$ mm. For applications outside these ranges, the following components require re-calibration:

- The specific impulse as a function of charge mass and geometry (Step 2 of the design procedure);
- The quasi-static gas pressure for different containment volumes;
- The dynamic strength enhancement factors for materials if different materials are used.

With these re-calibrations, the method can be extended to other structural scales and loading conditions without modification of its core analytical framework. For critical applications, final designs should be verified by detailed numerical simulations or experimental testing.

The two charge masses were not chosen to span the full range of internal-blast threats, but to bracket the design envelope of the MLCCS prototype tested in [5] and to cover the two response regimes identified in Section 2.2: at the smaller charge the response is governed by the inner layer's own resistance, whereas at the larger charge the pressure transmitted through the interlayer becomes decisive. Wider charge ranges, stand-off effects and different aspect ratios are identified as future work in Section 4.6. Correspondingly, the numerical value of the mode-transition threshold reported here, $\sigma_{p,cr} \approx 2.5$ MPa, is specific to $R_i = 0.5$ m under 5 kg TNT and must not be transferred to other geometries or charge masses. What is expected to transfer is the dual-bound mechanism itself: an upper bound set by the elastic capacity of the outer layer, and a lower bound set by the admissible inner-layer deformation and the minimum required interlayer thickness.

The behavior of the method can nevertheless be expressed in terms that do not depend on the absolute scale of the structure. Equations (2), (4), (8), (10) and (13) reduce to a small set of dimensionless groups: the mass ratio $(2R_i + h_i)/(2R_i)$ in Equation (2); the two resistance-to-inertia ratios $\sigma_h \mu_i / [\rho_i (R_i + h_i/2) P_g R_i]$ and $\sigma_p (R_i + h_i) / (P_g R_i)$, which together determine the sign and magnitude of the deceleration parameter K in Equation (4); the geometric ratios h_i/R_i , h_o/R_i and h_f/R_i ; the densification ratio k_e/γ in Equation (10); and the outer-layer flexibility ratio $2\sigma_p R_o / (E h_o)$, which is the dimensionless form of the Mode I criterion of Equation (13). Within the assessment window of the present study, these groups take the values $(2R_i + h_i)/(2R_i) = 1.175$; $\sigma_h \mu_i / [\rho_i (R_i + h_i/2) P_g R_i] = 1.675$; $\sigma_p (R_i + h_i) / (P_g R_i) = 0.79 - 1.98$; $h_i/R_i = h_o/R_i = 0.35$ and $h_f/R_i = 0.30 - 0.40$; $k_e/\gamma = 2.0$; and $2\sigma_p R_o / (E h_o) = 3.2 \times 10^{-4} - 8.0 \times 10^{-4}$. The response predicted by the method is governed by the values of these groups rather than by the absolute geometry, so a configuration with a different R_i but comparable group values should be reproduced by the same closed-form equations without modification. This proposition is consistent with the absence of any constant fitted to $R_i = 0.5$ m anywhere in the formulation; it has not, however, been verified numerically in the present study, and it is therefore identified as a specific target for the coupled-parameter assessment recommended in Section 4.6.

4.2. Limitations and Sources of Uncertainty

Several simplifications in the proposed method introduce uncertainty:

1. Rigid–plastic idealization of the inner layer: When the inner-layer displacement is small (e.g., Cases 4 and 5 in Table 5), the elastic deformation constitutes a non-negligible fraction of the total displacement. The simplified method tends to underestimate w_{max} in these cases (error up to -14%), partly attributable to the neglect of elastic deformation. An underprediction of this kind is non-conservative: the required interlayer thickness increases linearly with w_{max} through Equation (10), so a predicted displacement smaller than the actual value leads to an interlayer thinner than required and, hence, to an under-protected design. The underpredictions remain inside the $\pm 15\%$ working tolerance of Section 3.4, and they are largest precisely where the rigid–plastic idealization is least accurate, that is, where the elastic contribution to the total displacement is not negligible. They should therefore be treated as a bias to be removed by numerical verification of the final design (as already required in Section 3.4), rather than as a margin.
2. Instantaneous impulse transfer: The assumption that the entire blast impulse is instantaneously converted into the inner-layer initial velocity neglects the finite rise time of the pressure pulse. For very short-duration impulses, this approximation is reasonable; for longer-duration impulses, it may overestimate the peak displacement.
3. Uniform foam yield stress: The foam is assumed to exhibit a perfectly plastic plateau up to the densification strain. In reality, the plateau may not be perfectly flat, and local strain concentrations can cause earlier hardening. The safety factor in Equation (10) partially accounts for this.
4. Quasi-static gas pressure: The simple formula is an approximation. For structures with significant venting or leakage, the actual gas pressure may be lower, and the inner-layer displacement would be overestimated.
5. Single-blast scenario for pristine structures: The method addresses only a single internal explosion acting on a pristine structure with no initial damage. Successive explosions, pre-damaged conditions, or structures with existing cracks or corrosion are not covered. The proposed design equations are strictly applicable only to new, undamaged MLCCs; their extension to damaged or aged structures would require additional assessment.
6. Single-parameter-variation basis: The parametric analysis in Section 2 varies each parameter independently while holding the other three at their baseline values. Because the inner-layer plasticity, foam compaction, and outer-layer elastic response are coupled through a sequential load-transfer path, this one-at-a-time approach does not capture potential interaction effects when multiple parameters deviate simultaneously. The assessment in Table 5 reflects this: Cases 2–5 differ essentially only in σ_p , while Case 1 differs from Case 2 only in φ . The reported accuracy (mean absolute error 9.50%) therefore applies to single-parameter variations; the method's predictive accuracy under coupled multi-parameter conditions remains to be verified. Within this scope, the proposed equations are intended for preliminary design and parameter screening, and final designs involving significant simultaneous deviations from the baseline should be checked by detailed FSI simulation or experimental testing.
7. Constant dynamic enhancement factors: The dynamic tensile strength enhancement factors for concrete ($\times 2.0$) and steel (500 MPa) are taken as fixed constants following Chen [42], consistent with the recommendations of UFC 3-340-02 [30] and Krauthammer [32]. These values are appropriate for the loading rates considered in the present study, but may require recalibration for significantly different charge masses or stand-off distances where the strain rate regime changes substantially.

4.3. Comparison with Existing Design Approaches

Traditional blast-resistant design for RC structures follows the SDOF approach codified in UFC 3-340-02 [30]. While the SDOF method is well-established for single-layer RC members, it cannot capture the three-layer load-redistribution mechanism of MLCCSs for two reasons:

- The SDOF method assumes that the blast impulse acts directly on the structural member, whereas in MLCCSs, the impulse is first absorbed by the inner layer, then transmitted through the foam, and, finally, applied to the outer layer.
- The SDOF method does not distinguish between Mode I and Mode II failures; it treats all plastic deformation as acceptable, which contradicts the airtightness requirement of containment structures.
- The proposed method explicitly addresses these gaps by introducing:
 - A sequential load-transfer model that accounts for the inner-layer plastic deformation and foam compaction before the outer layer responds.
 - A Mode I/II failure criterion that guarantees the outer layer remains elastic.

Compared with the fully numerical approach (FSI simulation), the proposed method reduces the computational cost from hours to seconds, making it suitable for preliminary design and parametric sweeps.

4.4. Engineering Implications

The parametric study reveals that the foam-concrete yield strength is the most influential parameter controlling the Mode I/II transition. A common misconception is that a stronger interlayer always improves blast resistance; the present results show that beyond a certain σ_p , the outer layer becomes the weak link. The proposed method provides a quantitative tool for selecting the σ_p that maximizes inner-layer energy absorption without jeopardizing the outer-layer elasticity.

The existence of a minimum required interlayer thickness (Equation (10)) implies that simply increasing the foam thickness is an inefficient strategy for improving blast resistance once that minimum is met. Design effort should instead focus on tuning σ_p and the inner-layer reinforcement ratio.

4.5. Strategies for Enhancing Overall Blast Resistance

The blast resistance of MLCCSs is ultimately limited by the elastic capacity of the outer RC layer. Once the outer layer yields (Mode II), the structure loses its airtightness irrespective of the inner-layer and interlayer design. Therefore, enhancing the overall blast resistance requires increasing the elastic limit of the outer layer. Several strategies are available:

- (a) Increasing outer-layer thickness h_o : This directly reduces the peak hoop strain $\varepsilon_{\theta,peak}$ (Equation (13)) by increasing the shell stiffness. The trade-off is increased structural weight and cost.
- (b) Using high-strength concrete or fiber-reinforced concrete for the outer layer: A higher tensile strength raises the allowable strain $\varepsilon_{allowable}$, allowing the outer layer to withstand a larger transmitted impulse before yielding. Fiber reinforcement also improves crack control. The drawback is higher material cost and potentially reduced ductility.
- (c) Applying external prestressing to the outer layer: Circumferential prestressing introduces compressive hoop stresses that offset the tensile stresses induced by the blast, effectively raising the net tensile capacity. This approach can significantly increase the elastic limit without increasing thickness. However, it adds construction complexity and long-term maintenance requirements.
- (d) Optimizing the interlayer properties: Since the transmitted pressure to the outer layer is capped at σ_p (Section 3.2.1), reducing σ_p is the most direct way to protect the

outer layer. However, as shown in Section 2.2, a lower σ_p leads to larger inner-layer deformation, requiring a thicker interlayer to avoid foam densification. The chosen σ_p balances these two competing effects.

Among these strategies, options (a) and (d) are the most practical for conventional construction, while (b) and (c) offer pathways for higher-performance designs. The proposed simplified method can be readily adapted to evaluate the effect of these strategies by adjusting the input parameters (h_o , $\epsilon_{allowable}$, σ_p) in the design procedure (Section 3.3). A quantitative comparison of these strategies using the proposed simplified design method, with properly characterized material parameters and prestress levels, is beyond the scope of the present study and is planned as part of our ongoing research on MLCCS blast mitigation.

4.6. Future Work

The following extensions are recommended for future research:

- Generalization to other geometries: The method should be calibrated for different aspect ratios (L/R_i) and absolute sizes to broaden its applicability, with the assessment extended from the present single-parameter variations to coupled variations in geometry and charge mass, so that the dimensionless groups identified in Section 4.1 can be tested directly.
- Multi-parameter coupling assessment: Systematic combined variations of σ_p , h_f , φ , and h_i should be simulated (and, ideally, tested) to quantify interaction effects and to verify the simplified method under coupled conditions. This constitutes a direct follow-up to the present study.
- Other interlayer materials: The same framework can be adapted for aluminum foam, polymeric foam, or granular materials by replacing the foam constitutive law.
- Multi-hit scenarios: Extending the method to account for cumulative damage from successive explosions would enhance its relevance for military and industrial applications.
- Probabilistic treatment: Incorporating uncertainties in material properties and blast loading into a reliability-based design format.

5. Conclusions

This paper has presented a systematic parametric analysis and a simplified design method for multi-layer composite confinement structures (MLCCSs) with a foamed-concrete interlayer under internal blast. The main conclusions are drawn as follows.

1. Parametric analysis quantified the influence of four key parameters on the structural response. Among them, the foam-concrete yield strength σ_p is the most critical parameter controlling the Mode I/II transition: increasing σ_p reduces inner-layer displacement but increases outer-layer displacement and transmitted energy. Beyond a threshold value, the structure transitions from the desired Mode I (inner-layer collapse before outer-layer yielding) to the undesirable Mode II (premature outer-layer yielding). This threshold is jointly governed by the elastic capacity of the outer RC layer and the charge mass: a larger charge mass lowers the critical σ_p , while a stronger outer layer (higher h_o or f_i) raises it. The interlayer thickness h_f has negligible influence on displacement peaks but determines the timing of the outer-layer response and the margin against foam densification. The inner-layer reinforcement ratio φ and thickness h_i serve as secondary adjustment variables that effectively reduce both inner- and outer-layer displacements.
2. A novel, closed-form simplified design method was derived, which, for the first time, translates the complex three-layer load-redistribution mechanism into a practical engineering design tool. The method yields closed-form expressions for the inner-layer maximum displacement w_{max} , the minimum required interlayer

thickness $h_{f,min}$, and the outer-layer peak hoop strain $\varepsilon_{\theta,peak}$. A step-by-step design procedure incorporating the Mode I/II criterion was proposed. The method is based on the following core assumptions: (i) rigid–perfectly plastic inner layer under plane-strain conditions; (ii) rigid–perfectly plastic foam interlayer with load-controllability behavior; (iii) elastic outer-layer response; and (iv) instantaneous impulse transfer to the inner layer. Applications involving significantly different geometries, charge masses, or material types should recalibrate the relevant input parameters as discussed in Section 4.1.

3. Assessment against numerical simulations for five representative cases covering $\sigma_p = 1.0\sim 2.5$ MPa under 5 kg TNT showed that the simplified method predicts the inner-layer maximum displacement with a mean absolute error of 9.50%, the $\pm 15\%$ band being an internally adopted working tolerance for preliminary screening rather than an acceptance criterion. Because the Mode I/II threshold is calibrated from the same numerical dataset (Section 3.2.7), the agreement in mode classification is an internal self-consistency check and not an independent quantitative validation of the strain prediction. The method tends to overpredict the displacement for large-deformation cases (conservative for design) and underpredict for small-deformation cases (non-conservative but within acceptable bounds).
4. Application of the procedure to a prospective design brief whose configuration is not included in the assessment set (Section 3.5) selected a foam-concrete grade of $\sigma_p = 1.0$ MPa together with an interlayer thickness of 175 mm as the combination that minimizes the foam grade subject to inner-layer utilization, the densification constraint and outer-layer elasticity.
5. Engineering implications: The study corrects the misconception that a stronger interlayer always improves blast resistance; beyond a critical σ_p (upper bound), the outer layer becomes the weak link. The existence of a minimum required interlayer thickness implies that simply increasing the foam thickness is an inefficient design strategy once that minimum is met. The proposed method provides a rapid screening tool for preliminary design, reducing computational cost from hours (full FSI simulation) to seconds.

It should be noted that the simplified design method proposed herein has been assessed against numerical simulations from the same FSI modeling framework, and has not yet been independently verified; the quantitative claim it supports is limited to the inner-layer displacement, while the remaining outputs are checked for consistency only. As part of the authors' ongoing series of studies on MLCCSs, independent experimental validation constitutes the next step: the proposed method will be used to design a new MLCCS configuration, which will then be verified through independent full-scale or reduced-scale internal-blast tests. The results of this verification will be reported in a future publication. Furthermore, because the parametric study and the assessment in Table 5 are based on a single-parameter-variation approach, the accuracy of the design equations under simultaneous multi-parameter variations has not yet been established; their use should therefore be restricted to preliminary design and parameter screening, with final coupled-parameter designs verified by detailed simulation or experimentation.

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Appendix A. Representative Validation of the Numerical Framework

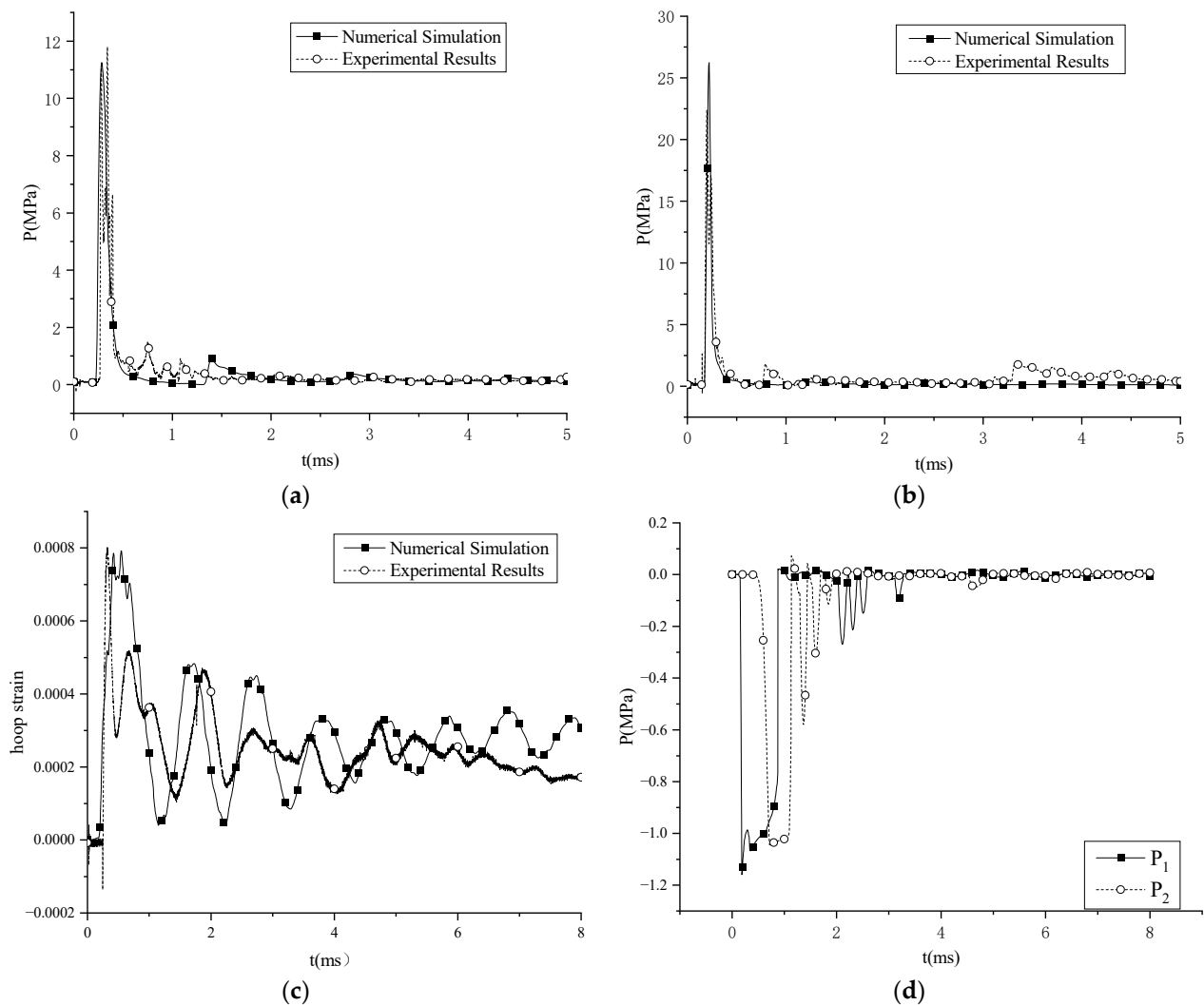


Figure A1. Representative validation of the FSI model used in the present study, reproduced from [36] with attribution. (a) Measured and computed inner-wall reflected pressure time histories at the blast-center cross-section for 0.3 kg TNT charges; (b) measured and computed inner-wall reflected pressure time histories at the blast-center cross-section for 0.8 kg TNT charges; (c) measured and computed hoop-strain time history at measurement point H1 (Model 1, foamed-concrete interlayer, 0.8 kg TNT); (d) computed contact-pressure time histories at the inner and outer interfaces of the foamed-concrete interlayer (Model 1, 3 kg TNT), illustrating the capping of the transmitted pressure at the foam yield stress; the experimental comparison of the interlayer contact pressures (0.8 kg TNT) is given in Table A2.

Figure A1 and Tables A1 and A2 collect the representative validation evidence for the numerical framework, reproduced with attribution from [36]. These are the data from which the error metrics quoted in Section 2.1 are obtained: panels (a) and (b) document the blast-loading input (peak overpressures within 16.4%, average absolute error about 9.3%

over five charge masses), panel (c) and Table A1 document the structural strain response (within 11% at the four instrumented points), and panel (d) and Table A2 document the interlayer stress transfer that underlies the load-controllability idealization of Section 3.2.1. The mesh-convergence study and the complete validation matrix, including the damage evolution of the tested models, remain in [36].

Table A1. Measured and computed peak hoop strains at the four instrumented points (Model 1, 0.8 kg TNT) [36].

Measurement Point	Calc. (10^{-6})	Exp. (10^{-6})	Error (%)
H-1	789.33	792.13	−0.4
H-2	581.43	602.89	−3.6
H-3	39.02	41.25	−5.4
H-4	34.59	37.84	−8.6

Note: H-1 to H-4 denote the inner and outer reinforcement of the inner RC layer and the inner and outer reinforcement of the outer RC layer, respectively.

Table A2. Measured and computed peak contact pressures at the foamed-concrete interlayer interfaces (Model 1, blast-center cross-section, 0.8 kg TNT) [36].

Interface	Calc. (MPa)	Exp. (MPa)	Error (%)
Inner (foam/inner RC layer)	0.659	0.744	−11.4
Outer (foam/outer RC layer)	0.302	0.400	−24.5

Note: the full table, including the sand-interlayer counterpart (Model 2), is given in [36].

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