Load-bearing behaviors of sandwich plates with non-uniformly distributed grid cores: Dynamic impact and blast

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Load-bearing behaviors of sandwich plates with non-uniformly distributed grid cores: Dynamic impact and blast

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Abstract: Sandwich plates with grid-core structures possess extensive applications, and non-uniform grid plates (NUGPs) show enhanced potential under varied loading modes. The current study embarks on an investigation into NUGPs by adjusting the dimensions of transverse and longitudinal grid walls. This comprehensive analysis explores the dynamic responses of these plates when subjected to impact and blast loads, focusing on deformation patterns, damage, and the underlying processes. The findings are illustrated as follows. 1) NUGPs, specifically those with an optimized configuration of grid walls termed the d2 structure, display improved resistance to blast and impact loads compared to their uniformly distributed grid plate counterparts. Notably, designs featuring a less dense grid in the central area yield a more evenly distributed damage pattern under impact loads and a lesser severity of damage from blast loads. 2) The study also reveals that under impact loads, the sandwich structure showcases distinct
behaviors through three stages evident in the load-displacement curves: initial localized indentation, followed by overall deformation, and ending with deformation recovery. 3) Moreover, when facing blast loads, the initial response involves rapid plastic deformation of the upper skin, succeeded by compression and buckling of the core. This sequence effectively dissipates energy, acting as a protective shield for the lower skin. 4) The optimized NUGPs design achieves a perfect balance between stiffness and ductility, reducing displacement through adequate stiffness while ensuring the effective absorption of dynamic forces through significant ductility, thus preventing catastrophic failure. This research significantly advances topology optimization for sandwich plates, providing essential insights for designing lightweight structures resilient under various modes.

**Keywords**: Sandwich plates, grid-core plates, non-uniform distribution, impact performance, blast performance

1 Introduction

The utilization of sandwich structures, particularly grid-core sandwich plates, has become increasingly widespread in fields such as aerospace[^1^,^2^], transportation[^3^], and beyond[^4^,^5^], owing to their remarkable attributes like high strength-to-weight ratio, stiffness, and energy absorption capacity. In these domains, especially where dynamic impact and blast resistance are crucial[^6^], the in-depth optimization of such structures assumes a paramount importance[^7^,^8^].

A novel avenue for lightweight research has emerged through the optimization of core’s topologies. Diverse periodic sandwich structures, including grid[^9^,^10^,^11^],...
laminated\textsuperscript{[12,13]}, pyramidal\textsuperscript{[14,15,16]}, curved\textsuperscript{[17,18]} and other types of lattices\textsuperscript{[19,20]} have been produced. In addition, many scholars have focused on optimizing common sandwich plate configurations by individually varying the geometric dimensions of each component. For instance, Li et al.\textsuperscript{[21]} engaged in theoretical analysis to explore how geometric parameters such as wedge width, aspect ratio, and foam core thickness influence the dynamic response of foam sandwich plates. The outcome highlighted the aspect ratio of 1 as the point of maximum deformation, with permanent deformation decreasing as wedge width and foam core thickness increased. Furthermore, Patel et al.\textsuperscript{[22]} varied the size dimensions of cells and studied steel grid plates featuring three different densities. Numerical simulations were conducted to evaluate the plates’ resistance to explosive forces. The findings highlighted the exceptional blast resistance of plates with lower densities under low-intensity explosive modes. These cumulative research outcomes not only underscore the significance of studying the impact and blast performance of sandwich plates but also furnish valuable insights that inform the current study.

However, these studies typically concentrate on designs with uniform cell distribution, thereby overlooking another potentially superior and promising optimization strategy: the non-uniform distribution of cells. In fact, while this strategy is not extensively discussed in academic research, its presence can be observed in practical applications, as illustrated in Fig. 1. This approach is particularly advantageous because adjusting the uniformity of grid patterns can potentially lead to significant improvements in structural performance without necessitating additional
material\textsuperscript{[23]}. This is especially relevant under dynamic modes such as impact and blast loads.

Thus, this paper specifically focuses on the most employed grid plates comprising horizontal and vertical walls. It represents a pioneering endeavor in probing the dynamic performance of non-uniform grid plates (NUGPs), encompassing an evaluation of their response to both impact and explosive loading modes. The structure of this study is delineated as follows: Firstly, we introduce the model design, finite element analysis (FEA) methods, and their validation. Subsequently, we conduct an exhaustive analysis of NUGPs, delving into aspects such as impact load-displacement curves, explosive performance, and deformation failure modes, while also elucidating the underlying mechanisms governing their resistance to impact and blasts. The outcomes of this research endeavor are poised to furnish indispensable guidance for the design of lightweight sandwich structures and serve as a pivotal reference for selecting grid sandwich plates under diverse load modes.

**Fig. 1** The applications of NUGPs in real structures: (a) aircraft (A380, Airbus)’s wing box (the important part connecting the main wing and the fuselage)\textsuperscript{[24]}, (b) high speed
2 Models and methods

2.1 Model design

The dimensional specifications for the impact and blast models conform to the ASTM D7766 standard\textsuperscript{[26]}, as illustrated in Fig. 2. Both impact and blast simulations employ identical model dimensions, measuring $100 \times 100 \text{ mm}^2$, while maintaining a core wall thickness of 0.2 mm. To ensure the representativeness of the structural test results, the models in orthogonal directions incorporate at least three uniformly distributed and complete cells\textsuperscript{[26]}. Concerning the arrangement of the core grid walls, the procedure commences with the fixation of the yellow grid walls (Fig. 2a1). Subsequently, non-uniform red grid walls are introduced at the 1/4 position (Fig. 2a2), with their precise positioning being governed by the parameters $\alpha$ and $\beta$ (Fig. 2b). Finally, adhering to principles of either central symmetry or axial symmetry, corresponding grid walls are inserted into other regions (Fig. 2a3), culminating in the creation of several diverse non-uniform grid plate models. To facilitate clear identification, each model is assigned a nomenclature: initially, based on the $\alpha$ values, all models are categorized into a-, b-, c-, d-, and e-type models. Further classification based on the $\beta$ values results in a total of 15 distinct models (Fig. 2b). Fig. 2(c) provides visual representation of non-uniform grid plate models, including its upper and lower skins. In these models, both the upper and lower skins of the grid plate possess a 1 mm thickness, while the core attains a height of 15 mm (Fig. 2c).
Fig. 2 Dimensions and schematic diagrams of the models. (a) Impact and blast model: (a1) core structure size and arrangement of fixed grid walls, (a2) insertion of non-uniform red grid walls in a 1/4 area, (a3) insertion of non-uniform grid walls in all areas. (b) Values and combinations of $\alpha$ and $\beta$. (c) An example model with the upper and lower skins.

2.2 Finite element analysis methods

Utilizing the dynamic-explicit solver in ABAQUS (version 2020, Dassault), simulations were conducted to model impact and blast modes, as depicted in Fig. 3.
Fig. 3 FEA models and their mesh partitioning: (a) impact model, (b) blast model.

To enable a more precise comparative assessment of the progressive failure behavior exhibited by various sandwich structures, this study utilized aluminum alloy EN AW-7108 T6\textsuperscript{[27]} as the material. Its mechanical properties were approximately characterized by a stress-strain curve as shown in (Fig. 4a), which typically consists of three stages: elastic (PATH A-B), plastic (undamaged, PATH B-C'), and damaged response (PATH C-D).
Fig. 4 The mechanical properties of aluminum alloy EN AW-7108 T6\textsuperscript{[27,28]}: (a) a schematic of the three stages in the true stress-strain diagram, (b) the true plastic stress-strain curve for PATH B-C’ at different strain rates (0-1000 $\varepsilon \cdot s^{-1}$), (c) the two types of fracture modes included in PATH C-D and their parameter settings.

Initially, the material has a density of 2,700 kg·m$^{-3}$. In PATH A-B, the Elastic Modulus is 70.0 GPa, and Poisson’s Ratio is 0.33. In PATH B-C’, the plastic stress-strain curves at varying strain rates are illustrated in (Fig. 4b). Following this, in PATH C-D, both ductile and shear damage initiation criteria are taken into account (Fig. 4c). The ductile criterion posits that the equivalent plastic strain at the onset of damage is a function of stress triaxiality ($\eta$) and strain rate. Meanwhile, the shear criterion is specified by providing the equivalent plastic strain at the onset of shear damage as a...
function of shear stress ratio and strain rate. Equations for both cases are showed in Eq. (1 and 2), respectively\textsuperscript{[27,28]}, with the relevant parameters depicted in (Fig. 4c).

Details of theoretical models are showed in the reference\textsuperscript{[27]}.

$$\bar{\varepsilon}^p_D(\eta, \dot{\varepsilon}^p) = \frac{\varepsilon^+_T \sinh[k_0(\eta^- - \eta)] + \varepsilon^-_T \sinh[k_0(\eta - \eta^+)]}{\sinh[k_0(\eta^- - \eta^+)]}$$  \hspace{1cm} (1)

$$\bar{\varepsilon}^p_S(\theta_s, \dot{\varepsilon}^p) = \frac{\varepsilon^+_S \sinh[f(\theta_s - \theta_s^-)] + \varepsilon^-_S \sinh[f(\theta_s^+ - \theta_s^-)]}{\sinh[f(\theta_s^+ - \theta_s^-)]}$$  \hspace{1cm} (2)

The parameters for shear stress under equibiaxial tension ($\theta_s^+$) and compression ($\theta_s^-$) are defined in Eq. (3 and 4), respectively.

$$\theta_s^+ = 2 - 4k_s$$  \hspace{1cm} (3)

$$\theta_s^- = 2 + 4k_s$$  \hspace{1cm} (4)

Discretization of the upper and lower skins was accomplished using eight-node linear hex brick elements (C3D8R) with reduced integration and hourglass control mechanisms, particularly designed to account for finite membrane strains. On the other hand, the grid core was discretized through the utilization of four-node general-purpose shell elements (S4R) equipped with reduced integration and hourglass control, thereby being well-suited for finite membrane strain analyses. Notably, reduced integration was applied to the shell element, a technique found applicable to the modeling of thin shells.

Furthermore, a mesh independence study was systematically conducted to validate the model’s robustness. To enforce contact interactions between the upper and lower skins and the core, tie constraints were meticulously implemented.

In the context of the impact model, as illustrated in Fig. 3(a), an impactor with a
hemispherical head measuring 25.0 mm in diameter was employed for the impact simulation. To confine the degrees of freedom of the sandwich plate, two clamping rectangular frames, referred to as clamps, were introduced. These clamps had dimensions of 100 mm in length, 10 mm in width, and 2 mm in thickness. Both the clamps and the impactor were treated as rigid bodies, where the clamps constrained all degrees of freedom, whereas the impactor retained only the degrees of freedom aligned with the impact direction. The impactor elements were specified with a size of 3 mm, while the elements for the clamps and the sandwich plate were set at 2 mm. The total impact energy is set to 15.0 J. The tangential friction coefficient is set to 0.3.

Regarding the blast model, the study made use of the CONWEP model integrated within ABAQUS. Notably, this model, owing to its consideration of air compressibility and lightweight characteristics, omitted modeling the stiffness and inertia effects of the air medium. As a result, when conducting aerial blast analyses with this model, explicit modeling of the air medium was dispensed with, and the calculations solely involved the structural model. This approach significantly reduced the computational burden while upholding a high level of accuracy. As shown in Fig. 3(b), the standoff distance from the blast point to the upper surface of the sandwich plate was established at 163 mm. The incident wave type was defined as air blast, and the TNT equivalent of the explosive was specified as 1 kg. Consistent with the impact model, two clamps identical to those used in the impact scenario were employed to restrict the degrees of freedom of the sandwich plate. The element size for the clamps was set at 2 mm, meanwhile the upper and lower skins were discretized with elements measuring 1 mm in size, and the
core grids’ elements were sized at 0.5 mm.

During the FEA, it is crucial to acknowledge that the mesh size can exert a profound influence on the dynamic response of the structure. Therefore, meticulous control over mesh density is imperative to circumvent computational distortion stemming from excessively large element sizes and to avert protracted computation durations resulting from excessively small element sizes. Consequently, adhering to the aforementioned principles, mesh independence assessments were conducted for both the impact and blast models in this study. These assessments confirmed that element sizes of 2 mm (for impact) and 0.5 mm (for blast) strike an optimal balance between computational efficiency and precision, in accordance with the desired level of accuracy. The convergence criterion adopted in the FEA adheres to the default convergence tolerance of 0.5% for the residual, as stipulated in ABAQUS.

2.3 Verification of the FEA models

In this section, three-dimensional digital models of experimental samples from other scholars’ studies on the dynamic response of sandwich plates are built. And they are incorporated into the FEA models of this study for solution. Important result curves and deformation characteristics will be compared with experimental results to verify the accuracy of the FEA models used in our study.

For the validation of the impact model, the selection of Lu et al.’s study on the sandwich plate with empty tubular cores under impact loads (Fig. 5a) serves as the benchmark. An impactor with a hemispherical head measuring 200 mm in diameter and
a drop height of 4.0 m is utilized, reflecting the setup in the referenced study. The material properties input into the FEA model are shown in Table. A 1. The comparison reveals that the simulated impact load-displacement curve closely aligns with the experimental results. Furthermore, the model accurately captures the localized indentation in the impact zone of the sandwich plate, with the geometry of this indentation consistent with the experimental results.

For the validation of the blast model, the selection of Mohamed\cite{Mohamed2017}'s study on the aluminum grid plate (Fig. 5b) serves as the benchmark. A quarter model with a unit size of 30.5 mm is simulated for the blast response under the effect of the TNT explosive (1 kg) at a height of 100 mm. The material properties input into the FEA model are shown in Table. B 1. The results show that the maximum displacements of the upper and lower skins in the FEA are 74.97 and 30.36 mm, respectively, compared to the experimental values of 72.0 and 28.4 mm, with an error controlled within 5%, indicating a great agreement between simulation and experimental results. Thus, the validation of the models above confirms the reliability and effectiveness of the FEA methods for impact and blast in this study.
Fig. 5 Comparison between experimental (EXP.) and FEA results. (a) impact behavior of the cladding sandwich plate with empty tubular cores\cite{29}, (b) blast behavior of the aluminum grid plate\cite{30}.

3 Results and discussions

This section will focus on exploring the resistance mechanisms against impact and blast in NUGPs, starting from their structural features. This exploration will be based on the FEA results of various sandwich plates subjected to impact and blast modes encompassing load-displacement curves, mechanical indicators, as well as core deformation and damage modes.

3.1 Results of impact tests and discussions

3.1.1 Load-displacement curves and mechanical parameters

Examining the configuration of the impact load-displacement curves for different sandwich plates, as depicted in Fig. 6(a) consistent pattern emerges in the behavior of
most plates. They exhibit a discernible ascent stage (Stage I, 0-cp1), followed by a
descent stage (Stage II, cp1-cp2), and conclude with a rebound stage (Stage III, post-
cp2). It is noteworthy, however, that plates denoted as a1, a2, b1, and b2 are
distinguished by their absence of Stage II, displaying only an ascent stage (Stage I) and
a rebound stage (Stage III) (Fig. 6a, b).

Fig. 6 Load-displacement curves for various plates under impact load: (a) GP and
a-type plates, (b) GP and b-type plates, (c) GP and c-, d-, e-type plates, (d) the ratios of
different mechanical parameters for a-, b-, c-, d-, e-type plates relative to GP.

In assessing the mechanical parameters that serve as metrics for impact resistance,
this study places particular emphasis on two key indicators: the maximum displacement
observed at the center of the lower skin ($U_{lower}$) and the peak impact load borne by the
structure \( (F_{\text{max}}) \). Notably, the maximum displacement of the lower skin offers insights into its resistance to impact, with smaller deflections signifying enhanced impact resistance. Conversely, the peak impact load denotes the maximum load that the sandwich plate can endure without succumbing to structural failure; lower values are indicative of superior impact resistance. Given the uniformly distributed nature of grid walls in GP, these walls serve as a benchmark for assessing the impact response of other NUGPs within this study. For the sake of comparison, this investigation presents the ratios of mechanical parameters for each grid plate in relation to the corresponding parameters for the uniformly distributed GP (Fig. 6d).

To begin with, for the maximum displacement at the center of the lower skin, it is noteworthy that a- and b-type plates exhibit relatively elevated values, reaching a peak at 1.47 times that of GP. In contrast, the performance of c-, d-, and e-type plates aligns closely with GP (Fig. 6d, Table. 1). Turning to the matter of the peak impact load, it becomes evident that c-, d-, and e-type plates (ranging from 1.79 to 2.80 kN) consistently manifest lower values when juxtaposed with GP and a-, b-type plates (ranging from 2.44 to 3.30 kN) (Fig. 6d, Table. 1). Considering the composite evaluation of these three paramount indicators, it becomes apparent that among the diverse array of sandwich plates, a-type plates exhibit the most diminished impact resistance. In sharp contrast, d-type plates manifest the most resilient impact resistance, as evidenced by their comparatively reduced maximum displacements and peak impact loads experienced by the lower skin. To provide specific context, for d1 and d2, it is worth highlighting that given d2’s more restrained peak impact load and lesser
maximum displacement of the lower skin, this investigation adjudges d2 plate to embody the apex of impact resistance (Fig. 6d, Table. 1).

Table. 1 Indexes of all the models.

<table>
<thead>
<tr>
<th>Type</th>
<th>$U_{\text{lower}}$ (mm)</th>
<th>$F_{\text{max}}$ (kN)</th>
</tr>
</thead>
<tbody>
<tr>
<td>GP</td>
<td>2.51</td>
<td>3.07</td>
</tr>
<tr>
<td>a1</td>
<td>3.69</td>
<td>3.08</td>
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<tr>
<td>a2</td>
<td>3.20</td>
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<td>a3</td>
<td>2.90</td>
<td>2.92</td>
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<td>a4</td>
<td>2.75</td>
<td>2.50</td>
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<td>a5</td>
<td>3.00</td>
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<td>b1</td>
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</tr>
<tr>
<td>b4</td>
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<td>2.44</td>
</tr>
<tr>
<td>c1</td>
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<td>2.46</td>
</tr>
<tr>
<td>c2</td>
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<td>2.62</td>
</tr>
<tr>
<td>c3</td>
<td>2.52</td>
<td>2.80</td>
</tr>
<tr>
<td>d1</td>
<td>2.57</td>
<td>2.14</td>
</tr>
<tr>
<td>d2</td>
<td>2.42</td>
<td>2.01</td>
</tr>
<tr>
<td>e1</td>
<td>2.56</td>
<td>1.79</td>
</tr>
</tbody>
</table>

3.1.2 Core deformation and damage

In alignment with the three distinct response Stages I-III delineated in the preceding section’s impact load-displacement curves, this section turns its attention to elucidating the deformation patterns exhibited within the structural core during the impact. To commence, focusing on the ascending Stage I of the load-displacement curve (Fig. 6a-c, I), the structure undergoes a phenomenon characterized by localized indentation (Fig. 7a1, b1). During this stage, the principal load-bearing components are the upper skin and the core, which experiences compression. Concurrently, the impact force exhibits a progressive escalation (Fig. 6a-c). Transitioning to the subsequent descent Stage II, which corresponds to the interval when the impact force attains its zenith within the
ambit of maximal impactor displacement (Fig. 6a-c, II), the structure embarks upon a trajectory of global deflection (Fig. 7a2, b2). At this juncture, the load predominantly finds its resistance in the comprehensive deflection of the structural ensemble (Fig. 7a2, b2), with the impact force tracing a trajectory of gradual attenuation (Fig. 6a-c). Ultimately, during the denouement in rebound Stage III, the structure systematically convalesces its formative integrity (Fig. 7a3, b3), and the impact load undergoes a precipitous descent, converging to the point of complete dissipation (Fig. 6a-c).

**Fig. 7** Deformation mode change diagram during the impact process. (a) GP, (b) d2. Subfigures 1-3 correspond to Stages I-III, respectively.

Based on the ultimate deformation modes observed during the impact process discussed earlier, the structural failure can be primarily categorized into three distinct damage modes: core buckling, core shear failure, and penetration of the sandwich plate. **Fig. 8** provides stress distribution maps and damage assessments of the core after eliminating the upper skin. It is evident that all sandwich plates in this study exhibit a
damage pattern characterized by core buckling. Following the impact, the upper skin of the sandwich plate experiences considerable deflection and deformation, leading to core buckling due to the compression imposed by the upper skin. As the core grid walls exhibit different distributions across the various sandwich plates, the extent and pattern of core buckling also exhibit corresponding variations. In a general sense, the damage zone in a-type plates is more concentrated in the central region, whereas d-type plates display a more uniform distribution of damage. This observation aligns with the earlier finding that d-type plates demonstrate the highest level of impact resistance, and further exploration of the underlying mechanisms will be presented in subsequent sections.

Fig. 8 Stress cloud diagram and damage mode of the core within representative...
plates.

3.2 Results of blast tests and Discussions

3.2.1 Performance and mechanical indexes

In the context of explosive events, it is customary to examine how a structure responds and absorbs energy following blast, as well as whether the structure ultimately maintains its structural integrity and functionality. On the left-hand side, Fig. 9 illustrates the temporal curves depicting the maximum displacements of the upper and lower skins of the sandwich plate. On the right-hand side, it presents the temporal curves depicting the energy absorption by the upper skin, core, and lower skin during the blast process. The mechanical metric, $EA$, is determined using the built-in ABAQUS Keyword: ALLIE$^{27,31}$. This metric entails the summation of internal energy across all elements (element $i$, ranging from 1 to $n$) within the specified region and is computed using Eq. 5.

$$EA = E_e + E_p + E_{dma} + E_{dc} + E_a$$

(Eq. 5.1)

$$E_e = \frac{1}{2} \sum_{i=1}^{n} \sigma_i \varepsilon_i^e$$

(Eq. 5.2)

$$E_p = \sum_{i=1}^{n} \sigma_i \varepsilon_i^p$$

(Eq. 5.3)

$E_e$ is the recoverable elastic strain energy (calculated by Eq. 5.2). $E_p$ is the energy dissipated through inelastic processes such as plasticity (calculated by Eq. 5.3). $E_{dma}$ is the energy dissipated through damage. $E_{dc}$ is the energy dissipated through distortion control. $E_a$ is the artificial strain energy and includes energy stored in hourglass
resistances and transverse shear in shell elements. $\sigma$ is the stress tensor. $\varepsilon^e$ and $\varepsilon^p$ are the strain tensors of elastic and plastic states, respectively.

As delineated in the left column of Fig. 9, in general, the displacements of both the upper and lower skins of various materials exhibit a predominantly linear progression. Furthermore, with variations in the type from a-type to e-type, the discrepancies in the maximum displacements of the upper and lower skins become increasingly conspicuous.

As illustrated in the right column of Fig. 9, with regard to the energy absorption within the upper skin, core, and lower skin, the lower skin displays the lowest energy absorption and adheres to a primarily linear incremental pattern. Concerning the energy absorption in the upper skin and core, during the initial stage of the blast, specifically within the first 0.2 ms, the energy absorption in the upper skin experiences a marked surge, holding an unequivocal dominance. After the initial 0.2 ms period, the energy absorption in the upper skin gradually diminishes, while the core’s energy absorption undergoes a significant upswing and subsequently stabilizes.

Regarding the mechanical metrics employed to assess blast resistance, the displacements of both the upper and lower skins serve as indicators of the degree of plate deformation under the influence of explosive loads. If the upper skin’s maximum displacement exceeds a certain threshold, it suggests that the plate may struggle to effectively dissipate the explosive loads, potentially resulting in structural damage or failure. Conversely, if the displacement is too small, the structure may be overly rigid,
impeding effective energy absorption. The disparity in maximum displacements between the upper and lower skins characterizes the deformation capacity of the core within the sandwich plate. A more substantial difference implies significant core deformation or compression, whereas a smaller difference signifies insufficient core deformation capacity, both of which can compromise subsequent energy absorption capabilities. Therefore, it is imperative to maintain this disparity within an appropriate range to effectively absorb and disperse explosive loads without compromising the structural integrity and subsequent performance of the sandwich plate.

Lastly, from an energy absorption standpoint, greater energy absorption in both the upper skin and core implies that the shock wave has been predominantly absorbed and dissipated before reaching the lower skin, signifying enhanced blast resistance. Conversely, if the lower skin exhibits a higher proportion of energy absorption, it implies that the upper skin and core of the sandwich plate have not effectively absorbed the energy of the shock wave, thereby posing a potential threat to the structural stability and integrity.

Drawing upon the aforementioned metrics, Table 2 presents specific numerical values corresponding to a blast time of 1.5 ms. $U_u$ and $U_l$ refer to the maximum displacement of upper skin and lower skin separately. In this context, $\Delta U$ represents the disparity in between, which, can be calculated by Eq.6.

$$\Delta U = |U_l - U_u|$$ (6)

To commence the discussion, in terms of the maximum displacements of both the
upper and lower skins, as well as the disparity between them, it is evident that c3, d-, and e-type plates exhibit upper skin displacements exceeding those of the reference GP, with the maximum difference being 17%. Conversely, the lower skin displacements generally fall below the GP values, resulting in relatively higher disparities between the upper and lower skin displacements compared to GP. However, these disparities remain relatively close to each other. In contrast, the remaining plates, specifically a-, b-type, and c1, c2 plates, display a contrasting pattern to the one previously described. Notably, the disparities in the upper and lower skin displacements are significantly narrower compared to GP, ranging from approximately 13% to 58% of the GP values. It is evident that c2 and c3 plates represent critical modes within this context.

Furthermore, when examining the proportion of energy absorption in the upper skin and core, c3, d-, and e-type plates consistently outperform GP, with d2 plate achieving the highest value (0.91, Table. 2). In contrast, other models tend to exhibit lower proportions.

Taking into consideration all the aforementioned parameters holistically, d2 plate emerge as noteworthy due to their displacement indicators closely resembling those of GP, as well as exhibiting the highest proportion of energy absorption in the upper skin and core. As a result, it is posited in this study that d2 plate display superior blast resistance characteristics. This assertion corroborates the earlier finding that d2 plate demonstrated the most robust impact resistance in the preceding section. Subsequently, a more detailed analysis will be presented in the subsequent section, which will encompass an evaluation of core deformations and damages.
Fig. 9 Displacement-time curves for the upper and lower skins of each sandwich
plate (left column) and energy absorption-time curves for the upper skin, core, and lower skin (right column). (a) GP and a-type plates, (b) GP and b-type plates, (c) GP and c-type plates, (d) GP, d-type, and e-type plates. Herein, subscript \( l \) denotes the lower skin, \( c \) represents the core, and \( u \) signifies the upper skin.

Table. 2 Indexes of each sandwich plate at 1.5 ms.

<table>
<thead>
<tr>
<th>Type</th>
<th>( U_u ) (mm)</th>
<th>( U_l ) (mm)</th>
<th>( \Delta U ) (mm)</th>
<th>Upper skin's ( EA ) indexes</th>
<th>Core’s ( EA ) indexes</th>
<th>Ratio</th>
<th>Ratio ((EA_u+EA_c))</th>
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Note: the subscript \( l \) denotes the lower skin, \( c \) represents the core, and \( u \) signifies the upper skin. Ratio refers to the proportion of the calculated part in the total energy absorption of the entire plate.

3.2.2 Deformation of the core

In this section, our focus shifts towards a detailed examination of the deformation processes and damage assessments pertaining to the core material. This analysis serves as a crucial foundation for unraveling the blast resistance mechanisms inherent in each
of the models.

Given the previously established superior impact resistance characteristics of the d2 plate, Fig. 10 is presented to elucidate the sequence of deformations observed in the d2 plate during the blast event. In the initial stages of the blast, the upper skin undergoes rapid plastic deformation directly in response to the applied blast load (Fig. 10a-c). This plate promptly assumes the role of the primary structural component responsible for energy absorption, leading to a swift and substantial increase in the energy absorption-time curve, where it assumes a predominant position (Fig. 9, right column). Subsequently, the core material undergoes compression and buckling due to the compressive forces exerted by the upper skin, and in some instances, it may experience crushing (Fig. 10d-f). This process effectively dissipates energy, providing a buffering effect against the shockwave and resulting in a gradual and stabilized increase in energy absorption by the core material over time, as discerned from the energy absorption-time curve (Fig. 9, right column). The combined deformations of the upper skin and the core material collectively attenuate the magnitude of the shockwave load transmitted to the lower skin, thereby minimizing its deformation. This protective mechanism subsequently leads to reduced displacements of the lower skin (Fig. 9, left column) and, consequently, the lowest levels of energy absorption (Fig. 9, right column).
Finally, the focus revolves around the analysis of stress distribution and the evaluation of structural damage within the core structure. As illustrated in Fig. 11, in accordance with the previously observed phenomenon of significantly reduced disparities in upper and lower skin displacements in a-type, b-type, c1, and c2 plates compared to GP, these plates exhibit more extensive regions of elevated stress levels within their core structures and demonstrate a higher degree of core integrity. Conversely, the core structures of c3, d-type, and e-type plates generally manifest a level of damage akin to that observed in GP. Importantly, when considering the c3, d-type, and e-type plates, it becomes evident that the d2 model consistently displays diminished stress amplitudes and the least pronounced degree of damage. This further substantiates the earlier conclusion regarding the superior blast resistance capabilities of the d2 plate.
Fig. 11 Stress cloud diagram and damage mode of the core within representative plates.

3.3 Mechanisms of resistance to impact and blast

Given that both impact and blast load primarily affect the central region of plates, the stiffness and plastic deformation capabilities of the central core structure critically determine the plate’s response to these loads. Should the core exhibit high stiffness, it can effectively minimize plate displacement, thereby restricting damage to a smaller area. Consequently, an overly sparse distribution of grid walls in the central zone is discouraged, as it may result in excessive displacements. Conversely, an excessively
dense grid can lead to abrupt material failure rather than deformation when subjected to loads surpassing a certain threshold, incurring the cost of more extensive core damage. Therefore, striking an optimal balance between stiffness and ductility is paramount for achieving superior resistance to impact and blast. The appropriate stiffness reduces displacement, while adequate ductility ensures the effective absorption of dynamic loads, preventing structural failure.

In models such as a-type and b-type, characterized by denser grids in the central region (Fig. 8, Fig. 11), stiffness is higher. Consequently, the deformation capacity of the core is reduced, or in some cases, entirely absent. This leads to increased displacement of the lower skin under impact loads, surpassing that of the uniformly distributed GP (Fig. 6d, Table. 1). Notably, a1 and b1, observed in a-type and b-type models, exhibit the highest stiffness in the central region, resulting in the most substantial displacement of their lower skins. Conversely, in c-type, d-type, and e-type models, including GP, the grids in the central area are relatively sparser (Fig. 8), enhancing deformation capacity. This translates to smaller displacements of the lower skins under impact loads (Fig. 6d). Under blast loads, the inferior deformation capacity of the core not only increases the displacement of the lower skin but also reduces the difference in maximum displacement between the upper and lower skins, while the energy absorption ratio of the upper skin and the core is relatively low (Table. 2).

However, as previously mentioned, an excessively sparse central grid is inadvisable. Therefore, the e-type plate is not optimal; instead, it is the d2 plate that strikes the right balance between stiffness and ductility, thereby displaying superior resistance to impact
and blast.

4 Conclusions

This study explores, for the first time, the mechanical performance of NUGPs concerning impact and blast in comparison to uniform grid plates via FEA. The following conclusions have been drawn:

1) Under impact loads, a-type plates manifest more concentrated damage in the central region, whereas d-type plates exhibit a more uniform damage distribution. Among them, the d2 plate displays the best impact resistance. The response to impact loads, including localized indentation, overall deformation, and deformation recovery, corresponds to three distinct response stages observed in the impact load-displacement curves.

2) Under blast loads, the d2 plate demonstrates the best blast resistance, characterized by consistently lower stress amplitudes and minimal damage to the core structure. In the initial stages of the blast, the upper skin undergoes plastic deformation upon direct impact. Subsequently, the core experiences compression and buckling, dissipating energy and safeguarding the lower skin.

3) Attaining an optimal equilibrium between stiffness and ductility is pivotal for achieving superior resistance to impact and blast. Adequate stiffness reduces displacement, while ample ductility ensures effective energy absorption, mitigating structural failure. The d2 plate strikes this equilibrium between stiffness and ductility, yielding the best performance in resisting impact and blast.
This study enriches the research on the topology optimization of sandwich plates and provides insights into the arrangement of grid walls in sandwich plates under various loading modes. It promotes the practical application of grid plates in engineering design and offers valuable guidance for the design of lightweight sandwich structures.

**Declaration of Competing Interest**

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

**Acknowledgment**

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Appendix A

To validate the impact model established in this study, simulations were conducted based on physical experiments documented in the reference [29], with material parameters for the relevant components as shown in Table. A 1.

Table. A 1 Material properties input into FEA.

<table>
<thead>
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<th>Component</th>
<th>Young’s modulus (GPa)</th>
<th>Poisson’s ratio</th>
<th>Density (g·cm(^{-3}))</th>
<th>Yield stress (MPa)</th>
<th>Plastic strain</th>
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Appendix B

To validate the impact model established in this study, simulations were conducted based on physical experiments documented in the reference [30], with material parameters for all the components as shown in Table. B 1.

Table. B 1 Material properties input into FEA.

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References


