



# A dimensionless metric for quantifying fluid–structure interaction in blast-loaded plates

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## ABSTRACT

In blast-loading scenarios, the interaction between the structural motion and the blast load (fluid–structure interaction, FSI) can dominate the response of lightweight or flexible structures. Prior studies have clarified key mechanisms and advanced semi-analytical models, but complex configurations still require numerical simulations. In practice, quantifying FSI typically relies on comparing results from two separate modelling strategies, which is often impractical. This work proposes a pragmatic procedure to obtain inexpensive estimates of FSI effects using uncoupled simulations only. The approach builds on one-dimensional theories that include non-linear gas compressibility and inertial effects, combined with saturation analysis, and it centres on a single nondimensional index. Two complementary options are provided: Option 1 uses the reflected gas state and plate properties; Option 2 identifies characteristic times from uncoupled structural simulations. The procedure yields dimensionless estimates of the reductions in peak midpoint velocity and deflection for clamped plates. Validation covers 28 cases representing a compressed-gas shock-tube configuration established in the literature, including steel and aluminium plates over multiple thicknesses and load levels. The estimates show good agreement with high-fidelity numerical benchmarks and are robust across cases. The proposed fast-running engineering method enables rapid, objective, and operator-independent assessment of FSI effects suitable for practical use, and it supports informed selection of the appropriate numerical strategy for subsequent detailed analyses.

## 1. Introduction

The response of structures to blast loading is a persistent concern in civil, mechanical, and defence engineering, as underlined by recent studies across applications [1–7]. When a structure is exposed to a blast wave, the interaction between the fluid and the target — commonly referred to as fluid–structure interaction (FSI) — may play a fundamental role in governing the dynamic response of the system [8–11]. While traditional blast-resistant design approaches often treat the blast wave as an external load acting independently of the target's mobility [12,13], numerous studies have demonstrated that FSI effects can significantly modify the pressure loading and the mechanical response, particularly in lightweight and flexible structures. The relevance of FSI is supported by recent shock-tube tests on metallic plates [11, 14], studies of blast-mitigation membranes [15], and urban-scale blast propagation [16].

Several theoretical models describing these phenomena were initially developed in the context of underwater explosions and were later extended to air-blast conditions. This historical progression is outlined below, as it provides the conceptual background for the present study.

Historically, Taylor has been recognised as a pioneer in studying FSI effects in blast-loaded plates. He proposed a one-dimensional (1D) theory for plates subjected to underwater blasts, incorporating the effect of the support by an equivalent spring model [17]. The simplified version of this theory, obtained by neglecting the effects of the supports and commonly referred to as *free-standing plate theory*, provides a clear interpretation of FSI effects. Although originally developed for underwater explosions, this theoretical framework has subsequently been extended to air-blast conditions, which are the main focus of the present study. According to the theory, lightweight structures acquire

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less impulse as a result of the pressure relief caused by their motion. This is further explained for air-blast scenarios by the classical literature on gas dynamics [18]. When a shock wave impinges on a structure, the fluid particles are brought to stagnation, as the structure is initially at rest. Then, as the structure starts to move, its motion generates a rarefaction wave that propagates back into the fluid domain, reducing the pressure acting on the surface.

Based on the first results of Taylor's studies, over the past two decades, considerable attention has been devoted to the study of FSI effects, focusing on identifying the governing parameters and developing increasingly accurate semi-analytical 1D models. These studies have primarily addressed blast-loaded plates, as these components are especially susceptible to intense FSI in real-world scenarios. In particular, recent studies have demonstrated that FSI effects play a significant role in determining the dynamic response of sandwich panels subjected to underwater blast loading [19–23]. Liu and Young extended the theory to water-backed configurations [24], while Teich and Gebbeken incorporated structural stiffness and damping within Taylor's original framework [25,26]. These works adopt an acoustic model for water, neglecting any non-linear compressibility effects. This approximation is commonly employed for underwater explosion scenarios. In contrast, it becomes inadequate in air, even for relatively small blast overpressures [27,28].

Taylor's free-standing plate theory was eventually extended by Kambouchev et al. by introducing non-linear compressibility [29]. In their model, commonly referred to as the KNR model, an approximate formulation was proposed to estimate the transmitted momentum as a function of both the blast intensity and the plate's areal mass. The same approach, initially applied to time-decaying pressure histories, was later applied by Kambouchev et al. to uniform shocks [30]. The analytical formulation was subsequently verified through numerical simulations [9], and its applicability was further explored in recent experimental investigations [31–33]. Moreover, the KNR moving-boundary correction has been embedded in design models for sacrificial claddings to account for front-plate motion [34]. In parallel, Peng et al. extended the KNR model by incorporating the effect of the shock-induced back pressure generated by the rapid motion of the plate [35]. Around the same period, Wang et al. developed an FSI model that incorporates non-linear compressibility effects, based on the assumption of an exponentially decaying reflected pressure profile [36]. These studies indicate that including non-linear compressibility, as opposed to relying on acoustic approximations, tends to reduce the transmitted impulse for lightweight structures, and to increase it in the case of heavier structures. In 2009, Subramaniam et al. developed a fully coupled numerical model based on a single-degree-of-freedom system to investigate FSI effects [37]. In their study, particular emphasis was placed on the role of the ratio between the structural velocity and the particle velocity behind the incident shock wave. Furthermore, FSI effects have also been linked to the deformed shape of the structure, as geometric changes can significantly influence the local pressure distribution and momentum transfer [8,11,38,39].

More recently, Ghoshal and Mitra demonstrated that the ideal gas assumption, commonly adopted in earlier studies, may no longer hold under strong shock conditions. They proposed a 1D coupled FSI model that incorporates real gas effects [40]. Gauch et al. following the approach of Subramaniam et al. formulated practical expressions for the rapid assessment of FSI relevance across a broad range of loading scenarios [41]. A similar objective was pursued by Cloete and Nurick, who introduced a dimensionless parameter to identify whether FSI effects should be considered in numerical simulations [42,43]. Nartu and Kumar adopted the same single-degree-of-freedom coupled framework used by Subramaniam et al. and Gauch et al. but extended it by introducing a more detailed representation of dissipative mechanisms and by correlating FSI effects with the scaled stand-off distance [44]. Pal and Ghoshal additionally included the underpressure phase and non-linear compressibility effects on the rear side of the plate [45].

The same moving-boundary coupling introduced by Subramaniam et al. has been applied more recently in an analytical energy-exchange model for air blast by Qi et al. who compute a velocity-dependent reflected pressure without solving the fluid domain [46]. Advanced FSI models have also been developed for underwater scenarios [47]. In addition, beyond semi-analytical and numerical approaches, machine learning techniques have recently been employed to capture and predict FSI effects [48,49].

Although simplified models and 1D formulations provide useful insight into the governing mechanisms of FSI, many real-world problems involve structural and loading complexities that require more advanced tools. In these cases, high-fidelity simulations, typically based on the explicit non-linear finite element method, are often adopted to capture both the loading and the structural response with sufficient accuracy [50–52]. Two main numerical frameworks are commonly used: (i) the uncoupled approach, where the fluid and structural problems are solved separately, assuming that the blast loading is unaffected by the structural motion [53,54], and (ii) the coupled approach, where their mutual interaction is accounted for in a single simulation [11, 13,55–57]. Immersed boundary FSI formulations now handle shocks and large deformations within a single framework [58], and GPU-accelerated models enable larger-scale 3D FSI analyses [59]. While the uncoupled approach is computationally efficient and widely used in engineering practice, it fails to capture the FSI effects, leading to biased load and response predictions [60]. The coupled approach, on the other hand, provides more accurate results, but at a significantly higher computational cost. This burden is evident in recent multiphase FSI implementations, which remain computationally demanding to ensure stable and accurate interface resolution [61,62].

In this context, a key challenge lies in determining when a coupled simulation is necessary. In fact, since a reliable predictive criterion is still missing, engineers are often forced to perform both coupled and uncoupled analyses to assess the relevance of FSI effects. Although several dimensionless parameters — and more recently machine learning techniques — have been proposed in the previously discussed literature to support this decision-making process [17,29,37,41,42,44,48,49], a practical engineering criterion that is concise, interpretable and readily applicable to realistic scenarios is still lacking. This gap often leads either to unnecessary computational effort or, conversely, to inaccurate predictions when the interaction is neglected.

To address the aforementioned issue, the present work proposes an error estimation framework to assist analysts and practitioners in selecting the appropriate modelling strategy, i.e. in deciding whether a coupled simulation is necessary, for a given shock-loading scenario. The error estimates are calculated using dimensionless numbers that are derived following the approach originally proposed by Cloete and Nurick [42]. The dimensionless numbers yield estimates of the difference in deflections predicted using uncoupled and coupled modelling strategies. However, the primary advantage of the proposed approach is that the error estimates can be extracted from a single uncoupled simulation.

The framework includes two distinct options, each offering increasing accuracy, which can be employed depending on the required level of detail. The first option provides error estimates based only on the fluid shock analysis step of an uncoupled simulation, i.e. the cost of a structural response analysis step is avoided. The second option offers an improved estimate based on both the fluid and structural steps of an uncoupled analysis. The methodology is demonstrated with reference to plates subjected to blast loading, as this is the type of structure most likely to experience FSI effects. Specifically, this study is intended for blast-loaded plates in membrane-dominated, large-inelastic regimes where inertia governs the peak response. Configurations governed by bending stiffness or appreciable damping are outside the scope of the estimates.

The accuracy of the proposed framework is discussed by considering the high-fidelity numerical simulations reported in Refs. [11,48].

These simulations have already been validated against shock tube experiments.

The manuscript is structured as follows. Section 2 presents the proposed methodology, deriving the dimensionless metric and outlining the overall framework. The section also summarises the key features of the experimental case studies and the numerical simulations used for validation. Section 3 presents and discusses the results with reference to two representative case studies selected from the available database. Finally, Section 4 provides the main conclusions of the work.

## 2. Methodology

This section presents the proposed two-option procedure for estimating FSI effects in blast-loaded clamped plates and is structured as follows. Section 2.1 introduces the development of the dimensionless metrics governing the problem and emphasises their physical interpretation (Section 2.1.1). Section 2.2 explains how the key time parameter ( $t$ ) is determined, either from closed-form solutions or through uncoupled numerical simulations. Section 2.3 summarises the overall procedure within its two alternative options, providing a clear and practical basis for implementation. Finally, Section 2.4 describes the numerical and experimental case studies adopted as benchmarks to validate the framework.

### 2.1. Dimensionless metrics

The dimensionless numbers developed in this section follow the approach originally proposed by Cloete and Nurick [42] and are derived analytically by considering the 1D scenario of a free-standing plate subjected to a spatially and temporally uniform shock wave. In other words, the framework is rigorously valid for unsupported systems in which inertia effects dominate the structural response, as the influence of structural stiffness (including boundary conditions) and damping is neglected. The same description, however, is also valid for the central region of a clamped plate undergoing large-inelastic (membrane-dominated) deformation, until the plastic hinge formed at the boundary reaches the plate centre [63,64], as illustrated in Fig. 1. In the figure, the quantity  $p(t)$  defines the pressure loading acting on the free-standing plate, while  $v(t)$  identifies the structural velocity. Configurations in which stiffness or damping influence are relevant lie outside the scope of the present estimates; discussions of these mechanisms, mainly under flexural-elastic conditions, can be found in Refs. [37,41,44,45].

Fluid–structure interaction effects are quantified through a comparison between the results of two analytical frameworks: (i) an uncoupled framework in which the shock load is assumed to be independent of the mobility of the structure and (ii) a coupled framework in which the effects of the mobility on the shock pressure are considered. It is important to clarify that the coupled and uncoupled frameworks considered in this section refer to analytical formulations of the problem, rather than to the numerical simulation strategies discussed earlier. Based on the comparison between these approaches, a dimensionless number for velocity and displacement is defined as:

$$\Delta v = \frac{v_U - v}{v_U}, \quad (1)$$

$$\Delta w = \frac{w_U - w}{w_U}, \quad (2)$$

where  $v$  and  $w$  define the velocity and displacement of the free-standing plate, respectively, computed from a coupled framework (no subscript) and an uncoupled analysis (subscript  $U$ ). The dimensionless number for displacement is hereafter referred to as *FSI efficiency*, adopting the terminology introduced in Ref. [41].

Fig. 2 presents a qualitative comparison between the response of a blast-loaded square clamped plate, with and without FSI effects. For each scenario (with/without FSI), only half of the plate is displayed

to facilitate the comparison. The figure shows a reduction in the plate displacement due to FSI effects. This reduction reaches its maximum at the centre of the plate and is denoted as  $\Delta w$ . Accordingly, the dimensionless metrics developed in this section for a free-standing plate are intended to estimate the reduction in velocity and/or displacement at the centre of a clamped plate.

Consider, therefore, a spatially and temporally uniform shock impinging normally on a homogeneous free-standing plate of uniform thickness. In the uncoupled analytical framework, the mobility of the plate is assumed not to affect the shock acting on it. Hence, the pressure history defines a rectangular pulse of intensity  $p_r$ , as described in Fig. 3. The acceleration  $a_U$  of the free plate is then expressed according to the momentum conservation equation,

$$a_U = \frac{\Delta p}{\rho H} = \frac{p_r - p_0}{\rho H}, \quad (3)$$

where  $p_r$  defines the constant reflected pressure,  $p_0$  the pressure on the distal face of the plate,  $\rho$  and  $H$  the density and thickness of the plate, respectively. The acceleration is indicated with a  $U$  subscript as it refers to the acceleration computed from an uncoupled analysis. The term  $p_0$  coincides with the initial pressure of the unshocked fluid. This reflects the assumption that the pressure on the rear face of the plate remains constant and equal to  $p_0$  throughout the loading phase.

The velocity and displacement of the plate are computed by a single and a double time integration of Eq. (3), resulting in:

$$v_U = a_U t, \quad (4)$$

$$w_U = \frac{1}{2} a_U t^2, \quad (5)$$

where  $t$  identifies the integration interval for velocity and displacement. According to the uncoupled analytical approach,  $t$  coincides with either the duration of the blast load  $\tau$  (Fig. 3) or with the duration of the structural acceleration [42,43], whichever is shorter. However, this assumption requires further consideration. The acceleration can be considered constant regardless of the structure's mobility only when the time profile is described by a rectangular wave while the plate is moving. This ideal assumption is reasonable only when the loading duration is significantly longer than the structural response time. This condition typically applies to confined explosions and compressed-gas-driven shock tubes [65–68]. It does not apply to other configurations, such as unconfined explosions or explosively driven shock tubes, where the pressure profile decays over a timescale comparable to that of the structural response.

In many of the works discussed in Section 1, and in most analytical models for blast-loaded plates [69], the blast duration is finite and comparable to the structural timescale. In those models, the load duration or its decay rate is explicitly introduced as a parameter. In the present formulation, where a constant pressure load is assumed, the time integral  $t$  is not directly specified based on loading parameters. To the authors' knowledge, this approach has not yet been addressed in existing FSI models, which contributes to the broader generality of the framework. In conclusion, the definition of the integration interval  $t$  is a critical aspect of the framework and is therefore addressed separately in Section 2.2.

In the coupled analytical framework, the plate is initially at rest, generating a reflected shock that slows the shocked particles to the stagnation pressure. Moreover, the velocity of the reflected shock is much higher than that of the plate, allowing it to travel a considerable distance before the plate starts to move significantly. Under these circumstances, the free plate can be considered as a projectile, acted upon by a uniform gas column at the pressure and temperature of the initial reflected shock. This phenomenon is well understood and explained in the internal ballistics literature [18]. As the plate begins to accelerate under the action of the gas pressure, expansion waves start

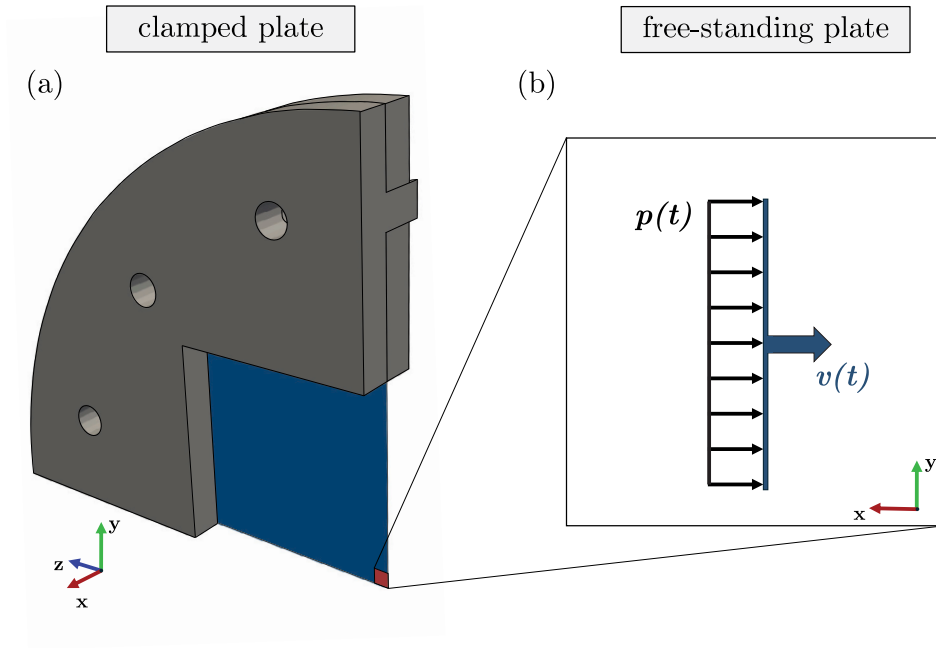


**Fig. 1.** Schematisation of the 1D free-standing-plate idealisation at the midpoint of a clamped plate. (a) quarter model of the fully clamped plate; (b) local representation used in the analysis, with a 1D element loaded by a spatially uniform, time-dependent pressure  $p(t)$  and moving out of plane with velocity  $v(t)$ .

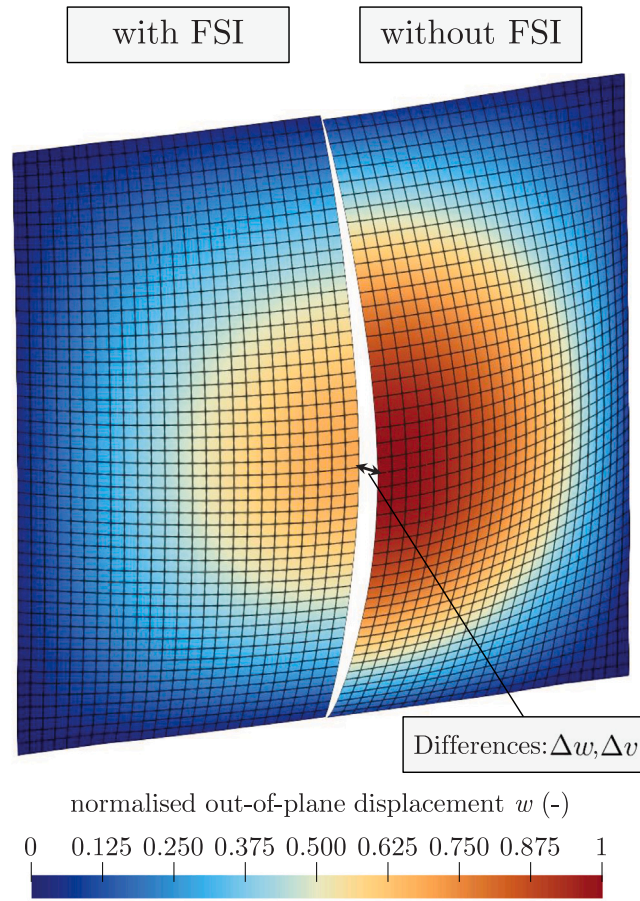


**Fig. 2.** Illustration of FSI-induced mitigation in a blast-loaded clamped square plate. Residual deflection fields with FSI (left) and without FSI (right) are overlaid to show the reduction in permanent out-of-plane displacement. Colours indicate the normalised displacement  $w$ .

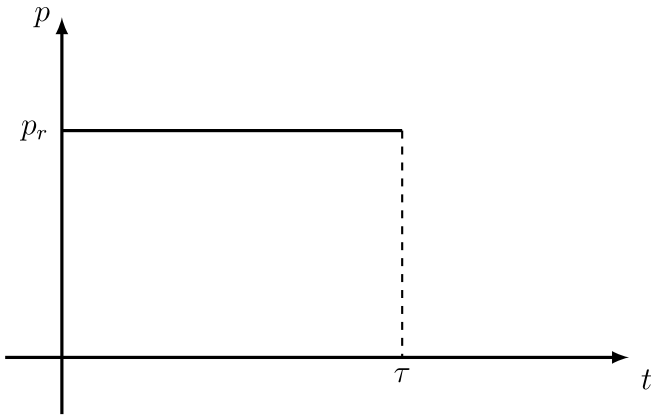


Fig. 3. Idealisation of the blast load used in the uncoupled analytical approach: rectangular pulse with peak level  $p_r$  and duration  $\tau$ .

to propagate from the plate surface into the gas column, decreasing the pressure exerted on the plate, according to the expression,

$$p(t) = p_r \left( 1 - \frac{\gamma - 1}{2} \cdot \frac{v(t)}{c_r} \right)^{\frac{2\gamma}{\gamma - 1}}, \quad (6)$$

where  $p(t)$  defines the pressure acting on the plate as a function of time,  $v$  defines the velocity of the plate,  $p_r$  is the initial reflected pressure which corresponds to the one employed in Eq. (3),  $\gamma$  is the ratio between the specific heat capacity at constant pressure and volume for the given gas (for air  $\gamma = 1.4$ ). Eq. (6) originates from gas-dynamics considerations [18], and is employed in several coupled models in the literature to capture FSI effects [37,41,42,44]. In this manuscript, such model is referred to as the *expansion wave approach*, following the terminology of Cloete and Nurick [42]. In Eq. (6),  $c_r$  defines the initial speed of sound, that is, the one computed at the beginning of the reflection phase. The speed of sound for a perfect gas is computed according to the expression,

$$c_r = \sqrt{\gamma R T_r}, \quad (7)$$

where  $T_r$  corresponds to the initial reflected temperature,  $R = \mathfrak{R}/M$  defines the gas constant and is computed as the ratio between the universal gas constant  $\mathfrak{R}$  (8314 J/kmol K) and the molar mass  $M$  of the given species (kg/kmol).

Eq. (6) models FSI effects, as the plate velocity influences the pressure temporal profile. The mutual interaction between pressure and the mobility of the structure can be studied by combining the momentum conservation equation and Eq. (6), resulting in,

$$\frac{dv}{dt} = \frac{p_r}{\rho H} \left[ 1 - \frac{\gamma - 1}{2} \cdot \frac{v(t)}{c_r} \right]^{\frac{2\gamma}{\gamma - 1}} - \frac{p_0}{\rho H}. \quad (8)$$

Eq. (8) describes a non-linear first-order Ordinary Differential Equation (ODE). Hence, it can be solved only numerically, leading to the semi-analytical approaches already discussed [37,41,44]. However, to obtain an engineering estimate of FSI effects, the equation can be simplified under two different conditions:

- strong shock ( $p_r \gg p_0$ ), the second term of Eq. (8) can be reasonably neglected. The resulting equation can be integrated analytically by separating the variables [43].
- weak shock ( $p_r \sim p_0$ ): the non-linear term may be simplified by leveraging the Taylor polynomial, centred at  $t = 0$  and truncated at the first order. This approach is more general than the strong shock approximation described above and also holds for the case studies considered in this work. Additionally, while recent literature shows a growing interest in increasingly accurate semi-analytical 1D models (see Section 1), the present work aims

to define a readily applicable engineering criterion. The use of the Taylor expansion is fully consistent with this objective and is therefore adopted hereafter.

The non-linear term of Eq. (8) can be expanded as described by the simplified expression,  $(1 + Bt)^n \approx 1 + nBt$  [42], leading to the linear ODE,

$$\frac{dv}{dt} = \frac{p_r}{\rho H} \left( 1 - \frac{\gamma}{c_r} \cdot v(t) \right) - \frac{p_0}{\rho H} \Rightarrow \frac{dv}{dt} = \frac{p_r - p_0}{\rho H} - \frac{p_r \gamma}{c_r \rho H} v. \quad (9)$$

where the constant  $\psi = \frac{c_r}{p_r \gamma} \rho H$  is introduced for simplicity. Furthermore, one can easily recognise the uncoupled acceleration  $a_U$  in the first term of Eq. (9). The simplified expression of the linear ODE is then rewritten as,

$$\frac{dv}{dt} = a_U - \frac{1}{\psi} v(t). \quad (10)$$

Eq. (10) directly expresses the role of fluid–structure interaction. The mobility of the plate indeed reduces its acceleration compared to the uncoupled scenario. The quantity  $\psi$  has units of time and has already been recognised in the literature as a FSI indicator, as it weights the contribution of FSI effects [11,17,48]. In particular, it depends on the mass per unit area of the plate, expressed by the product of density and thickness ( $\rho H$ ). In addition, it is noted that the role of mass per unit area is modulated by the thermodynamic state of the gas in the initial reflection condition.

Upon integration of Eq. (10), the velocity of the plate is described by,

$$v(t) = \psi a_U (1 - e^{-t/\psi}). \quad (11)$$

To obtain a dimensionless term for assessing FSI effects on velocities, the exponential in Eq. (11) is approximated by the second-order Taylor expansion about  $t = 0$  [42],  $(1 - e^{-t/\psi}) \approx \frac{t}{\psi} - \frac{t^2}{2\psi^2}$ . This results in the expression described by,

$$v(t) \approx a_U t \left( 1 - \frac{t}{2\psi} \right). \quad (12)$$

The definition of the dimensionless term is straightforward and described by,

$$\Delta v = \frac{v_U(t) - v(t)}{v_U(t)} = \frac{t}{2\psi} = \frac{1}{2} \cdot \sqrt{\frac{\gamma}{R}} \cdot \frac{p_r}{\sqrt{T_r}} \cdot \frac{1}{\rho H} \cdot t. \quad (13)$$

Eq. (13) shows that the FSI-induced reduction in velocity is governed by the non-dimensional ratio  $t/\psi$ . This time-scale ratio has a clear physical meaning that makes the metric readily interpretable; a detailed discussion of  $\psi$  and of the role of  $t/\psi$  is provided in Section 2.1.1. The dimensionless number of Eq. (13), originally proposed by Cloete and Nurick [42,43], provides a conservative estimation of FSI effects. As shown in their work, the error introduced by the use of the Taylor expansion tends to overestimate the actual reduction in velocity due to FSI effects, which is consistent with the engineering objective of the present study. In the dimensionless metric, three quantities are required from a single uncoupled simulation: the reflected pressure  $p_r$  and temperature  $T_r$ , and the duration  $t$ . The strategy adopted to extract these parameters is detailed in Sections 2.2 and 2.3.

Despite the simplicity of Eq. (13), a displacement-based metric is more pragmatic and easier to interpret when assessing the significance of FSI effects and determining whether uncoupled simulations should be avoided. Accordingly, the work of Cloete and Nurick [42,43] is here extended by formulating an analogous metric based on the displacement of the free-standing plate. To derive the dimensionless term for the displacements, Eq. (11) is integrated to calculate the displacements using the expansion wave approach, resulting in,

$$w(t) = \psi^2 a_U \left( e^{-t/\psi} + \frac{t}{\psi} - 1 \right). \quad (14)$$

To express an immediate dimensionless term, the third-order Taylor polynomial is required, centred at  $t = 0$ . The choice of orders in

the developments is reasonable. For acceleration, FSI effects appear in the first order of the Taylor polynomial, while for velocities, they appear in the second order of the Taylor polynomial. It is therefore logical to conclude that the FSI effects emerge with the third-order term for displacements, justifying this choice. The third-order polynomial is described by,  $(e^{-t/\psi} - 1) \approx -\frac{t}{\psi} + \frac{t^2}{2\psi^2} - \frac{t^3}{6\psi^3}$ . The displacement function is then re-expressed as,

$$w(t) \approx \frac{1}{2} a_U t^2 \left(1 - \frac{t}{3\psi}\right). \quad (15)$$

The definition of the dimensionless term for displacement is then straightforward and described by,

$$\Delta w = \frac{w_U(t) - w(t)}{w_U(t)} = \frac{t}{3\psi} = \frac{1}{3} \cdot \sqrt{\frac{\gamma}{R}} \cdot \frac{p_r}{\sqrt{T_r}} \cdot \frac{1}{\rho H} \cdot t. \quad (16)$$

From the same quantities already calculated for velocity, it is thus possible to calculate a dimensionless term that assesses the role of FSI effects on displacements.

### 2.1.1. Physical interpretation

To better understand the physical meaning of the dimensionless numbers, the expression for  $\psi$  is rewritten in a more insightful form. Specifically, the role of the acoustic impedance of the gas at the reflection instant is identified by introducing the density at the reflection instant  $\rho_r$  and recognising the expression of the sound speed  $c_r^2 = \gamma p_r / \rho_r$ , which results in,

$$\psi = \frac{c_r}{\gamma p_r} \frac{\rho_r}{\rho_r} \rho H = \frac{\rho H}{\rho_r c_r}. \quad (17)$$

In Eq. (17),  $\rho_r c_r$  defines the gas acoustic impedance or, according to Seigel [18], the acoustic inertia at the reflection state, as it expresses the mass per unit time traversed by a disturbance wave.

The role of the plate's inertia is straightforward. As the inertia of the structure increases, its resistance to acceleration will be greater, resulting in a lower velocity and thus in a reduction of FSI effects. By contrast, the gas inertia determines the magnitude of pressure changes required to produce given velocity changes. In other words, for a given increase in the plate's velocity, a larger acoustic impedance determines a larger reduction in the reflected pressure, that is, larger FSI effects. This result also explains why larger reflected pressures determine larger FSI effects, as already observed by Aune et al. [11]. In fact, the acoustic impedance at the initial reflected state increases as the reflected pressure increases.

Another physical interpretation of the dimensionless numbers can be obtained by considering the expression  $t/\psi = \rho_r c_r / \frac{\rho H}{t}$  which defines a ratio of impedances. In particular, the ratio between the acoustic impedance of the gas at the initial reflection state and the mechanical impedance  $\rho H/t$  of the free-standing plate.

The importance of such parameters has already been highlighted in the literature [9,29,30,41]. However, to the authors' knowledge, the direct relation to the FSI efficiency has not been elucidated. The dimensionless parameter  $t/\psi$ , as extensively discussed by Kambouchev et al. [29], represents the counterpart of the FSI index originally proposed by Taylor when non-linear compressibility effects are considered.

In this paper, the main difference with respect to previous works lies in the definition of  $t$ . In earlier studies,  $t$  was associated with a characteristic of the loading profile, specifically the decay time of the exponential function used to describe the pressure history. In the present work, since the load duration is typically longer than the structural response,  $t$  is instead expressed as a function of structural response parameters, as described in Section 2.2.

### 2.2. Definition of the integration interval $t$

To properly evaluate the two dimensionless quantities  $\Delta v$  and  $\Delta w$ , the time interval  $t$  must be specified. This interval defines the time window over which analytical integration is performed to calculate velocity and deflection, serving as a critical parameter throughout the study.

The framework developed up to this point is general and formally valid for a free-standing plate, where inertia effects dominate and boundary conditions are neglected. In order to apply this formulation to more realistic scenarios, such as clamped plates, a conceptual transition is introduced at this stage. This is achieved by expressing the integration time  $t$  as a characteristic time associated with the transient response of the target structure. Essentially, this means that the response of a free-standing plate is evaluated at a time that characterises the behaviour of a clamped plate. This modelling choice is justified by the fact that, as previously discussed, the central region of a clamped plate behaves similarly to a free-standing plate until the plastic hinges propagate to the midpoint.

As a result, all the considerations that follow are specific to the structures considered in this work, that is, fully clamped square metal plates subjected to shock loads with a duration significantly longer than the structural response time and undergoing large inelastic deformation [11]. Under these conditions,  $t$  cannot be derived from the blast profile alone, but it must be related to the structural response. Since the present framework is developed with reference to the midpoint of a clamped plate,  $t$  must be selected to capture the transient behaviour of either the velocity or the displacement at that location, depending on the quantity of interest. As evident from Eqs. (13) and (16), both  $\Delta v$  and  $\Delta w$  grow linearly with time. Consequently, it is logical to associate  $t$  with the time instant at which the midpoint velocity or displacement reaches its peak value. This choice ensures that the metric captures the maximum effect of FSI within the transient response and provides a representative estimate.

Two approaches are introduced to compute  $t$ , reflecting different levels of accuracy and computational cost. These define the two options of the framework. The first option provides a fast, but less accurate estimate of the FSI effects. It uses only the fluid phase of an uncoupled simulation, typically an Eulerian analysis [13,55,56], from which the reflected shock pressure  $p_r$  and density  $\rho_r$  (or any other combination of two thermodynamic properties of the gas) are extracted. The time  $t$  is estimated from the expected duration of the structural response and depends only on parameters known a priori. No structural simulation is needed. This option is suitable for identifying cases with negligible or dominant FSI effects, where the FSI efficiency is either very low or very high. In intermediate cases, a second option can be introduced to improve accuracy. This involves also running the structural phase of the uncoupled simulation, computing the velocity or deflection time histories at the centre of the plate. This allows for a more refined estimate of  $t$ , and consequently, of the FSI efficiency.

In the first option of the proposed framework, the time interval  $t$  is estimated without computing the structural response. This is achieved by relying on two distinct closed-form solutions, each providing a characteristic timescale associated with a specific aspect of the plate's behaviour: one related to the transient velocity at the plate centre, and the other to the transient deflection at the same location.

To characterise the velocity transient, an estimate of the time taken by the travelling hinge to reach the centre of the plate is considered. In particular, the closed-form solution originally proposed by Taylor [43, 70] for a clamped circular diaphragm subjected to an impulsive load is adopted. Hudson [71] provides further insights into the transient response of impulsively loaded circular diaphragms. In Taylor's formulation, the material is assumed to behave as rigid-perfectly plastic, with no strain hardening and no sensitivity to strain rate effects. Under these assumptions, the hinge speed is constant. The corresponding time to

reach the plate centre, referred to in this work as the Taylor duration  $t_T$ , is then,

$$t_T = L \cdot \sqrt{\frac{\rho}{\sigma_y}}, \quad (18)$$

where  $\rho$  and  $\sigma_y$  are the density and static yield strength of the material, respectively.  $L$  denotes the shortest distance from a boundary to the centre of a plate, which corresponds to half the width of the square plates considered in this work.

For thin plates, it can be shown that the travelling hinge mechanism represents the first phase of the overall dynamic response. The instant at which the plastic hinges reach the plate centre can be regarded as a meaningful transition point. Up to this time, the central region behaves essentially as a free-standing plate. Once the boundary influence reaches the centre, the velocity begins to decay [43]. Therefore, the Taylor duration provides a practical estimate of the time at which the transverse velocity at the plate centre reaches its maximum. The characteristic time  $t_T$  is therefore used in the present framework to estimate the FSI-induced reduction in transverse velocity at the centre of the plate.

The time  $t$  in the dimensionless metric  $\Delta w$  (Eq. (16)) is defined by leveraging the concept of impulse saturation, commonly adopted in the analysis of blast-loaded plates. When the load duration is sufficiently long, the transverse displacement of the plate no longer increases after it reaches a certain magnitude, even if the load persists [72,73]. With this saturation phenomenon, it is possible to identify the transient response of the plate through the so-called saturated duration  $t_{\text{sat}}$ . In practical terms, it marks the end of the transient deflection phase.

The concept of impulse saturation was first recognised by Jones [74] and later by Shen et al. [75] in pulse-loaded structures. It was formally defined by Zhao et al. [76] and subsequently extended by Zhu and Yu [72]. Over the years, the saturation analysis has been widely adopted to develop analytical and semi-analytical models for square and rectangular plates under different loading conditions [73,77–79]. More recent contributions have combined it with the Membrane Factor Method (MFM), enabling predictions that account for geometric and material non-linearities, strain-rate sensitivity, and elastic effects [80–85]. In particular,  $t_{\text{sat}}$  has been shown to depend on structural properties. In this work,  $t$  is estimated using the analytical expression for the saturated duration proposed by Bai et al. [77]. The approach refers to a square plate of width  $2L$ , fully clamped at its boundaries and subjected to a uniform pressure pulse of long duration as described in Appendix A (Fig. A.17(a)). The structural response is described through a modal approximation, in which the deformation mechanism remains fixed throughout the response. This technique originates from the works of Jones [86] and of Martin and Symonds [87]. In particular, the locations of plastic hinges are considered stationary in time, following a collapse mechanism consistent with that observed in quasi-static conditions. The modal solution is shown in Appendix A (Fig. A.17(b)). The material behaviour is modelled as rigid-perfectly plastic, neglecting elastic and strain-rate effects. These assumptions lead to the closed-form expression for the saturated duration [77],

$$t_{\text{sat}} = \frac{\pi}{\sqrt{6}} \cdot L \cdot \sqrt{\frac{\rho}{\sigma_y}}. \quad (19)$$

Eq. (19) incorporates the Taylor duration (Eq. (18)), up to a multiplicative constant. While the expressions for the Taylor and saturated durations are similar, they originate from different theoretical assumptions. The former is derived by considering the inward propagation of a plastic hinge front, whereas the latter neglects this transient behaviour. This difference, however, does not affect the applicability of the two closed-form expressions in the present context, as the objective of this work is to develop a practical and readily applicable criterion for estimating FSI effects.

The durations  $t_T$  and  $t_{\text{sat}}$  are obtained from rigid-perfectly plastic idealisations without strain-rate effects to enable rapid estimates. In

large-inelastic response this choice is generally conservative: the analytical transients are longer than the actual ones, and because the estimators scale with the transient time, the predicted FSI reductions are larger. For materials exhibiting pronounced work-hardening and/or high yield strength, or marked strain-rate sensitivity, these idealisations may lead to overly conservative estimates. This limitation is recognised and evaluated in Section 3.1.

In conclusion, the Taylor duration  $t_T$  and the saturated duration  $t_{\text{sat}}$  are used here only as characteristic time scales. Each is obtained from established closed-form analyses under stated assumptions and is then employed as an input to the dimensionless metrics, which combine  $\{t_T, t_{\text{sat}}\}$  with quantities extracted from the first phase of single uncoupled analysis to estimate FSI effects on the peak velocity and displacement. For clarity, Table 1 summarises the inputs, assumptions and outputs of the analytical time scales and compares their role with that of the present metrics.

In the second option of the proposed framework, a structural analysis — the second phase of an uncoupled simulation — is required. This phase allows for the evaluation of the time histories of velocity  $v_U$  and deflection  $w_U$  at the midpoint of the plate. Once these histories are known, the time interval  $t$  used for computing the dimensionless numbers  $\Delta v$ ,  $\Delta w$  can be identified from the peak values of velocity and deflection. These two conditions correspond to distinct instants, denoted as  $t_v$  and  $t_w$ , respectively.

From an analytical standpoint, selecting the time instant corresponding to the peak velocity or deflection is consistent with the integration procedure used in the analytical formulation, even though this choice is initially guided by the trends observed in the numerical simulations. It is important to recall that the two dimensionless numbers originate from the difference between either velocities or displacements, which are themselves derived by integrating acceleration or velocity over time, respectively. In reality, the time histories of these quantities — especially acceleration — are continuous, in contrast to the simplified loading scheme assumed in Fig. 3. The pressure is idealised as a rectangular pulse (Fig. 3) which, when introduced into Eq. (3), results in a piecewise-constant acceleration profile that suddenly drops to zero at the end of the loading duration. As a result of the continuity of the actual acceleration profile, to express the velocity analytically, one must integrate the acceleration over its positive phase, which defines  $t_v$ . Similarly, deflection is obtained by integrating velocity over its positive phase, which defines  $t_w$ . The definition of the two time intervals  $t_v$  and  $t_w$  is illustrated in Fig. 4, which shows qualitative, yet representative, acceleration, velocity, and deflection time histories derived from both coupled and uncoupled simulations. These charts are sketched to reflect the midpoint behaviour of a uniformly loaded, fully clamped plate, and are consistent with the results discussed in Section 3.

### 2.3. Two-option procedure

The framework developed in this study is structured into two distinct options, schematically illustrated in Fig. 5, and explained hereafter:

- Option 1: An uncoupled blast analysis (i.e., the first phase of an uncoupled simulation) is carried out to characterise the load in terms of reflected pressure  $p_r$  and density  $\rho_r$ , or any other pair of thermodynamic properties. Given the plate geometry (square shape with extension  $2L$  and thickness  $H$ ) and material properties (density  $\rho$  and static yield strength  $\sigma_y$ ), the estimation of the dimensionless indicators  $\Delta v$  and  $\Delta w$  is performed.
- Option 2: If the outcome of Option 1 is inconclusive, one may then proceed with Option 2. This may occur, for example, when the estimated indicators fall within an intermediate range or when a more accurate evaluation is required. In Option 2, a structural simulation is performed (i.e., the second phase of an uncoupled

**Table 1**

Comparison between the Taylor and saturated characteristic times and how they are employed in the present framework to build the error metrics.

| Item                             | Inputs & assumptions                                                                                                            | Output                      | Role in this work                                                             | Refs.      |
|----------------------------------|---------------------------------------------------------------------------------------------------------------------------------|-----------------------------|-------------------------------------------------------------------------------|------------|
| Taylor duration ( $t_T$ )        | Impulsive loading; rigid–perfectly plastic hinge propagation; no strain-rate; geometry and material ( $L, \sigma_y, \rho$ )     | $t_T$ (time scale)          | Input to the metric $\Delta v$                                                | [43,70,71] |
| Saturated duration ( $t_{sat}$ ) | Finite/long positive phase; rigid–perfectly plastic material with no strain-rate; geometry and material ( $L, \sigma_y, \rho$ ) | $t_{sat}$ (time scale)      | Input to the metric $\Delta w$                                                | [77]       |
| Present framework (Option 1)     | Uncoupled fluid quantities (e.g., $p_r, \rho_r$ ) combined with $\{t_T, t_{sat}\}$                                              | Dimensionless error metrics | Estimates of peak-level differences between uncoupled and coupled predictions | –          |

simulation) to extract the time histories of velocity and deflection at the plate centre. From these, the characteristic time instants  $t_v$  and  $t_w$ , corresponding to the peaks in velocity and deflection, are identified. These allow for a more accurate assessment of FSI effects.

The two options can thus be selected based on the desired level of accuracy. In many practical cases, the first option alone may already provide a clear indication of the magnitude of FSI effects and can therefore guide the choice between coupled and uncoupled numerical models. Furthermore, while the present framework is explicitly formulated for fully clamped square plates, the extensive literature on saturation analysis (briefly reviewed in Section 2.2) allows for a straightforward extension to plates of different shapes (e.g. rectangular) and with different boundary conditions. In particular, the same metrics apply upon choosing the characteristic travel length  $L$  and the saturated duration for the target geometry. In a fully clamped rectangle,  $L$  may be taken as  $\frac{1}{2} \min(a, b)$  (with  $a$  and  $b$  the side lengths); for a fully clamped circular plate,  $L = R$  (with  $R$  the radius). The corresponding  $t_{sat}$  should then be taken from the closed-form saturation analysis developed for that geometry (e.g., Ref. [77]). In addition, for simple cases, the uncoupled blast analysis can be replaced by the analytical solution of the reflected shock, as described in Ref. [88] and previously employed by Cloete and Nurick [42] or by using the empirical equations of Kingery and Bulmash [89].

#### 2.4. Case studies for framework assessment

The case studies considered in this work are based on a set of high-fidelity numerical simulations of fully clamped square plates subjected to blast loading, validated against experiments. All simulations, including the numerical models, boundary conditions, and results, are taken directly from Aune et al. [11]. No modifications are introduced. These data provide a reliable reference for assessing FSI effects. Validation is carried out in Ref. [11] against experiments conducted at the SIMLab Shock Tube Facility (SSTF) [90], but is briefly summarised here for completeness. Fig. 6 shows an overview of the SSTF and the corresponding numerical model used to generate the dataset in Refs. [11,48]. The facility is a compressed-gas driven shock tube that generates large stand-off blast-like loading conditions. At the end of the tube, a clamping assembly was used to mount the plates, resulting in a blast-exposed area of  $2L \times 2L = 300 \text{ mm} \times 300 \text{ mm}$ . The pressure profile acting on the specimens remains reasonably constant during the transient structural response [11], making this configuration ideal for evaluating the framework proposed in this work. The dataset includes square plates of different thicknesses made of steel and aluminium alloys, tested under different loading conditions obtained by varying the firing pressure in the driver section of the shock tube. Table 2 reports all the considered case studies, including material, geometric, and load properties. For each material, only the information required to apply the framework proposed in this work is provided. It should be noted that all materials were modelled accounting for strain hardening,

thermal softening, and strain-rate effects [11,48]. The value of  $\sigma_y$  in Table 2 therefore refers to the static yield strength. Further details can be found in Refs. [48,91–93], and are omitted for brevity. Furthermore, each load case is denoted by the label LC<sub>X</sub> or LC<sub>A</sub>Y, where LC indicates a general load case and A specifies that the configuration involves aluminium plates only. The index X corresponds to the absolute firing pressure, rounded down to the nearest multiple of 5 bar. In the case of aluminium configurations, the index Y is rounded down to the nearest multiple of 2.5 bar.

Part of the dataset was introduced in the study by Lomazzi et al. [48], which extended the configurations of Ref. [11] by including new materials and thicker plates. These cases were generated using the same numerical framework, ensuring full consistency. Although the newly introduced configurations were not directly validated against experiments, their coherence with the experimentally validated dataset supports their use in the present analysis.

The numerical simulations were performed using the Europlexus (EPX) code [94], employing explicit finite element methods. The plate was modelled with Reissner–Mindlin shell elements (Q4GS), using a Lagrangian formulation and an initial mesh size of 10 mm. These are 4-node quadrilateral elements with 6 degrees of freedom per node and 20 integration points (4 in-plane and 5 through the thickness). The clamping assembly was explicitly modelled using CUB8 solid elements in EPX.

Steel and aluminium plates were modelled using a modified Johnson–Cook constitutive model (VPJC in EPX), with material parameters taken from Ref. [48]. The fluid domain was discretised using cell-centred finite volumes (CCFV), which are well-suited for capturing the discontinuities generated by shock waves. This domain followed an Eulerian formulation, where the mesh remains fixed and the fluid flows through it.

As illustrated in Fig. 6, both 1D finite volumes (TUVF) and 3D finite volumes (CUVF) were used, coupled via TUBM junction elements. The base mesh size was 10 mm, except within the tank where the 3D mesh was radially coarsened up to 80 mm. Adaptive mesh refinement (AMR) was used to locally refine the fluid mesh near the plate to 5 mm resolution. More detailed information on the performance and capabilities of the numerical framework can be found in Ref. [11].

Both fully coupled and uncoupled simulations presented in Refs. [11,48] are considered in this work, allowing for an accurate quantification of FSI contributions and the extraction of reference values for the dimensionless parameters introduced in this study. In particular, Table 3 lists the fluid-dynamic parameters required to apply the framework. These include the pressure  $p_r$  and density  $\rho_r$  of the gas at the beginning of the reflection phase, as extracted from the fluid analysis of the uncoupled simulations. The acoustic impedance  $\rho_r c_r$  is also reported for completeness, as it is used to support the discussion of the results in Section 3.

The pressure histories acting on the plates for the various load cases are shown in Fig. 7 to provide additional clarity. These are obtained from uncoupled simulations and illustrate the long decay times that

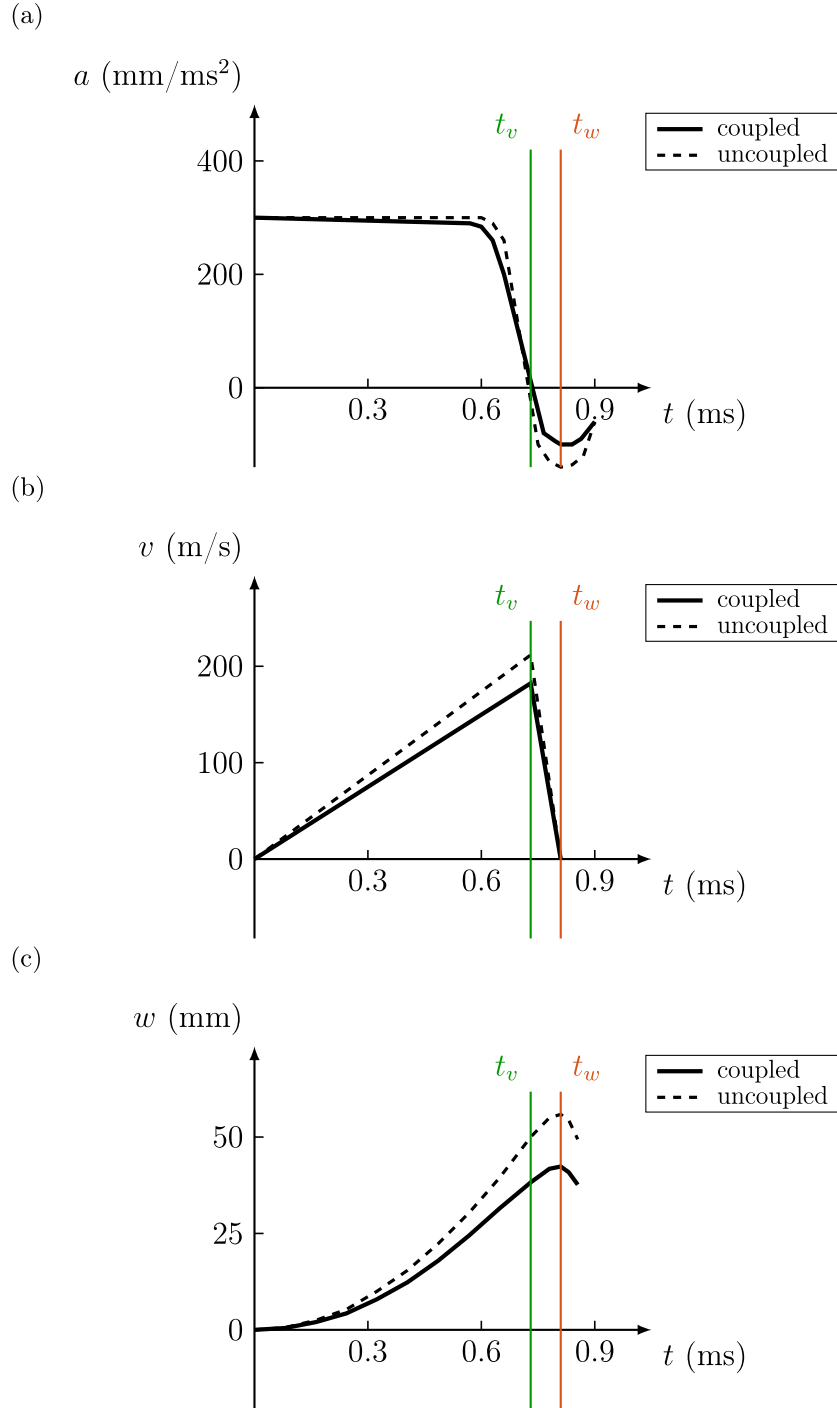


**Fig. 4.** Definition of the characteristic times used in the framework. Schematic time histories at the plate centre are shown for (a) acceleration  $a$  (mm/ms<sup>2</sup>), (b) velocity  $v$  (m/s), and (c) displacement  $w$  (mm); the horizontal axis is time  $t$  (ms). The vertical markers identify the reference instants: green  $t_v$  (time of peak velocity) and red  $t_w$  (time of peak displacement). Tick values are illustrative and included for readability; the curves are qualitative and not taken from a specific test case.

characterise the blast loads. For further details on the load cases, readers are referred to Refs. [11,48].

The combination of load cases, materials, and thicknesses results in a dataset of 28 configurations, in which FSI effects exhibit varying intensity. The proposed dimensional framework is evaluated against this dataset to assess its accuracy and predictive capability. Specifically, the results of the fully coupled and uncoupled simulations are used to calculate the reference values of the dimensionless parameters  $\Delta v$  and  $\Delta w$ , which define the target values that the proposed framework aims to predict. The uncoupled simulations alone are then employed to

extract the quantities required to estimate these parameters, following the two-option methodology introduced in Section 2.3.

In summary, the validation relies on structural numerical benchmarks whose coupled models have been validated against shock-tube experiments. Direct, time-resolved pressure fields on deforming plates are not available for the present configurations; hence the framework's blast-loading predictions (e.g., motion-induced pressure relief) cannot yet be verified by measurement. Future work may combine full-field pressure reconstruction on deforming plates with DIC-based material identification to test these predictions directly [95–97].

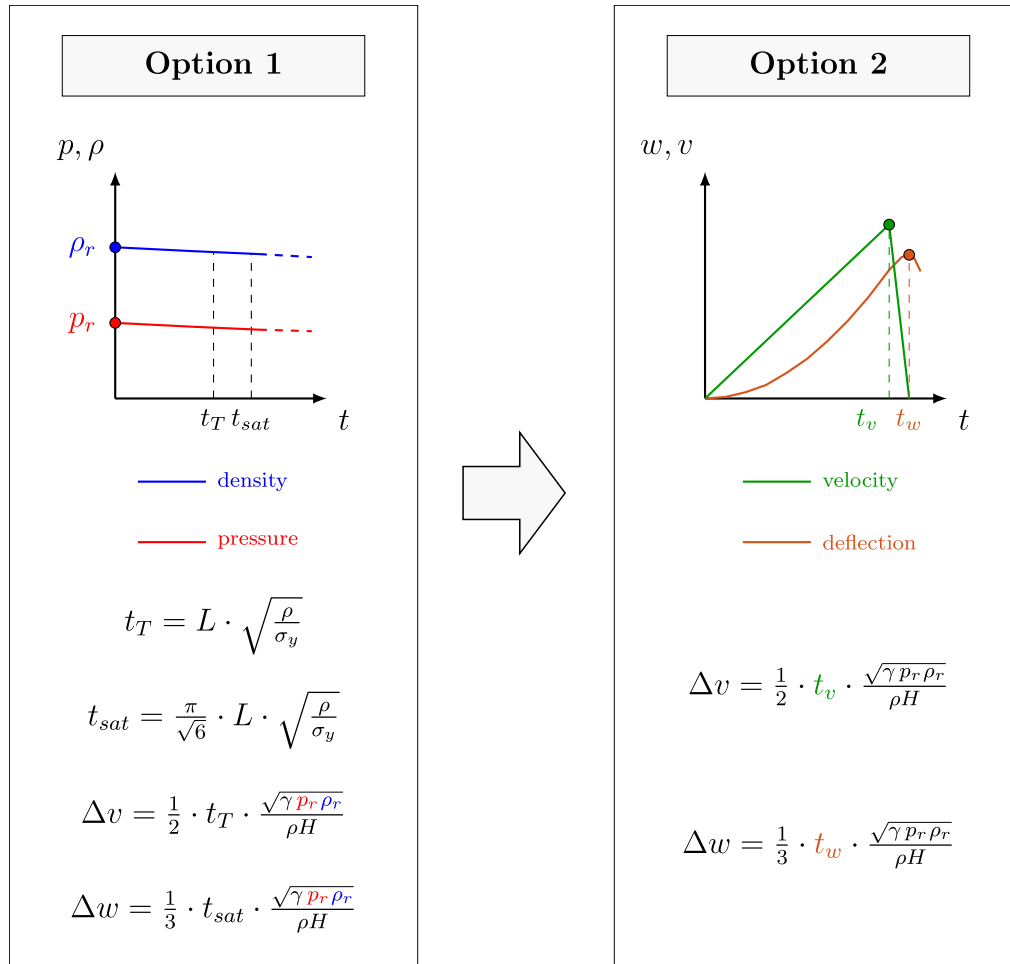


**Fig. 5.** Schematic of the two-option procedure for estimating FSI effects from uncoupled data. Option 1 uses the gas state at reflection onset ( $p_r, \rho_r$ ) to derive  $t_r$  and  $t_{sat}$ , then evaluates  $\Delta v$  and  $\Delta w$  via the dimensionless metrics. Option 2 uses uncoupled structural simulations to extract  $t_v$  and  $t_w$  and compute the metrics.

**Table 2**  
Tested configurations for the square blast-loaded plates.  
Source: Structural parameters taken from Ref. [48].

| Plate         |                             |                  |                | Load cases                                                 |
|---------------|-----------------------------|------------------|----------------|------------------------------------------------------------|
| Material      | $\rho$ (kg/m <sup>3</sup> ) | $\sigma_y$ (MPa) | Thickness (mm) |                                                            |
| AA1050-H14    | 2700                        | 80.0             | 0.8            | LC <sub>A</sub> 5, LC <sub>A</sub> 7.5, LC <sub>A</sub> 10 |
| AA6082-T6     | 2700                        | 291.8            | 0.8            | LC5, LC15, LC25, LC35, LC60                                |
| Docol 600 DL  | 7850                        | 325.7            | 0.8, 2.0       | LC5, LC15, LC25, LC35, LC60                                |
| Docol 1000 DP | 7850                        | 468.8            | 0.8            | LC5, LC15, LC25, LC35, LC60                                |
| Docol 1400 M  | 7850                        | 1114.8           | 0.8            | LC5, LC15, LC25, LC35, LC60                                |

**Table 3**  
Blast-loading parameters for each load case. The columns report the reflected peak pressure  $p_r$  (MPa), the reflected-gas density  $\rho_r$  (kg m<sup>-3</sup>), and the acoustic impedance  $\rho_r c_r$  (kg m<sup>-2</sup> ms<sup>-1</sup>) evaluated at the onset of the reflection phase. Source: All quantities are extracted from the uncoupled Eulerian simulations of Refs. [11,48].

| Load case           | $p_r$ (MPa) | $\rho_r$ (kg m <sup>-3</sup> ) | $\rho_r \cdot c_r$ (kg m <sup>-2</sup> ms <sup>-1</sup> ) |
|---------------------|-------------|--------------------------------|-----------------------------------------------------------|
| LC <sub>A</sub> 5   | 0.258       | 2.327                          | 0.916                                                     |
| LC <sub>A</sub> 7.5 | 0.301       | 2.568                          | 1.041                                                     |
| LC <sub>A</sub> 10  | 0.368       | 2.972                          | 1.237                                                     |
| LC5                 | 0.360       | 2.917                          | 1.213                                                     |
| LC15                | 0.711       | 4.589                          | 2.137                                                     |
| LC25                | 0.928       | 5.427                          | 2.656                                                     |
| LC35                | 1.230       | 6.405                          | 3.321                                                     |
| LC60                | 1.352       | 6.731                          | 3.570                                                     |

### 3. Results and discussion

The results are presented and discussed in Section 3.1, where the two-option procedure is applied to estimate the magnitude of FSI effects across all 28 scenarios of the dataset introduced in Section 2.4. Section 3.2 presents the applicability limits of the procedure with reference to two representative case studies. The same scenarios are then analysed in greater detail in Section 3.3 to further investigate the validity of the proposed framework.

#### 3.1. Full dataset

To apply the dimensionless framework proposed in this study, the pressure and density of the fluid at the beginning of the reflection phase, as well as the time  $t$ , are required. The two parameters  $p_r$  and  $\rho_r$  are obtained from the Eulerian simulation and have already been

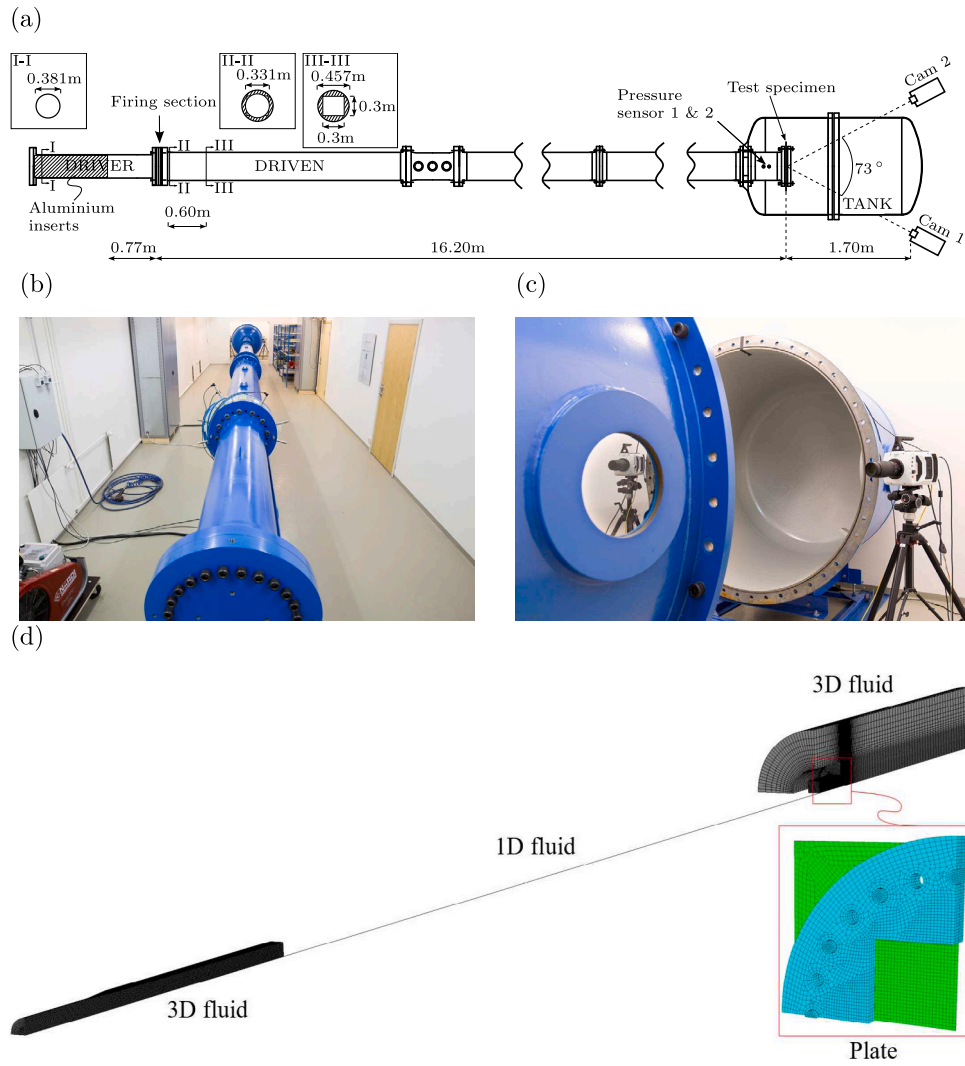


**Fig. 6.** The SIMLab Shock Tube Facility. (a) Schematic of the experimental setup, (b) overview image of the facility, (c) close-up image of the dump tank, and (d) corresponding numerical model.  
Source: Reprinted from Refs. [11,90].

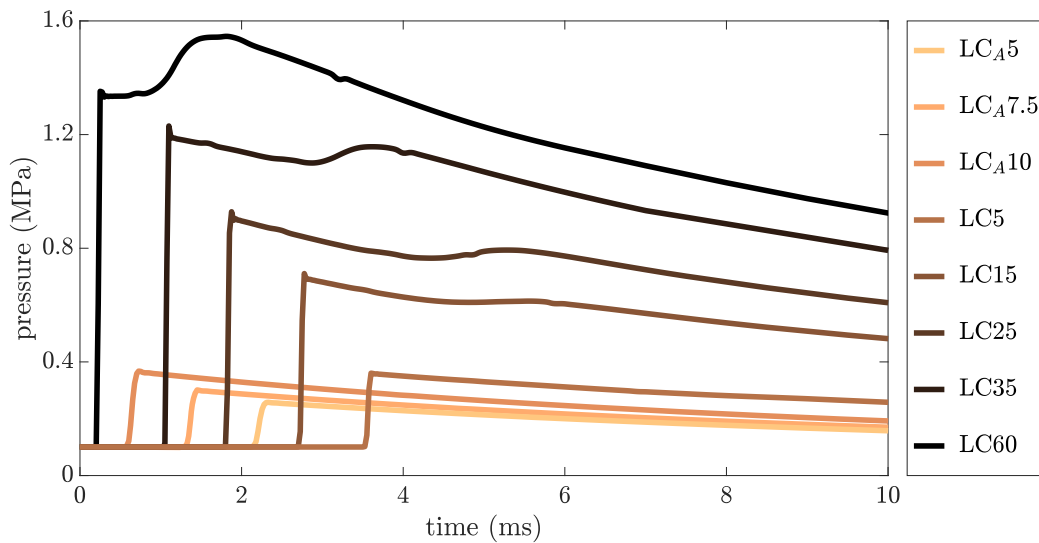


**Fig. 7.** Pressure-time histories at the plate location for all load cases (LC<sub>A</sub>5-LC60). Curves are taken from the uncoupled (fluid-only) simulations of Refs. [11,48], which impose a rigid-wall boundary and therefore do not depend on the plate material. For readability, each history has been time-shifted while preserving its shape.

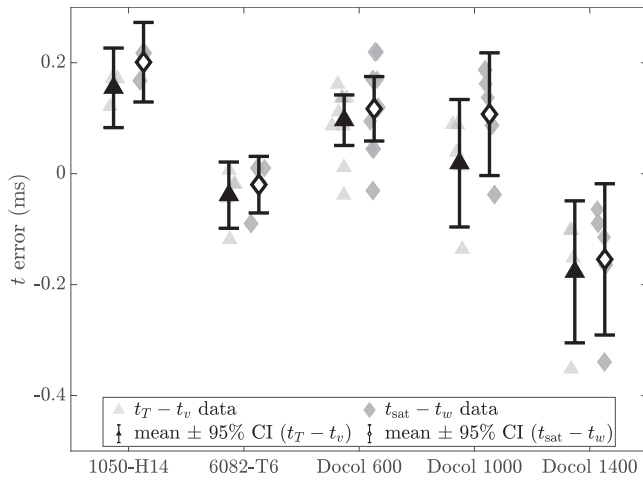


**Fig. 8.** Graphical summary of errors in characteristic durations: velocity transient  $t_T - t_v$  (triangles) and displacement transient  $t_{sat} - t_w$  (diamonds). Light grey symbols denote individual cases; black markers are material means with vertical whiskers showing the 95% confidence interval of the mean ( $\bar{\varepsilon} \pm t_{0.975, n-1} \varsigma / \sqrt{n}$ ). Errors are expressed in milliseconds.

**Table 4**

Characteristic times for the dimensionless framework:  $t_T$  and  $t_{sat}$  from analytical models;  $t_v$  and  $t_w$  from uncoupled structural simulations.

| Material      | Load case           | Thickness (mm) | $t_T$ (ms) | $t_{sat}$ (ms) | $t_v$ (ms) | $t_w$ (ms) |
|---------------|---------------------|----------------|------------|----------------|------------|------------|
| AA1050H14     | LC <sub>A</sub> 5   | 0.8            | 0.871      | 1.100          | 0.750      | 0.925      |
|               | LC <sub>A</sub> 7.5 | 0.8            | 0.871      | 1.100          | 0.700      | 0.900      |
|               | LC <sub>A</sub> 10  | 0.8            | 0.871      | 1.100          | 0.700      | 0.900      |
| AA6082T6      | LC5                 | 0.8            | 0.456      | 0.585          | 0.575      | 0.675      |
|               | LC15                | 0.8            | 0.456      | 0.585          | 0.500      | 0.600      |
|               | LC25                | 0.8            | 0.456      | 0.585          | 0.475      | 0.575      |
|               | LC35                | 0.8            | 0.456      | 0.585          | 0.475      | 0.600      |
|               | LC60                | 0.8            | 0.456      | 0.585          | 0.450      | 0.575      |
|               | LC60                | 0.8            | 0.456      | 0.585          | 0.450      | 0.575      |
| Docol 600 DL  | LC5                 | 0.8            | 0.736      | 0.944          | 0.775      | 0.900      |
|               | LC15                | 0.8            | 0.736      | 0.944          | 0.650      | 0.775      |
|               | LC25                | 0.8            | 0.736      | 0.944          | 0.650      | 0.775      |
|               | LC35                | 0.8            | 0.736      | 0.944          | 0.600      | 0.725      |
|               | LC60                | 0.8            | 0.736      | 0.944          | 0.600      | 0.725      |
|               | LC5                 | 2.0            | 0.736      | 0.944          | 0.725      | 0.975      |
|               | LC15                | 2.0            | 0.736      | 0.944          | 0.625      | 0.900      |
|               | LC25                | 2.0            | 0.736      | 0.944          | 0.575      | 0.850      |
|               | LC35                | 2.0            | 0.736      | 0.944          | 0.600      | 0.825      |
|               | LC60                | 2.0            | 0.736      | 0.944          | 0.600      | 0.825      |
| Docol 1000 DP | LC5                 | 0.8            | 0.614      | 0.787          | 0.750      | 0.825      |
|               | LC15                | 0.8            | 0.614      | 0.787          | 0.600      | 0.700      |
|               | LC25                | 0.8            | 0.614      | 0.787          | 0.575      | 0.650      |
|               | LC35                | 0.8            | 0.614      | 0.787          | 0.525      | 0.625      |
|               | LC60                | 0.8            | 0.614      | 0.787          | 0.525      | 0.600      |
| Docol 1400 M  | LC5                 | 0.8            | 0.398      | 0.510          | 0.750      | 0.850      |
|               | LC15                | 0.8            | 0.398      | 0.510          | 0.575      | 0.675      |
|               | LC25                | 0.8            | 0.398      | 0.510          | 0.550      | 0.625      |
|               | LC35                | 0.8            | 0.398      | 0.510          | 0.500      | 0.575      |
|               | LC60                | 0.8            | 0.398      | 0.510          | 0.500      | 0.600      |

reported, for each load case, in Table 3. The time parameter  $t$  is either computed via the Taylor duration  $t_T$  and the saturation duration  $t_{sat}$  (Option 1), or directly extracted from the time histories of velocity  $t_v$  and displacement  $t_w$  at the centre of the plate (Option 2), as obtained from the uncoupled structural simulations (see Fig. 5). As a result, four characteristic time instants are defined for each case study in the dataset.

Table 4 reports the values of  $t_T$ ,  $t_{sat}$ ,  $t_v$ , and  $t_w$  for all 28 scenarios. The time resolution of the numerical results is 0.025 ms. An overview by material is provided in Fig. 8, which plots the errors  $t_T - t_v$  and  $t_{sat} - t_w$  with means and 95% confidence intervals of the mean. These

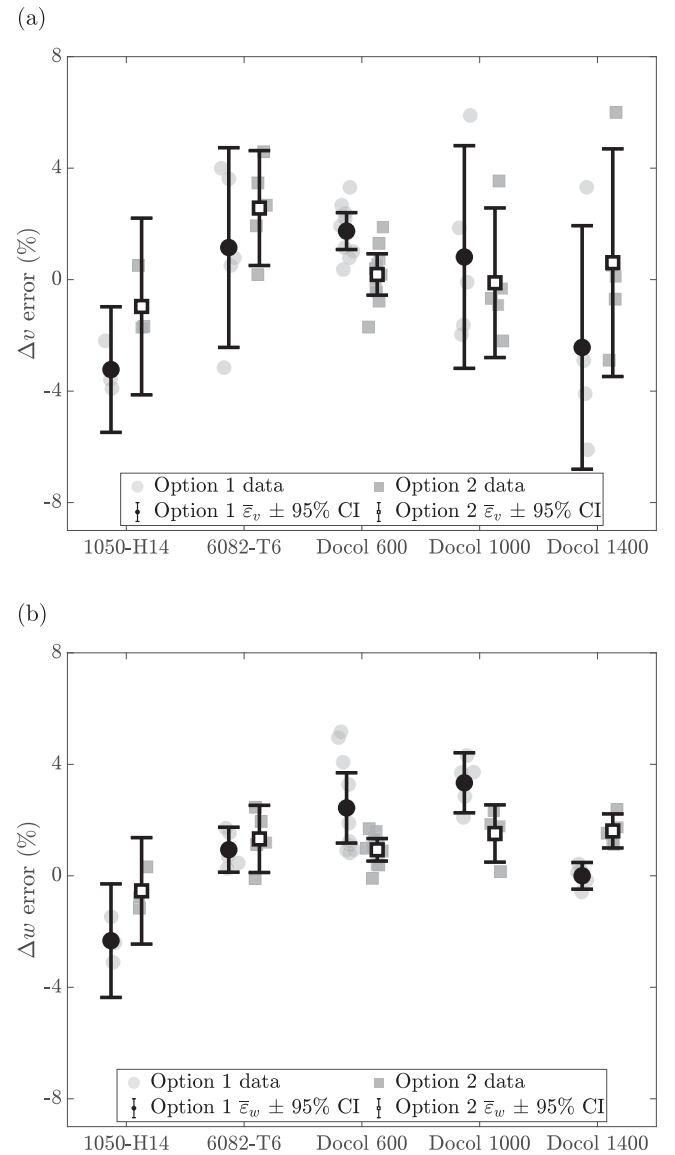


**Fig. 9.** Graphical summary of accuracy by material: (a) error in peak-velocity reduction,  $\Delta v$ , and (b) error in peak-displacement reduction,  $\Delta w$ . Light symbols are individual-case errors (Option 1: circles; Option 2: squares). Black markers report the mean error  $\bar{\varepsilon}_x$ , with vertical whiskers showing the 95% confidence interval of the mean ( $\bar{\varepsilon}_x \pm t_{0.975, n-1} \varsigma_x / \sqrt{n}$ ); filled circles correspond to Option 1 and open squares to Option 2. Errors are (estimate – numerical) in percent.

are computed as  $\bar{\varepsilon} \pm t_{0.975, n-1} \varsigma / \sqrt{n}$ , where  $\bar{\varepsilon}$  is the material mean error,  $\varsigma$  the sample standard deviation within the material,  $n$  the number of cases in that material, and  $t_{0.975, n-1}$  the 97.5th quantile of Student's  $t$  distribution with  $n - 1$  degrees of freedom. Joint inspection of Table 4 and Fig. 8 enables the evaluation of the reliability of the closed-form solution provided by the Taylor duration (Ref. [70]) and the saturation analysis of Ref. [77]. They indicate that the closed-form solutions provide a conservative yet reasonable estimate of the transient phase of the structural response. This is observed for all scenarios, with the exception of those involving Docol 1400 plates, where both the Taylor duration and the saturation time slightly underestimate the actual transient. A possible explanation for this discrepancy lies in the elastic response of the plate, which is neglected in the closed-form formulations adopted in this work, as they assume a rigid-perfectly plastic material behaviour. The Docol 1400 steel exhibits a significantly higher static yield strength compared to the other materials (see Table

2), making this result reasonable. To further support this hypothesis, one may refer to the findings of Hu et al. [85] and Huang et al. [98], who investigated the differences between elastic-perfectly plastic and rigid-perfectly plastic models in blast-loaded plates. Their studies adopt the energy ratio  $R$ , defined as the ratio between the plastic energy dissipation and the maximum elastic strain energy that the structure can store. They showed that elastic effects are not negligible when  $R$  falls below a threshold value of approximately 5. Applying their analytical formulation for square plates to the present dataset yields  $R < 5$ , or values close to this threshold, in all Docol 1400 scenarios. As a result, the influence of elasticity cannot be neglected and likely contributes to the longer transient phase observed in Table 4 and the negative mean errors in Fig. 8. A similar argument can be made for the Docol 1000 plate under load case LC5. Nevertheless, the resulting approximation is still considered acceptable for the purpose of a first-order estimation.

The conservative nature of the estimate is expected, as the definition of  $t_T$  and  $t_{sat}$  is based on a simplified model that neglects several effects observed both numerically and experimentally, such as strain-rate sensitivity and work-hardening. By contrast, the numerically obtained durations  $t_v$  and  $t_w$  do incorporate these effects through the Johnson–Cook material law. This choice is intentional and aligns with the aim of providing a first estimate of FSI effects, without relying on complex models. Indeed, more advanced formulations for the saturation duration could have been used, but none would have matched the objective of a fast estimation based on a small set of structural parameters known a priori. Developing simple, closed-form corrections that embed these effects is identified as a direction for future work.

It is worth noting that, starting from the saturation analysis presented by Bai et al. [77], it can be shown that the analytical velocity peak occurs at  $t_{sat}/2$ . However, adopting this value would disregard the motion of the plastic hinges, resulting in an underestimation of the transient duration of the velocity  $t_v$ , as is evident from Table 4. For this reason, the Taylor duration  $t_T$  is retained for velocity.

The data reported in Tables 3 and 4 are used to apply the two-option procedure. The results are presented in Table 5 for all case studies in the dataset. For each material, thickness, and load case, the estimated values of the parameters  $\Delta v$  and  $\Delta w$  are shown according to both options of the framework, allowing direct comparison with the values obtained from coupled numerical simulations. Figs. 9(a)–9(b) summarise, by material, the errors in the predicted reductions  $\Delta v$  and  $\Delta w$ . The errors are computed as  $\Delta v_i^{est} - \Delta v_i^{num}$  and  $\Delta w_i^{est} - \Delta w_i^{num}$ , where *est* and *num* indicate the estimated and the numerical value. Light symbols indicate individual cases; black markers denote the material mean, with vertical bands giving the 95% confidence interval of the mean, computed as  $\bar{\varepsilon} \pm t_{0.975, n-1} \varsigma / \sqrt{n}$ , as in Fig. 8. The notation and quantile definition follow that figure. The panels show that Option 2 generally yields means closer to zero with narrower intervals, while Option 1 already performs well, particularly for  $\Delta w$ . These observations are consistent with the aggregate statistics of Table 6, which reports the average error  $\bar{\varepsilon}_x$  and the standard deviation  $\varsigma_x$  of the estimates, computed as,

$$\bar{\varepsilon}_x = \frac{1}{N} \sum_{i=1}^N (\Delta x_i^{est} - \Delta x_i^{num}), \quad (20)$$

$$\varsigma_x = \sqrt{\frac{1}{N} \sum_{i=1}^N [(\Delta x_i^{est} - \Delta x_i^{num}) - \bar{\varepsilon}_x]^2}, \quad (21)$$

where  $N = 28$ , and  $x_i$  refers to either velocity  $v$  or displacement  $w$ .

Overall, the procedure demonstrates robust accuracy for both options; FSI effects are consistently captured across variations in thickness, yield strength, and blast intensity. Specifically, the proposed framework predicts increasing FSI effects for lower areal mass ( $\rho H$ ) at increasing load intensities, with the predictions of option 2 showing the best agreement with numerical benchmarks.

For the velocity metric  $\Delta v$ , the mean error is already very low at option 1 and slightly increases with option 2, while becoming more

consistent due to the reduction in standard deviation. Although this metric is less interpretable than its displacement-based counterpart, it proves to be effective in capturing the magnitude of FSI effects.

Turning to the displacement metric  $\Delta w$ , option 1 already provides a high level of accuracy and reflects the essence of the practical and readily applicable criterion proposed in this work. The interpretability makes it a valuable indicator for distinguishing between scenarios where FSI effects are either negligible or significant. A substantial improvement in accuracy is then achieved with option 2, thus supporting the use of structural simulations when a more precise estimate is required.

In addition, the discrepancies observed in the characteristic times for Docol 1400 plates (see Table 4 and Fig. 8) are reflected in the final estimates, particularly for velocities. In these cases, the accuracy is slightly reduced, although the deviation remains acceptable for engineering applications.

Finally, sources of uncertainty and their expected influence are briefly discussed. The metrics inherit the uncertainty of their inputs. For Option 1, the estimates depend monotonically on the reflected peak level as well as on  $t_T$  and  $t_{sat}$ ; overestimation leads to conservative predictions of FSI effects, whereas underestimation reduces them. Option 2 additionally relies on a structural analysis; predictions are typically sharper but more sensitive to input uncertainties. As a result, variability in thickness, yield strength and areal mass, together with uncertainty in the duration of velocity and displacement transients, can shift the predicted peaks and increase the variability of the estimates.

Figs. 10(a) and 10(b) introduce an additional graphical assessment of the estimates, quantifying precision and revealing the expected global trend across the dataset. The first of these two plots refers to option 1 of the framework. Specifically, it displays all numerical results in terms of the reduction in peak mid-point velocity and displacement. These values are plotted against the acoustic impedance ratio between the fluid and the plate, i.e., the dimensionless parameter  $t/\psi$ . Since this corresponds to option 1, time  $t$  is defined either as the Taylor duration  $t_T$  or the saturation time  $t_{sat}$ . The numerical results (dots) are compared with the analytical predictions, which are represented by straight lines with slopes of 1/2 and 1/3 for velocity and displacement, respectively. These lines correspond to the dimensionless metric proposed in Eqs. (13) and (16). Since the velocity and displacement reductions  $\Delta v$ ,  $\Delta w$  are expressed in percentage terms, the slopes are also defined accordingly. The coefficients of determination are  $R^2 = 0.8699$  and  $R^2 = 0.8823$ , respectively. They indicate that a reasonable estimate of FSI effects can be achieved using only a limited number of parameters, thus confirming the predictive capability of option 1. In particular, the plot highlights the conservative nature of the displacement reduction estimates. This outcome is fully aligned with the intended goal of option 1, confirming its value as a practical tool for engineering applications. The estimate of the velocity reduction is also conservative on average (see Table 6), although the trend is less pronounced compared to that observed for  $\Delta w$ . Specifically, for materials with higher yield strength, the estimated values tend to slightly underestimate the corresponding numerical results.

Fig. 10(b) addresses the second option of the procedure. It shows all numerical results in terms of the reduction in peak mid-point velocities and displacements, plotted against the acoustic impedance ratio between the fluid and the plate. In this case, the time  $t$  is selected based on the specific transient being considered:  $t_v$  for velocity and  $t_w$  for displacement. The numerical data are compared with two straight lines representing the analytical estimates from option 2, corresponding to slopes of 1/2 and 1/3 (expressed in percentage terms) for velocity and displacement, respectively. These lines correspond to the dimensionless metrics for velocity and displacement presented in Eqs. (13) and (16). The agreement is satisfactory, particularly for the displacements, as also confirmed by the corresponding  $R^2$  values. The results highlight the accuracy and robustness of the proposed framework.

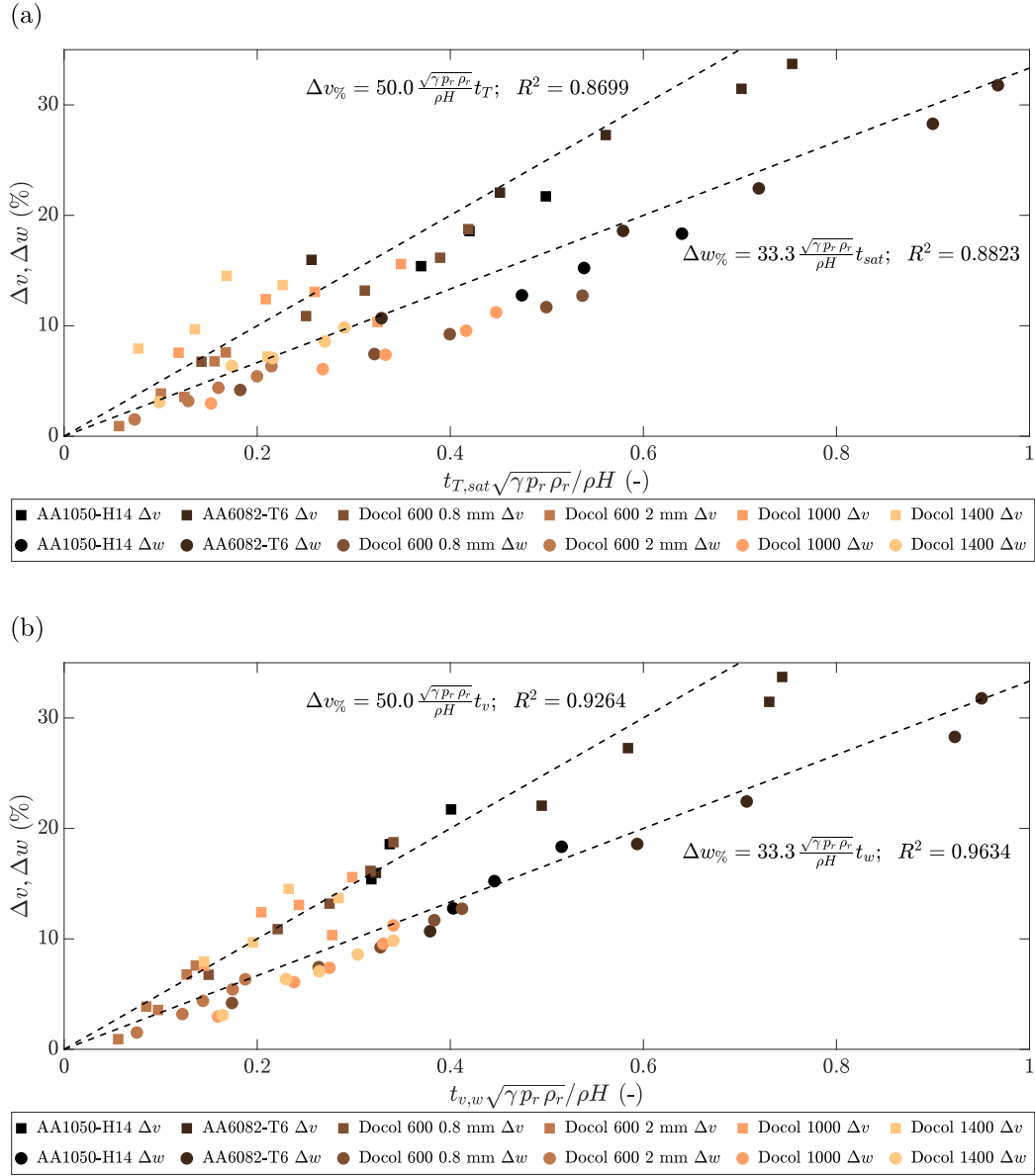


**Fig. 10.** Assessment of the predictive trends for the two options of the framework. Panels (a)–(b) plot, for the 28 case studies, the percent reductions in peak response due to FSI, namely  $\Delta v$  (circles) and  $\Delta w$  (squares), against the dimensionless abscissa  $t\sqrt{\gamma p_r \rho_r} / (\rho H)$ , with  $t$  chosen as follows: in (a) (Option 1)  $t = t_T$  for  $\Delta v$  and  $t = t_{sat}$  for  $\Delta w$  (closed-form times); in (b) (Option 2)  $t = t_v$  for  $\Delta v$  and  $t = t_w$  for  $\Delta w$  (times extracted from uncoupled structural histories). Dashed lines show the model predictions with fixed slopes 1/2 (velocity) and 1/3 (displacement). The reported  $R^2$  values quantify the agreement.

To conclude, it is worth highlighting the strong physical significance of the proposed estimates. Indeed, they are closely related to quantities already recognised as key parameters in FSI. To support the physical interpretation of the estimates, Fig. 11 shows the trend of  $\Delta v$  and  $\Delta w$  as a function of the blast load intensity, considering all case studies in the dataset sharing the same load cases (LC5 ÷ LC60). In particular, the load cases have been spaced along the horizontal axis according to the trend of the gas acoustic impedance (see Table 3). As previously discussed in Section 2.1.1, FSI effects primarily depend on the areal mass of the plate,  $\rho H$ , and the acoustic impedance of the gas at the beginning of the reflection state,  $\rho_r c_r$ . Consequently, for a fixed material and plate thickness, an increase in the driver pressure in the shock tube leads to higher gas impedance and, therefore, to stronger FSI effects. According to the estimates, the relationship between FSI and acoustic impedance is linear, as shown in Fig. 11. This proportional trend is also reasonably reflected in the numerical results. The figure also highlights the role of areal mass, as observed when comparing results for aluminium

and steel plates. Finally, the duration of the structural response also influences FSI effects, although to a lesser extent. Nevertheless, the figures show that, for a given load case (that is, for a fixed  $\rho_r c_r$ ) and areal mass, a longer structural response duration is associated with more pronounced FSI effects. Once again, the main drivers of these phenomena are confirmed to be gas compressibility and the areal mass of the plates.

### 3.2. Limits of applicability

The limits of applicability of the framework are discussed with reference to two representative scenarios selected from the dataset introduced in Section 2.4. These are (i) an aluminium plate made of 6082-T6 with a thickness of 0.8 mm subjected to the blast load from load case LC60, and (ii) a plate made of Docol600 with a thickness of 2 mm subjected to the blast load from load case LC25. These two configurations are identified as representative of significant and almost negligible FSI effects, as shown in Table 5.

**Table 5**

Comparison of FSI-induced peak-reduction metrics by case. For each material, load case (LC), and thickness, the percentage reductions in peak velocity and displacement are listed:  $\Delta v$  and  $\Delta w$  are defined as  $100 \cdot (x_U - x)/x_U$  (%) with  $x$  being either  $v$  or  $w$ . Option 1 reports the estimates using the closed-form times  $t_T$  and  $t_{sat}$ ; Option 2 uses the durations  $t_v$  and  $t_w$  extracted from the uncoupled structural step; num. gives the reference coupled-simulation values.

| Material      | Load case           | Thickness (mm) | $\Delta v$ (%) |          |       | $\Delta w$ (%) |          |       |
|---------------|---------------------|----------------|----------------|----------|-------|----------------|----------|-------|
|               |                     |                | Option 1       | Option 2 | num.  | Option 1       | Option 2 | num.  |
| AA1050-H14    | LC <sub>A</sub> 5   | 0.8            | 13.20          | 15.91    | 15.40 | 11.29          | 13.08    | 12.76 |
|               | LC <sub>A</sub> 7.5 | 0.8            | 15.00          | 16.86    | 18.58 | 12.82          | 14.45    | 15.23 |
|               | LC <sub>A</sub> 10  | 0.8            | 17.82          | 20.04    | 21.72 | 15.24          | 17.18    | 18.34 |
| AA6082-T6     | LC5                 | 0.8            | 12.81          | 16.15    | 15.97 | 10.96          | 12.64    | 10.69 |
|               | LC15                | 0.8            | 22.57          | 24.73    | 22.06 | 19.30          | 19.78    | 18.59 |
|               | LC25                | 0.8            | 28.05          | 29.20    | 27.27 | 23.99          | 23.56    | 22.44 |
|               | LC35                | 0.8            | 35.08          | 36.05    | 31.46 | 29.99          | 30.75    | 28.29 |
|               | LC60                | 0.8            | 37.71          | 37.18    | 33.71 | 32.40          | 31.68    | 31.78 |
| Docol 600 DL  | LC5                 | 0.8            | 7.11           | 7.48     | 6.75  | 6.08           | 5.80     | 4.19  |
|               | LC15                | 0.8            | 12.53          | 11.06    | 10.88 | 10.71          | 8.79     | 7.44  |
|               | LC25                | 0.8            | 15.57          | 13.75    | 13.19 | 13.31          | 10.93    | 9.24  |
|               | LC35                | 0.8            | 19.47          | 15.87    | 16.16 | 16.65          | 12.78    | 11.69 |
|               | LC60                | 0.8            | 20.93          | 17.05    | 18.75 | 17.90          | 13.74    | 12.73 |
|               | LC5                 | 2.0            | 2.85           | 2.80     | 0.92  | 2.43           | 2.51     | 1.52  |
|               | LC15                | 2.0            | 5.01           | 4.26     | 3.87  | 4.29           | 4.08     | 3.19  |
|               | LC25                | 2.0            | 6.23           | 4.86     | 3.56  | 5.33           | 4.79     | 4.39  |
|               | LC35                | 2.0            | 7.79           | 6.35     | 6.78  | 6.66           | 5.82     | 5.43  |
|               | LC60                | 2.0            | 8.37           | 6.82     | 7.59  | 7.16           | 6.25     | 6.34  |
| Docol 1000 DP | LC5                 | 0.8            | 5.93           | 7.24     | 7.56  | 5.07           | 5.31     | 2.97  |
|               | LC15                | 0.8            | 10.44          | 10.21    | 12.41 | 8.93           | 7.94     | 6.08  |
|               | LC25                | 0.8            | 12.98          | 12.16    | 13.07 | 11.10          | 9.16     | 7.38  |
|               | LC35                | 0.8            | 16.23          | 13.88    | 10.34 | 13.88          | 11.02    | 9.56  |
|               | LC60                | 0.8            | 17.45          | 14.92    | 15.59 | 14.92          | 11.37    | 11.22 |
| Docol 1400 M  | LC5                 | 0.8            | 3.84           | 7.24     | 7.94  | 3.29           | 5.47     | 3.09  |
|               | LC15                | 0.8            | 6.77           | 9.79     | 9.68  | 5.79           | 7.66     | 6.37  |
|               | LC25                | 0.8            | 8.42           | 11.63    | 14.52 | 7.20           | 8.81     | 7.07  |
|               | LC35                | 0.8            | 10.53          | 13.21    | 7.21  | 9.00           | 9.69     | 8.58  |
|               | LC60                | 0.8            | 11.31          | 14.21    | 13.69 | 9.67           | 11.37    | 9.84  |

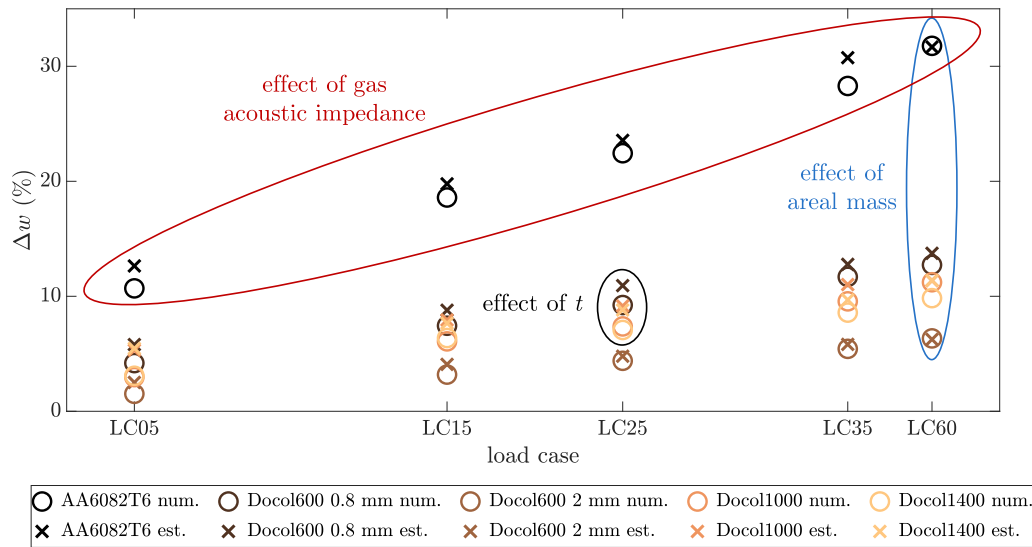


**Fig. 11.** Comparison between estimated and numerical  $\Delta w$  across load cases (LC05–LC60). The ordinate reports  $\Delta w$  (%); open vs. filled symbols denote estimates vs. numerical results, and colours identify plate configurations. Elliptical annotations indicate qualitative drivers: increasing gas acoustic impedance with load case (red), areal-mass effects at fixed load (blue), and the influence of the structural transient duration  $t$  (black).

The response of the blast-loaded plates included in the dataset is characterised by damage mode I, that is, large inelastic deformation [99–101]. In all cases, a positive permanent deflection is observed. The dimensionless framework proposed in this work originates from Eq. (10) and is therefore purely inertial, i.e., it neglects any contribution from structural stiffness or damping. As already noted in Section 2.1, the validity of the dimensionless parameters is limited to the transient phase in which the plastic hinges that form at the boundary propagate

towards the central region. As long as this condition holds, the central area remains reasonably rigid [69] and can be accurately described using the 1D expansion wave approach. To support this interpretation, Figs. 12(a) and 12(b) show the time evolution of the deformed shape for the two case studies discussed above. These deformed shapes are extracted from the coupled simulations of Refs. [11,48]. They are evaluated along the diagonal of the plate and expressed in terms of out-of-plane deflection, providing a two-dimensional (2D) representation

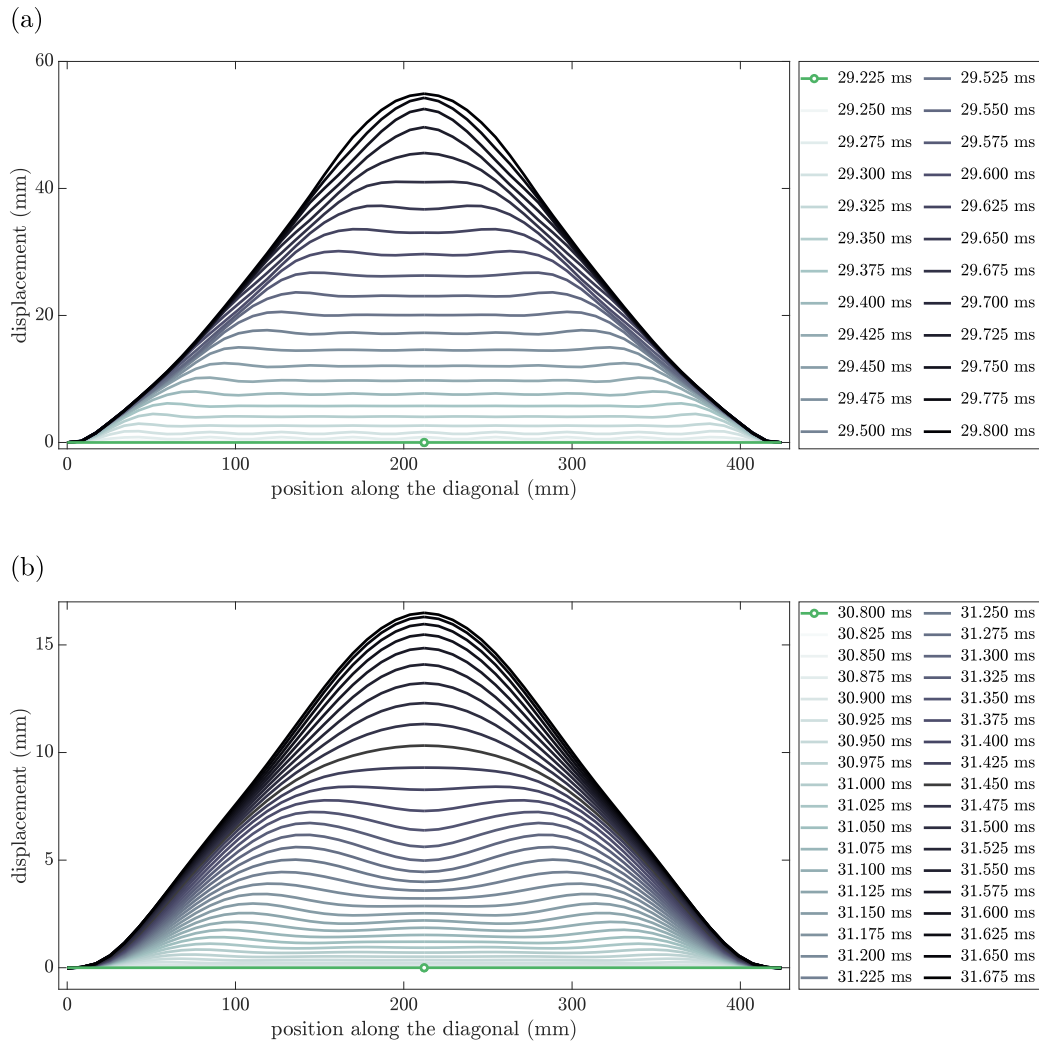


**Fig. 12.** Evolution of the deformed shape along the plate diagonal for two representative load cases: (a) AA6082-T6, 0.8 mm, LC60 — impact at 29.225 ms; (b) Docol600, 2 mm, LC25 — impact at 30.8 ms. Each curve reports the out-of-plane displacement profile at successive instants after impact. The abscissa is the position from corner to corner along the diagonal. The green trace is the undeformed reference, defined at the impact time, i.e., when the shock front first impinges on the plate.

**Table 6**

Mean error and standard deviation of  $\Delta v$  and  $\Delta w$  estimates for option 1 and option 2 of the proposed framework.

| Index                            | $\Delta v$ (%) |          | $\Delta w$ (%) |          |
|----------------------------------|----------------|----------|----------------|----------|
|                                  | Option 1       | Option 2 | Option 1       | Option 2 |
| Mean error $\bar{\epsilon}_x$    | +0.19          | +0.51    | +1.39          | +1.10    |
| Standard deviation $\varsigma_x$ | 2.94           | 2.08     | 2.08           | 0.87     |

of the structural response over time. The diagonal is considered to investigate the motion of the plastic hinges. Figs. 13 and 14 provide further insight into the deformed shapes and the propagation of the yield lines. In these figures, the time evolution of the mid-point deflection is compared with the motion of the plastic hinges. The latter is manually evaluated based on the curvature of the deformed plate profile. For completeness, the position of the plate centre along the diagonal is reported in the figures. It should be noted that the motion of the plastic hinges is shown only up to the point where it could be clearly identified. The final time instant displayed can therefore be regarded as a representative indicator of the strict validity limit of the dimensionless framework. To aid the interpretation, each figure is paired with six snapshots of the 3D deformed shape.

A central rigid region, behaving similarly to a free-standing plate, clearly emerges in the first scenario. Its extent is progressively reduced by the propagation of the yield lines, which can be considered reasonably completed around the time instant of 29.7 ms. This instant marks the temporal validity limit of the 1D inertial framework introduced in this work. Beyond this point, the response of the plate centre can no longer be accurately described. However, the time  $t_w$  (or  $t_{sat}$ ) used to estimate the FSI effects on displacements is defined based on the duration of the transient phase of the structural response. As visible in Figs. 12(a) and 13, this transient phase ends at 29.8 ms. In other words, the influence of FSI effects is evaluated by extrapolating the results beyond the validity range of the framework. This extrapolation, however, leads to conservative estimates, which are consistent with the objective of the present study.

A global behaviour, similar to that just described above, is also observed in Fig. 12(b) for the other representative scenario, although the deformed shape appears more complex. While plastic deformation initiates near the edges and the yield lines propagate towards the centre, the central region exhibits a slower response compared to the other case study due to the higher areal mass. As a result, the edges of the plate move out-of-plane more rapidly than the centre, temporarily generating a relative inward dome in the central region. These dynamic effects complicate the identification of the propagation

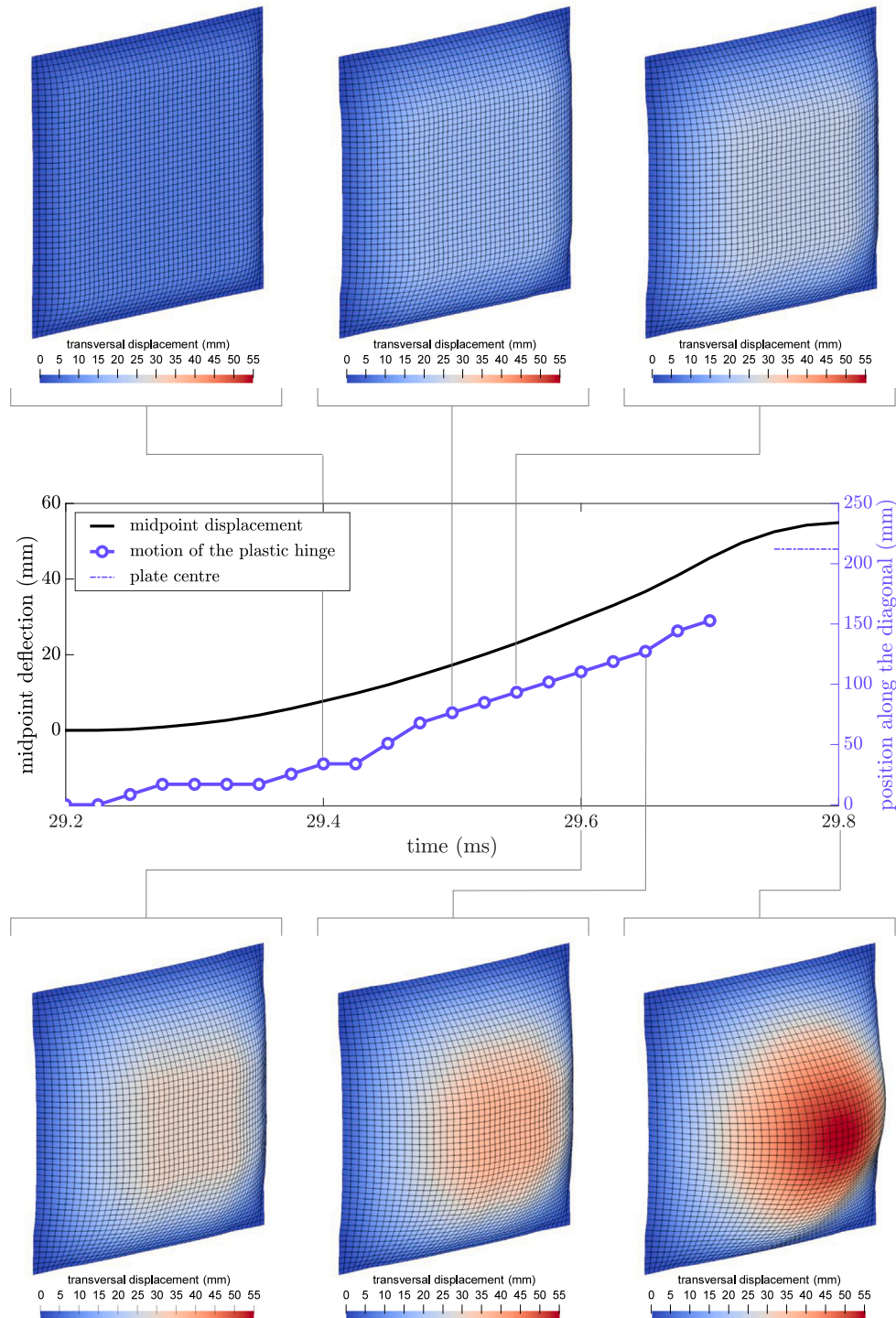


**Fig. 13.** Mid-point deflection and plastic hinge motion for a 0.8 mm-thick plate made of 6082-T6 subjected to the blast load from load case LC60 (a magnification factor of 1.5 is considered to visualise the 3D deformed shapes). The impact time is 29.225 ms.

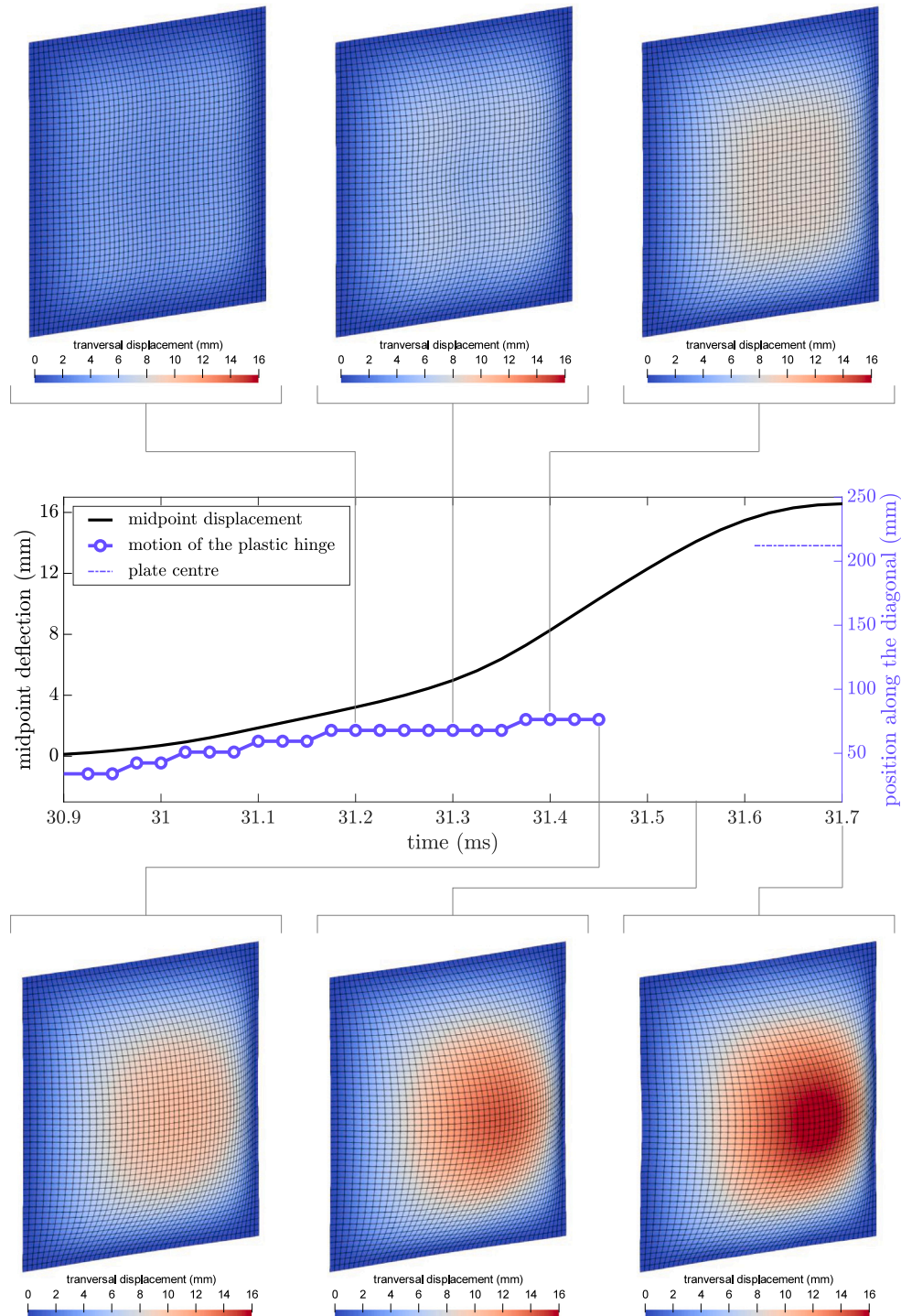


**Fig. 14.** Mid-point deflection and plastic hinge motion for a 2 mm-thick plate made of Docol600 subjected to the blast load from load case LC25 (a magnification factor of 5 is used to visualise the 3D deformed shapes). The impact time is 30.8 ms.

of yield lines towards the centre. Nevertheless, it is evident that the inertial framework can be considered valid for this configuration up to approximately 31.45 ms. The time considered for the estimation of FSI effects is instead defined as 31.7 ms, once again resulting in a conservative extrapolation beyond the validity range of the framework.

### 3.3. Detailed analysis

This section provides a more in-depth examination of the proposed framework by revisiting the same scenarios discussed in Section 3.2. The aim is to offer additional insight into the framework's validity, as the results presented in Section 3.1 are based on instantaneous quantities — specifically, the differences between two analytical approaches (coupled and uncoupled). While the framework has demonstrated high accuracy (see Table 6), these results alone do not fully establish the reliability of the underlying analytical models.

Although this detailed investigation may not be essential for readers primarily interested in applying the two-option procedure introduced in this work, it is included here to reinforce the overall robustness and credibility of the proposed framework.

#### 3.3.1. 0.8 mm 6082-T6 plate

For an aluminium plate made of 6082-T6 with a thickness of 0.8 mm subjected to the blast load from load case LC60, FSI effects are significant, resulting in an FSI efficiency  $\Delta w$  of 31.78% and a velocity reduction  $\Delta v$  of 33.71%. These values are obtained from algebraic differences between displacements or velocities calculated through coupled and uncoupled analytical approaches, as detailed in Fig. 15. Specifically, Fig. 15(a) shows the pressure and density profiles obtained from the uncoupled Eulerian simulation (with the coupled pressure profile also reported for comparison). The figure illustrates how the pressure  $p_r$  and density  $\rho_r$  are extracted. Additionally, the plots highlight the constancy of the uncoupled blast load over the saturated duration interval. Fig. 15(b) displays the structural response in terms of out-of-plane velocity and displacement at the centre of the plate. For each plot, the numerical results from the coupled and uncoupled simulations of Ref. [48] are shown, together with the closed-form solutions obtained from the corresponding analytical approaches proposed here.

One should note that the pressure profiles exhibit a non-negligible initial transient (Fig. 15(a)). Consequently, to enable a fair comparison between numerical and analytical trends, this initial transient profile was converted into an equivalent rectangular pulse. The equivalence was based on the specific impulse, defined as the overpressure integral. The procedure, detailed in Appendix B (Fig. B.18(a)), was used to determine the appropriate time shift required to define the initial instant  $t = 0$  in the analytical curves.

This rectangular equivalence is not intended as a physically rigorous description of the loading or of FSI; it is a visualisation aid to align the numerical loading history with the step-function profile adopted in the analytical model (see Fig. 3), which neglects the initial transient. By matching the impulse, a consistent starting time was obtained, making the comparison in Fig. 15(b) more transparent. This equivalence was used only for visual alignment and is not part of the framework implementation in Section 2.1, nor of the estimates reported in Section 3.1. All calculations were performed directly on the original uncoupled pressure signals.

The analytical trends of Fig. 15(b) are reasonably consistent with the numerical ones, with a closer match observed in the displacement plots. The velocity response at the plate centre is highly sensitive to pressure fluctuations occurring in the central region. As explained in Ref. [11], these fluctuations are caused by the formation of a complex wave pattern in the fluid behind the reflected shock wave. That is, the pressure waves forming in front of the plate are driven by its motion and deformation. A sequence of planar compression and expansion waves is generated from the central region of the plate, where plastic yield lines create the planar zone that gradually shrinks as the yield

lines propagate inward. These waves contribute to a pressure drop ahead of the plate. Simultaneously, radial waves are initiated at the edges of this planar region — along the advancing yield lines — and begin propagating from the plate boundary. Eventually, these radial waves converge at the plate centre, leading to a focusing effect that causes a local pressure increase due to FSI effects. This explains the lower velocity observed in the coupled simulations compared to the analytical solution between 29.4 ms and 29.6 ms, and the subsequent increase due to the focusing effect (see Ref. [11]). These pressure fluctuations in the coupled solution (see Fig. 15(a)) originate from strongly three-dimensional (3D) phenomena that cannot be captured by the analytical model. Nevertheless, the coupled model provides a reasonable description of the velocity transient. These fluctuations instead have a minor impact on the displacement response.

In addition, the plots in Fig. 15(b) display all the characteristic time instants involved in the two-option procedure. Time  $t_v$  is defined based on the peak velocity from the uncoupled solution, which occurs simultaneously with the velocity peak of the fully coupled numerical simulation. Similarly,  $t_w$  is defined from the peak displacement in the uncoupled case (more precisely, at the end of the transient phase), showing strong synchronisation with the coupled response. The Taylor time  $t_T$  and the saturated time  $t_{sat}$  accurately capture the duration of the velocity and displacement transient, respectively.

Moreover, as discussed in Section 3.2, the evaluation of FSI effects on displacement is carried out outside the time validity range of the inertial framework, defined by time  $t_{hinge}$ . This instant marks the arrival of the yield lines at the centre of the plate. Conversely, the estimation of FSI effects on velocity remains within the framework's range of validity, since  $t_v < t_{hinge}$ .

Additionally, it should be noted that the Taylor duration  $t_T$ , which is used in Option 1 to characterise the velocity transient, is by definition an estimate of the time required for the travelling hinge to reach the centre of the plate. The observed proximity between  $t_T$  and  $t_{hinge}$  confirms the reliability of this estimate, despite the simplifying assumptions adopted in its derivation.

#### 3.3.2. 2 mm Docol600 plate

For a steel plate made of Docol600 with a thickness of 2 mm subjected to the blast load from load case LC60, FSI effects are almost negligible. A FSI efficiency  $\Delta w$  of 4.39% and a velocity reduction  $\Delta v$  of 3.59% are identified, as shown in Table 5. Fig. 16(a) shows the pressure and density profiles obtained from the uncoupled Eulerian simulation, including the coupled pressure signal for comparison. Fig. 16(b) presents the structural response at the centre of the plate in terms of velocity and displacement, obtained from the numerical and analytical models, for both the coupled and uncoupled cases. The rectangular-pulse equivalence was also applied in this case to enhance the graphical comparison; details are provided in Appendix B (Fig. B.18(b)). As in the previous scenario, all time instants relevant to the two-option procedure are indicated, including the temporal limit of validity, which corresponds to the arrival of the plastic hinges in the central region ( $t_{hinge}$ ).

Although FSI effects are accurately estimated by the framework, as shown in Table 5, the analytical trends diverge from the numerical ones. This is especially evident for the velocity response, as a result of the behaviour already discussed in Section 3.2. Indeed, the high areal mass of the plate leads to the velocity and displacement oscillations observed in Fig. 16(b). These effects are not reproduced by the model, as they are not compatible with the 1D formulation. Despite these differences, the relative distance between the coupled and uncoupled models is in agreement with the numerical reference. The resulting estimates thus provide a reliable assessment of FSI effects, consistent with the overall goal of this study.

The Taylor time  $t_T$  and the saturation time  $t_{sat}$  once again provide a reasonable estimate of the transient duration of velocity and displacement, although they prove to be more conservative than in the previous scenario.

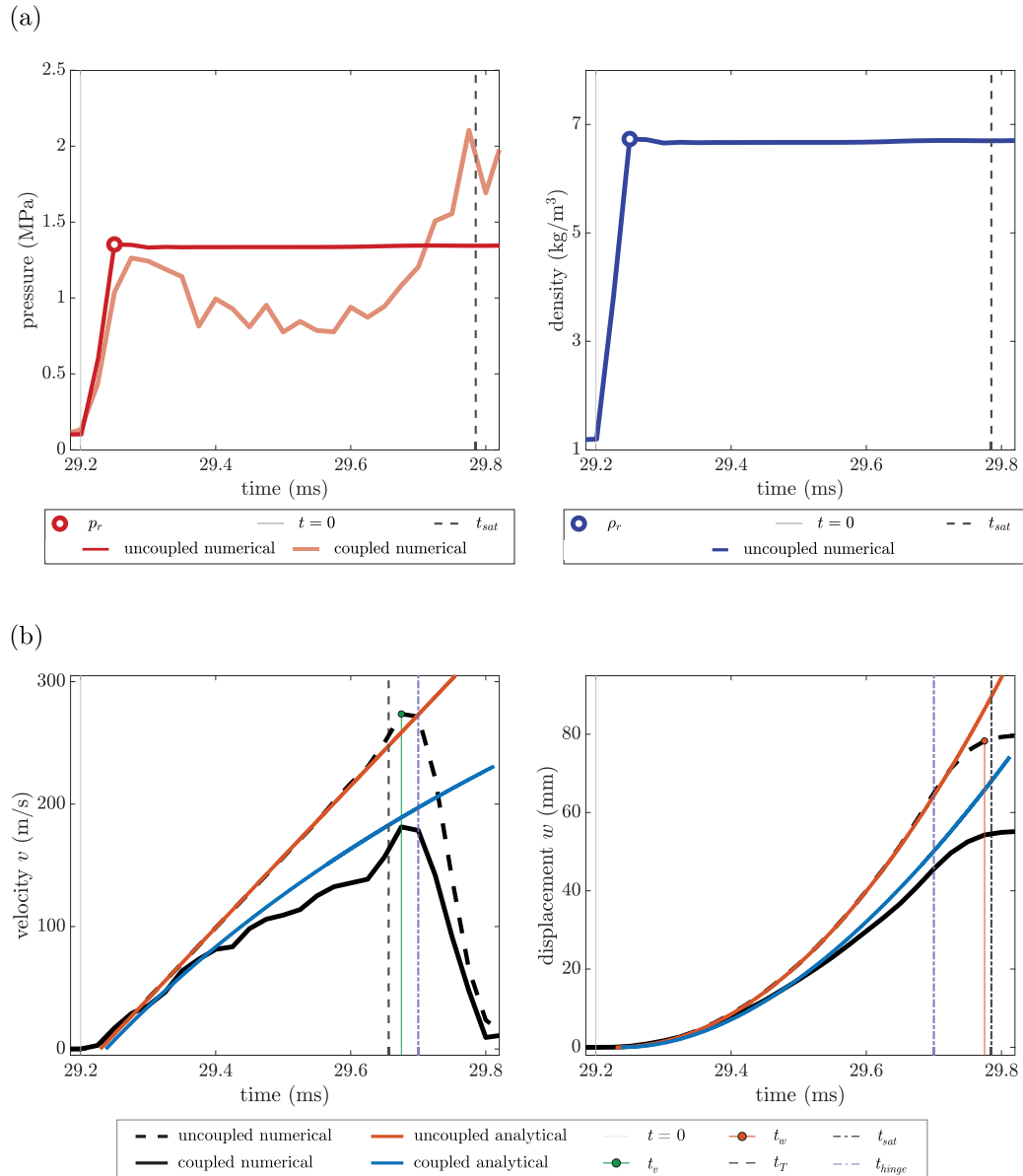


**Fig. 15.** Characteristic quantities for the configuration with an 6082-T6 plate (thickness 0.8 mm) subjected to the blast load from load case LC60. (a) Time evolution of pressure and density at the midpoint of the plate extracted from simulations. (b) Time evolution of the central velocity and displacement, as obtained from uncoupled and coupled simulations and analytical models.

#### 4. Conclusions

In this work, a new practical procedure has been developed to estimate the influence of FSI effects in blast-loaded square plates. The proposed framework is articulated in two distinct options, which differ in terms of accuracy and computational cost. FSI effects are documented in terms of the dimensionless reduction of peak out-of-plane velocity and out-of-plane displacement at the plate centre. The procedure relies on 1D theories accounting for non-linear gas compressibility and inertial effects. The resulting estimates can be computed using a limited set of fluid-dynamic and structural parameters. In particular, both options of the procedure rely on the solution of the fluid problem through an uncoupled simulation (first phase). They differ, however, in the definition of the time interval used in the estimation. In Option 1, to maximise efficiency, this interval is determined from closed-form expressions derived from the motion of the plastic hinges and from saturation analysis. In Option 2, aimed at improving accuracy at

a higher computational cost, the duration of the structural transient is obtained by also solving the structural problem in the uncoupled simulation.

The reliability of the framework has been assessed against 28 numerical scenarios involving a compressed-gas-driven shock tube configuration, previously validated in the literature. These scenarios vary in terms of material behaviour, plate thickness, and load intensity, providing a representative dataset. The results demonstrate the robustness and validity of the proposed estimates across the entire dataset. Option 1 of the procedure captures the displacement reduction due to FSI with an average overestimation of only 1.39%. In Option 2, this error is further reduced to 1.10%, and the accuracy in estimating the velocity reduction due to FSI is also improved. As a result, the framework can support blast-resistant design of plated structures by helping to assess whether computationally demanding fully coupled simulations are necessary. The methodology thus addresses the need for a quick assessment of

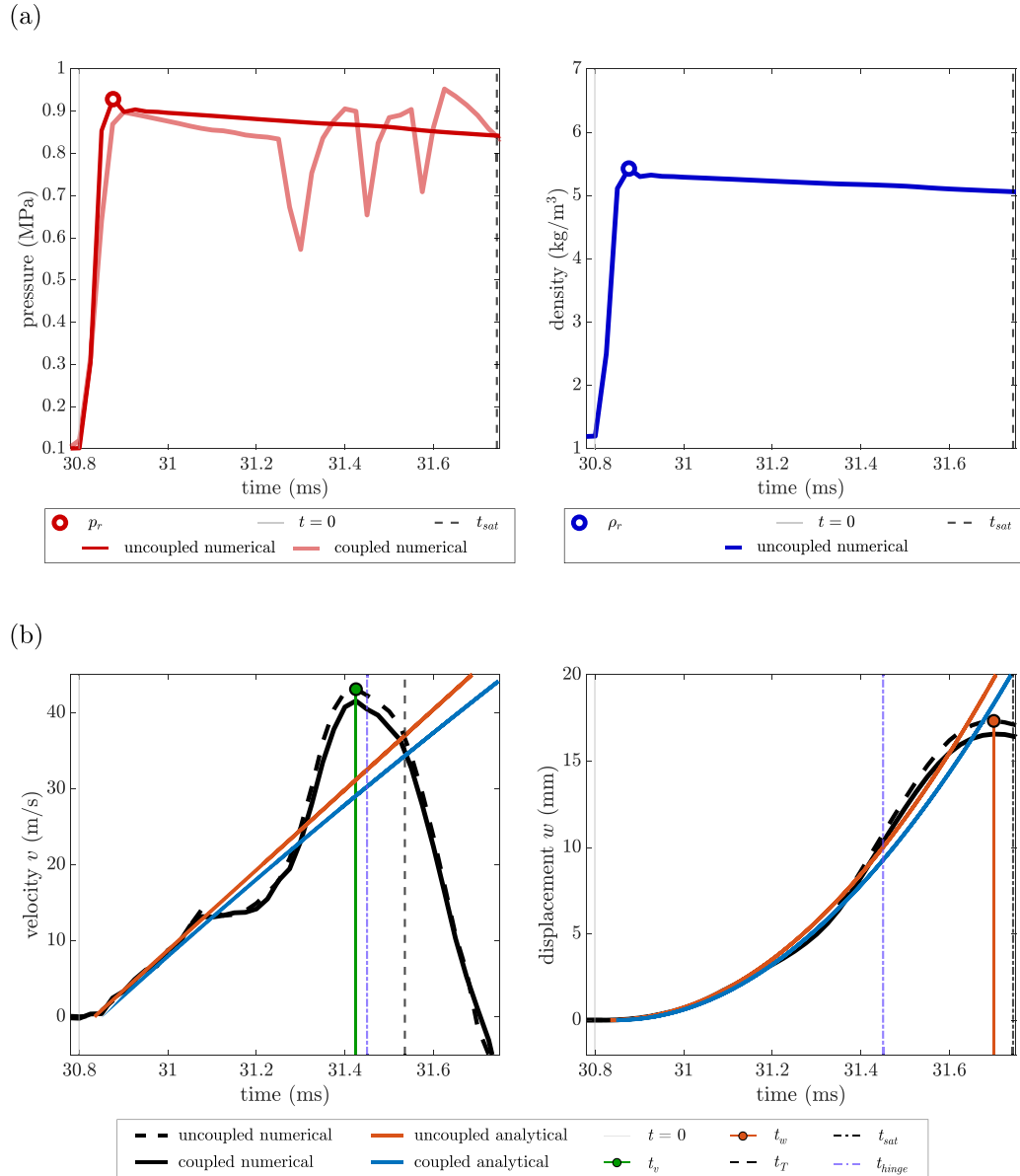


**Fig. 16.** Characteristic quantities for the configuration with a Docol600 plate (thickness 2 mm) subjected to the blast load from load case LC25. (a) Time evolution of pressure and density at the midpoint of the plate extracted from simulations. (b) Time evolution of the central velocity and displacement, as obtained from uncoupled and coupled simulations and analytical models.

the relevance of FSI effects, without relying on a direct comparison between modelling strategies, which is often not feasible in practice.

In addition, the validity limits of the framework have been analysed and shown to be consistent with the intended goal of fast running engineering methods (FREM). The framework targets square, fully clamped plates in membrane-dominated, large-inelastic response with approximately constant pressure during the structural transient. Rectangular and circular plates can be treated by using the closed-form Taylor and saturation times appropriate to the chosen geometry and the corresponding hinge-travel length  $L$ . Cases governed by stiffness or damping, non-clamped supports, strongly non-uniform loading, or markedly decaying positive phases are outside the scope. Future work will address these configurations and incorporate rate sensitivity and work-hardening through corrections to the dimensionless metrics. This

study should therefore be seen as a first step towards the development of FREM predictive tools for FSI effects in blast-loading scenarios.

#### CRediT authorship contribution statement

**Giovanni Marchesi:** Writing – review & editing, Writing – original draft, Visualization, Validation, Software, Methodology, Investigation, Formal analysis, Data curation, Conceptualization. **Luca Lomazzi:** Writing – review & editing, Visualization, Supervision, Project administration, Methodology, Conceptualization. **Vegard Aune:** Writing – review & editing, Validation, Software, Investigation, Conceptualization. **Gerald Nurick:** Supervision, Conceptualization. **Trevor John Cloete:** Writing – review & editing, Visualization, Supervision, Methodology, Formal analysis, Data curation, Conceptualization. **Andrea Manes:** Writing – review & editing, Supervision, Resources, Project administration.

### 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.

### Appendix A. Supplementary analytical details

See Fig. A.17.

### Appendix B. Rectangular equivalence of the initial pressure transient

This appendix outlines the visualisation aid used to align the numerical loading histories with the step-function pulse assumed in the analytical model, so that comparisons in Section 3.3 are fair and readable. The device is only for visual and quantitative alignment; it is not a rigorous treatment of FSI nor a calibration step.

Figs. B.18(a) and B.18(b) illustrate the rectangular-pulse equivalence for two representative cases. In each panel, the red shaded area corresponds to the impulse of the original numerical transient, whereas the blue shaded rectangle is the equivalent step pulse chosen to have the same peak overpressure, indicated by the horizontal dashed line, and the same specific impulse over the initial window. The vertical dashed line marks the onset of the equivalent pulse, which defines  $t = 0$  for the analytical curves. To mitigate limited time resolution, a least-squares fit is adopted in the early portion of the signal. The left plots report uncoupled pressure histories and the right plots report coupled ones; the same construction is applied consistently across cases to facilitate a transparent comparison between numerical simulations and analytical solutions.

### Data availability

Data will be made available on request.

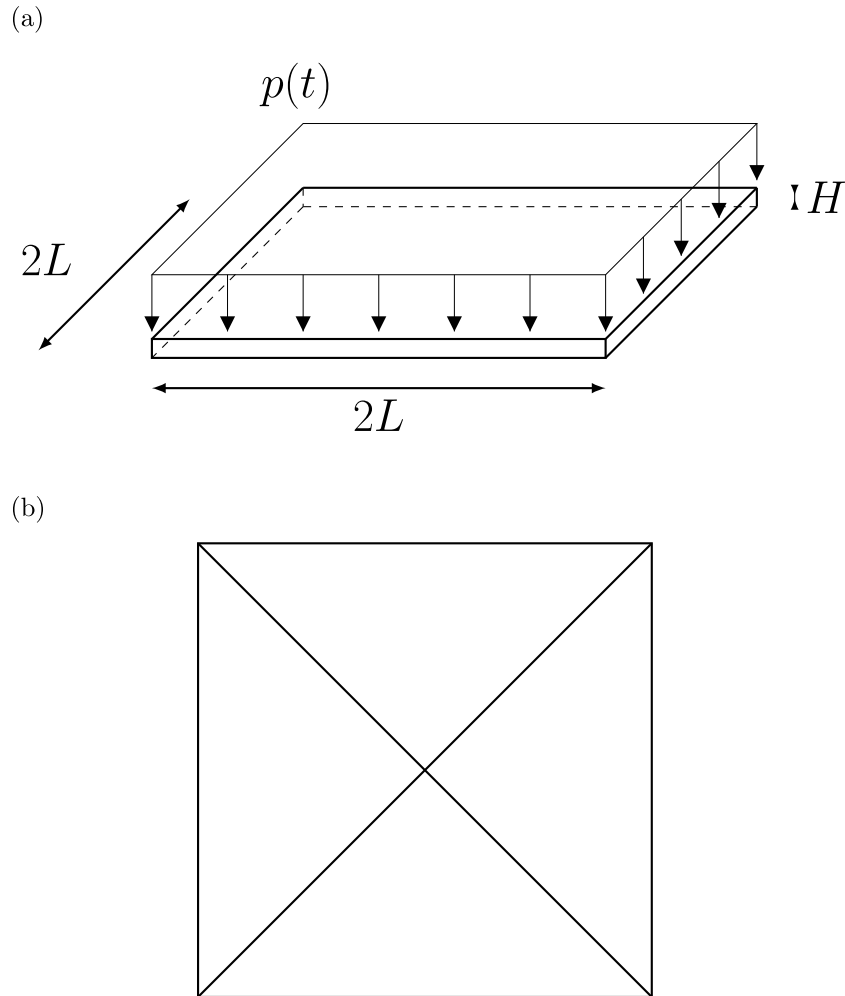


**Fig. A.17.** Geometry and boundary conditions of the clamped square plate subjected to a spatially and temporally uniform pressure pulse. (a) Main geometric parameters: plate side length  $2L$ , thickness  $H$ , and applied pressure history  $p(t)$  acting over the exposed surface. (b) shape of the modal solution used in the analytical formulation for deriving the saturated duration associated with the plate response.

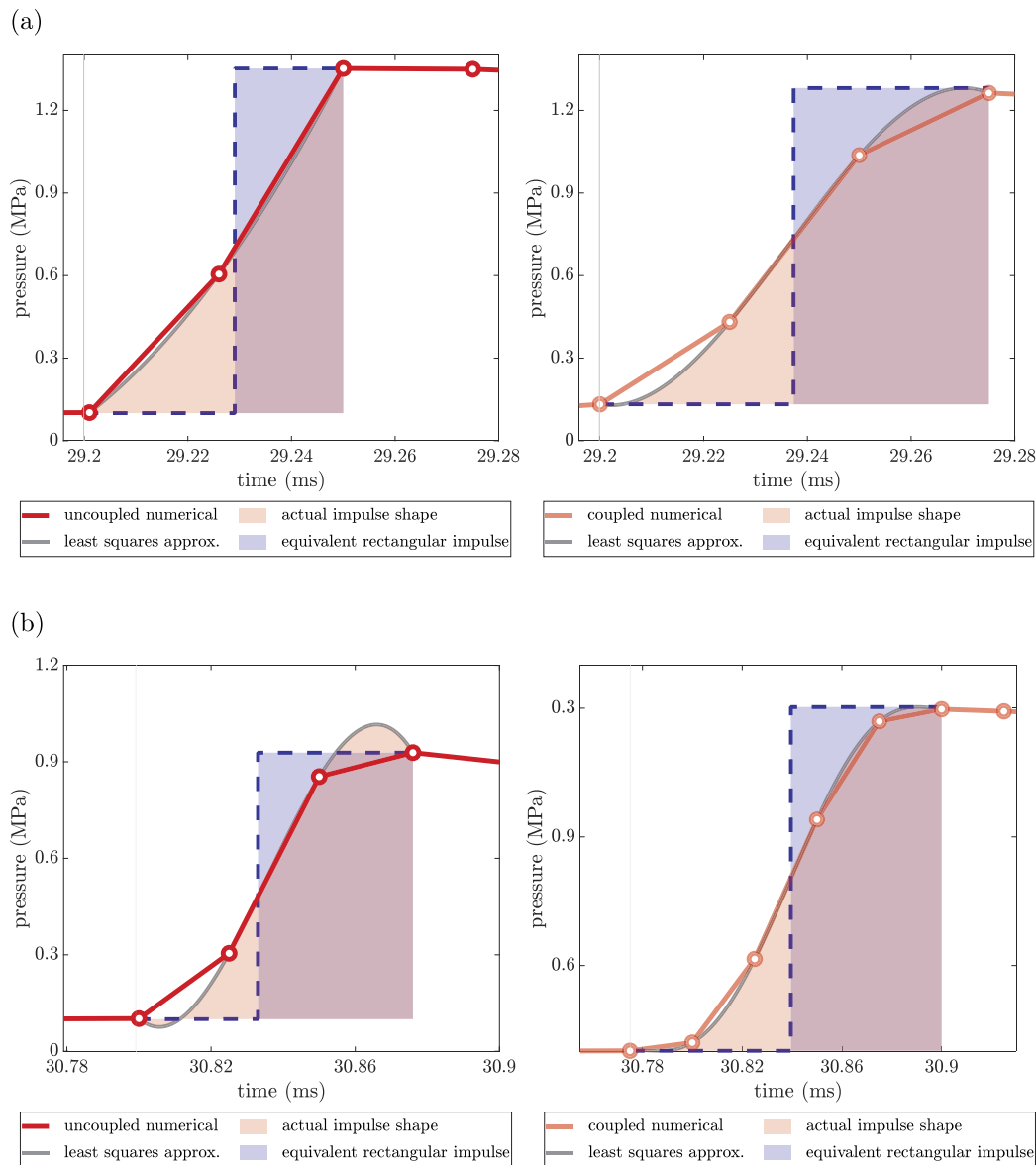


**Fig. B.18.** Rectangular pulse approximation of the initial pressure transient for representative cases. The equivalence is used only for visualisation purposes and is based on the specific impulse and least-squares fitting of the uncoupled (left) and coupled (right) pressure signals. (a) refers to the AA6082-T6 plate (LC60), and (b) to the Docol600 2-mm plate (LC25).

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