

Article

Dynamic Response and Failure Mechanisms of Multi-Layer Composite Confined Structures Under Internal Blast Loading

Pengfei Ning ^{1,*} and Degao Tang ²¹ School of Engineering Science, Shandong Xiehe University, Jinan 250109, China² State Key Laboratory of Disaster Prevention & Mitigation of Explosion & Impact, Army Engineering University of PLA, Nanjing 210007, China

* Correspondence: ningpengfei@sdxiehe.edu.cn

Abstract

The containment of internal explosions remains a critical challenge in the design of protective infrastructure for ammunition storage and hazardous industrial facilities. To address the limitations of conventional single-layer reinforced concrete (RC) structures, this study proposes a multi-layer composite confined structure comprising RC layers and energy-absorbing interlayers. A fluid–structure interaction (FSI) numerical model was developed and validated against experimental data from scaled blast tests on two multi-layer composite confined structures reported in our companion study. Comparative analyses were performed between the composite structures and an equivalent single-layer RC structure under varying charge masses. The results indicate that the energy-absorbing layer transforms the monolithic structural response into a progressive, layer-by-layer deformation mode, effectively decoupling the outer layer from the direct blast impulse. Foamed concrete demonstrated superior performance by limiting the transmitted stress to its yield plateau (~1.0 MPa) and reducing the outer-layer energy to 8.77% of that in the single-layer structure. Two distinct failure modes were identified for the first time: the desired Mode I, where the inner layer reaches its deformation limit while the outer layer remains elastic, and the undesired Mode II, characterized by premature plastic failure of the outer layer. These findings provide a rational basis for the design of blast-resistant composite structures, highlighting the efficacy of foamed concrete in ensuring containment integrity.

Keywords: multi-layer composite confined structure; energy-absorbing layer; internal blast; fluid–structure interaction; progressive deformation; failure mode

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1. Introduction

Explosives release enormous energy during detonation, yet in many scenarios, it is necessary to confine this energy within a specific region. Blast confinement technology is widely applied in protective engineering, including blast test facilities, combustion test chambers, storage and transportation of explosives and pyrotechnics, explosive disposal devices, pressure vessels and pipeline engineering, nuclear reactor engineering, and explosive processing equipment [1–6]. Such confinement is typically achieved through anti-internal-explosion structures, and the growing demand for blast containment has imposed higher requirements on these structures.

Reinforced concrete (RC) structures are a widely used type of anti-internal-explosion structures. Numerous RC anti-internal-explosion structures have been designed and constructed worldwide, accompanied by extensive research. Edri et al. [7] conducted internal blast tests on rectangular RC rooms with circular vent openings and analyzed the influence of charge equivalent on overpressure distribution at different positions within the room. Cheng et al. [8] combined numerical simulation and experiments to analyze the internal blast overpressure distribution on the inner wall of a box-shaped RC confined structure. Guo et al. [9] performed field tests and finite element analyses on RC shear wall structures under internal TNT explosions, showing that blast loads increase as the vent area decreases. Liu et al. [10] designed and constructed a full-scale single-room RC building, and used combined experimental and numerical methods to study its blast resistance under internal loading, analyzing structural damage evolution and shock wave propagation patterns, and proposing dimensionless weighted parameters to classify failure modes.

Recent studies have further refined the understanding of internal blast behavior in RC structures. Li et al. [11] revisited the characteristics of internal blast loads and dynamic responses in RC box structures, emphasizing the amplification effect of confinement on impulse transmission. Qi et al. [12] conducted numerical simulations of reinforced concrete structures subjected to internal blast loading using an LS-DYNA-based fluid–structure interaction algorithm and incorporated strain-rate effects in the concrete and reinforcement constitutive models, demonstrating that the FSI approach can accurately capture the dynamic response and failure patterns of RC components under confined explosions. Zhou et al. [13] extended the investigation to RC frame-masonry wall structures, quantifying progressive damage mechanisms under internal detonations. Do et al. [14] assessed the vulnerability of physical barriers in nuclear facilities through internal explosion demonstration tests, highlighting the critical role of RC walls and doors in mitigating localized blast effects.

Traditional single-layer RC anti-blast structures face challenges such as low resistance and susceptibility to cracking. Stress waves undergo reflection and transmission at interfaces between media with different acoustic impedances, altering peak stress values and causing waveform dispersion [15]. Leveraging this characteristic, multi-layer structures incorporating energy-absorbing materials are often adopted in protective engineering to enhance blast resistance. Gu et al. [16] numerically and experimentally analyzed the blast resistance of multi-layered aluminum foam sandwich panels, demonstrating that arranging aluminum foams in descending density order improves overall structural performance. Santosa et al. [17] investigated the blast mitigation mechanism of aluminum foam through experiments and simulations, identifying foam thickness as a key influencing factor. Cheng et al. [18] experimentally studied the effect of aluminum foam liners on the blast resistance of steel cylinders, identifying three deformation modes under large charges. Yun et al. [19] analyzed the energy absorption and buffering effect of aluminum foam in steel-concrete composite walls, showing that it significantly reduces blast pressure. Fang et al. [20,21] experimentally and numerically studied the influence of foamed concrete on composite protective structures, proposing a “load-controllable main structure” design concept—by adjusting the strength grade and thickness of foamed concrete, the blast load transmitted to the main structure can be limited to approximately its yield strength. Aminou et al. [22] further evaluated calcium-silicate-based mineral foam as a crushable core material, confirming its capacity for substantial energy absorption while limiting back-face acceleration.

Existing studies mostly focus separately on RC structures under internal blast or foam-like energy-absorbing materials, with limited research on the integrated dynamic response of multi-layer composite confined structures under internal blast loading. While

experimental tests are indispensable for capturing real failure phenomena, they are often limited by high costs and the difficulty of measuring internal contact pressures and energy distribution. Numerical simulations allow for the parametric analysis of mechanisms that are impossible to observe physically, such as stress wave propagation through the energy-absorbing layer and energy transfer between layers. Therefore, integrating experimental validation with high-fidelity numerical analysis is essential to elucidate the true protective mechanism of these composites.

This study aims to bridge this gap by investigating the dynamic response of multi-layer composite confined structures with two distinct energy-absorbing materials: foamed concrete and dense sand. Scaled internal blast tests were conducted to acquire authentic failure data, and a sophisticated fluid–structure interaction (FSI) numerical model was developed to replicate and extend beyond the experimental limits. The principal conclusions expected from this work are: (1) The energy-absorbing layer transforms the structural response from monolithic to progressive deformation; (2) Foamed concrete significantly outperforms dense sand due to its unique yield plateau mechanism; (3) Two distinct failure modes govern the structural integrity, providing a clear design criterion for high-risk containment facilities.

It is important to clarify the relationship between this work and our companion experimental study [23]. The experimental data presented in Section 2.1 were originally reported in [23]. The present manuscript extends beyond that work in the following original aspects: (1) development and rigorous validation of a high-fidelity fluid–structure interaction (FSI) numerical model that reproduces the experimental results with quantified accuracy; (2) systematic comparative analysis between the multi-layer composite confined structures and an equivalent single-layer RC structure under varying charge masses, which reveals the fundamental transformation from monolithic to progressive deformation; (3) quantitative characterization of energy redistribution mechanisms across the structural layers, demonstrating that the energy transmitted to the outer layer is reduced to less than 10% of that in the single-layer counterpart; and (4) first-time identification and classification of two distinct failure modes (Mode I and Mode II) governing the structural integrity of multi-layer composite confined structures, providing a clear design criterion for high-risk containment facilities.

2. Experiment and Numerical Analysis Models

2.1. Experimental Models

The design, fabrication, and internal blast testing of the multi-layer composite confined structures were detailed in our previous work [23]. For the sake of completeness, the key geometric dimensions and material parameters are briefly recapitulated here. Two experimental models with identical geometric dimensions were fabricated using C40 concrete as shown in Figure 1, adopting a three-layer structural configuration of reinforced concrete layer–energy-absorbing interlayer–reinforced concrete layer. Both the inner and outer reinforced concrete layers featured double reinforcement cages, with $\Phi 14$ bars (Maanshan Iron & Steel Company Limited, Ma'anshan, Anhui, China) spaced at 200 mm in both hoop and axial directions.

Two energy-absorbing materials commonly used in protective engineering were selected: foamed concrete for Model 1 and dense sand for Model 2, filling the interlayers respectively. The material parameters of the foamed concrete and sand are listed in Tables 1 and 2. The inner radius of the experimental models was 0.5 m, the outer radius was 1 m, and the internal net length was 5 m. The cross-sectional dimensions and sensor layout are shown in Figure 2. The ends of the models were sealed with 0.3 m thick reinforced concrete end plates, connected to the main structure via pre-embedded bolts.



Figure 1. Schematic diagram of the test model.

Table 1. Material parameters of the foamed concrete energy-absorbing layer in Model 1.

Density (kg/m ³)	Elastic Modulus E (MPa)	Poisson's Ratio	Yield Stress
610	270	0.10	1.0

Table 2. Material parameters of the sand energy-absorbing layer in Model 2.

Density (kg/m ³)	Elastic Modulus E (MPa)	Friction Angle φ (°)	Cohesion C (kN)
1560	27.38	21.6	20

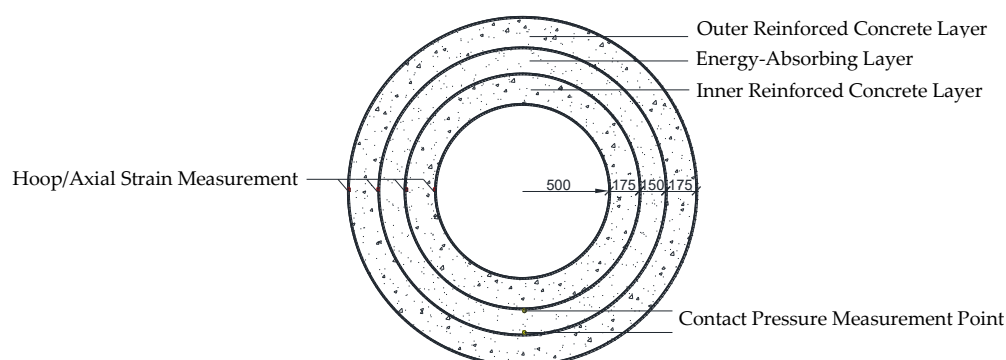


Figure 2. Cross-sectional dimensions (mm) and sensor layout.

Strain-gauge-type pressure sensors were pre-embedded at the blast-center cross-section to measure the contact pressure between each structural layer. Bases were reserved for installing piezoelectric pressure sensors (Jiangsu Lianneng CY-YD205; Jiangsu Lianneng Electronic Technology Co., Ltd., Yangzhou, China.) to measure the internal blast shock wave loads on the inner surface of the structure. Strain gauges were bonded to the reinforcing bars to measure the strain at various points of the experimental models. At the mid-span cross-section of each model, one hoop strain measurement point and one axial strain measurement point were set on both the inner and outer hoop reinforcements of the inner and outer reinforced concrete layers, respectively.

2.2. Numerical Calculation Model

2.2.1. Geometric Model

The numerical calculation model was established with identical dimensions to the experimental models, using a Cartesian coordinate system. The origin of the coordinate system was located at the geometric center of the structure, with the Z-axis coinciding with the structural axis. The thicknesses of the inner reinforced concrete layer, energy-absorbing layer, and outer reinforced concrete layer were 0.175 m, 0.15 m, and 0.175 m, respectively. An automatic surface-to-surface contact algorithm was applied between the energy-absorbing layer and each adjacent structural component, including the inner RC

layer, the outer RC layer, and the end plates, with a static friction coefficient of $\mu_s = 0.6$, a dynamic friction coefficient of $\mu_d = 0.5$, and a viscous damping coefficient (VDC) of 20. In the experimental setup, the end plates were connected to the inner and outer RC layers via pre-embedded bolts. During the tests, no obvious deformation or failure of the bolted connections was recorded, indicating that the end plates remained securely attached to the main structure throughout the loading process. Accordingly, in the numerical model, these bolts were not explicitly modeled; instead, the end plates were connected to the inner and outer RC layers through shared nodes. This simplification is justified because the present study focuses primarily on the blast-resistant performance of the multi-layer composite confined structure (inner RC layer–energy-absorbing layer–outer RC layer) at the blast-center cross-section, where the influence of the bolted end-plate connections is limited. Contact algorithms were used between the end plates and the energy-absorbing layer, as well as between the energy-absorbing layer and the RC layers, to allow for potential separation and sliding at these interfaces. Utilizing the symmetry of the structure, a 1/8 symmetric model was established, as shown in Figure 3. Symmetry boundary conditions were applied on the three orthogonal planes passing through the geometric center: $u_x = 0$ on plane $X = 0$, $u_y = 0$ on plane $Y = 0$, and $u_z = 0$ on plane $Z = 0$.

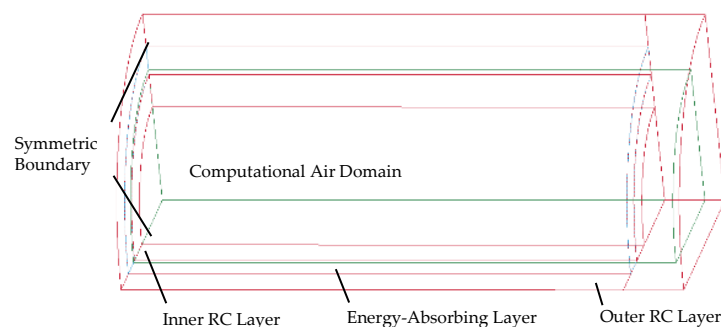


Figure 3. Numerical calculation model of multi-layer explosive confinement structure. The origin of the Cartesian coordinate system is located at the geometric center of the structure, with the Z-axis coinciding with the structural axis. Symmetry planes: $X = 0$, $Y = 0$, $Z = 0$. Symmetric boundary conditions ($u_x = 0$, $u_y = 0$, $u_z = 0$) were applied on the respective symmetry planes.

The loading of blast loads is a critical step in the numerical calculation of structural dynamic responses under blast loading. The simplicity and accuracy of applying blast loads to structural surfaces directly affect the calculation accuracy and time. This paper adopts the fluid–structure coupling algorithm to account for the interaction between the structure and the internal blast flow field. An explosion flow field model (the air computation domain shown in Figure 3) enclosing the inner layer of the structure was established. The explosion flow field used an Eulerian mesh; the explosive charge was centered at the coordinate origin, and its position and shape were defined using the initial volume fraction method (INITIAL_VOLUME_FRACTION keyword). This method assigns explosive material properties directly to specified element regions, avoiding explicit modeling of the explosive entity, and employs the JWL equation of state to accurately describe the expansion work of detonation products, thereby generating high-precision blast shock wave loads within the air computation domain. The structure used a Lagrangian mesh. To precisely capture the complex interaction between blast shock waves, products, and the structure, a penalty-function-based fluid–structure coupling algorithm was adopted. In LS-DYNA (R11.2), this was implemented via the CONSTRAINED_LAGRANGE_IN_SOLID keyword. At each time step, the algorithm automatically maps the pressure (load) generated by the explosion in the Eulerian domain onto the nodes of the

Lagrangian structural mesh, driving structural deformation; simultaneously, it feeds back the displaced nodes and velocities of the deformed structure into the Eulerian domain to update the flow field boundary, thus autonomously simulating the reflection, diffraction, and pressure evolution of shock waves on the structural surface. Similar LS-DYNA FSI modeling strategies have been successfully applied to the numerical analysis of RC structures under internal blast loading [12], supporting the validity of the present numerical framework.

Studies have shown that mesh size significantly affects the calculation accuracy of explosion flow fields [24,25]. When using the fluid–structure coupling algorithm to calculate structural dynamic responses under blast loading, excessively fine meshing leads to a sharp increase in computation time. To reduce the impact of mesh size on fluid–structure coupling calculation accuracy, after trial calculations and comparative analysis, this paper adopted a radial mesh size of 5 mm, and a graded mesh along the structural axis—with larger mesh sizes farther from the blast-center cross-section—dividing the axis into 200 elements.

The peak overpressure and reflected impulse are the quantities particularly sensitive to mesh size in fluid–structure interaction simulations of blast loading [24,25]; therefore, these two quantities were selected as the primary indicators for the mesh sensitivity analysis. A mesh sensitivity analysis was performed to verify grid independence. Four mesh sizes (10 mm, 7.5 mm, 5 mm, and 2.5 mm) were evaluated by comparing the ratio coefficients of the reflected overpressure peak and impulse at the monitoring point, with the 2.5 mm mesh results taken as the reference. As shown in Figure 4, the results converge as the mesh is refined. When the mesh size is reduced from 10 mm to 5 mm, the ratio coefficients change notably; however, further refinement from 5 mm to 2.5 mm yields minimal variation, indicating that the solution becomes essentially grid-independent at a mesh size of 5 mm. Convergence was also confirmed for the arrival time and positive phase duration, with the 5 mm mesh producing results within 5% of the 2.5 mm reference solution. Therefore, a radial mesh size of 5 mm was adopted for all subsequent calculations to balance accuracy and computational efficiency.

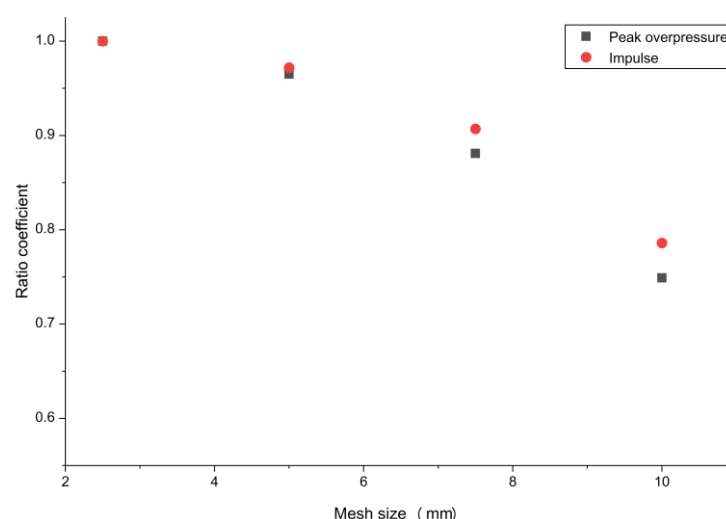


Figure 4. Mesh sensitivity analysis of reflected overpressure peak and impulse. The ratio coefficient is defined as the numerical result divided by the reference solution (2.5 mm mesh).

2.2.2. Material Models

(1) Concrete Material Model

The concrete material was modeled using the Karagozian and Case (K&C) Concrete Model, Release III (MAT_072R3). This model inherits the three-invariant failure surface

framework of the original pseudo-tensor model [26] but utilizes a parameter generation capability that derives the complex failure surface shapes (tensile and compressive meridians) primarily from the unconfined compressive strength (f'_c) of the concrete, eliminating the need for manual calibration of individual shape coefficients. For the present study, the concrete mix corresponds to the standard C40 grade (as defined in the companion experimental work [23]), with a density of 2400 kg/m³. According to the Chinese National Standard GB 50010-2010 [27], the nominal cube compressive strength ($f_{cu,k}$) of 40 MPa corresponds to a cylinder compressive strength (f'_c) of 32 MPa (applying the standard conversion factor of 0.8) and an elastic modulus (E_c) of 32.5 GPa, calculated via the empirical formula $E_c = 10^5 / (2.2 + 34.7/f_{cu,k})$. A Poisson's ratio of 0.20 was adopted. The strain rate effect followed the dynamic increase factor (DIF) model of Malvar and Ross [28], which defines the rate-dependent scaling of concrete strength based on logarithmic strain rate dependence. The resulting DIF relationships were adopted to scale the strength envelope of the MAT_072R3 model. The concrete damage function recommended in the LS-DYNA keyword manual [29] was activated to output effective plastic strain, with the ultimate crush strain set to 0.0033 in accordance with code-specified limits.

(2) Foamed Concrete Material Model

Foamed concrete uses the crushable foam material model (MAT_CRUSHABLE_FOAM). The material parameters of the foamed concrete used are shown in Table 1. The relationship between yield stress and volumetric strain is shown in Table 3.

Table 3. Relationship between volumetric strain and yield stress of foamed concrete.

Volumetric Strain (v/v_0)	0.02	0.30	0.45	0.52
Yield Stress (MPa)	1.0	1.0	1.5	2.0

(3) Sand Material Model

In blast impact problems involving large deformations and material failure, simple constitutive models cannot meet engineering accuracy requirements, while complex constitutive models entail excessive computation and difficult-to-determine parameters. The MAT_SOIL_AND_FOAM material model provided in LS-DYNA is a simplified model proposed for soil and foam materials, suitable for simulating large deformation behavior and directly capturing failure behavior under large deformation. The dense sand in this paper used this material model, with parameters listed in Table 2. The relationship between pressure and volumetric strain is shown in Table 4.

Table 4. Relationship between pressure and volumetric strain of dense sand.

Vol. Strain	Pressure (MPa)	Vol. Strain	Pressure (MPa)
0	0	0.19	20.8
0.005	0.42	0.21	27.1
0.04	3.53	0.22	39.2
0.11	6.76	0.25	56.6
0.15	12.74	0.30	123

(4) Reinforcement Steel Material Model

The reinforcement steel material model adopted the MAT_PLASTIC_KINEMATIC model, which is a strain-rate-dependent, isotropic/kinematic hardening mixed model considering failure [30]. The effect of strain rate on strength was accounted for using the Cowper-Symonds model. The relationship between yield stress and strain rate is as follows:

$$\sigma_y = \left[1 + \left(\frac{\dot{\epsilon}}{C} \right)^{\frac{1}{p}} \right] (\sigma_0 + \beta E_p \varepsilon_p^{eff}) \quad (1)$$

where σ_0 is the initial yield stress, $\dot{\varepsilon}$ is the strain rate, C , P and are Cowper-Symonds strain rate parameters, ε_p^{eff} is the effective plastic strain, and E_p is the plastic hardening modulus.

The material parameters for the reinforcement steel in this paper are: density $\rho = 7800 \text{ kg/m}^3$, elastic modulus $E = 212 \text{ GPa}$, yield strength $\sigma_y = 300 \text{ MPa}$, adopted from the manufacturer's specifications and relevant standards. The strain rate parameters $C = 40.4/\text{s}$, $p = 0.2$ were adopted from the literature [30,31].

(5) Explosive and Air Material Models and Parameters

The explosive adopted the high-explosive material model provided by LS-DYNA, and the JWL equation of state was used to describe the relationship between detonation product pressure, internal energy, and relative volume. The JWL equation of state is expressed as [32]:

$$P = A \left(1 - \frac{\omega}{R_1 V}\right) e^{-R_1 V} + B \left(1 - \frac{\omega}{R_2 V}\right) e^{-R_2 V} + \frac{\omega E}{V} \quad (2)$$

where A , B , R_1 , R_2 and ω are material constants, V is the relative volume, and E is the initial internal energy density. The parameters for the TNT explosive JWL equation of state in this numerical calculation are: density $\rho = 1630 \text{ kg/m}^3$, detonation velocity $D = 6930 \text{ m/s}$, $A = 3.712 \times 10^5 \text{ MPa}$, $B = 3.231 \times 10^3 \text{ MPa}$, $R_1 = 4.15$, $R_2 = 0.95$, $\omega = 0.30$, $E = 7.0 \times 10^9 \text{ J/m}^3$. These parameters were adopted with reference to the values reported in [10].

Air was modeled using the linear polynomial equation of state, where pressure is calculated as:

$$P = C_0 + C_1 \mu + C_2 \mu^2 + C_3 \mu^3 + (C_4 + C_5 \mu + C_6 \mu^2) E \quad (3)$$

where $\mu = \rho/\rho_0 - 1$ and ρ/ρ_0 is the ratio of current density to initial density. When air is treated as an ideal gas, $C_0 = C_1 = C_2 = C_3 = C_6 = 0$, and $C_4 = C_5 = \gamma - 1$. γ is the specific heat ratio of the gas, generally taken as 1.4. The air equation-of-state parameters were adopted with reference to the values reported in [10].

3. Validation of the Numerical Calculation Model

Five internal blast tests were conducted on each of the two experimental models. The test charges were 0.15 kg, 0.20 kg, 0.30 kg, 0.475 kg, and 0.80 kg of TNT, placed at the geometric center of the structure and initiated by electric detonators. The detailed experimental data and procedures are documented in our published work [23]. Numerical calculations were performed using the same charge masses as the tests, and the results were compared with the experimental data. By default, the numerical simulation outputs positive overpressure. Since the experimental data use the opposite sign convention, they have been adjusted accordingly for comparison. Figures 5 and 6 present the comparison between the calculated and experimental time-history curves of blast shock wave loads on the inner surface at the blast-center cross-section for charge masses of 0.3 kg and 0.8 kg, respectively. The comparison shows reasonable agreement between the numerical and experimental results in terms of shock wave arrival time, pressure rise rate, overpressure peak, and positive phase duration.

Table 5 lists the peak overpressure values of the blast shock wave loads at the blast-center cross-section obtained from numerical calculations under all five charge masses (0.15 kg, 0.20 kg, 0.30 kg, 0.475 kg, and 0.80 kg). Across all five charge masses, the numerical results show consistent agreement with the experimental data, with the maximum relative error being 16.4% (Model 1, 0.8 kg) and the average absolute error being approximately 9.3%. The representative time-history comparisons for the 0.3 kg (moderate loading) and 0.8 kg (maximum loading) cases are presented in Figures 5 and 6 to illustrate the

overall quality of the numerical predictions in terms of arrival time, pressure rise rate, and positive phase duration.

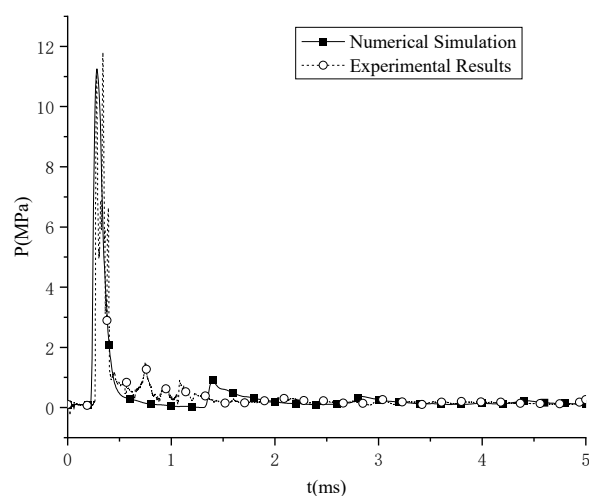


Figure 5. Comparison of calculated and tested explosion shock wave load time-history curves at the blast center ring face for 0.3 kg charge.

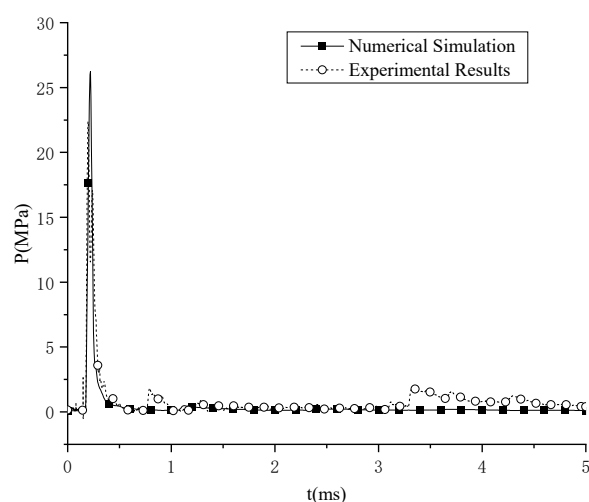


Figure 6. Comparison of calculated and tested explosion shock wave load time-history curves at the blast center ring face for 0.8 kg charge.

Table 5. Comparative analysis of numerical and experimental peak overpressure values (MPa).

Charge Mass (kg)	Numerical (Calc.)	Model 1 (Exp.)	Error (%)	Model 2 (Exp.)	Error (%)
0.15	5.242	5.904	−11.2%	5.345	−1.9%
0.20	7.585	7.071	+7.3%	7.086	+7.0%
0.30	11.246	11.779	−4.5%	10.161	+10.7%
0.475	18.617	16.221	+14.8%	17.136	+8.6%
0.80	26.275	22.575	+16.4%	24.881	+5.6%

Figure 7 illustrate the comparison between the numerical and experimental time-history curves of hoop strain at measurement point H1 under a 0.8 kg charge mass for Models 1 and 2, respectively. The numerical results for both models align well with the experimental data. Table 6 presents the peak hoop strain values at each measurement point for Models 1 and 2 under a 0.8 kg charge mass. The peak strain values from the numerical

calculations are generally consistent with the experimental results, with the maximum deviation not exceeding 11%, as quantified in Table 6.

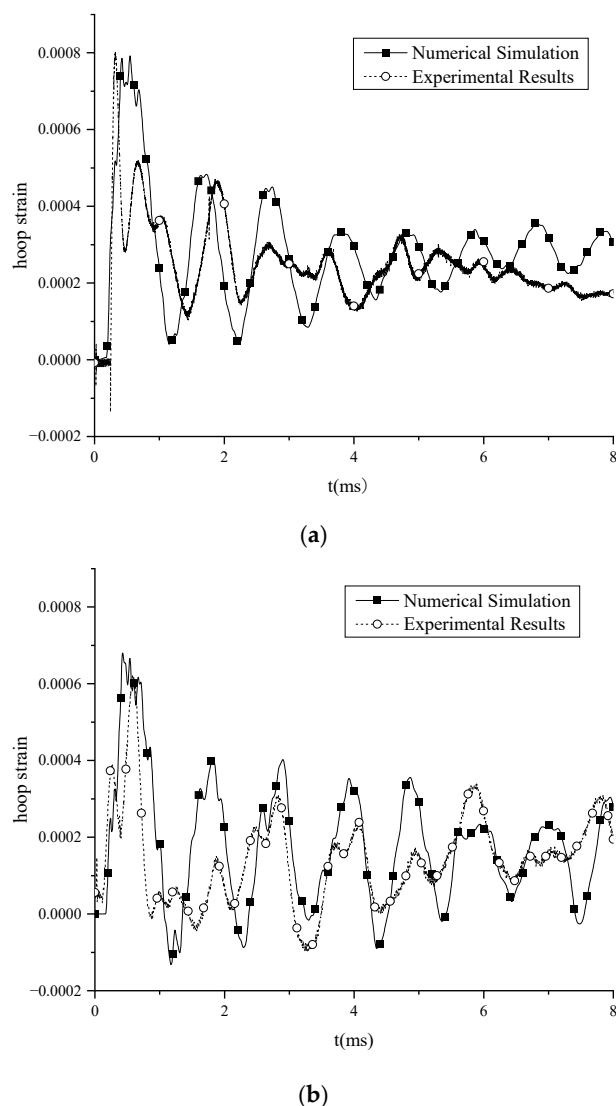


Figure 7. Comparison of numerical calculation and test results of circumferential strain time-history curves at measuring point H1. (a) Model 1. (b) Model 2.

Table 6. Peak hoop strain values ($\times 10^{-6}$) of the structure under a 0.8 kg charge mass.

Measurement Point	Model 1 (Calc.)	Model 1 (Exp.)	Error (%)	Model 2 (Calc.)	Model 2 (Exp.)	Error (%)
H-1	789.33	792.13	−0.4%	674.23	609.32	+10.7%
H-2	581.43	602.89	−3.6%	489.76	467.24	+4.8%
H-3	39.02	41.25	−5.4%	49.08	44.26	+10.9%
H-4	34.59	37.84	−8.6%	40.79	38.69	+5.4%

Note: Measurement points H1 to H4 represent the inner reinforcement of the inner RC layer, the outer reinforcement of the inner RC layer, the inner reinforcement of the outer RC layer, and the outer reinforcement of the outer RC layer, respectively.

The comparison between the numerical results and experimental data demonstrates that the numerical model provides a reasonable representation of the blast loading and structural response within the validated range. Specifically, the model reproduces the inner-surface pressure time histories (Figures 5 and 6, Table 5) and the hoop strain responses

(Figure 7, Table 6) with acceptable accuracy. To further assess the model's capability in predicting interlayer load transfer, the peak contact pressures at the interfaces between the energy-absorbing layer and the inner/outer structural layers under a 0.8 kg TNT charge are compared in Table 7.

Table 7. Comparison of numerical and experimental peak contact pressures at the blast-center cross-section under a 0.8 kg TNT charge.

Model	Interface	Calc. (MPa)	Exp. (MPa)	Error (%)
Model 1 (Foamed concrete)	P_1 (inner interface)	0.659	0.744	−11.4
	P_2 (outer interface)	0.302	0.400	−24.5
Model 2 (Dense sand)	P_1 (inner interface)	1.331	1.276	4.3
	P_2 (outer interface)	0.572	0.522	9.6

Note: P_1 denotes the contact pressure at the interface between the energy-absorbing layer and the inner structural layer; P_2 denotes the contact pressure at the interface between the energy-absorbing layer and the outer structural layer.

The results show reasonable agreement, with errors ranging from −24.5% to 9.6%, providing additional confidence in the model's ability to predict interlayer load transfer. It should be noted that the validation is performed against the available experimental measurements, namely the inner-surface pressure time histories (Figures 5 and 6, Table 5), the hoop strain responses (Figure 7, Table 6), and the interlayer contact pressures (Table 7). Quantities such as energy distribution across layers and the failure mode transition at higher charge masses are derived from the validated numerical model and provide valuable insight into the load-transfer and failure mechanisms that are difficult to obtain experimentally.

4. Results

To comparatively analyze the stress and deformation characteristics of the multi-layer composite confined structure under internal blast loading, a numerical calculation model of a single-layer reinforced concrete structure with the same overall dimensions was established. This model has a total wall thickness of 0.5 m, with inner and outer RC layers identical to those of the composite structures and the energy-absorbing interlayer replaced by C40 grade concrete. Due to the replacement of the lighter energy-absorbing materials (foamed concrete at 610 kg/m³ or sand at 1560 kg/m³) with normal-weight concrete (2400 kg/m³), the mass of this model is greater than that of the composite structures. The end-plate configuration and boundary conditions are identical to those of the composite models. Its dynamic response under different charge masses was calculated and compared with that of the multi-layer composite confined structure. For ease of description, this single-layer structure is referred to as Model 3 in the subsequent analysis. Numerical calculations were performed for all models under internal blast loads of 0.8 kg, 1.5 kg, and 3.0 kg TNT.

The charge masses were selected based on the following rationale. The 0.8 kg charge corresponds to the maximum charge mass used in the experimental program, serving as the upper bound of the validation range and establishing confidence in the numerical model. The 1.5 kg and 3.0 kg charges represent moderate and severe loading scenarios, respectively, designed to explore the structural response beyond the validated range and to reveal the evolution of deformation and energy distribution under increasing blast intensity. The 5.0 kg charge was applied exclusively to Model 1 (foamed concrete) to investigate whether the inner layer could reach its deformation limit while the outer layer remained elastic under extreme loading. For Model 2 (sand), the 3.0 kg case had already induced significant plastic deformation in the outer layer (see the failure mode analysis in

Section 4.4), so further increasing the charge to 5.0 kg would not provide additional insight into the structural behavior.

The analysis focuses on structural stress, deformation characteristics, energy distribution, and failure modes.

It should be noted that Model 3 was designed as an equal-thickness reference structure to isolate the effect of the layering configuration. By keeping the total wall thickness identical (0.5 m) between the composite structures (Models 1 and 2) and the single-layer structure (Model 3), any observed differences in blast response can be attributed solely to the presence of the energy-absorbing interlayer and the resulting decoupling mechanism, rather than to differences in overall wall thickness.

If an equal-mass comparison were adopted instead, the single-layer structure would have a smaller total thickness, since the energy-absorbing materials (foamed concrete at 610 kg/m³ and dense sand at 1560 kg/m³) are lighter than normal-weight concrete (2400 kg/m³). A thinner single-layer structure would exhibit even poorer blast resistance, which would only amplify the apparent advantage of the composite design. Therefore, the equal-thickness comparison adopted in this study provides a conservative benchmark for evaluating the benefits of the multi-layer composite configuration. The conclusions drawn from this comparison—namely, the superior load attenuation and energy redistribution capabilities of the composite structures—are thus conservative and would only be strengthened under an equal-mass comparison.

4.1. Structural Stress Analysis

A key function of energy-absorbing materials is to attenuate the load transmitted to the protected target, reducing its peak value and prolonging the pressure rise time. For all charge masses, the peak contact pressure at the blast-center cross-section was the highest among all measuring points along the axis. Therefore, elements at the blast-center cross-section on the inner and outer surfaces of the energy-absorbing layer were selected for analysis. Due to the very low Poisson's ratio and light density of the energy-absorbing materials, the contact pressure between the energy-absorbing layer and the adjacent structural layers can be approximated by the radial stress at the corresponding locations.

Table 8 presents the peak values of contact pressures P_1 (between the energy-absorbing layer and the inner structural layer) and P_2 (between the energy-absorbing layer and the outer structural layers), along with their corresponding occurrence times, for Models 1 and 2 under charge masses of 0.8 kg, 1.5 kg, and 3 kg. For the single-layer RC structure of Model 3, P_1 and P_2 are defined as the radial stresses of concrete elements located at positions corresponding to the inner and outer sides of the energy-absorbing layer, respectively.

Table 8. Peak pressures (MPa) at the interfaces between the energy-absorbing layer and the inner/outer structural layers and their occurrence times (ms).

Charge Weight	Model	P_1 (Peak, MPa)	P_1 (Time, ms)	P_2 (Peak, MPa)	P_2 (Time, ms)
0.8 kg	1	0.659	0.238	0.302	0.966
	2	1.331	0.238	0.572	0.782
	3	15.33	0.224	8.409	0.284
1.5 kg	1	1.038	0.209	0.718	0.936
	2	2.256	0.239	1.456	0.824
	3	24.39	0.214	13.05	0.269
3 kg	1	1.167	0.192	1.042	0.702
	2	3.397	0.376	2.663	0.811
	3	46.67	0.208	36.54	0.223

Note: For Model 3 (single-layer RC structure), and represent the radial stresses of concrete elements at positions corresponding to the inner and outer surfaces of the energy-absorbing layer in the multi-layer models, respectively.

Under all three charge masses, the peak pressures at the interfaces between the energy-absorbing layer and the inner/outer layers of Models 1 and 2 are significantly lower than those of the single-layer Model 3. Furthermore, the time at which the peak pressure at the outer interface occurs is substantially delayed compared to Model 3. For all charge masses, the peak pressure transmitted to the outer layer in Model 1 (foamed concrete) is consistently lower than that in Model 2 (dense sand), indicating that foamed concrete provides better attenuation of the load transmitted to the outer structure.

The load attenuation mechanism of the energy-absorbing layer operates through two aspects: First, reflection and transmission occur at the interface between the energy-absorbing layer and the inner structural layer, significantly reducing the intensity of the stress wave entering the energy-absorbing layer (i.e., the contact pressure P_1). Second, the stress wave attenuates as it propagates through the energy-absorbing layer, and its propagation path is lengthened, further reducing the peak and duration of the load acting on the outer structure (i.e., the contact pressure P_2). For dense sand, which lacks a distinct yield strength, the peak pressures at both interfaces increase with increasing charge mass. For foamed concrete, which has a clear yield point, the difference between and is significant at smaller charge masses. At 0.8 kg, P_2 in Model 1 is only 36.1% of P_1 , indicating substantial attenuation within the energy-absorbing layer itself. As the charge mass increases, the peak contact pressures on the inner and outer sides of the energy-absorbing layer become nearly equal.

It should be clarified that the “capping” behavior applies specifically to the stress transmitted to the outer structural layer, i.e., the contact pressure P_2 at the interface between the energy-absorbing layer and the outer RC layer. As shown in Table 8, P_2 for Model 1 remains ≤ 1.042 MPa across all charge masses (0.8 kg to 3.0 kg), which is indeed close to the yield stress of 1.0 MPa. The inner interface pressure P_1 can momentarily exceed the yield stress (e.g., 1.167 MPa at 3.0 kg) due to the high-velocity impact from the inner RC layer being driven outward by the blast impulse. However, this overshoot is rapidly attenuated as the stress wave propagates through the foam layer, such that by the time it reaches the outer interface, the pressure has been reduced to near the yield plateau level. Thus, the capping effect is manifested in the stress transmitted to the outer layer, not in the stress at the inner interface.

Consequently, the attenuation of the load acting on the outer structure is reflected more significantly in the reduction in its duration, as shown in Figure 8.

The variation in peak contact pressure along the structural axis was also analyzed. Figure 9 illustrates the variation in peak pressures at the interfaces between the energy-absorbing layer and the inner/outer layers with distance from the blast center under a 3.0 kg charge. The horizontal coordinate X/R represents the ratio of the distance from the blast center to the inner radius of the structure. For both Models 1 and 2, the peak pressure at the inner interface decreases with increasing distance from the blast-center cross-section, reflecting the rapid decay of inner-layer deformation away from the blast source. At all distances, the peak pressure P_2 at the outer interface in Model 2 (sand) is lower than P_1 , and both decrease with distance from the blast center, indicating that the sand layer concentrates the load transfer between the layers. In contrast, for Model 1 (foamed concrete), remains approximately constant at a level close to the yield strength of the foamed concrete over a considerable distance from the blast center. That is, the foamed concrete tends to transform the load acting on the outer layer into a quasi-uniform distributed load with a peak value equal to its yield strength. A larger internal charge results in greater

deformation of the inner RC layer, expanding the range and duration of this uniform load, while the peak pressure changes very little—provided the foamed concrete has not entered the compaction stage.

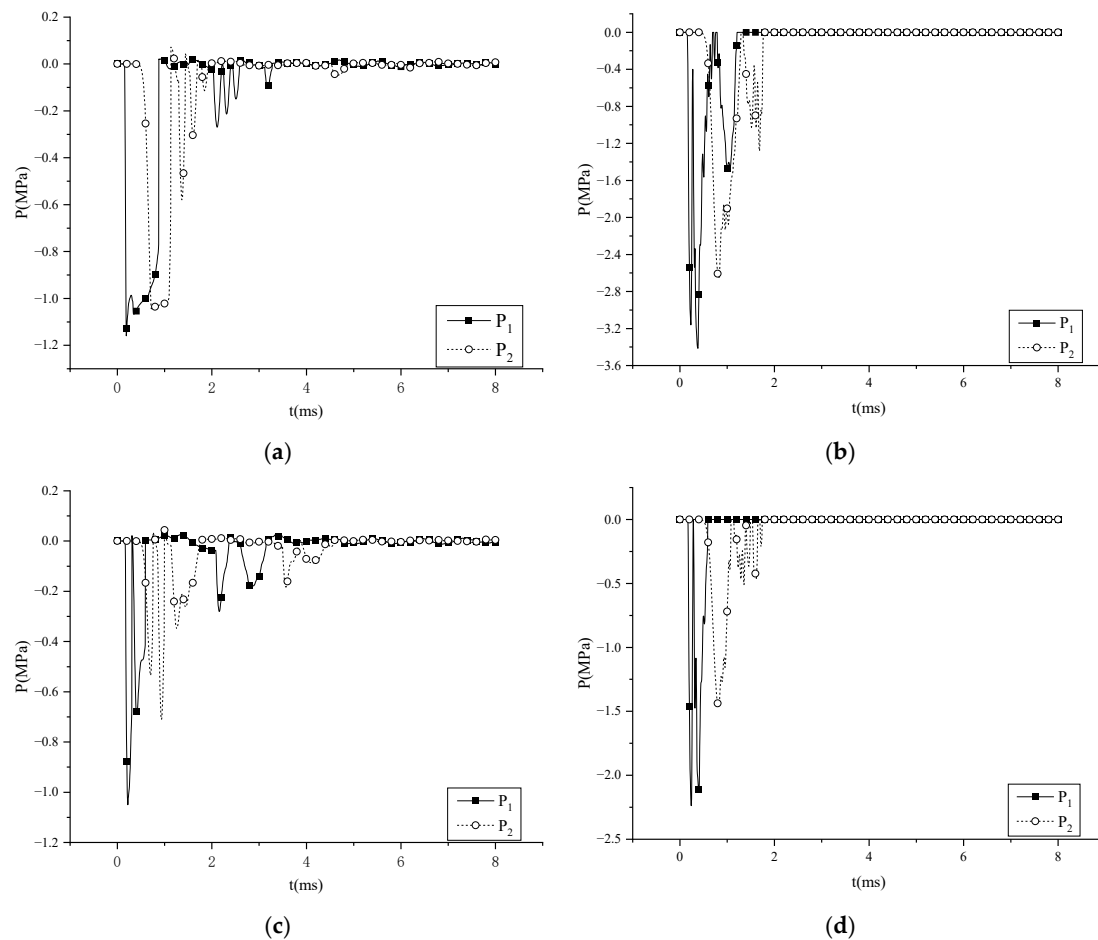
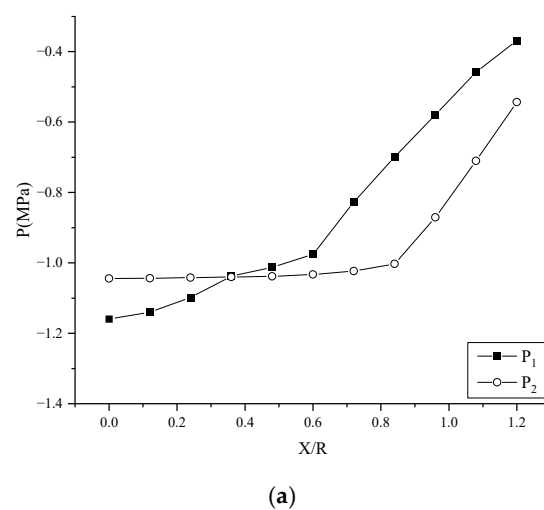


Figure 8. Time-history curves of contact pressure at the energy-absorbing layer interfaces. (a) Model 1 with 3 kg charge. (b) Model 2 with 3 kg charge. (c) Model 1 with 1.5 kg charge. (d) Model 2 with 1.5 kg charge.



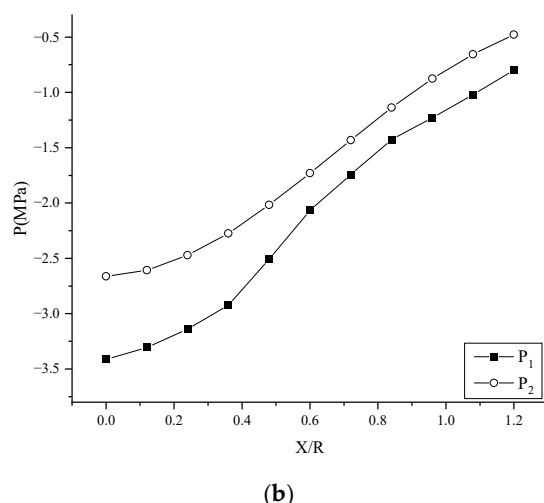


Figure 9. Variation in peak pressure at energy-absorbing layer interfaces along the axial direction under 3 kg charge. (a) Model 1; (b) Model 2.

4.2. Structural Deformation Analysis

To analyze the deformation characteristics of the multi-layer composite confined structure, the radial displacements of the inner and outer structural layers at the blast-center cross-section were examined. Here, the “inner layer displacement” refers to the radial displacement of the inner hoop reinforcement nodes of the inner RC layer, and the “outer layer displacement” refers to the radial displacement of the inner hoop reinforcement nodes of the outer RC layer. For the single-layer RC structure (Model 3), displacements refer to nodes at corresponding positions. Table 9 lists the peak radial displacements of the inner and outer structural layers for all models under different charge masses.

Table 9. Peak radial displacement (mm) of the structure and the time (ms) to reach the peak.

Charge Weight	Model	Inner Layer Peak (mm)	Inner Layer Time (ms)	Outer Layer Peak (mm)	Outer Layer Time (ms)
0.8 kg	1	0.435	0.668	0.043	1.035
	2	0.317	0.679	0.042	1.077
	3	0.129	0.585	0.079	0.484
1.5 kg	1	3.595	1.177	0.084	1.123
	2	3.062	1.014	0.177	1.169
	3	0.484	0.605	0.231	0.529
3 kg	1	8.201	1.415	0.152	1.215
	2	6.204	1.248	0.486	1.357
	3	1.023	0.857	0.875	0.814

Under all charge masses, the peak displacement of the inner layer in Models 1 and 2 is larger than that in the single-layer Model 3, while the peak displacement of the outer layer is significantly smaller. As the charge mass increases, the inner and outer layer displacements of Model 3 increase synchronously. In contrast, the inner layer displacement of Models 1 and 2 increases rapidly, while the outer layer displacement increases very slowly—especially for Model 1 with foamed concrete. The presence of the energy-absorbing layer significantly reduces the peak displacement of the outer layer. At 3.0 kg, the outer layer peak displacement of Models 1 and 2 is only 18.4% and 58.9% of that of Model 3, respectively. Comparing the two energy-absorbing materials, foamed concrete is significantly more effective in reducing outer-layer displacement.

Unlike single-layer structures, which respond as a monolithic entity under blast loading, the multi-layer composite confined structure deforms in a progressive, layer-by-layer manner. Under blast loading, the inner layer is driven by the instantaneous impulse and begins to move first, compressing the energy-absorbing layer, which in turn drives the deformation of the outer layer. Figure 10 shows the radial displacement time-history curves for all models under a 3.0 kg charge. It is evident that for the single-layer Model 3, the inner and outer layers start moving almost simultaneously, and their displacement curves follow a similar trend. In contrast, for the multi-layer Models 1 and 2, the outer layer begins to displace only after the inner layer has already reached a significant deformation. The displacement curves of the inner and outer layers differ markedly: the inner layer deforms rapidly to its peak, dominated by plastic deformation; the outer layer starts moving later, reaches a much smaller peak displacement, and exhibits primarily elastic vibration thereafter.

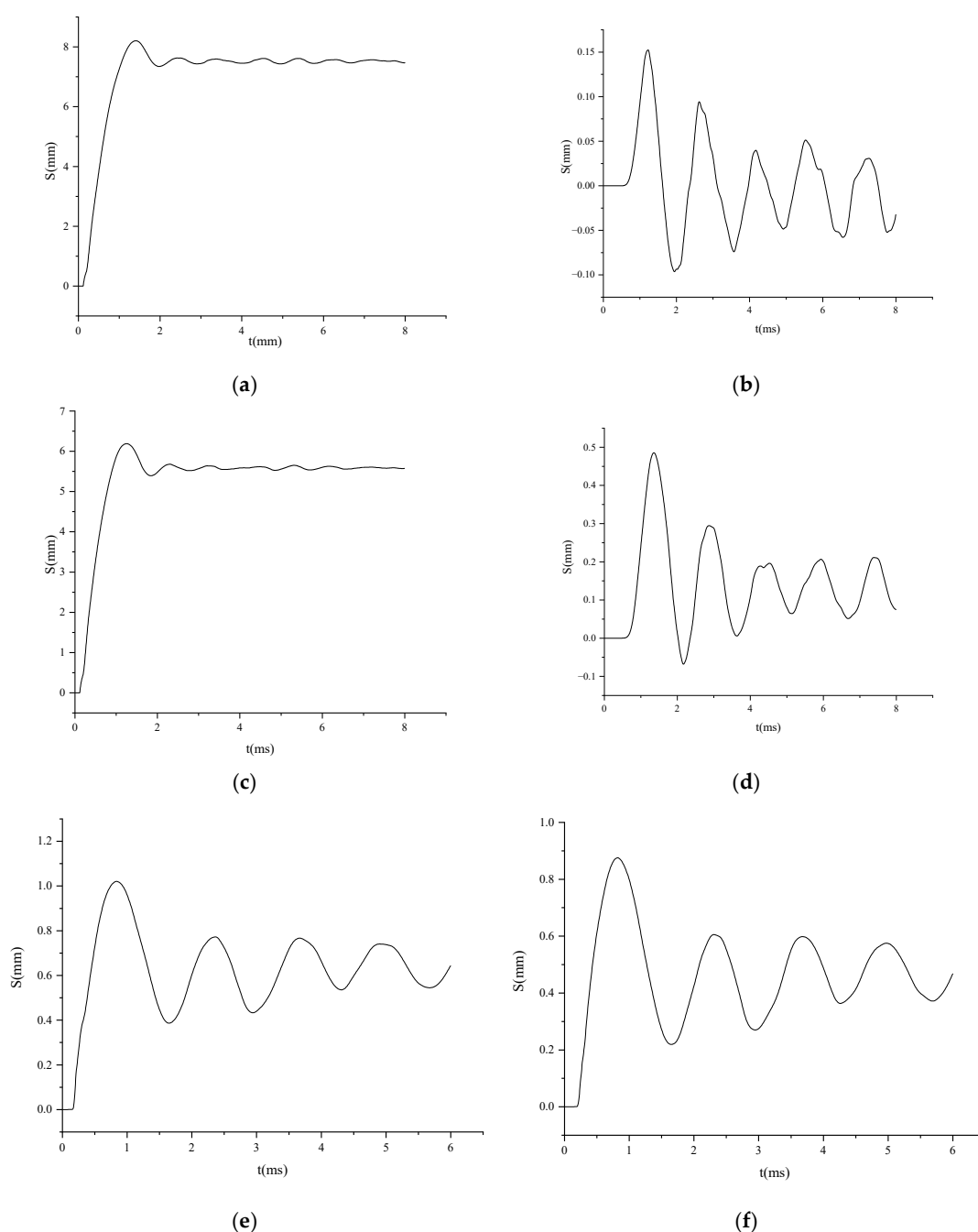
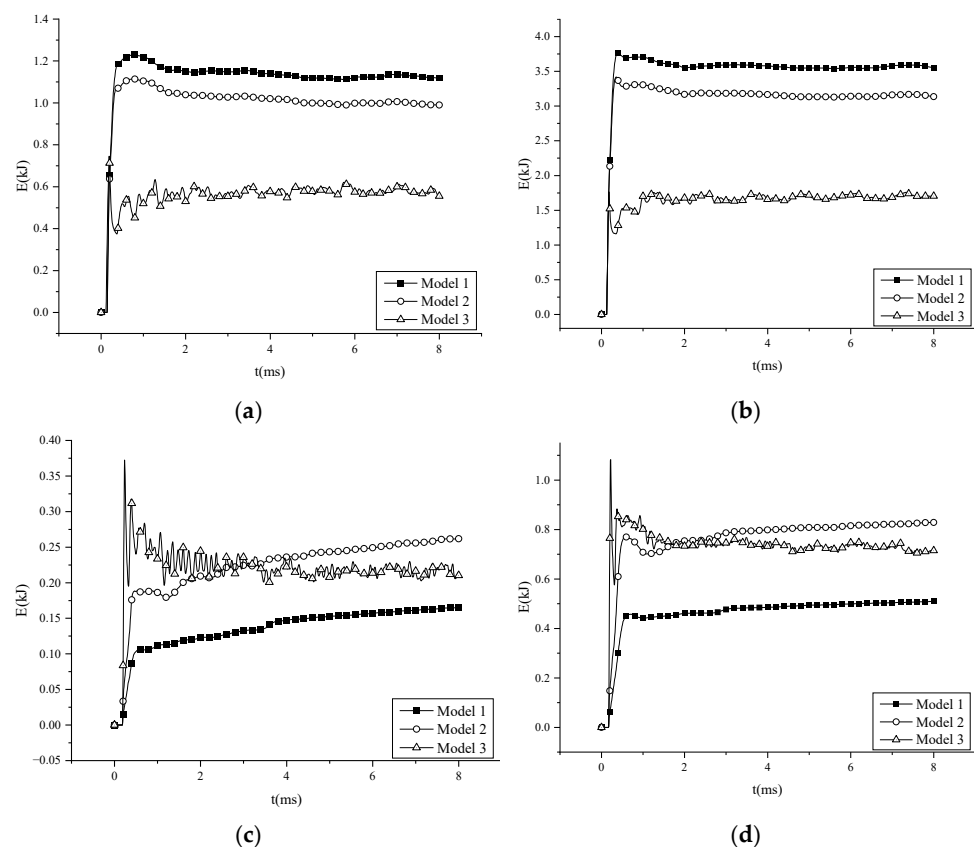


Figure 10. Time-history curves of radial displacement of the structure under 3 kg charge. (a) Displacement of inner layer of Model 1. (b) Displacement of outer layer of Model 1. (c) Displacement of inner layer of Model 2. (d) Displacement of outer layer of Model 2. (e) Displacement of inner layer of Model 3. (f) Displacement of outer layer of Model 3.

4.3. Energy Distribution

Energy serves as a measure of the magnitude of structural motion velocity and deformation severity. In the numerical model, the inner RC layer, energy-absorbing layer, and outer RC layer were defined as independent parts, allowing the total energy (sum of internal energy and kinetic energy) of each layer to be extracted directly from the LS-DYNA output. The purpose of the energy analysis is to evaluate the effect of the energy-absorbing layer on the energy distribution among the structural components, particularly its ability to attenuate the energy transmitted to the outer layer. Generally, it is desirable to minimize the energy transmitted to the protected target under blast loading.

The energy-absorbing layer significantly improves the energy distribution within the structure. Figure 11 illustrates the energy distribution for all models under 1.5 kg and 3.0 kg charges. At both charge levels, the energy contained in the inner layer of Models 1 and 2 is substantially higher than that in the inner layer of Model 3, while the energy in the outer layer is significantly reduced. At 3.0 kg, the outer layer energy of Models 1 and 2 is only 8.77% and 9.65% of that of Model 3, respectively. Thus, both multi-layer configurations dramatically improve energy distribution compared to the single-layer structure. Additionally, the peak energy in the outer layer of Models 1 and 2 is reached after a much longer duration compared to Model 3, where the energy peaks in all parts occur almost immediately after shock wave arrival. This indicates that the energy-absorbing layer not only reduces the magnitude of energy transmitted to the outer layer but also slows down the rate of energy transfer from the inner to the outer layer.



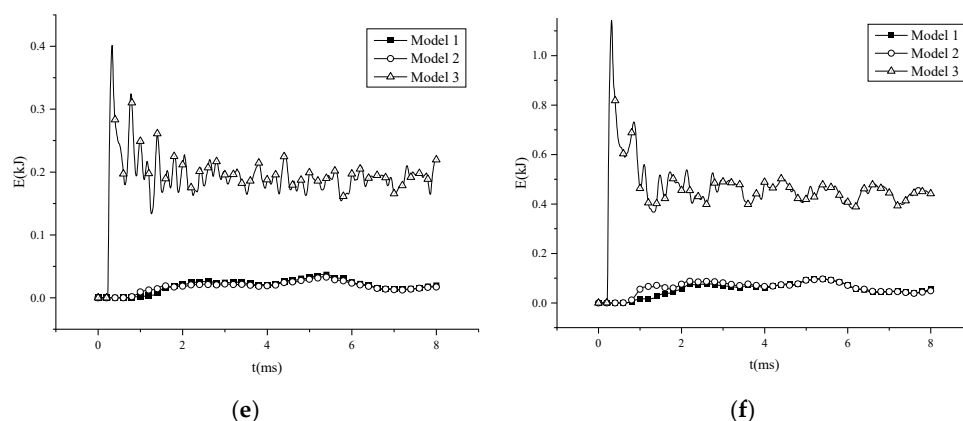


Figure 11. Energy distribution of structures under different charge weights. (a) Energy of inner structural layer under 1.5 kg charge; (b) Energy of inner structural layer under 3.0 kg charge. (c) Energy of energy-absorbing layer under 1.5 kg charge. (d) Energy of energy-absorbing layer under 3.0 kg charge. (e) Energy of outer structural layer under 1.5 kg charge. (f) Energy of outer structural layer under 3.0 kg charge.

Comparing the energy distribution between Models 1 and 2 reveals that, although their total structural energies are similar and both are higher than that of Model 3 (due to the lower stiffness and mass of the multi-layer structure leading to greater energy input), the energy absorbed by the foamed concrete layer in Model 1 is less than that absorbed by the sand layer in Model 2, while the energy retained in the inner layer of Model 1 is higher. This suggests that foamed concrete achieves load attenuation more efficiently through controlled yielding rather than pure energy dissipation.

4.4. Failure Mode Observation

Multi-layer composite confined structures are primarily used in scenarios involving high-risk explosive storage. After an internal explosion, the outer layer must maintain its integrity to prevent leakage of hazardous substances. For reinforced concrete structures, it is assumed that the outer layer must remain elastic under internal blast loading. Therefore, the appearance of plastic deformation in the outer layer is considered a failure of the multi-layer composite confined structure. This criterion is based on the functional requirement of containment structures: the outer layer serves as the ultimate barrier against the release of hazardous materials. Once the outer layer undergoes plastic deformation, its load-bearing capacity and sealing integrity are compromised, and the structure can no longer guarantee containment. In contrast, plastic deformation of the inner layer is acceptable as long as the outer layer remains elastic, because the inner layer is not a containment barrier.

For Model 2 (sand energy-absorbing layer), under a 3.0 kg TNT internal blast, the outer layer experiences significant deformation, and effective plastic strain appears in the outer layer concrete elements, as shown in Figure 12. For structures requiring the outer layer to remain elastic, this constitutes failure, resulting in the loss of containment function. The effective plastic strain in Model 2 is mainly concentrated at the blast-center cross-section. This is attributed to two factors. First, the lack of a distinct yield point in sand causes the peak pressure transmitted to the outer layer to increase rapidly with charge mass and inner-layer deformation. Second, the poor load redistribution capability of sand leads to excessive localized loading on the outer layer.

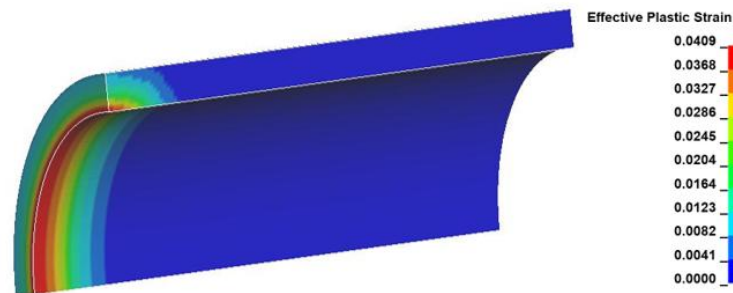


Figure 12. Effective plastic strain distribution in the outer layer of Model 2 under 3 kg charge.

The deformation magnitude of reinforced concrete structures under blast loading is a key indicator of structural performance and a basis for judging failure. Literature [33] on internal blast tests of single-layer RC cylindrical confined structures indicates that the hoop limit strain of reinforcement in such structures can reach 7.5–15%. In this study, a hoop strain limit of 5% is adopted; when the hoop deformation reaches this value, the structure is deemed to have failed due to reaching its deformation limit.

The 5% hoop strain limit was adopted as a conservative criterion for containment integrity, considering the following factors. First, for the reinforcement steel used in this study, the yield strain is approximately 0.142%; a 5% hoop strain represents a plastic deformation exceeding 35 times the yield strain, indicating severe structural distress. Second, the multi-layer composite confined structures studied herein are intended for high-risk applications such as ammunition storage and hazardous industrial facilities, where the outer layer must serve as the ultimate barrier against environmental release. For such safety-critical applications, a conservative failure criterion is appropriate. Third, it should be noted that the qualitative distinction between Mode I failure (inner-layer-limited failure, outer layer remains elastic; see Section 5.3) and Mode II failure (premature plastic failure of the outer layer; see Section 5.3) is not sensitive to the exact value of the strain limit. If a higher limit (e.g., 7.5% from Ref. [33]) were adopted, the critical charge mass at which Mode I failure occurs would increase, but the fundamental classification—whether the failure is inner-layer-limited (Mode I) or outer-layer-premature (Mode II)—remains unchanged. Therefore, the adoption of a 5% limit does not undermine the validity of the failure mode classification.

For Model 1 (foamed concrete energy-absorbing layer), increasing the internal charge to 5.0 kg TNT results in an inner layer displacement of 25.33 mm, at the blast-center cross-section, as shown in Figure 13a, at which the corresponding hoop strain of the inner reinforcement exceeds the 5% failure threshold. In contrast, the outer layer displacement remains only 0.23 mm (Figure 13b), confirming that the outer layer remains elastic under this extreme loading condition. At this point, the structure fails due to the inner layer reaching its deformation limit. Clearly, this failure mode can withstand a larger internal blast load while maintaining structural integrity and containment, representing the desired failure mode for the design of anti-internal-explosion multi-layer composite confined structures.

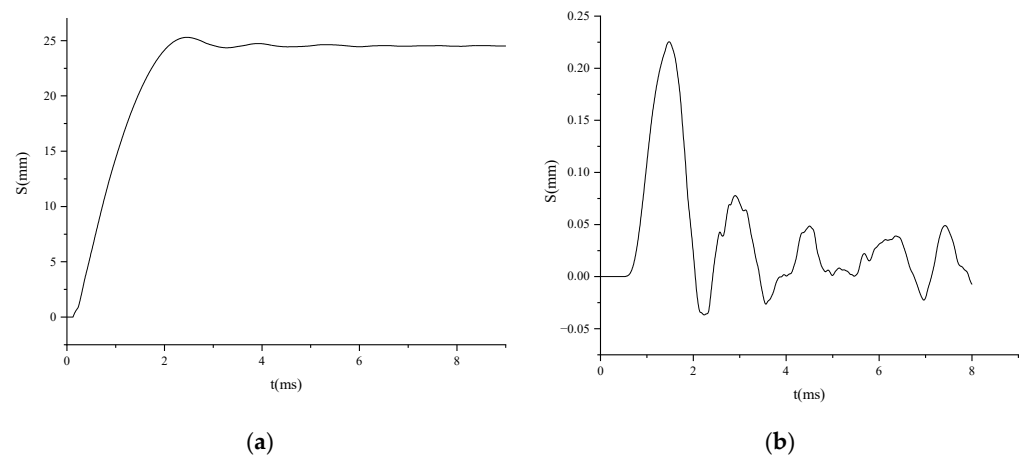


Figure 13. Time-history curves of radial displacement for Model 1 under a 5.0 kg TNT charge at the blast-center cross-section. (a) Inner structural layer, showing a peak displacement of 25.33 mm, at which the hoop strain exceeds the 5% failure threshold. (b) Outer structural layer, showing a peak displacement of only 0.23 mm, confirming elastic behavior.

4.5. Sensitivity Analysis of Foamed Concrete Yield Strength

To assess the robustness of the conclusions to variations in the foamed concrete material properties, a sensitivity analysis was performed by varying the yield strength σ_p from 1.0 MPa to 2.5 MPa. The material parameters and key results under a 5.0 kg TNT charge are summarized in Table 10.

Table 10. Material parameters and key results for different foamed concrete yield strengths under a 5.0 kg TNT charge.

Parameter	$\sigma_p = 1.0$ MPa	$\sigma_p = 1.5$ MPa	$\sigma_p = 2.0$ MPa	$\sigma_p = 2.5$ MPa
Density (kg/m ³)	610	660	710	750
Inner layer peak displacement (mm)	25.33	22.17	19.73	17.75
Outer layer peak displacement (mm)	0.23	0.33	0.55	0.82
Inner interface pressure P_1 (MPa)	1.254	1.795	2.289	2.831
Outer interface pressure P_2 (MPa)	1.042	1.576	2.077	2.603
Outer layer status	Elastic	Elastic	Elastic	Plastic

As shown in Table 10, the stress transmitted to the outer layer (P_2) remains approximately equal to the yield strength of the foamed concrete across all cases, confirming that the load attenuation mechanism is governed by the yield plateau behavior. Notably, at $\sigma_p = 2.5$ MPa under the 5.0 kg charge, the outer RC layer develops effective plastic strain, indicating a transition from the desired Mode I failure to the undesired Mode II failure. This demonstrates that the failure mode of the multi-layer composite confined structure is influenced by the energy-absorbing layer properties, and that an appropriate yield strength is essential for achieving the desired failure mode. In the present study, the foamed concrete with $\sigma_p = 1.0$ MPa, as adopted from our companion experimental study [23], satisfies this requirement. The development of a comprehensive design methodology for such structures, including the optimization of material parameters and layer dimensions, is planned as future work.

5. Discussion

5.1. Mechanism of Load Attenuation: The Role of the Yield Plateau

The numerical results in Section 4.1 clearly demonstrate the distinct load attenuation behaviors of the two energy-absorbing materials. The superior performance of foamed concrete over dense sand stems from their fundamentally different deformation mechanisms. As shown in Table 8, for the dense sand energy-absorbing layer (Model 2), the peak pressure at the outer interface (P_2) increased proportionally with the charge mass, reaching 2.663 MPa under 3.0 kg TNT. This is attributed to the granular nature of sand, where resistance relies on continuous particle rearrangement and friction without a distinct yield limit. Consequently, as the inner layer deforms more severely, the load transmitted to the outer layer escalates correspondingly, leading to excessive localized loading.

In contrast, foamed concrete (Model 1) exhibited a distinct yield plateau (Table 3). Although the inner interface pressure (P_1) increased from 0.659 MPa to 1.167 MPa as the charge increased from 0.8 kg to 3.0 kg, the outer interface pressure (P_2) remained capped at approximately 1.0 MPa. This confirms that the load attenuation effect is specifically a property of the stress transmitted to the outer layer (P_2), while the inner interface pressure (P_1) may temporarily exceed the yield stress due to the high-rate impact from the inner RC layer. The foamed concrete exhibits a distinct yield plateau, which effectively restricts the transmitted stress to approximately 1.0 MPa, thereby preventing excessive loading on the outer layer. This observation aligns with and validates the “load-controllable main structure” concept proposed by Fang et al. [20,21], wherein the foam layer ensures the outer structure operates within a safe stress range.

Furthermore, the axial distribution of pressure in Figure 9 provides deeper insight. Unlike the sand energy-absorbing layer, where pressure decreased with distance from the blast center, the foamed concrete energy-absorbing layer maintained a nearly constant pressure along the structure’s length. This indicates that the foamed concrete effectively transforms the concentrated blast load into a quasi-uniformly distributed load on the outer layer, optimizing the structural response by avoiding localized stress concentrations.

5.2. Progressive Deformation and Energy Evolution

The transition from a monolithic to a progressive deformation mode, as illustrated in Figure 9, is critical for structural survivability. In the single-layer structure (Model 3), the high stiffness coupled the inner and outer surfaces, forcing the outer layer to endure the full brunt of the blast simultaneously. This resulted in significant outer layer displacement and energy input, as quantified in Table 9 and Figure 11.

Conversely, the multi-layer configuration decoupled the layers. The time lag between inner and outer layer displacements allowed the inner layer to absorb the majority of the kinetic energy through plastic deformation. For example, for Model 1 at 0.8 kg, the inner layer peaked at 0.668 ms while the outer layer peaked at 1.035 ms. As discussed in Section 4.3, this decoupling reduced the outer layer energy to less than 10% of the single-layer model. This mechanism highlights the efficiency of the composite system: the outer layer is shielded from the high-intensity, short-duration impulse, experiencing only a delayed, attenuated response that is primarily elastic.

5.3. Failure Modes and Practical Implications

The identification of two distinct failure modes provides a clear design criterion for high-risk containment facilities. Mode I, observed in the foamed concrete model (Figure 13), represents the desired state. Here, the inner layer reached its deformation limit (hoop strain > 5%) while the outer layer remained elastic. This mode ensures containment integ-

rity even under severe internal blast, as the outer layer—the primary barrier against environmental release—is preserved.

Mode II, observed in the sand energy-absorbing layer model under 3.0 kg TNT (Figure 12), represents the undesired state. The lack of a yield plateau in sand led to excessive localized loading, causing premature plastic strain in the outer layer before the inner layer reached its limit. This failure mode compromises the structural integrity and must be avoided in design. These findings suggest that for high-consequence storage, energy-absorbing materials possessing a distinct yield plateau, such as foamed concrete, are preferable over traditional granular fills like dense sand.

5.4. Comparison with Related Studies on Layered Structural Systems

Although the loading conditions differ fundamentally between seismic excitation and internal blast, the underlying principle of using a properly designed intermediate layer to control the failure mode of a multi-component structural system transcends individual application domains. The progressive deformation and failure mode control mechanisms identified in the present study find conceptual parallels in recent investigations on layered structural systems subjected to dynamic loading in other engineering contexts.

Fan et al. [34] showed that optimizing energy-dissipating components in a mega-sub controlled structure system could effectively improve its failure mode, resonating with the present observation that the energy-absorbing layer determines whether Mode I or Mode II failure occurs. Shahzad et al. [35] highlighted the importance of strategically placing energy-dissipation layers to redistribute dynamic energy and protect the primary structure, aligning with the load-attenuation function of the foamed concrete layer in this study. Wang et al. [36] emphasized the need for rigorous material characterization and sensitivity analysis—an approach also adopted in Section 4.5.

Collectively, these studies reinforce the broader applicability of the layered energy-dissipation concept across different engineering fields—from seismic protection of high-rise buildings to blast containment of confined structures—and support the design philosophy advocated in this work: that a properly designed energy-absorbing layer can effectively control the failure mode and enhance structural resilience under extreme loading.

5.5. Limitations and Future Work

While this study provides valuable insights into the dynamic response of multi-layer composite confined structures, several limitations should be acknowledged.

First, the 1/8 symmetry model employed in this study assumes central initiation of the explosive charge. While this assumption is valid for the centrally initiated blast scenario investigated herein, real-world storage and containment scenarios often involve non-central charge placement. Our previous study on scaled internal blast tests [37] demonstrated that the peak reflected overpressure is highly sensitive to charge position eccentricity, whereas the impulse is relatively less affected, with a maximum relative difference not exceeding 13%. This suggests that eccentric initiation would primarily alter the local pressure distribution on the inner layer rather than the overall momentum transferred to the structure. For the foamed concrete configuration (Mode I), the elevated local pressure on the near-side wall would cause the inner layer to reach its deformation limit earlier locally, potentially reducing the critical charge mass for Mode I failure. For the sand configuration (Mode II), the localized pressure concentration would exacerbate the non-uniform loading on the outer layer, potentially triggering Mode II failure at even lower charge masses. Nevertheless, the fundamental distinction between the two failure modes—controlled yielding versus uncontrolled overload—is expected to remain valid, as the underlying mechanisms (yield plateau vs. granular compaction) are material-in-

trinsic rather than geometry-dependent. A full 3D investigation of eccentric initiation effects, which would require a complete model without symmetry simplifications, is planned as future work.

Second, the long-term durability of foamed concrete under cyclic environmental loads (e.g., moisture ingress, freeze–thaw cycles) was not considered but is vital for practical engineering applications. Future work should investigate these variables to refine the design guidelines. It should be noted that this study does not derive explicit design formulas or specific guidance for the anti-internal explosion design of such multi-layer composite confined structures. The development of practical design codes and calculation methods remains a key objective for our future research.

6. Conclusions

Based on the experimental tests and numerical simulations of multi-layer composite confined structures with foamed concrete and dense sand energy-absorbing layers under internal blast loading, the following conclusions are drawn. The experimental data from our companion study [23] were used to validate a high-fidelity FSI numerical model for charge masses up to 0.8 kg. The validated model was then employed to conduct parametric analyses at higher charge masses (up to 5.0 kg) and to compare the multi-layer composite structures with an equivalent single-layer model. The conclusions that follow are based on the validated numerical model, which has been shown to reproduce the experimental measurements with acceptable accuracy.

Load Attenuation Mechanism: The energy-absorbing layer effectively decouples the inner and outer layers, significantly reducing the hoop tensile stress in the outer concrete. Foamed concrete, due to its distinct yield plateau, caps the transmitted stress at approximately 1.0 MPa, outperforming dense sand in both load attenuation and redistribution capability.

Deformation Mode: Unlike single-layer structures that respond as a monolithic entity, the multi-layer composite confined structure exhibits a progressive, layer-by-layer deformation mode. The outer layer displacement is significantly mitigated, with the peak displacement under 3.0 kg TNT reduced to only 18.4% of that in an equivalent single-layer RC structure.

Energy Distribution: The energy-absorbing layer drastically optimizes the energy flow within the structure. At a charge mass of 3.0 kg TNT, the energy contained in the outer layer of the multi-layer structures with foamed concrete and dense sand energy-absorbing layers is only 8.77% and 9.65% of that in the single-layer RC structure, respectively.

Failure Mode Control: Two primary failure modes were identified for the first time, governing the structural behavior. The desired Mode I involves the inner layer reaching its deformation limit while the outer layer remains elastic, ensuring containment integrity. The undesired Mode II involves premature plastic failure of the outer layer due to inadequate load attenuation by the sand energy-absorbing layer. These findings provide a rational basis for selecting appropriate energy-absorbing materials in high-risk containment design.

Author Contributions: Conceptualization, P.N. and D.T.; methodology, P.N.; software, P.N.; validation, P.N. and D.T.; formal analysis, P.N.; investigation, P.N.; resources, D.T.; data curation, P.N.; writing—original draft preparation, P.N.; writing—review and editing, D.T. and P.N.; visualization, P.N.; supervision, D.T.; project administration, D.T.; funding acquisition, D.T. All authors have read and agreed to the published version of the manuscript.

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