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Finite Element Analysis of Ballistic Resistance Performance Degradation in Damaged Small Arms Protective Inserts

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Abstract

Small Arms Protective Inserts (SAPIs) were developed as reliable stand-alone protection systems and have demonstrated high effectiveness against small- to medium-calibre threats, including armour-piercing projectiles. Despite their satisfactory nominal ballistic performance, during service in harsh environments SAPIs can experience off-design events, such as low velocity impacts or improper handling, which can induce non-visible or barely visible damage and drastically degrade their response to subsequent ballistic threats. These factors, combined with the complex damage mechanisms involved in their constituent materials, make assessing the damage state of SAPIs and predicting their residual ballistic performance particularly challenging.

This work aims to develop and validate an high fidelity finite element modeling framework based on the literature to assess the ballistic response of an undamaged and pre-damaged SAPI. The armour configuration consists of a 12 mm thick alumina (Al_2O_3) front layer backed by a 6.3 mm thick UHMWPE (Dyneema[®]) laminate. To assess behind-armour blunt trauma (BABT), a 50 mm thick Roma Plastilina[®] No. 1 layer is placed in contact with the rear face of the armour system.

Two damage scenarios are investigated: (i) multiple ballistic hits against the same protection and (ii) ballistic impact on an armor with a through-thickness crack in the ceramic layer. For each scenario, multiple simulations are performed varying the distance between the impact point and the pre-existing damage location.

The results highlight a strong correlation between the distance from the pre-existing damaged zone and the ballistic performance of the armour, quantified in terms of residual projectile velocity and back-face clay deformation.

Keywords: Finite element analysis, ballistic impact, SAPI, multiple hits

Abstract in lingua italiana

Gli inserti protettivi SAPI (Small Arms Protective Inserts) sono stati sviluppati come sistemi di protezione stand-alone affidabili e hanno dimostrato un'elevata efficacia contro minacce di piccolo e medio calibro, inclusi i proiettili perforanti. Nonostante le loro soddisfacenti prestazioni balistiche nominali, durante il servizio in ambienti ostili le SAPI possono essere soggette a eventi "fuori progetto", come impatti a bassa velocità o manipolazione impropria, che possono indurre danni non visibili o appena visibili e degradare drasticamente la loro risposta a successive minacce balistiche. Questi fattori, combinati con i complessi meccanismi di danno che interessano i materiali costitutivi, rendono particolarmente impegnativa la valutazione dello stato di danno dei SAPI e la previsione della loro protezione balistica residua.

Questo lavoro mira a sviluppare e validare un framework di modellazione a elementi finiti (FEM) ad alta fedeltà basato sulla letteratura per valutare la risposta balistica di SAPI intatta e pre-danneggiata. La configurazione della protezione consiste in uno strato frontale di allumina (Al_2O_3) spesso 12 mm, seguito da un laminato in UHMWPE (Dyneema[®]) spesso 6,3 mm. Per valutare il trauma da contusione dietro la corazza (BABT - behind-armour blunt trauma), uno strato di Roma Plastilina[®] No. 1 spesso 50 mm viene posto a contatto con il lato posteriore della protezione.

Vengono studiati due scenari di danno: (i) colpi balistici multipli sulla stessa protezione e (ii) impatto balistico su una protezione con una cricca attraversante tutto lo spessore dello strato ceramico. Per ogni scenario, vengono eseguite simulazioni multiple variando la distanza tra il punto di impatto e la posizione del danno preesistente.

I risultati evidenziano una forte correlazione tra la distanza dalla zona danneggiata preesistente e la prestazione balistica della corazza, quantificata in termini di velocità residua del proiettile e deformazione della plastilina posteriore (back-face deformation).

Parole chiave: Analisi ad elementi finiti, impatto balistico, SAPI, colpi multipli

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1 | Introduction

Individual and vehicular protection from ballistic threats is a crucial priority in defence and safety fields. Early work on this subject has been done by M. Wilkins et al. in 1967-1969 [1–3], who conducted experimental ballistic tests, capturing the impacts with high speed cameras and flash x-rays, and proposed rudimentary numerical models to predict the impact.

Traditionally, armors were composed of monolithic structures, usually made of high strength steel (Rolled Homogeneous Armour, RHA) [4]. However, the growing need for mobility and lightness has driven research towards the development of advanced composite materials and structures with a high strength-to-weight ratio.

Modern solutions of ballistic protection (such as SAPI, Small Arms Protective Inserts) employ multilayer structures, where the armor is generally composed of a front plate of ceramic material and one (or more) backing plate of a more ductile material, such as the solution presented in figure 1.1. This configuration offers excellent performance in protecting human users or sensible assets against the ballistic impact of small to medium caliber projectiles, granting high levels of reliability, as defined in standards like NATO STANAG 4569 [5] and the U.S. NIJ Standard 0108.01 [6].

The ceramics, thanks to their exceptional hardness and high elastic modulus, have the primary role of eroding and shattering the entering projectile, dissipating a significant part of the kinetic energy. Among the most studied ceramics there are Alumina (Al_2O_3), Silicon Carbide (SiC) and Boron Carbide (B_4C). Because ceramics are inherently brittle and have low tensile strength, they cannot be used alone. The backing panel, typically made of fiber-reinforced polymer (FRP) composites, such as Ultra-High Molecular Weight Polyethylene (UHMWPE), Aramid (Kevlar) and Carbon fibers, is essential to provide mechanical support to the ceramic panel, absorb the residual kinetic energy of the deformed projectile, and contain the ceramic and projectile fragments.

A criticality of these multilayer solutions still under active research by many scholars is the multi hit resistance: the ability of the ceramic composite protections to spread the energy of the projectile over a large area results in a more vast damaged area when

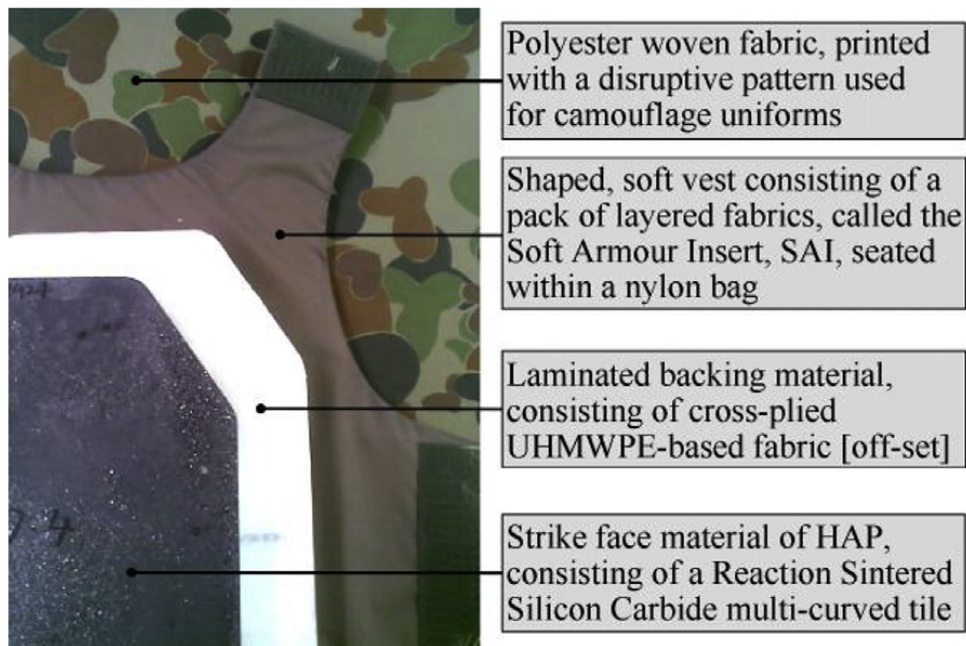


Figure 1.1: Typical sub-systems and material groups used in a body armour system. For photographic purposes, the UHMWPE backing material has been off-set from the ceramic tile [7]

compared to traditional monolithic steel protections. This means that after a ballistic hit the residual ballistic protection is drastically reduced in a large area surrounding the first impact locus [8].

Furthermore, composite ceramic protections are vulnerable to low velocity impacts and collisions, common during their normal operation in harsh environments. These low entity traumas may lead to the development of non-visible or barely-visible impact damage on the ceramic tile, that significantly conditions the ballistic performance of the protections. Cracks in the ceramic layer of the armor (that might even not be detected by a visual inspection) dramatically affect the ability of the armor to stop a threat that would be nominally defeated by the armor itself.

Because of the complex mechanical behavior of ceramic materials, that often requires a stochastic modeling [9], it's extremely hard to assess health state of a ceramic composite armor and above all to formulate a reliable and quantitative prognosis. Minor damages often are not even visible to the naked eye, and a visual inspection of a protection impacted by a ballistic threat may drastically underestimate the true extension of the damage in the ceramic tile.

As for now, the standard procedure for damage control in composite protections is x-ray radiography, that is able to detect even the not visible cracks in the material, as

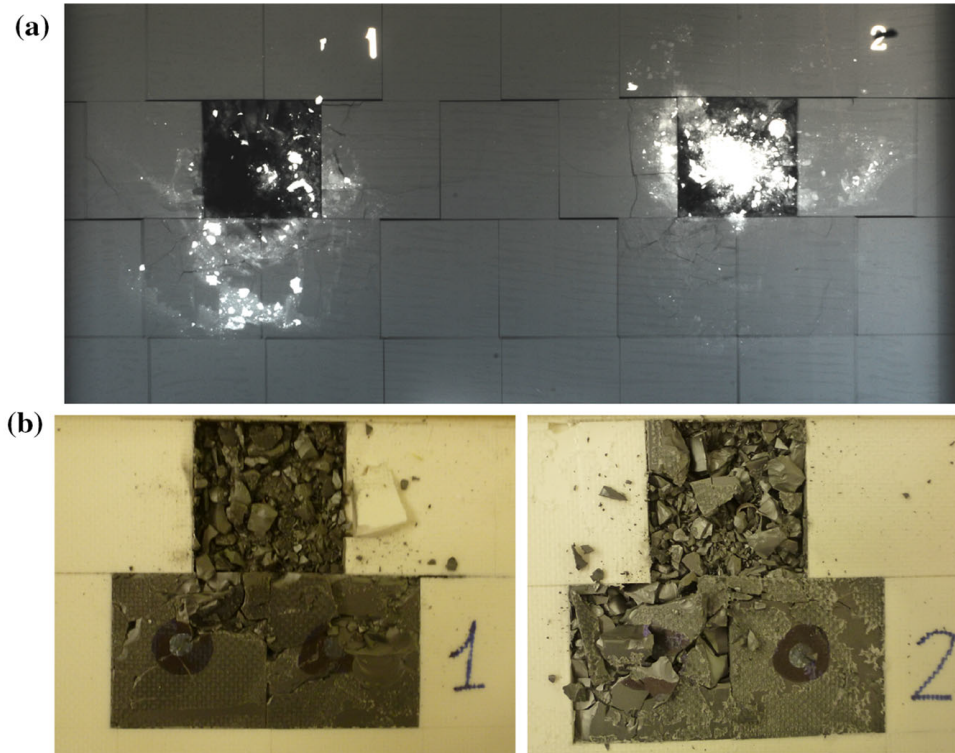


Figure 1.2: Multiple ballistic impact on a SiC/Dyneema composite protection: comparison between (a) radiography and (b) visible damage [10]

shown in figure 1.2. But even with those images, that require high level technicians to be interpreted, providing a prognosis of how the protection will perform against a future threat is purely qualitative, extremely complex and inconsistent. Furthermore, x-rays are not able to detect damage (such as delamination) in the fiber reinforced composite backing [10].

This kind of approach makes very expensive and time consuming surveying a big population of SAPIs; so the common practice consists of inspecting few random specimens out of a batch instead of examining the whole batch. The conclusions of those surveys are statistically not relevant, because the load history of each specimen might be even radically different from others.

The scope of this work is to investigate numerically: (i) the ballistic impact response of an undamaged SAPI, (ii) the protection degradation after a ballistic impact and (iii) how a crack in the ceramic tile affects the protective capability of the armor.

In detail the work consisted in the set-up, calibration and validation of a finite element model (based on literature review) of an Armor-Piercing (AP) projectile impacting a composite SAPI made of an Alumina (Al_2O_3) front face and a Dyneema[®] (UHMWPE)

back layer.

Multiple simulations have been conducted on the undamaged armor and introducing several degrees of damages, such as cracks and prior ballistic impact damage. The simulation setup included also a block of Plastilina to assess the BFD, in compliance with the NIJ standard 0101.06 [11].

This work is structured as follows. In Chapter 2 is presented an overview of the current state of the art regarding personal protections, a description of their constituent materials (and relative material models) and numerical simulation techniques. Chapter 3 introduces the reference scientific article which was relied upon for realizing the high fidelity numerical simulation framework. In Chapter 4 the technical details of the numerical model are described; Chapter 5 presents the results of all the cases taken into consideration and finally Chapter 6 summarizes the findings of this work, proposing future directions.

2 | State of the art

2.1. Personal protections constructive solutions

Personal protections, such as body armor, helmets, and Small Arms Protective Insert (SAPI) plates, are designed to defeat high-velocity projectiles and fragments while minimizing weight to ensure user mobility [12–14]. Modern advanced composite armors (ACA) typically utilize a multi-layered system where each component performs a complementary role in the penetration resistance process [10, 15, 16].

A primary design priority for personal protections is to minimize weight (in terms of areal density) while maintaining unchanged protection levels; this goal has been pursued by many scholars investigating lower density materials and alternative armor designs.

Another challenge for lightweight armors is to avert (or to mitigate) Behind-Armor Blunt Trauma (BABT). Even without penetration, the rapid deformation of the armor (back-face deformation, BFD) can cause lethal internal injuries. Solutions like adding foam anti-trauma pads or optimizing interlaminar bonding strength are used to reduce the peak impact depth of the backing material into the user's body [17]. This topic is discussed more in detail in section 2.2.

2.1.1. Standard Multi-Layer Architecture

The most common constructive solution for defeating armor-piercing (AP) threats is a bi-layer or multi-layer configuration consisting of a hard strike face and a tough backing material [18–20]

- **Strike Face (Ceramic):** High-hardness ceramic tiles (e.g., Alumina, Silicon Carbide, or Boron Carbide) are used to blunt, shatter, and erode the projectile tip upon impact. This process dissipates a major portion of the impact energy and spreads the remaining load over a wider
- **Backing Plate (Ductile Material):** A relatively tough layer, typically made of fiber-reinforced polymer composites (like Kevlar/aramid or UHMWPE/Dyneema) or duc-

tile metals (like aluminum), supports the ceramic. Its primary role is to absorb the residual kinetic energy of the fragmented projectile and ceramic debris through plastic deformation, bulging, and membrane stretching

2.1.2. Innovative Structural Configurations

Scholars have proposed several bio-inspired variations to the previous standard to improve multi-hit capability and energy absorption:

- Hexagonal mosaic armor: In these designs, multiple small ceramic tiles are arranged in a patterned array. This configuration inhibits crack propagation, ensuring that the failure of one tile causes minimal damage to adjacent tiles, which is critical for surviving multiple hits [21, 22]. The main drawback of this design is that when compared to an equivalent monolithic structure the mosaic armor offers lower ballistic resistance.
- Nacre-like staggered systems: Inspired by the "brick-and-mortar" structure of seashells, these utilize staggered ceramic tablets bonded with tough interlayers like polyurea [23]. This arrangement enhances toughness by thousands of times compared to the constituent materials by enabling mechanisms like tablet pull-out and interlocking
- Corrugated structures: Inspired by tortoise shells, corrugated ceramic/metal designs use inclined surfaces to deflect the projectile's trajectory, increasing interaction time and improving the ballistic limit [24]

2.1.3. Supplementary Construction Features

- Ceramic types: a wide range of advanced ceramics are available on the market, and can be grouped in two main categories: lower cost sintered or higher cost hot pressed ceramics. A tile of the same chemical composition can express a significantly different mechanical behavior depending on its manufacturing process [25]
- Confinement and wrapping: Covering the ceramic tile with a composite cover (fiber-glass or aramid) or a thin protective layer helps restrain fragmented ceramic material, keeping it in the path of the projectile to force continued erosion [14]
- Interlayers: The introduction of rubber or foam layers between the ceramic and the backing plate can improve multi-hit capability, delay penetration, and reduce Back Face Deformation (BFD) [12, 17]
- Geometric curvature: To ensure a better fit for the human body and improve er-

gonomics, personal armors are often manufactured with a specific curvature (e.g., 400 mm radius [26]). Convex surfaces also help distribute impact stresses over a larger volume of material [27].

2.2. Behind-Armor Blunt Trauma (BABT)

Behind-Armor Blunt Trauma (BABT) is a critical issue in personal protections, defined as the injury resulting from a non-penetrating ballistic impact on body armor [28]. The blunt trauma may result from minor skin abrasions to, in extreme situations, severe sternal fractures, cardiac and liver tissue injuries and pulmonary contusions [29, 30]. So, even though the armor is able to stop the threat (namely a bullet, an high velocity fragment or shrapnel) the impact may lead to serious injury or death.

To evaluate the risk of BABT, several criteria are employed, often focusing on the deformation of the armor backing material.

2.2.1. Back Face Deformation (BFD)

The most common method for evaluating the potential for blunt injury involves measuring the maximum depth of the deformation of the armor backing, known as BFD.

This procedure is described in detail by the NIJ standard 0101.06 [11, 31]:

- The armor to be tested is positioned right in front of a block of plastilina Roma No.1 (RP #1)
- Experimental ballistic tests are conducted in a shooting range
- After the shooting test, the permanent indentation left on the plastilina block (as visible in figure 2.1) is measured
- Standard requirements mandate that the maximum depth of back deformation thus measured should not exceed 44 mm to ensure adequate protection

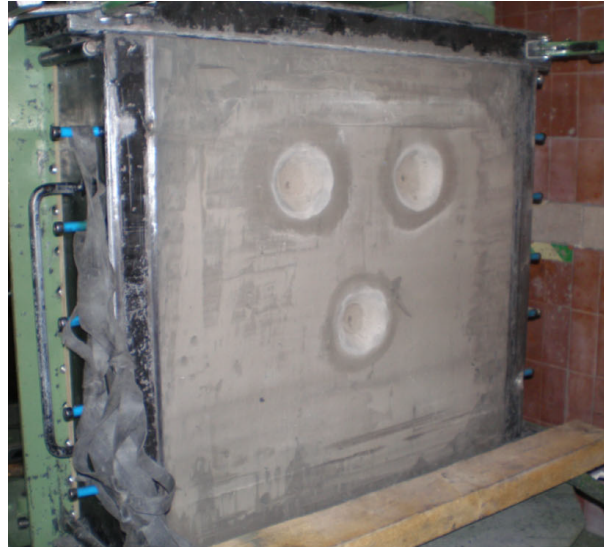


Figure 2.1: Typical indentations on plastilina block after ballistic testing [10]

2.2.2. Injury scales

Blunt injury assessment can also be conducted using established criteria such as the Abbreviated Injury Scale (AIS) and the Bullet Coefficient (BC).

The Abbreviated Injury Scale [32] is a common concise grading scale in collision biomechanics, categorizing the degree of injury based on anatomical structures and threat to life into six levels:

- 1: Minor injury
- 2: Moderate injury
- 3: Severe injury
- 4: Critical injury
- 5: Critical injury with uncertain survival
- 6: Fatal injury

The Bullet Coefficient is an energy-based model developed by Sturdivan, used to assess blunt injury risk caused by blunt objects, projectiles, and body armor impacts. For more details on how the coefficient is computed, see Sturdivan's publication [33].

Specific values of the BC correspond directly to AIS scores, as shown in Table 2.1.

The study conducted by Zhu et al. [26] has indicated that the gap between the body armor and the human body significantly influences the blunt injury assessment: as the

AIS score	0	1	2	3	4	5	6
BC value	0	0.125	0.215	0.478	0.787	1.403	1.876

Table 2.1: AIS scores and BC values correspondence [33]

gap increased from 3 mm to 11 mm, the calculated BC values decreased from 0.9339 to 0.1114, and the corresponding AIS level decreased from 4 (critical injury) to 0 (no apparent injury).

2.3. Alumina (Al_2O_3)

Ceramics are commonly employed in composite lightweight protections, due to their high impact strength and hardness [18]. The ceramic tile, usually made of alumina, boron carbide (B_4C) or silicon carbide (SiC), is often covered by a thin layer of composite material, which serves the purpose of confining the ceramic fragments and thus improving the multi-hit performance [14].

2.3.1. Impact behavior of ceramic materials

The failure behavior of ceramics undergoing a ballistic impact is a complex, multi-stage process characterized by high initial resistance, projectile erosion, and rapid brittle fragmentation. Unlike metals that defeat projectiles through ductile deformation, ceramics utilize their extreme hardness and compressive strength to shatter and erode the incoming threat.

The interaction between a projectile and a ceramic tile can be subdivided in two distinct phases, based on the state of the ceramic material [34, 35].

- Phase 1: Shock Wave Propagation and Crack Initiation (figure 2.2): When a projectile strikes the ceramic surface its tip is often shattered or blunted instantly and an high-amplitude compressive shock wave travels from the impact site to the rear surface of the ceramic. Upon reaching the rear face (a free boundary or the interface with a soft backing), it reflects as a tensile wave. Because ceramics have significantly lower tensile strength than compressive strength, this reflected wave initiates radial cracks on the back surface that propagate toward the front

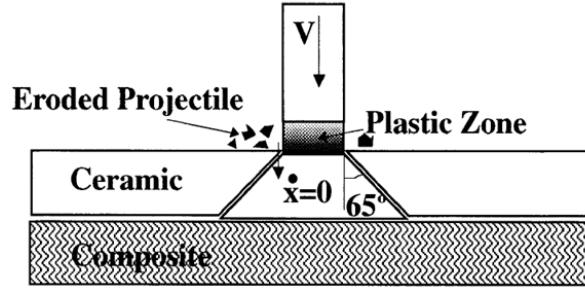


Figure 2.2: Schematization of the first phase [34]

- Phase 2: Conoid Formation and Fragmentation (figure 2.3): As the projectile continues to advance, a conical damage zone (conoid) is formed beneath the impact area. This conoid is composed of comminuted (pulverized) ceramic fragments, often referred to as the Mescal zone. The formation of this conoid is vital as it spreads the concentrated impact load over a much wider area of the backing plate, preventing premature failure of the overall armor system

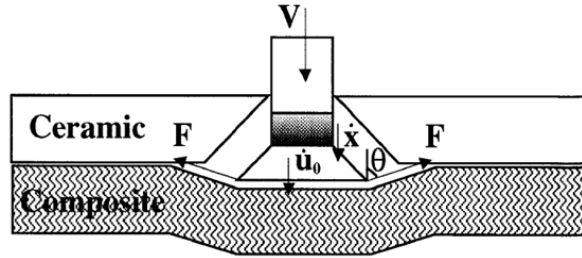


Figure 2.3: Schematization of the second phase [34]

2.3.2. Johnson-Holmquist II constitutive model

A widespread model used to describe the mechanical behavior of alumina, and ceramics in general, is the Johnson-Holmquist II (JH-2) model [36]. This model was developed to simulate ceramics, glass and brittle materials subjected to high strain rates and pressures. The model consists of three components:

- A representation of the strength of intact and fractured material
- A damage model that transitions from an intact state to a fractured state
- An equation of state (EOS) to characterize the pressure density relation

The equivalent strength is expressed in function of intact strength, fractured strength, strain, strain rate, pressure, and damage.

The equivalent stress is calculated as:

$$\sigma^* = \sigma_i^* - D(\sigma_i^* - \sigma_f^*) \quad (2.1)$$

where the σ^* , σ_i^* and σ_f^* are normalized equivalent stress w.r.t. the Hugoniot Elastic Limit (HEL), σ_i^* is the normalized intact equivalent stress, σ_f^* is the normalized fractured equivalent stress and D is the damage variable.

σ_i^* and σ_f^* are expressed as:

$$\sigma_i^* = A(P^* + T^*)^n(1 + C \ln \dot{\epsilon}^*) \quad (2.2)$$

$$\sigma_f^* = B(P^*)^m(1 + C \ln \dot{\epsilon}^*) \leq \sigma_{f,MAX}^* \quad (2.3)$$

where A , B , C , n and M are material constants, p^* is the normalized pressure defined as $P^* = P/P_{HEL}$ and T^* is the maximum normalized tensile pressure strength $T^* = T/P_{HEL}$; P_{HEL} is the pressure at the Hugoniot Elastic Limit. $\dot{\epsilon}^*$ is the normalized strain rate w.r.t. a reference strain rate $\dot{\epsilon}_0$ and $\sigma_{f,MAX}^*$ is a maximum normalized fractured strength.

Figure 2.4 shows a graphical interpretation of the strength part of the JH-2 model, including damaged, intact and fractured strength.

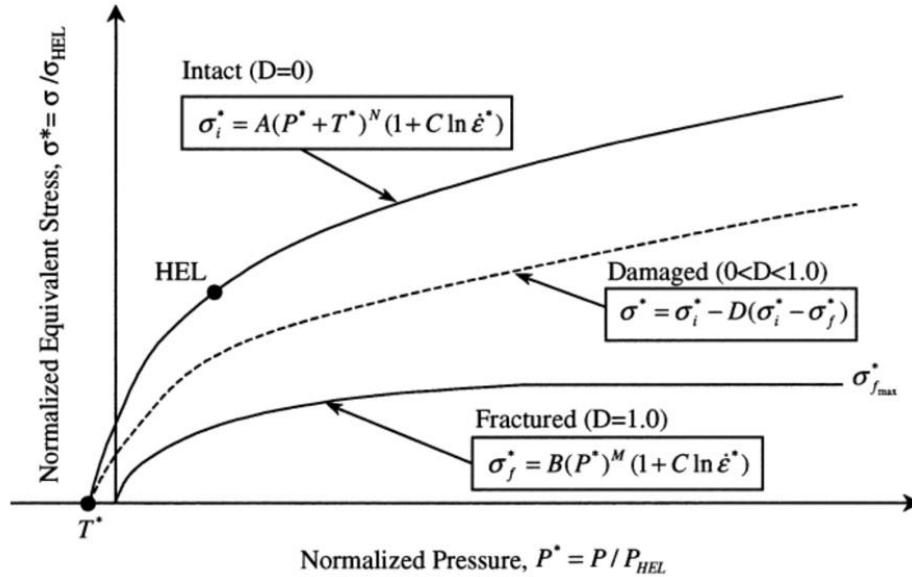


Figure 2.4: Strength model graphical representation [36]

The damage variable D in equation 2.1 is the cumulated damage, calculated as:

$$D = \sum \frac{\Delta \varepsilon_p}{\varepsilon_p^f} \quad (2.4)$$

where $\Delta \varepsilon_p$ is the incremental equivalent plastic strain and ε_p^f (plotted in figure 2.5) is expressed as:

$$\varepsilon_p^f = D_1 (P^* + T^*)^{D_2} \quad (2.5)$$

where D_1 and D_2 are material parameters. Element erosion is verified when the equivalent plastic strain reaches a value equal to FS . Figure 2.5 provides a graphical interpretation of the damage equation.

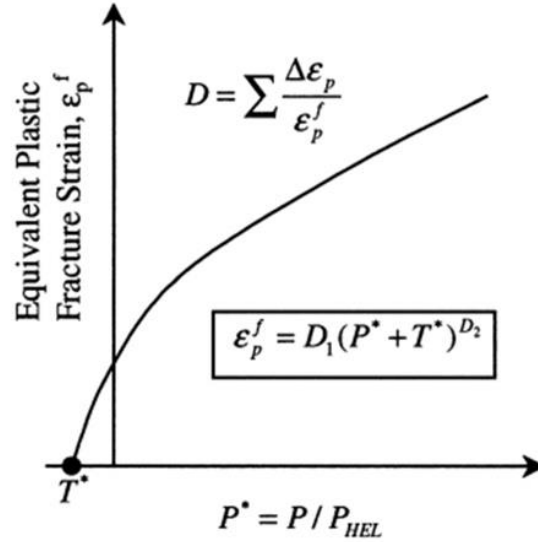


Figure 2.5: Damage model graphical representation [36]

The pressure P comprehends the effects of bulking, represented through the transfer of internal energy from decreased shear and deviator stresses to potential internal energy associated with increased hydrostatic pressure, that is given as:

$$P = \begin{cases} K_1 \mu + K_2 \mu^2 + K_3 \mu^3 + \Delta P & (\mu \geq 0) \\ K_1 \mu + \Delta P & (\mu \leq 0) \end{cases} \quad (2.6)$$

where K_1 , K_2 and K_3 are material constants and $\mu = \rho / \rho_0$, with ρ and ρ_0 being current and initial density. As the damage in the material increases an incremental pressure ΔP is added to the equation of state. The value of this additional pressure changes from

$\Delta P = 0$ when $D = 0$ to $\Delta P = \Delta P_{MAX}$ when $D = 1$. The pressure increment ΔP is caused by a conversion of a fraction β (between 0 and 1) of elastic energy loss into hydrostatic potential energy. Figure 2.6 summarizes the pressure component of the JH-2 model.

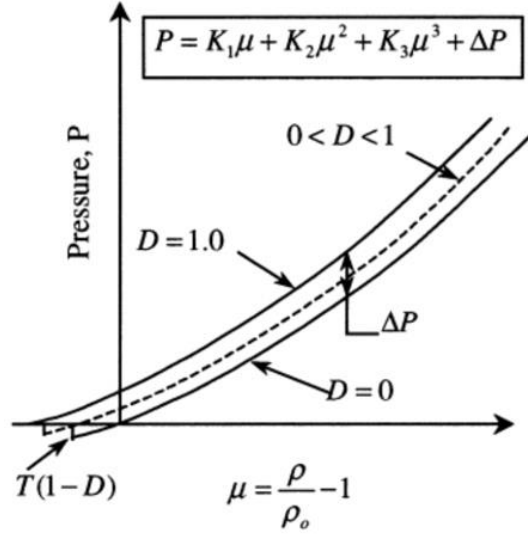


Figure 2.6: Pressure model graphical representation [36]

2.4. UHMWPE

In the defence and security fields, fiber reinforced composites are commonly used in ballistic protections, due to their extremely high strength to weight ratio. Among the previously mentioned examples in the introduction, Ultra-High-Molecular-Weight PolyEthylene, commercially known as Dyneema[®], stands out as one of the most promising solutions.

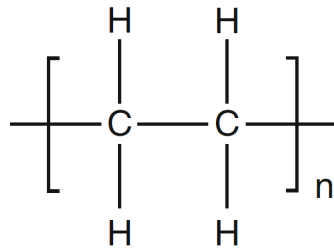


Figure 2.7: Polyethylene unit chemical structure

UHMWPE is a subset of thermoplastic polyethylene; its molecular chain can consist of as many as 200,000 ethylene repeat units (shown in figure 2.7) [37]. This long chain grants

an effective load transfer mechanism through the laminate, resulting in a very tough material, with the highest impact strength of any thermoplastic currently manufactured [38]. UHMWPE laminates are able to accumulate tremendous amounts of strain before breaking, which makes them extremely effective at absorbing energy, often surpassing materials such as Kevlar or Carbon fiber.

For personal and vehicular protective applications, both civil and military, UHMWPE is typically employed as a part of composite plates, arranging parallel fibers in multiple unidirectional orthotropic plies (meaning that mechanical properties are different along three orthogonal axes).

2.4.1. Failure behavior of composite materials

The failure of fiber-reinforced polymer (FRP) composites under ballistic impact is a complex, multi-scale phenomenon involving several distinct mechanisms that often occur simultaneously. These failure modes can be broadly categorized into intraply (within a single layer) and interply (between layers) damage [39–41].

Intraply damage refers to the degradation of the fibers and the matrix within the individual laminae of a composite structure.

- Fiber fracture: fibers are broken by either tensile or compressive loads. Tensile failure occurs when the longitudinal strain in the fibers exceeds their rupture limit, often resulting in a "neat" fracture (as seen in Kevlar) or a "plastic/rough" fracture with diameter reduction (as seen in UHMWPE). Compressive failure is often dictated by micro-buckling or the formation of kink bands within the plies [42].
- Matrix cracking: failure of the polymer resin occurs due to transverse tension, compression, or in-plane shear stresses. While matrix cracking itself dissipates some energy, its main role is to increase resistance to delamination [43].
- Shear plugging: a nearly cylindrical "plug" of the target material, approximately the same diameter as the projectile, is punched out. It is driven by intense transverse shear forces and is most common when blunt projectiles strike thin or intermediate target plates at high velocities [44].

Interply failure occurs at the interfaces between the individual plies or between the composite and a ceramic facing.

- Delamination: involves the separation of adjacent layers and is considered one of the most significant energy-absorbing mechanisms in thick laminates. It is driven

by through-thickness tensile and shear stresses that exceed the interlaminar fracture toughness of the matrix [45].

- Interfacial debonding: in composite armors, failure occurs at the bond between the ceramic front plate and the composite backing. This debonding allows for more localized deformation and can significantly influence the overall ballistic resistance of the system

The dominant failure mode is influenced by several internal and external parameters:

- Material type: Different fibers exhibit unique failure traits; for example after failure Kevlar fibers (due to their dense packing) can sometimes "close" the hole after perforation [46].
- Interface strength: stronger bonding between layers can actually decrease ballistic performance by inhibiting delamination, which leads to premature tensile failure on the rear face or a transition to shear plugging [47]
- Impact velocity: at low velocities, composites dissipate energy primarily through tensile stretching and bulging. At high velocities, the failure mode often transitions to lower-energy shear failure (plugging) because the projectile perforates the material before global deformation can fully develop [22, 48]
- Projectile shape: blunt or flat-nosed projectiles are more likely to induce shear plugging, whereas hemispherical or ogival projectiles tend to stretch the fibers to failure [49]

2.4.2. Numerical implementation of composites lay-up

Several numerical modeling techniques have been developed to reproduce the mechanical behavior of composites. The modeling approaches can be grouped in two main families:

- Global orthotropic: the laminate is assumed to be homogeneous (single phase); there is not distinction between matrix and fibers [16]
- Local orthotropic: Matrix and fibers are treated as two distinct and independent materials (dual phase); the two constituents are bond together with tiebreak contacts or cohesive elements to reproduce adhesive forces [50]

The choice of the modeling approach depends on which physical phenomena are to be investigated and reproduced by the means of the simulation. In the global approach the laminate is modeled on a macroscale: the plies are modeled without any segregation between fiber and matrix and the material properties are assigned to the plies based on their

orientation. The local approach consists in modeling on a mesoscale: the fibers and the matrix are built up in separated way and they result clearly distinguishable in the numerical model. The latter methodology is able of an high fidelity morphological reproduction depiction of the damage phenomena, but is significantly more computationally expensive. For this reason complex geometries and large dimensions the global technique is often the only feasible option.

The lay-up modeling is also influenced by the element formulation choice; the laminates can be modeled with different methodologies:

- Single shell element through the thickness: for thin laminates (where delamination can be neglected) a single shell with multiple integration points through the thickness (one for each ply) can provide good results [51]
- Multiple shell elements through the thickness: this variant of the previous approach consists of defining multiple shell elements (with one or more integration points each) for each ply of the laminate, connected with tiebreak contacts, in order to properly model delamination.
- Multiple solid elements through the thickness: a more detailed approach is to use multiple solid elements to reproduce the various plies, in order to capture stresses in the normal direction and out of plane shear stresses, that are characteristic of ballistic impacts. Again the elements constituting the various plies can be bonded with cohesive (zero thickness) elements or tiebreak contacts.

It's important to notice that some of the material models implemented in finite element softwares can be used only with specific element formulations; for example MAT_058 in LS-Dyna (LAMINATED _ COMPOSITE _ FABRIC) is available only for shell and thick shell elements, but not for solid elements, while MAT_059 (COMPOSITE _ FAILURE _ MODEL) is compatible also with solid elements.

2.4.3. Tsai-Wu criteria for failure in composites

When using the global approach previously described, the Tsai-Wu failure criterion [52] is commonly used to model the response of composite materials. The failure criterion is

coupled with a 3D orthotropic elastic constitutive relation:

$$\begin{Bmatrix} \varepsilon_{11} \\ \varepsilon_{22} \\ \varepsilon_{33} \\ 2\varepsilon_{23} \\ 2\varepsilon_{31} \\ 2\varepsilon_{12} \end{Bmatrix} = \begin{pmatrix} \frac{1}{E_{11}} & -\frac{\nu_{21}}{E_{22}} & -\frac{\nu_{31}}{E_{33}} & 0 & 0 & 0 \\ -\frac{\nu_{12}}{E_{11}} & \frac{1}{E_{22}} & -\frac{\nu_{32}}{E_{33}} & 0 & 0 & 0 \\ -\frac{\nu_{13}}{E_{11}} & -\frac{\nu_{23}}{E_{22}} & \frac{1}{E_{33}} & 0 & 0 & 0 \\ 0 & 0 & 0 & \frac{1}{G_{23}} & 0 & 0 \\ 0 & 0 & 0 & 0 & \frac{1}{G_{31}} & 0 \\ 0 & 0 & 0 & 0 & 0 & \frac{1}{G_{12}} \end{pmatrix} \begin{Bmatrix} \sigma_{11} \\ \sigma_{22} \\ \sigma_{33} \\ \sigma_{23} \\ \sigma_{31} \\ \sigma_{12} \end{Bmatrix} \quad (2.7)$$

where ε_{ij} are the strain components, σ_{ij} are the stress components, G_{ij} , E_{ii} and ν_{ij} are the shear modulus, the elastic modulus and the poisson's ratio respectively.

The material failure modes are defined as following:

- in-plane tension

$$\begin{cases} \left(\frac{\sigma_{11}}{X_T}\right)^2 + \left(\frac{\sigma_{12}}{S_{21}}\right)^2 + \left(\frac{\sigma_{13}}{S_{31}}\right)^2 \geq 1 \\ \left(\frac{\sigma_{12}}{S_{21}}\right)^2 + \left(\frac{\sigma_{22}}{Y_T}\right)^2 + \left(\frac{\sigma_{23}}{S_{32}}\right)^2 \geq 1 \end{cases} \quad (2.8)$$

- through thickness shearing

$$\begin{cases} \left(\frac{\sigma_{11}}{X_T}\right)^2 + \left(\frac{\sigma_{13}}{S_{31}}\right)^2 \geq 1 \\ \left(\frac{\sigma_{22}}{Y_T}\right)^2 + \left(\frac{\sigma_{23}}{S_{32}}\right)^2 \geq 1 \end{cases} \quad (2.9)$$

- delamination

$$\left(\frac{\sigma_{33}}{Z_T}\right)^2 + \left(\frac{\sigma_{13}}{S_{31}}\right)^2 + \left(\frac{\sigma_{23}}{S_{32}}\right)^2 \geq 1 \quad (2.10)$$

- longitudinal and transverse compression

$$\begin{cases} \left(\frac{\sigma_{11}}{X_C}\right)^2 \geq 1 \\ \left(\frac{\sigma_{22}}{Y_C}\right)^2 \geq 1 \end{cases} \quad (2.11)$$

- through thickness compression

$$\left(\frac{\sigma_{33}}{Z_C}\right)^2 \geq 1 \quad (2.12)$$

- longitudinal and transverse compression

$$\begin{cases} \left(\frac{\sigma_{11}}{S_{21}+S_{31}} \right)^2 + \left[\left(\frac{X_C}{S_{21}+S_{31}} - 1 \right)^2 \right] \frac{\sigma_{11}}{|X_C|} + \left(\frac{\sigma_{12}}{S_{21}} \right)^2 + \left(\frac{\sigma_{13}}{S_{31}} \right)^2 \geq 1 \\ \left(\frac{\sigma_{22}}{S_{21}+S_{32}} \right)^2 + \left[\left(\frac{Y_C}{S_{21}+S_{32}} - 1 \right)^2 \right] \frac{\sigma_{22}}{|Y_C|} + \left(\frac{\sigma_{12}}{S_{21}} \right)^2 + \left(\frac{\sigma_{23}}{S_{32}} \right)^2 \geq 1 \end{cases} \quad (2.13)$$

2.5. Johnson-Cook plasticity model for metals

The Johnson-Cook (J-C) model is a phenomenological constitutive model used to describe the plastic behavior of metals such as steel, aluminum, and lead under extreme conditions of large strains, high strain rates, and high temperatures [53].

The constitutive relation is formulated as follows:

$$\sigma_{VM} = (A + B \bar{\varepsilon}_p^n) \left(1 + C \ln \frac{\dot{\bar{\varepsilon}}_p}{\dot{\bar{\varepsilon}}_0} \right) [1 - (T^*)^m] \quad (2.14)$$

The model defines the equivalent flow stress (σ_{VM}) as the product of three independent terms, each representing a distinct physical phenomenon:

- Strain hardening (first round brackets): This term accounts for the static yield strength and how the material hardens as it undergoes plastic deformation: $\bar{\varepsilon}_p$ is the equivalent plastic strain, A is the initial yield stress, B is the hardening constant, and n is the hardening exponent
- Strain rate sensitivity (second round bracket): This term represents the increase in material strength as the strain rate increases: C is the strain rate constant, $\dot{\bar{\varepsilon}}_p$ is the equivalent plastic strain rate and $\dot{\bar{\varepsilon}}_0$ is a reference strain rate. This allows the model to model the viscosity of the metal under large strain rates, accurately reproducing how metals become "stiffer" during high-velocity ballistic impacts compared to static or quasi-static loading of conventional material characterization tests.
- Thermal Softening (square brackets): This term accounts for the reduction in material strength due to the heat generated during rapid deformation (adiabatic heating): m is the thermal softening exponent and T^* is the homologous temperature defined as:

$$T^* = \frac{T - T_R}{T_M - T_R} \quad (2.15)$$

where T is the element temperature, T_R is the room temperature and T_M is the

melting temperature.

As the temperature approaches the material's melting point, this term forces the flow stress toward zero.

The great advantage of this material model is its simplicity and modularity: each component of the model can be independently deactivated, to ignore for example strain hardening effects or thermal softening (or both).

2.5.1. Modified Johnson-Cook plasticity model

This variant of the previously introduced model presents a slight different formulation of the strain rate term of the equivalent stress equation:

$$\sigma_{VM} = (A + B \bar{\varepsilon}_p^n) \left(1 + \frac{\dot{\bar{\varepsilon}}_p}{\dot{\bar{\varepsilon}}_0}\right)^C [1 - (T^*)^m] \quad (2.16)$$

In this variation the flow stress depends directly on the adimensional strain rate without the natural logarithm. All the constants have the same meaning as in the previously discussed equation 2.14.

2.5.2. Johnson-Cook fracture criterion

The Johnson-Cook fracture model follows the same logic of the stress flow model; the cumulated damage of an element is calculated as:

$$D = \sum \frac{\Delta \varepsilon_p}{\varepsilon_p^f} \quad (2.17)$$

where ε_p is the equivalent plastic strain and ε_p^f is calculated as:

$$\varepsilon_p^f = \left[D_1 + D_2 \exp \left(D_3 \frac{\sigma_m}{\sigma_{VM}} \right) \right] \left(1 + D_4 \ln \frac{\dot{\bar{\varepsilon}}_p}{\dot{\bar{\varepsilon}}_0} \right) (1 + D_5 T^*) \quad (2.18)$$

where σ_m is the mean stress, T^* is the homologous temperature calculated as in equation 2.15 and D_1 , D_2 , D_3 , D_4 and D_5 are material parameters.

2.5.3. Cockcroft-Latham damage criterion

The Cockcroft-Latham damage criterion [54, 55] is shown in equation 2.19. This fracture criterion needs only one material constant W_C to express the amount of ductile damage, therefore it can be determined only with one experiment [56].

$$D = \frac{1}{W_C} \int \sigma_1 \, d\bar{\varepsilon}_p \leq D_C \quad (2.19)$$

where σ_1 is the maximum principal stress, $\bar{\varepsilon}_p$ is the equivalent plastic strain and D_C and W_C are material parameters.

Furthermore, element failure verifies also when reaching a critical temperature T_C , set as the 90% of the melting temperature [57, 58]. The temperature (increasing due to adiabatic heating) is calculated as:

$$T = T_0 + \int \chi \frac{\sigma_{VM}}{\rho \, c_p} \, d\bar{\varepsilon}_p \quad (2.20)$$

where σ_{VM} is the Von Mises equivalent stress, $\bar{\varepsilon}_p$ is the equivalent plastic strain, T_0 is the initial temperature of the material, χ is the Taylor-Quinney coefficient which is a parameter that represents the amount of plastic work converted into heat, ρ is the material density, and c_p is the specific heat of the material.

2.6. Equation of State (EOS)

The Johnson-Cook plasticity model (as many other constitutive relations) needs an equation of state that correlates the variation in density (expansion or compression) with the value of hydrostatic pressure. Many models of equation of state have been developed and are implemented in commercial softwares; here will be presented the Mie-Grüneisen and the Linear-Polynomial models, that have been employed in this work.

2.6.1. Mie-Grüneisen EOS

The Mie-Grüneisen equation of state [59, 60] defines pressure for the compressed material as:

$$p = \frac{\rho_0 C^2 \mu \left[1 + \left(1 - \frac{\gamma_0}{2} \right) \mu - \frac{A}{2} \mu^2 \right]}{\left[1 - (S_1 - 1) \mu - S_2 \frac{\mu^2}{\mu+1} - S_3 \frac{\mu^3}{(\mu+1)^2} \right]} + (\gamma_0 + A \mu) E \quad (2.21)$$

and for expanded material as:

$$p = \rho_0 C^2 \mu + (\gamma_0 + A \mu) E \quad (2.22)$$

where C , A , S_1 , S_2 , S_3 and γ_0 are EOS parameters, E is the internal energy per volume and μ is defined as:

$$\mu = \frac{\rho}{\rho_0} - 1 \quad (2.23)$$

where ρ_0 is the initial density of the material.

2.6.2. Linear-Polynomial EOS

The Linear-Polynomial equation of state [61] computes the pressure for the expanded material as:

$$p = C_0 + C_1 \mu + C_2 \mu^2 + C_3 \mu^3 + (C_4 + C_5 \mu + C_6 \mu^2) E \quad (2.24)$$

and for the compressed material as:

$$p = C_0 + C_1 \mu + C_3 \mu^3 + (C_4 + C_5 \mu) E \quad (2.25)$$

where C_0 , C_1 , C_2 , C_3 , C_4 , C_5 and C_6 are EOS constants, E is the specific internal energy and μ is the same as in eq. 2.23.

2.7. Analytical and Numerical methods for ballistic impact modeling

The modeling of ballistic impact is generally approached through three methodologies: empirical methods, analytical modeling, and numerical simulations.

The empirical methods are the most widespread due to their simplicity of use and the complexity of the problems they are modeling. It offers high accuracy but applies only to the specific threat–target system studied [62]. Analytical modeling and numerical simulation represent the current state-of-the-art for providing predictive tools and deep insight into the complex penetration processes, especially for lightweight armour systems [35].

2.7.1. Analytical models

Analytical modeling is primarily valued for its relative simplicity and speed of computation, making it an highly useful tool for the preliminary design and optimization of ceramic/metal and composite armors [44, 63].

Analytical models are often one-dimensional, analyzing only the normal impact of a metallic projectile [35, 62, 63]. The foundation of many theoretical penetration models rely on hydrodynamic theory, in particular on the work of Tate [64, 65] and Alekseevskii [66]. Their formulation, derived from fluid dynamics theory and modified to integrate the effects of material strenght, models the projectile penetration (in particular long rods) into metallic armors. Sternberg [67], Rosenberg et al. [68], Hauver et al. [69], Bresciani et al [35] have extended it to advanced ceramics.

The modified Bernoulli equation, adopted from this approach, relates the velocities of a deformable projectile and ceramic, assuming a hydrodynamic behavior of the parts in proximity to the impact area [67, 70, 71].

Florence proposed an analytical model based on global energy balances [72], derivating the ballistic limit velocity (BLV). This model assumes that when the projectile strikes the hard ceramic, the tip shatters, and the ceramic breaks into a cone of fractured material that spreads the impact load over the metallic backing plate. The hypothesis proposed by Florence suggests that the energy absorbed by the target at an impact velocity higher than the BLV is the same as the energy absorbed at the BLV.

Ben-Dor et al. [73] presented an improved version of Florence model, considering the ballistic resistance of the ceramic layer in a two component composite armor.

Zaera et al. [62] introduced a fully developed analytical model simulating ballistic impact on ceramic/metal armors. It uses the Tate and Alekseevskii equation for projectile penetration into the ceramic tile and the ideas of Woodward's [70] and Den Reijer's [74] models for the metallic backing response.

Naik et al. [12] proposed an energy-based model predicting ballistic impact behavior of composites and ceramic-composite armors. This model is based on wave theory and the energy balance between the projectile's kinetic energy and energy absorbed by different mechanisms; dissipation modes considered include: kinetic energy of the "V-tent" deformation, deformation energy of primary and secondary yarns, delamination, matrix cracking, bending (considerable in thick targets), compression, and shear plugging.

2.7.2. Numerical simulations

Numerical models solve the full set of equations governing the mechanics of continuous media, and therefore the penetration process, providing detailed insights that aid armor design. Its accuracy depends on the knowledge and understanding of the physical phenomena happening in the projectile-armor interaction and on the choice of material models used, but its major drawback is the high computational time required for each simulation.

In the field of impact engineering and ballistic research the Finite Element Method (FEM) is extensively used, typically employing explicit dynamics. Explicit time integration is best suited for nonlinear problems that undergo time-dependent behavior over short durations [75]. Commonly used explicit dynamic solvers are LS-Dyna [76], Abaqus/Explicit [77], Autodyn [78] and Radioss [79].

FEM models struggle with large deformation problems (like extensive plastic strains) because mesh distortion can cause numerical problems to the solver (so calculations can stall or terminate prematurely) or produce nonphysical or inaccurate results [46]. To prevent excessive distortion, element erosion algorithms are implemented to remove critical elements once certain failure criteria (e.g., maximum effective plastic strain or maximum accumulated damage) are met. However, this deletion is unphysical as it removes mass and energy related to those elements from the system, potentially producing inaccurate results, especially with residual velocities [18].

Scazzosi et al. [18] proposed to couple the Smoothed-Particle Hydrodynamics (SPH) approach with traditional FEM analysis. SPH is a meshfree method initially developed to study fluid problems with a Lagrangian approach, but it is well suited for simulating large deformations in solids, discontinuities, crack formation, comminution, bypassing the intrinsic limitation of fixed-mesh FEM simulations. In the case of ballistic impact on ceramic composite armors, SPH is extremely useful to reproduce the damage mechanics of the material, because each time a solid element reaches a damage level, it gets replaced by one (or more) particles, that are still able to mimic the mechanical resistance to penetration that comminuted ceramic has.

The results of his work, showed in figure 2.8, highlight how the implementation of SPH is beneficial for an higher fidelity reproduction of the damage morphology in the armor.

Kudryavtsev et al. [80] tested an hybrid numerical approach consisting of traditional FEM combined with Discrete Element Method (DEM), shown in figure 2.9 a. In particular, DEM is a discontinuous approach implemented in many CAE commercial softwares that

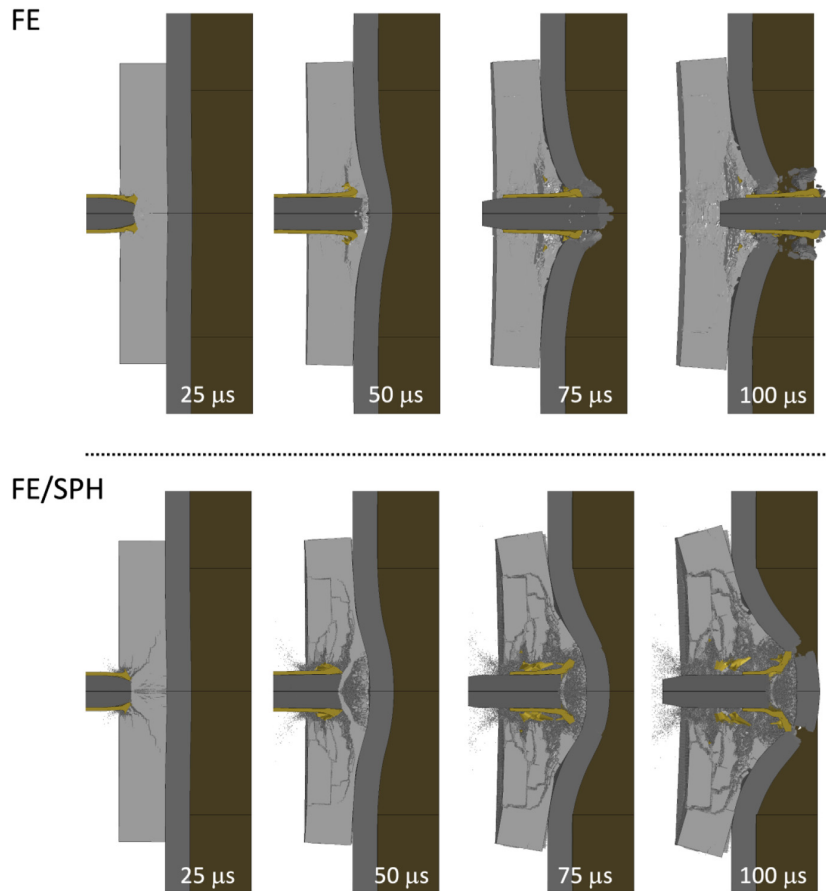


Figure 2.8: Impact sequence of a 12.7 mm AP projectile against Al_2O_3 a multilayer target: comparison of the two numerical methods [18]

uses rigid spherical particles linked by truss-like bonds to simulate elastic solids. In particular, when applied to ceramic materials, Kudryavtsev et al. observed that DEM allows for realistic fragmentation of ceramic targets for various initial velocities and avoids the issue of mass defect caused by element erosion, making it useful for simulating brittle material fracture. The results of the campaign of numerical simulations (for various projectile velocities and particle diameters) are summarized in figure 2.9 b.

To model the large ductile deformations of the projectile, Rahbek et al. [14] introduced the Arbitrary Lagrangian-Eulerian (ALE) method, in combination with a Lagrangian model for the target. The ALE approach decouples the mesh motion from the material motion. The "arbitrary" part means the mesh nodes can be programmed to move in any way that is beneficial. Originally this method was developed to solve complex problems in Computational Fluid Dynamics (CFD) that neither of the traditional methods could handle alone, such as fluid flows involving large deformations and moving boundaries [81]. In this case, the motion of the mesh nodes is optimized to reduce the high distortion

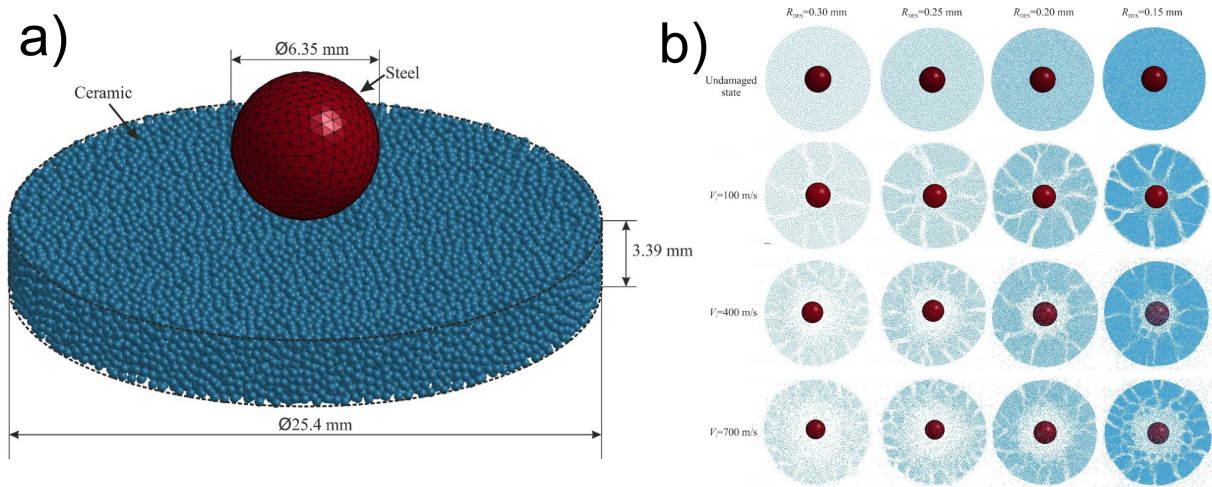


Figure 2.9: a) FE/DEM projectile and target model, b) simulation results for different particle size and projectile velocity [80]

inherent to the large ductile deformations of the projectile (Eulerian advantage), while still obtaining a good boundary-tracking accuracy (Lagrangian benefit). The remapping and rezoning of the mesh adds significant computational cost and generally the hybrid approach is much more difficult to program and implement than "pure" methods. Figure 2.10 shows the coupling of the lagrangian model (the alumina tile) with the eulerian section (projectile); the model includes a surrounding air volume to accomodate the ALE formulation.

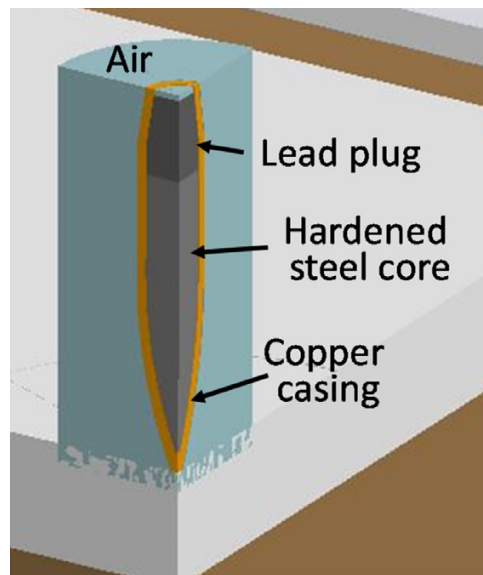


Figure 2.10: Detail of the model with the alumina target (lagrangian) and the 7.62 mm projectile (eulerian) [14]

3 | Reference scientific article

As mentioned in the introduction, this work features a finite element model framework based on a literature review of scientific publications about ballistic impacts on composite Small Arms Protective Inserts.

The main article referenced for the present work is "*Numerical investigation of the high-velocity impact performance of body armor panels*" by Jun Lin et al., 2023 [17]. In this paper the authors discussed numerical models of (i) a 7.62x25 mm Tokarev FMJ projectile impacting a Dyneema soft body armor and (ii) a 7.62 APM2 projectile against a Alumina/Dyneema composite SAPI. The work focuses also on back-face deformation induced by the ballistic impacts that is measured using Roma Plastilina No. 1 clay as backing material, as required by the NIJ standard 0101.06 [11].

The finite element simulations have been validated with experimental ballistic test of both the protection systems.

3.1. Soft body armor

The soft body protection consists of a 250x300 mm UHMWPE laminate with a thickness of 6.3 mm. The model consists of 15 independent sub-laminates stacked without bonding. The laminate is backed by a 50 mm thick plastilina block with the same geometry of UHMWPE panel to measure the armor back-face signature. The simulation consisted of an impact of a 7.62x25 FMJ projectile with 445 m/s initial velocity.

The finite element model is showed in figure 3.1.

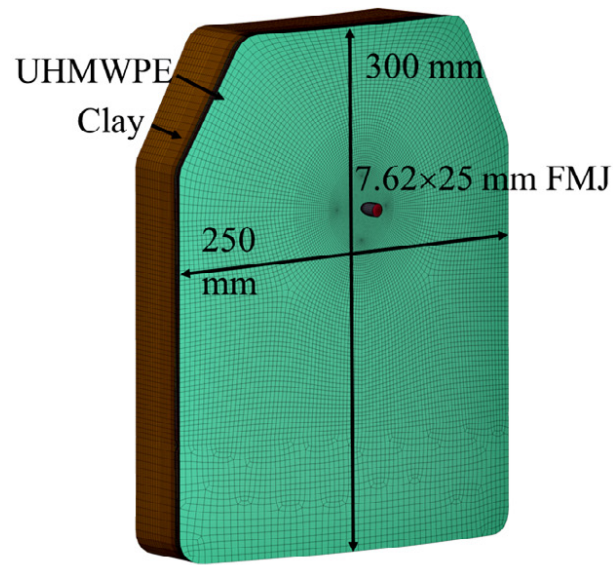


Figure 3.1: Finite element model of the soft body armor [17]

The numerical model has been validated with an experimental ballistic testing campaign, with a conventional setup, to compare both failure morphology in the laminate (figure 3.2) and the permanent dent in the plastilina (figure 3.3). The deformation on the plastilina is equal to 24.5 mm for the experimental test and 23.1 for the finite element simulation.

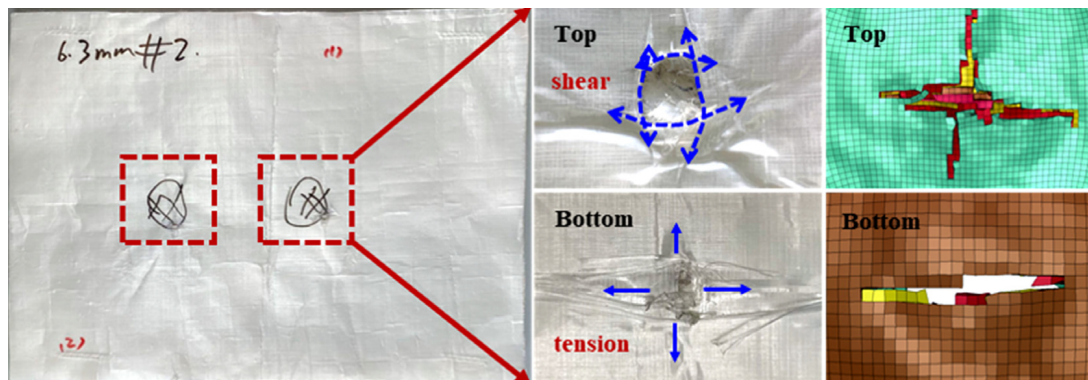


Figure 3.2: UHMWPE damage comparison, experimental vs numerical [17]

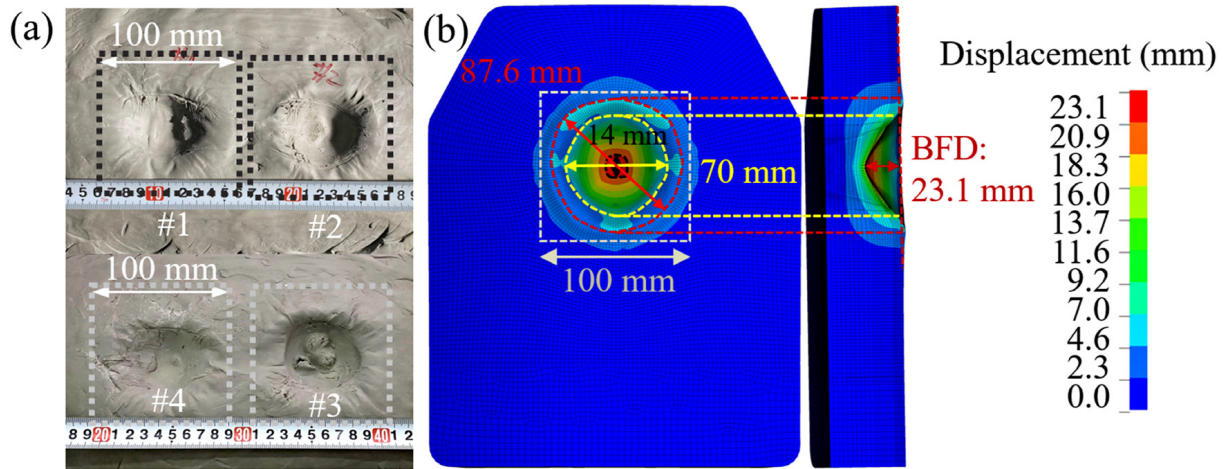


Figure 3.3: Plastilina deformation comparison: (a) experiment, (b) numerical simulation [17]

The results of the numerical simulation are shown in figure 3.4.

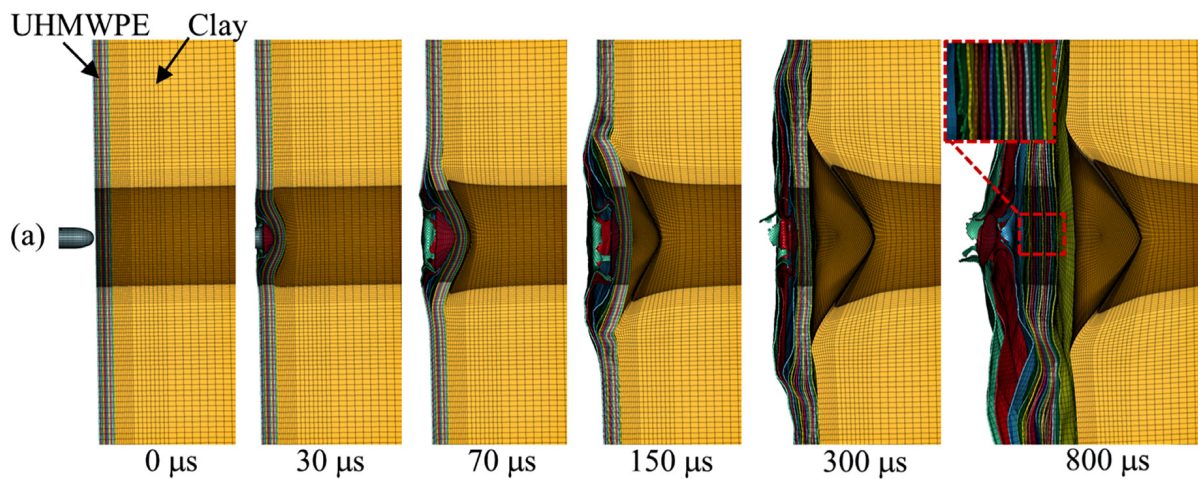


Figure 3.4: Numerical simulation results: impact sequence on the soft body armor [17]

3.2. Ceramic composite armor

The second case considered by J. Lin et al. is the impact of a 7.62 mm APM2 projectile on a composite armor made of a 12 mm thick layer of alumina front face backed by the same UHMWPE panel described in the previous section. Again, a 50 mm thick plastilina block is placed behind the armor system to assess the back face deformation. The initial velocity of the projectile was set to 865 m/s.

Figure 3.5 shows the numerical model previously described.

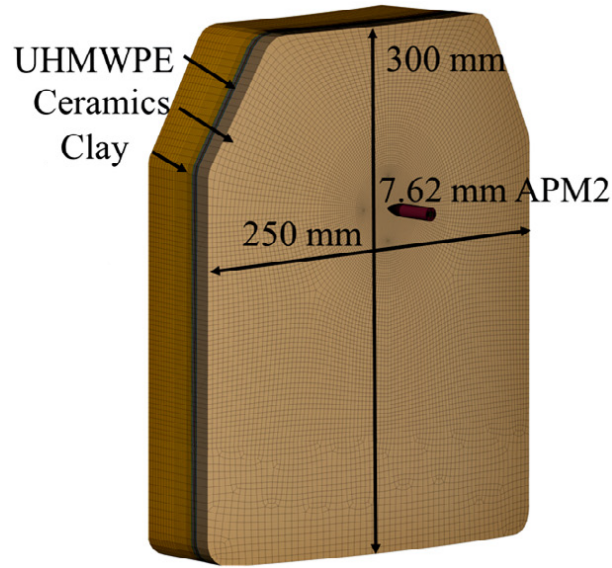


Figure 3.5: Finite element model of the ceramic composite armor [17]

The validation of the finite element model was done comparing the results of a numerical simulation of a ballistic impact of a 7.62 APM2 projectile on the ceramic plate alone with experimental tests conducted by other scholars [82] on the alumina tile. The comparison is shown in figure 3.6.

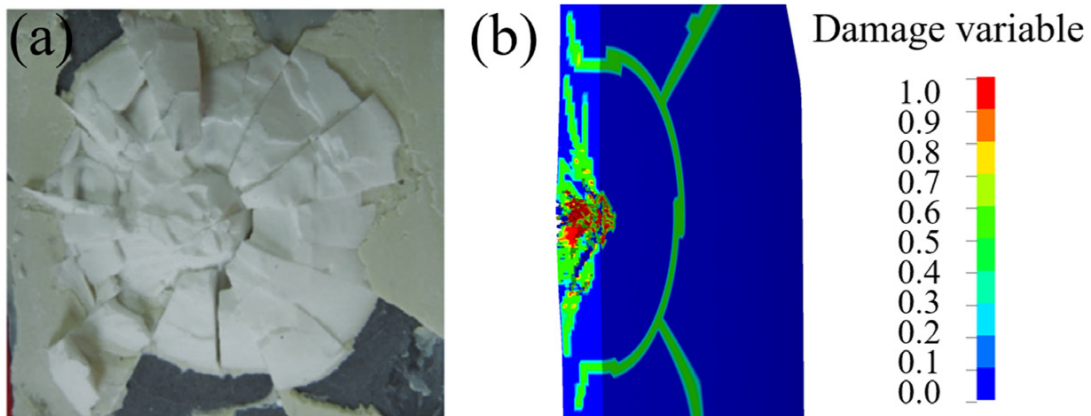


Figure 3.6: Ceramic tile damage comparison: (a) experimental [82] and (b) numerical [17]

The results of the numerical simulation of the ballistic impact on the whole armor exhibit a permanent dent on the clay that measures 19.7 mm. The displacements on the plastilina block are shown in figure 3.7.

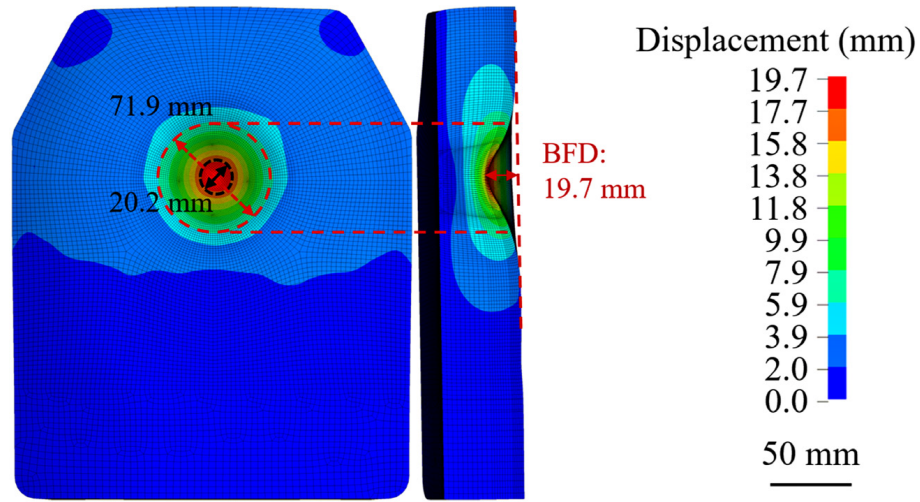


Figure 3.7: Plastilina deformation for the composite ceramic armor [17]

Finally, the impact sequence is presented in figure 3.8.

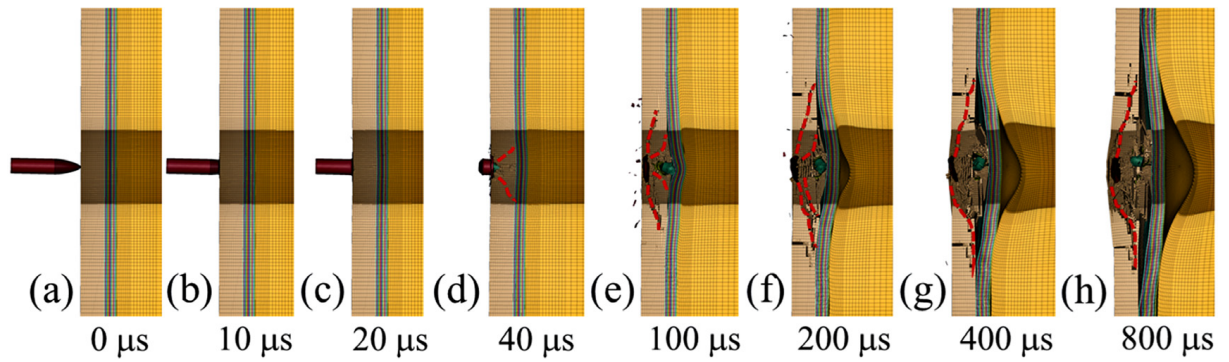


Figure 3.8: Numerical simulation results: impact sequence on the ceramic composite armor [17]

4 | Model description

In this chapter, the details of the finite element model proposed to investigate the degradation of the ballistic performance of the SAPI will be discussed. All the numerical simulations in this work have been run using the LS-Dyna commercial code.

The work of J. Lin et al. [17] (presented in the previous chapter) was used as a starting reference to create an high fidelity replica of the numerical model; omitted data (such as material parameters) in the paper come from a literature review of similar cases or were calibrated based on the results presented in the article.

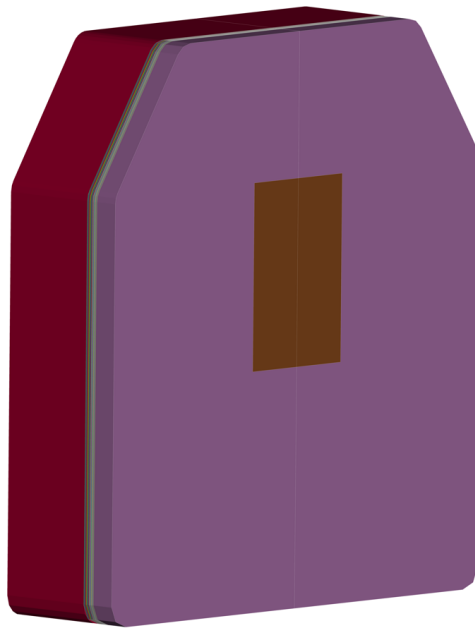


Figure 4.1: Finite element armor system model

The armor system analyzed (shown in figure 4.1) is a dual layer composite Small Arms Protective Insert. The front layer is a 12 mm thick alumina (Al_2O_3) plate, backed by 6.3 mm thick UHMWPE laminate. The laminate is bonded to the ceramic tile with an epoxy resin adhesive. Usually ceramic composite armors are encapsulated in an anti-crack coating [12, 14], but since its ballistic properties are negligible [22] it was omitted and not modeled in the present work.

The resulting geometry of the armor assembly measures 250 mm in width, 300 mm in height and 18.3 mm in thickness. To assess the behind-armor deformation, the model includes a 50 mm thick block of Roma Plastilina #1 clay, in compliance with the NIJ standard 0101.06 [11]. The whole structure is shown in figure 4.1.

Figure 4.2 presents the geometry of the 7.62 mm APM2 bullet. The projectile consists of an hardened steel core, a lead point filler and brass jacket and rear cap, for a total weight of 11 g.

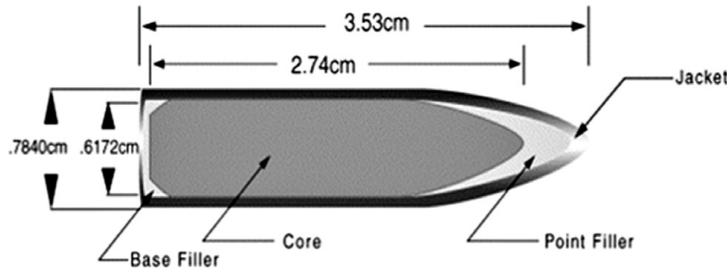


Figure 4.2: 7.62 APM2 projectile geometry and dimensions

4.1. Mesh

For problems with large strains and element erosion, meshing is a crucial step when setting up the analysis. Specifically, there's strong correlation between a material's damage parameters and mesh size; research indicates that several key material parameters are explicitly mesh-dependent, meaning that a set of parameters validated for one mesh size may not be accurate for another [23, 49, 83, 84].

A common practice that has been followed in this work is to model only a portion of both the projectile and the armor [10, 85, 86], considering the symmetry of the system. In this case only half of the protection and bullet have been simulated, to reduce computational time.

4.1.1. Armor assembly

Considering the geometry of the armor is consistent across the thickness and the components, the meshing procedure followed is the same for both the ceramic and composite layers. Each layer of the armor was subdivided in two distinct parts: a rectangular refined section where damage and erosion (due to projectile interaction) was expected and an outer part where the armor contribution was limited to elastic response to stress shock waves; the two distinct parts were joined together with a tie contact, later discussed in

section 4.4.

The first section, colored in brown in figure 4.3, was meshed with element size constant in y direction equal to 0.2 mm and linearly growing element size in x direction from 0.2 mm (near the symmetry plane) to 0.4 mm (closer to the edge). The remaining section of the armor (appropriately partitioned to reduce element distortion and have better control on element sizing) was meshed with a medial axis algorithm [87], creating elements from 1 to 5 mm. A detail of the interface between the two partitions is shown in figure 4.4.

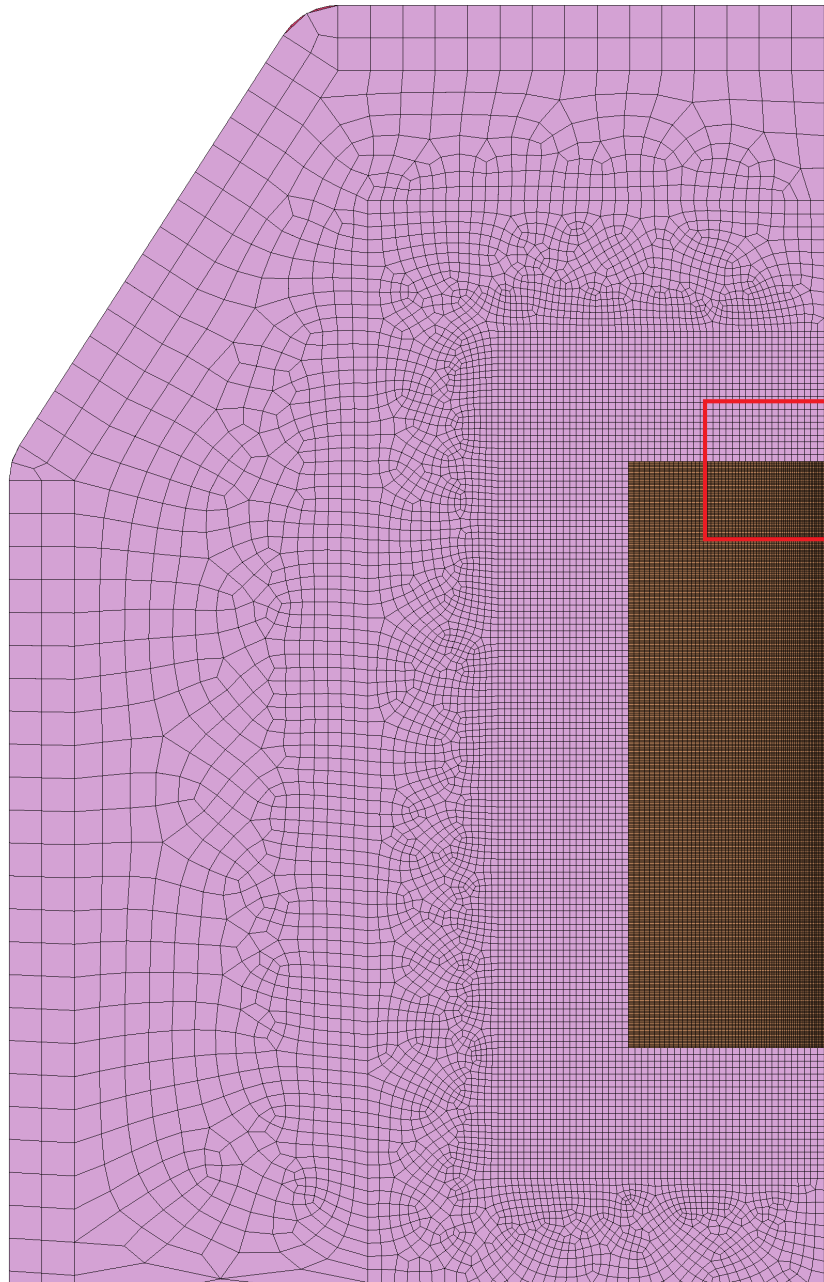


Figure 4.3: Armor mesh; the area in the red box is shown in detail in figure 4.4.

After meshing the 2D front face, tridimensional mesh has been created with an extrusion procedure with a fixed pace of 1 mm for the coarser section and 0.2 mm for the refined area.

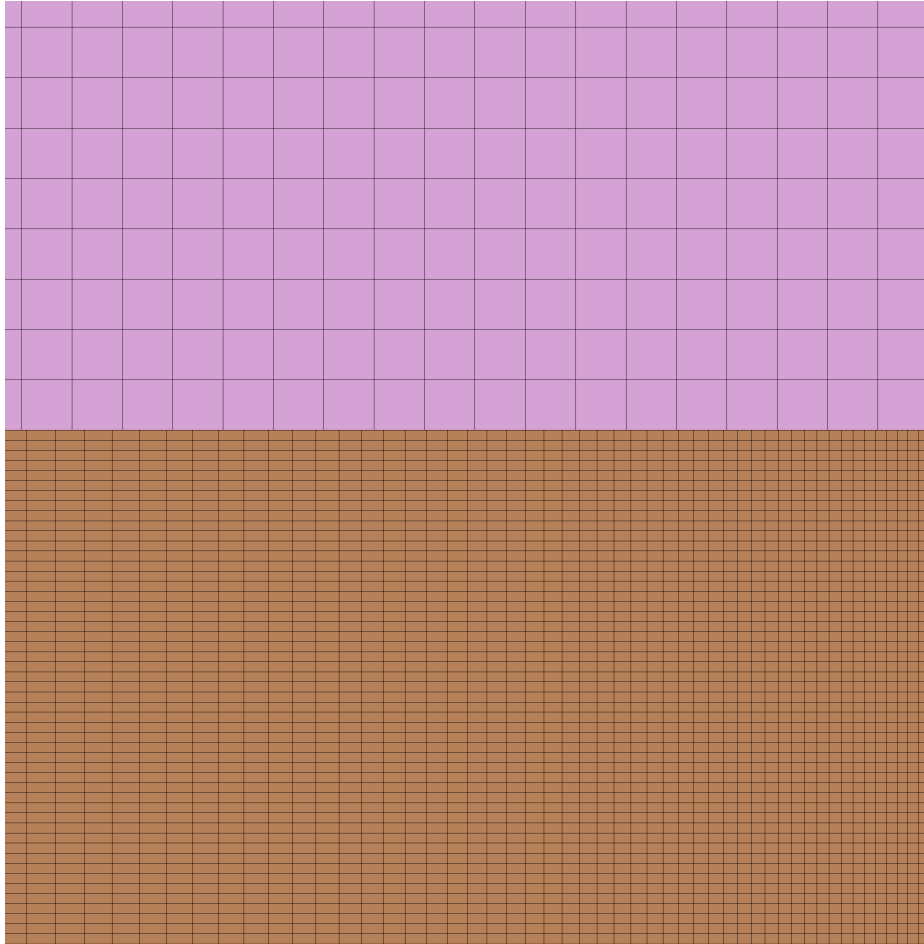


Figure 4.4: Armor mesh detail: interface between coarse and refined sections

To reproduce the delamination damage phenomenon the UHMWPE laminate has been subdivided in 15 plies of equal thickness (0.42 mm) [20, 88], with each layer characterized by two elements through the thickness, as visible in figure 4.5.

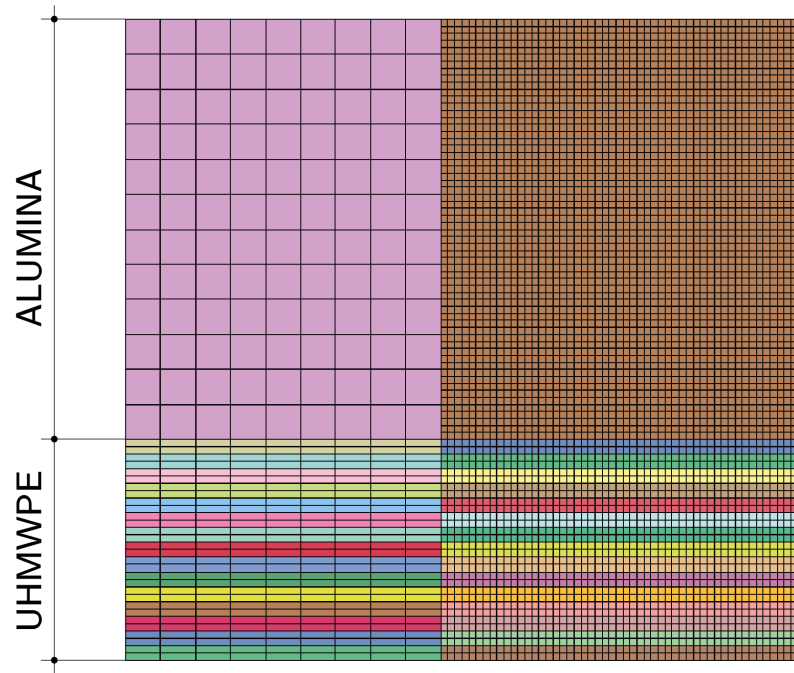


Figure 4.5: Detail side view of the armor mesh

4.1.2. Projectile

The projectile's components (jacket, core, filler and cap) have been meshed partitioning all the geometries into elementary shapes, to uniform element shape and reduce distortion.

To prevent stiffness mis-match and contact issues, the projectile's element size was set to match that of the composite structure in the impact area [22, 49]. The resulting mesh is shown in figure 4.6.

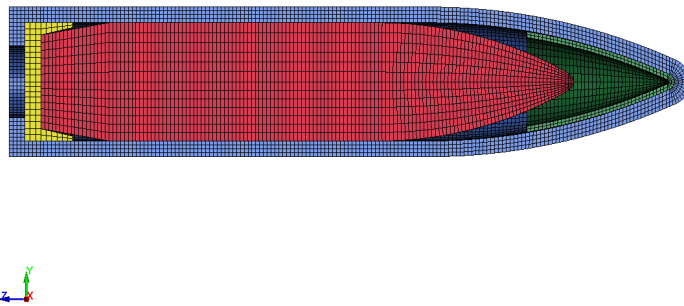


Figure 4.6: Mesh of the projectile

4.1.3. Plastilina

Since no erosion is expected on the plastilina block that registers the back face signature, a coarser 2.5 mm mesh is generated extruding a medial-axis meshed face through the thickness of the part. Figure 4.7 presents the resulting mesh for the part; it's noticeable how on this part is not present the refined mesh partition introduced for the alumina and UHMWPE layers, visible in figure 4.3.

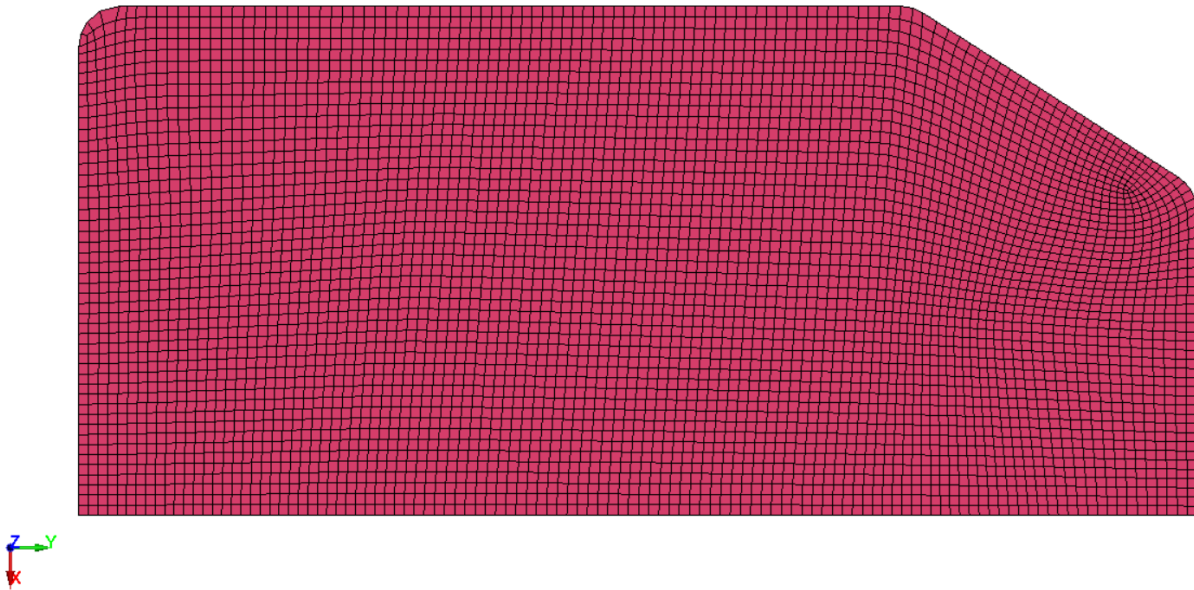


Figure 4.7: Mesh of the plastilina block

4.1.4. Element and Hourglass formulation

8 node solid elements with reduced integration (constant stress brick element) have been chosen for the refined sections of the armor, the projectile and the plastilina.

Since some of the elements of the coarser section of the armor had relatively big width and height compared to their thickness, they were assigned LS-Dyna -2 type solid formulation, that is a fully integrated S/R solid intended for elements with poor aspect ratio [89]; they offer the advantage of the fully integrated solid with reduced shear locking.

The use of reduced integration elements offers several significant advantages in high-velocity impact and large-deformation simulations [90]:

- Significant computational performance improvement: reduced integration elements can cut in half the computational time when compared to fully integrated elements
- Alleviation of "locking" phenomena: fully integrated elements can exhibit spurious

shear energy (shear locking) and artificial incompressibility (volumetric locking). These symptoms are alleviated in lower order elements.

- Improved stability under large deformations: reduced integration elements function effectively even during severe distortion common in impact analysis

Despite these advantages, reduced integration elements are susceptible to hourglassing (non-physical modes of deformation that produce zero stress). To be used effectively, they must be paired with an appropriate hourglass control algorithm (such as Flanagan-Belytschko stiffness or viscous forms) to resist these modes while keeping the associated non-physical energy below 10% of the internal energy [91].

In preliminary simulations severe hourglassing was observed in the UHMWPE laminate elements, as visible in figure 4.8, because of the low stiffness of the material that gets even more reduced by the damage model (previously introduced in section 2.4.3) once the failure criteria are satisfied.

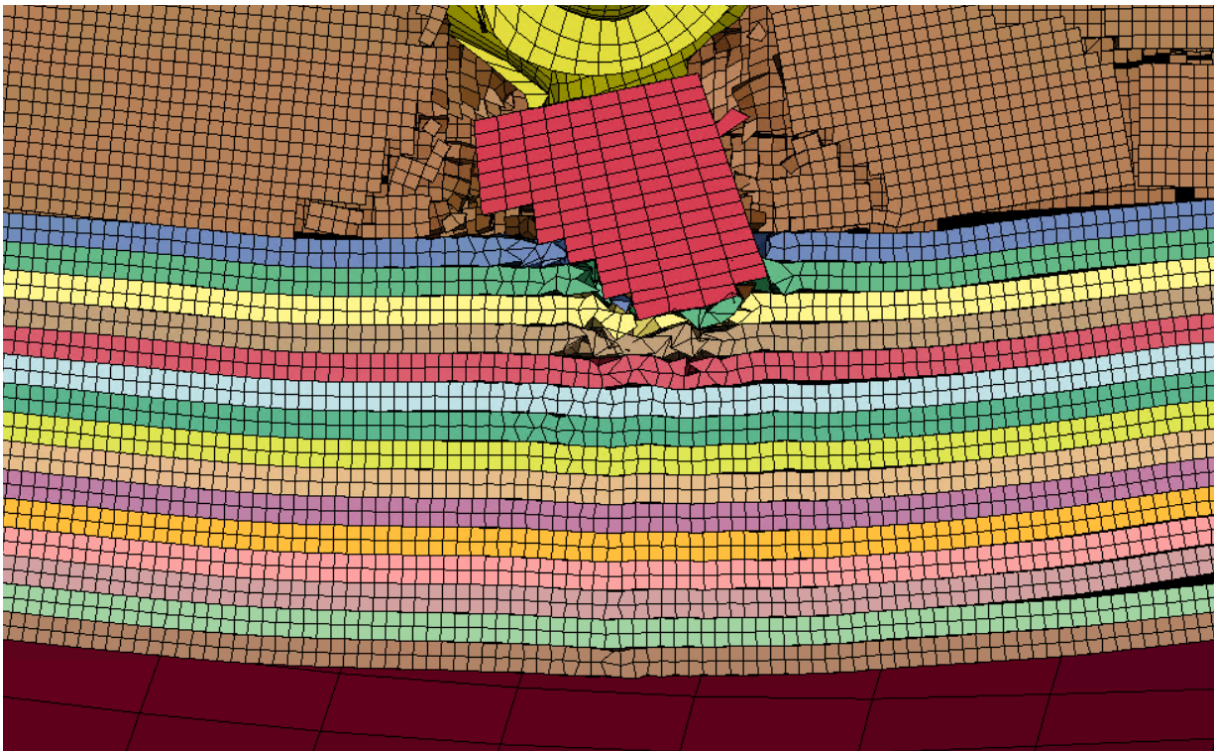


Figure 4.8: Severe hourglassing in the UHMWPE laminate, with no hourglass control

Flanagan-Belytschko stiffness form with exact volume integration hourglass control (type 5) has been selected for all the UHMWPE elements. Stiffness formulation is often preferable over viscous formulation in case of low strain rates (as in the case of the UHMWPE)

[89]. The adoption of this control resulted in a drastic reduction of the hourglass phenomenon, as shown in figure 4.9.

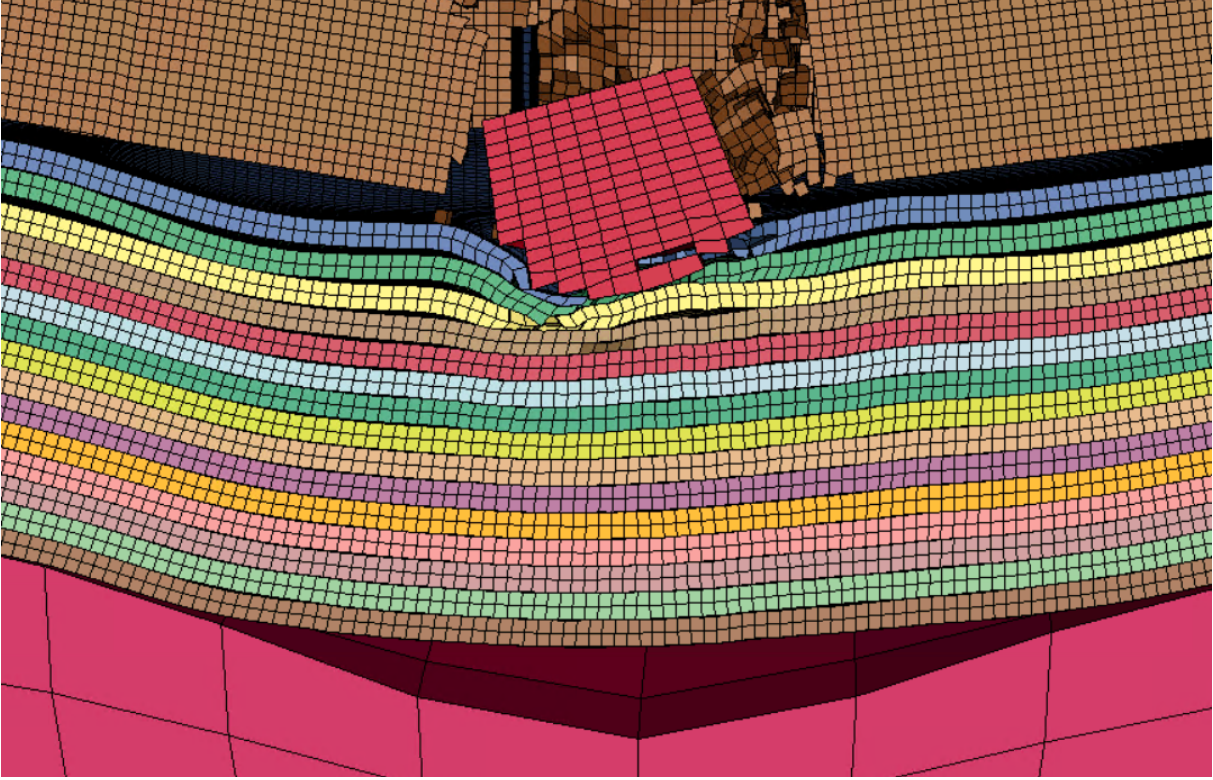


Figure 4.9: UHMWPE laminate with Flanagan-Belytschko stiffness form hourglass control: low to none hourglassing

For all the other reduced integration elements (projectile, ceramic layer and plastilina block) standard LS-Dyna type 1 (viscous) hourglass control was selected.

In table 4.1 are summarized the whole assembly mesh statistics. It is possible to note that approximately 64% of the elements of the model are utilized to model the ceramic layer of the armor.

Part	Element size [mm]	No. of nodes	No. of elements
Ceramic	0.2 - 5.0	1.99e6	1.85e6
UHMWPE	0.2 - 5.0	1.79e6	883e3
Plastilina	1.0 - 2.5	114e3	109e3
Projectile	0.2 - 0.35	43.7e3	38.3e3
Total	0.2 - 5.0	3.94e6	2.88e6

Table 4.1: Mesh statistics by part

4.2. Material models

4.2.1. Alumina

The alumina (Al_2O_3) ceramic front plate has been modeled with the Johnson-Holmquist II [36], previously described in section 2.3.2. This model is implemented in LS-Dyna as MAT_110 (JOHNSON_HOLMQUIST_CERAMICS) [61].

The related material constants are reported in section A.1.

4.2.2. UHMWPE

The UHMWPE laminates mechanical behavior is described with an elastic orthotropic material model with the Tsai-Wu stress failure theory for composites [52]. The corresponding material model selected in LS-Dyna is MAT_59 [92].

The in-plane compressive failure, consisting of micro-buckling in the laminate, (controlled by equation 2.13) is ignored, setting an infinite value of in-plane compressive strengths (X_C and Y_C).

To model delamination more effectively, the laminate is meshed as 15 distinct plies of solid elements, as already described in section 2.4.2. The delamination failure criterion of the Tsai-Wu theory (equation 2.10) is thus ignored, assuming that delamination is possible only between the 15 meshed plies.

The contacts between the plies are modeled by means of a surface-to-surface tiebreak [89], reproducing the de-bonding of the adhesive layers, with the following failure criteria:

$$\left(\frac{|\sigma_n|}{NFLS}\right)^2 + \left(\frac{|\tau|}{SFSL}\right)^2 \geq 1 \quad (4.1)$$

where $|\sigma_n|$ and $|\tau|$ are peak values of normal and shear contact stresses and $NFLS$ and $SFSL$ are material parameters.

Figure 4.10 shows a comparison between two simulations conducted on the "soft" body armor case (see chapter 3.1) to highlight the effects of the $NFLS$ and $SFSL$ tiebreak parameters: in figure 4.10a the values of the parameters are set to 12 and 26 MPa respectively, leading to an overestimation of the strength of the armor; figure 4.10b shows the results of the same simulation with proper $NFLS$ and $SFSL$ parameters gathered from literature[26]. For these parameters the laminate shows clear signs of delamination and a more realistic damage morphology.

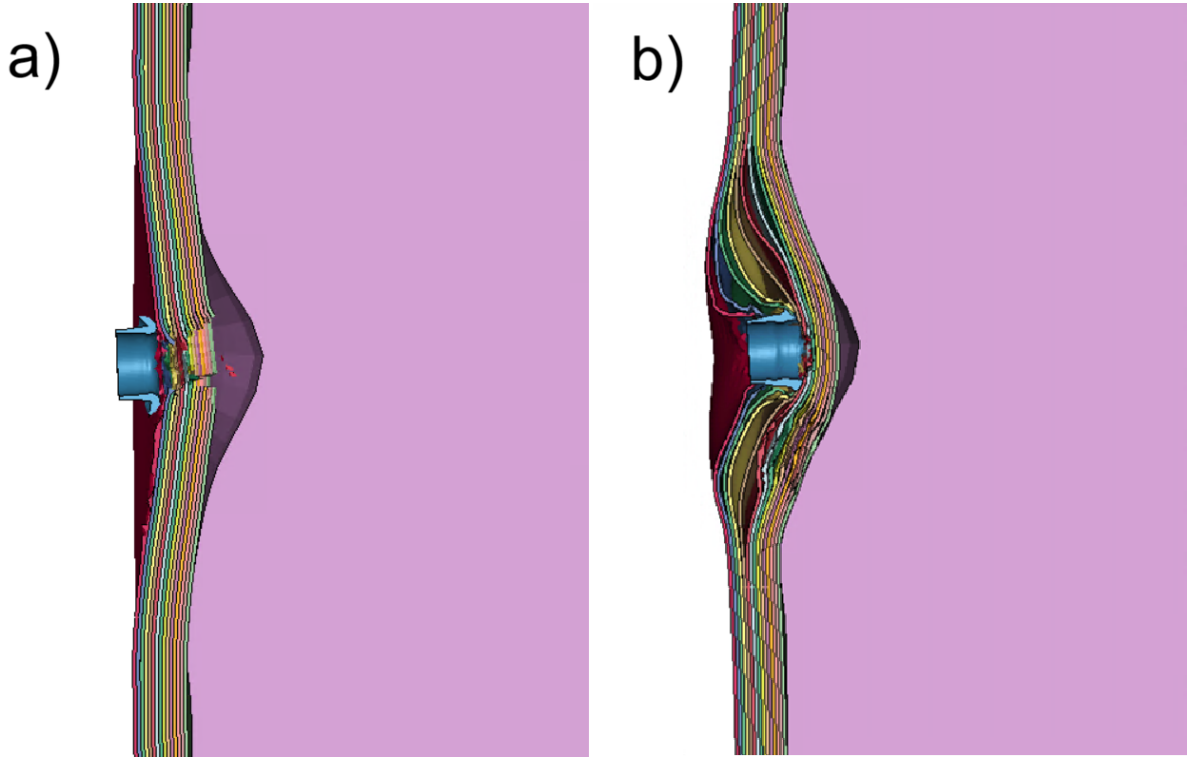


Figure 4.10: Impact simulation comparison at $70 \mu s$ for different $NFLS$ and $SFSL$ parameters values

The $NFLS$ and $SFSL$ values that have been selected for all the simulations are reported in table 4.2.

Parameter	Value
Maximum normal contact stress $NFLS$ [MPa]	1 [26]
Maximum shear contact stress $SFSL$ [MPa]	1.6 [26]

Table 4.2: Surface-to-surface tiebreak contact parameters

The material model `MAT_59` does not have an element erosion criterion [61]: once one of the failure criteria is satisfied, the relative stiffness of the failed element goes to zero; this results in a sharp strain increase and causes the element to distort in a non-physical manner. To prevent this behavior an erosion criterion has been added with the card `ADD_EROSION` [61]: limit values of maximum principal strain and volumetric strain are adopted.

Parameter	Value
Maximum principal strain <i>MXEPS</i> [-]	0.8 [17]
Volumetric strain <i>VOLEPS</i> [-]	-0.8 [49]

Table 4.3: UHMWPE erosion parameters

The UHMWPE material parameters used in the MAT_59 material card are listed in section A.2.

4.2.3. Projectile

The 7.62 mm APM2 projectile's components, namely the hardened steel core, the brass jacket and rear cap and the lead filler, have been modeled with the Johnson-Cook plasticity model and its modified variant (described in section 2.5). They are implemented in LS-Dyna as MAT_015 and MAT_107 respectively [61].

The J-C model was coupled with the Mie-Grüneisen equation of state, EOS_004 (section 2.6.1) [61]. The failure criteria used were the Johnson-Cook (section 2.5.2) and the Cockcroft-Latham (section 2.5.3) fracture models.

Table 4.4 summarizes the models selected to model the material behavior of the projectile's components.

Part	Material model	Failure criterion	EOS
Steel Core	Modified Johnson-Cook	Johnson-Cook	-
Lead Filler	Modified Johnson-Cook	Cockcroft-Latham	-
Brass Jacket and Cap	Johnson-Cook	Johnson-Cook	Mie-Grüneisen

Table 4.4: Material model, failure criterion and equation of state by part

The parameters for the material models and equation of state of steel, brass and lead can be found in sections A.3, A.4 and A.5 respectively.

4.2.4. Plastilina

The plastilina block that registers the back-face signature of the armor system has been modeled with the Johnson-Cook model coupled with the Linear-Polynomial equation of state (already introduced in section) 2.6.2) that is implemented as EOS_001 in LS-Dyna

[61]. The material parameters for the plastilina model (constitutive relation and equation of state) are listed in section A.6.

4.3. Boundary conditions

Symmetry constraint has been applied to the nodes of the assembly (armor, plastilina and projectile) in correspondence of the half symmetry plane (no displacement in the normal direction and rotation allowed only around the axis perpendicular to the plane). The nodes on the lower surface of plastilina block are constrained in the z direction (the projectile's axis).

An initial velocity of 865 m/s (total kinetic energy of 4057 J) was applied to the projectile, to obtain comparable results with Lin's work [17]. Figure 4.11 summarizes the boundary conditions imposed on the system.

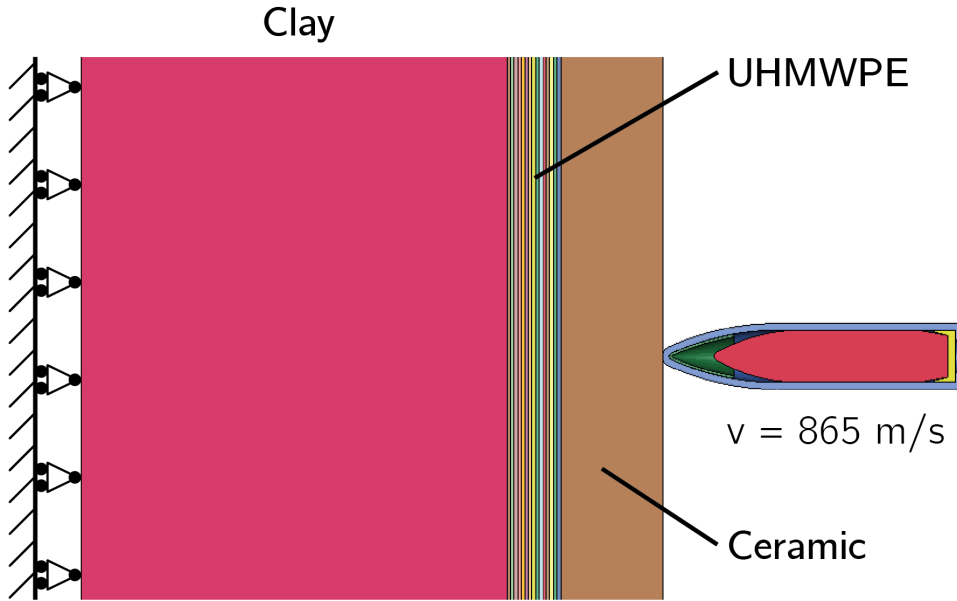


Figure 4.11: Boundary and initial conditions of the ballistic impact

4.4. Contact models

An eroding surface-to-surface contact formulation was used to model the interactions between all the parts of the projectile and the armor system, including self contact for each part.

This contact features a segment-based penalty formulation (SOFT option set to 2) that checks surface penetrations and edge-to-edge penetration (DEPTH option equal to 5) to

prevent initial penetrations between severely distorted jacket elements (made of brass) and the ceramic front layer of the armor [89].

The refined mesh subsection of each layer of the armor (as described previously) is joined to the rest of the relative layer with a tied surface-to-surface contact, to prevent detachments or slips.

To reproduce the adhesive between the alumina front layer and the UHMWPE laminate an additional tiebreak contact has been added, joining the ceramic and the first UHMWPE ply, with the same formulation reported in equation 4.1. The *NFLS* and *SFLS* values selected are 12 and 26 MPa respectively, to mimic the properties of an epoxy resin [22].

4.5. Damage creation

Modeling damage in a SAPI is a complex task and involves reproducing the failure phenomena of the constitutive components of the protection system. The aforementioned radiography diagnostics, while being able to provide data about cracks, dishing and comminution in the ceramic tile, is not able to detect any damage in the composite laminate: because of its low density the UHMWPE (and consequently any of its damage) does not appear visible in x-ray images [10].

Two damage scenarios have been considered in this work, that are common in decommissioned armors coming from operative missions:

- Previous ballistic hit
- Crack in the ceramic tile

4.5.1. Previous ballistic hit

Even with radiographic data, reproducing the damage of a ballistic impact in a ceramic SAPI with a finite element model is difficult to achieve; regular x-ray data is in format of bidimensional images (as shown in figure 4.12), while the damage morphology is tridimensional, furthermore meshing the cracks and the comminuted material can be a challenge because of the highly irregular shape of those features that classical finite elements struggle to reproduce.

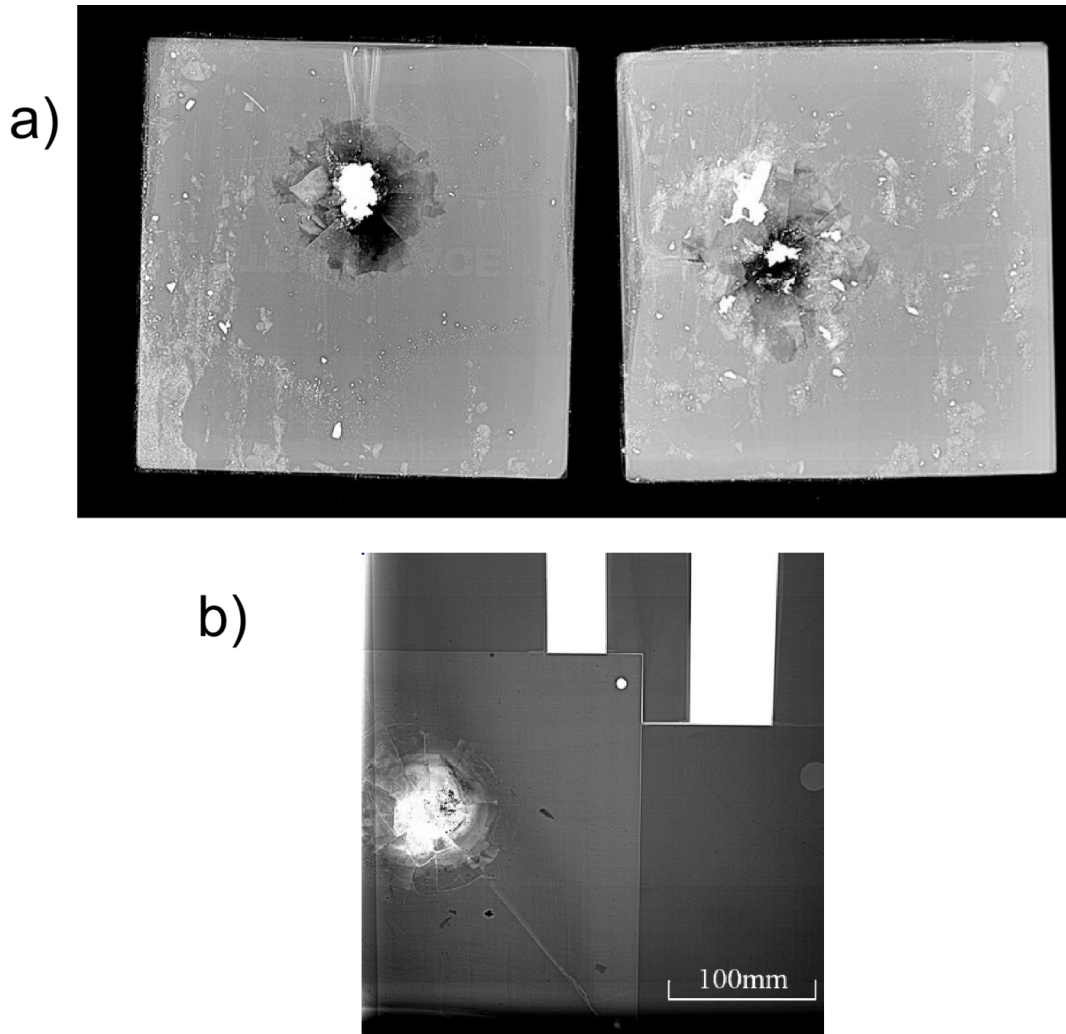


Figure 4.12: Example of radiographies of SAPIs after ballistic impact: a) 12.7 mm projectile against B_4C /UHMWPE armor [45], b) 12.7 mm API projectile against B_4C /UHMWPE armor [93]

The approach followed in this work to model damage caused by a previous ballistic hit on the armor is to run a simulation of a first impact on an intact protection and use the resulting final state of the armor as the initial damaged condition for subsequent simulations.

The damaged state of the armor was obtained with the LS-Dyna card `INTERFACE_SPRINGBACK_LSDYNA` [89]. This card was originally developed to simulate the spring-back phenomenon following metal sheet forming processes (hence the name): the solver saves the final state of a simulation as an input file that can be used for a following simulation with different load cases, boundary conditions, contacts etc. Here is stored all the information about displacement field (and its derivatives), stress states and cumulated

damages for each element.

In the first simulation of impact (used to create the damage), heavy damping is applied after 0.5 milliseconds (when the projectile has completely stopped and the damage morphology is fully developed) to remove non relevant vibrations. The solver stops at 1.5 milliseconds and this final state is saved. In this process the projectile is removed from the simulation, since its effect on the residual ballistic resistance of the armor is considered negligible and its highly distorted elements make drop the timestep (and so slowing down the following simulation). The resulting model created with this procedure is shown in figure 4.13.

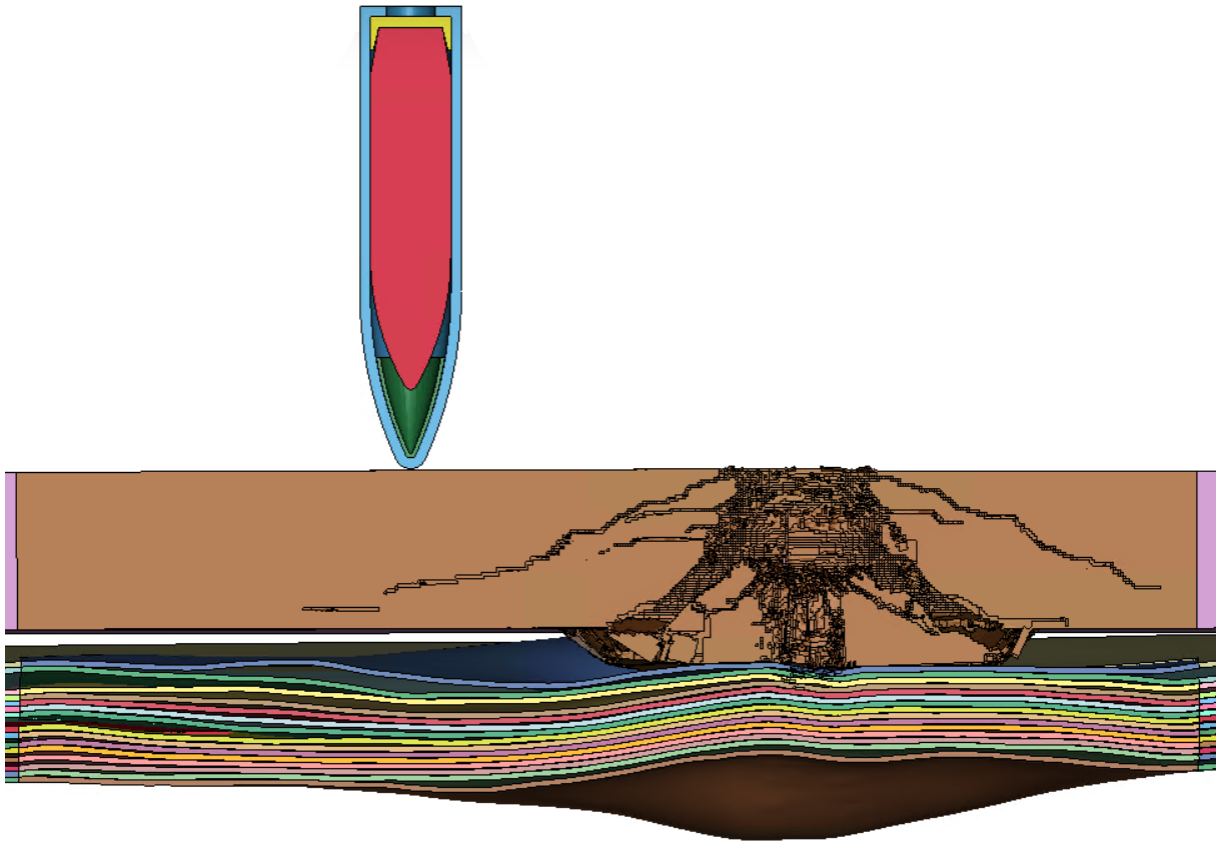


Figure 4.13: Initial state of second hit simulation

4.5.2. Crack in the ceramic tile

Cracks in the ceramic plate are a common problem in composite SAPIs, they can be a result of low velocity, non-ballistic impacts (frequently occurring in combat situations [94]), or simply mishandling by the operator. The severity of this kind of damage is variable and in some critical cases it may result in a premature end of the service life of the protection. Figure 4.14 shows a typical x-ray image of what a cracked tile in a SAPI

looks like; the prediction of the residual ballistic protection of this kind of damage is very hard to perform with traditional non-destructive methods.



Figure 4.14: Radiography of a cracked ceramic layer of a SAPI [94]

In the current work, it was chosen to simulate a severe damage consisting of a crack through all the thickness and the width of the plate; to create this kind of damage the finite element model of the ceramic tile of the SAPI was splitted in two sub-parts. Without remeshing the model, new nodes were created on the crack separation interface to make elements that were connected (and sharing the same nodes) independent.

The result of this procedure, shown in figure 4.15, is a crack through all the width and the thickness of the alumina plate that follows the shape of the elements that have been separated; since the mesh was generated with an extrusion, the crack profile stays constant through all the thickness of the plate.

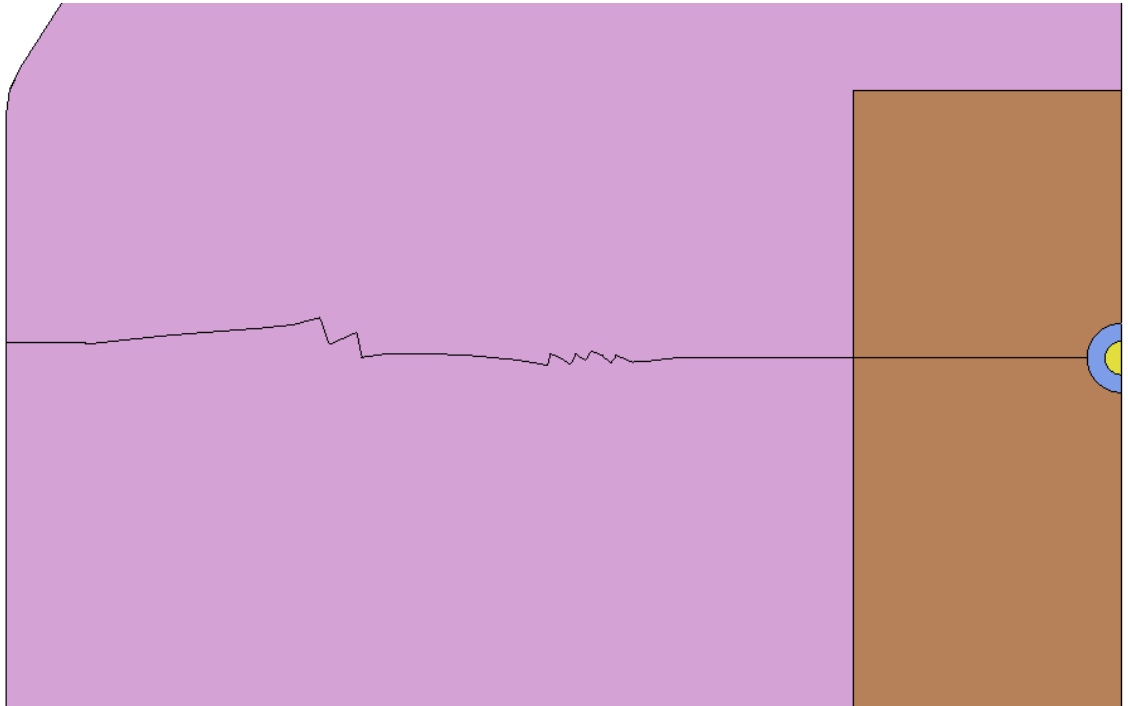


Figure 4.15: Finite element model of the cracked ceramic front plate

5 | Results

5.1. Model validation

The validation of the modeling framework presented in the previous chapter is performed comparing the results of the simulations with the results reported by Lin et al. [17]. In details the two cases taken in to exam in the referenced paper (namely the "soft" body armor and the composite ceramic SAPI) were reproduced, to confront the failure morphologies and the back face signatures (in terms of plastilina displacement) of the armor systems between Lin's publication and the current work.

5.1.1. Soft body armor

The first case proposed by Lin et al. is a ballistic impact of a 7.62x25 mm FMJ Tokarev against the 6.3 mm thick UHMWPE sheet. The initial projectile velocity is 445 m/s.

Figures 5.1 and 5.2 show the impact sequence comparison of the referenced paper and the current work for 0, 30, 70, 150, 300 and 800 μ s.

The comparison between the two simulation results confirms the validity of the UHMWPE model presented in this work. The projectile's core gets completely eroded after piercing through the first 4 plies of the UHMWPE laminate and the tearing morphology in the UHMWPE is closely reproduced too.

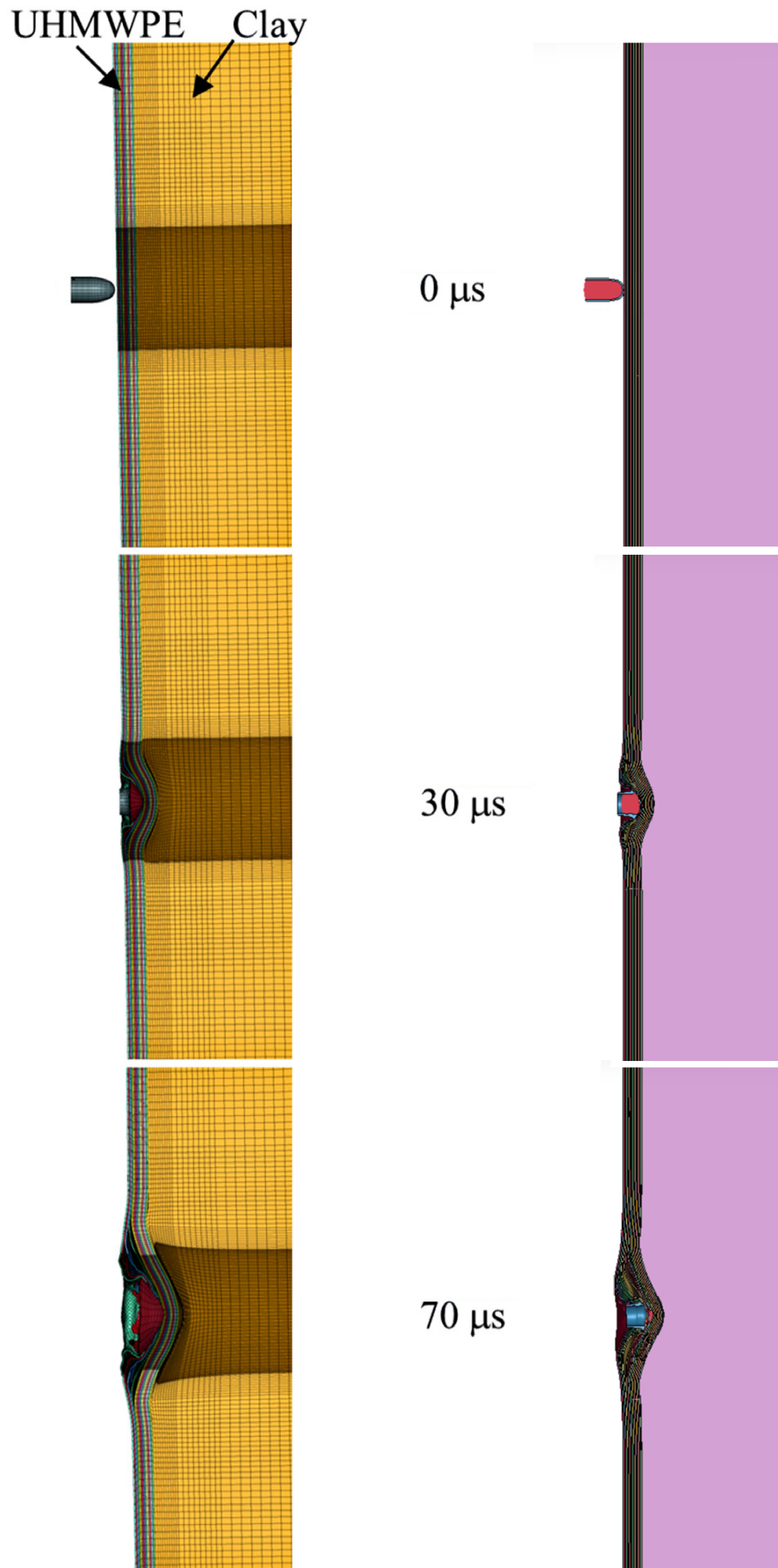


Figure 5.1: Impact sequence on the soft body armor comparison (first half), on the left the original work by Lin et al. [17], on the right the current work.

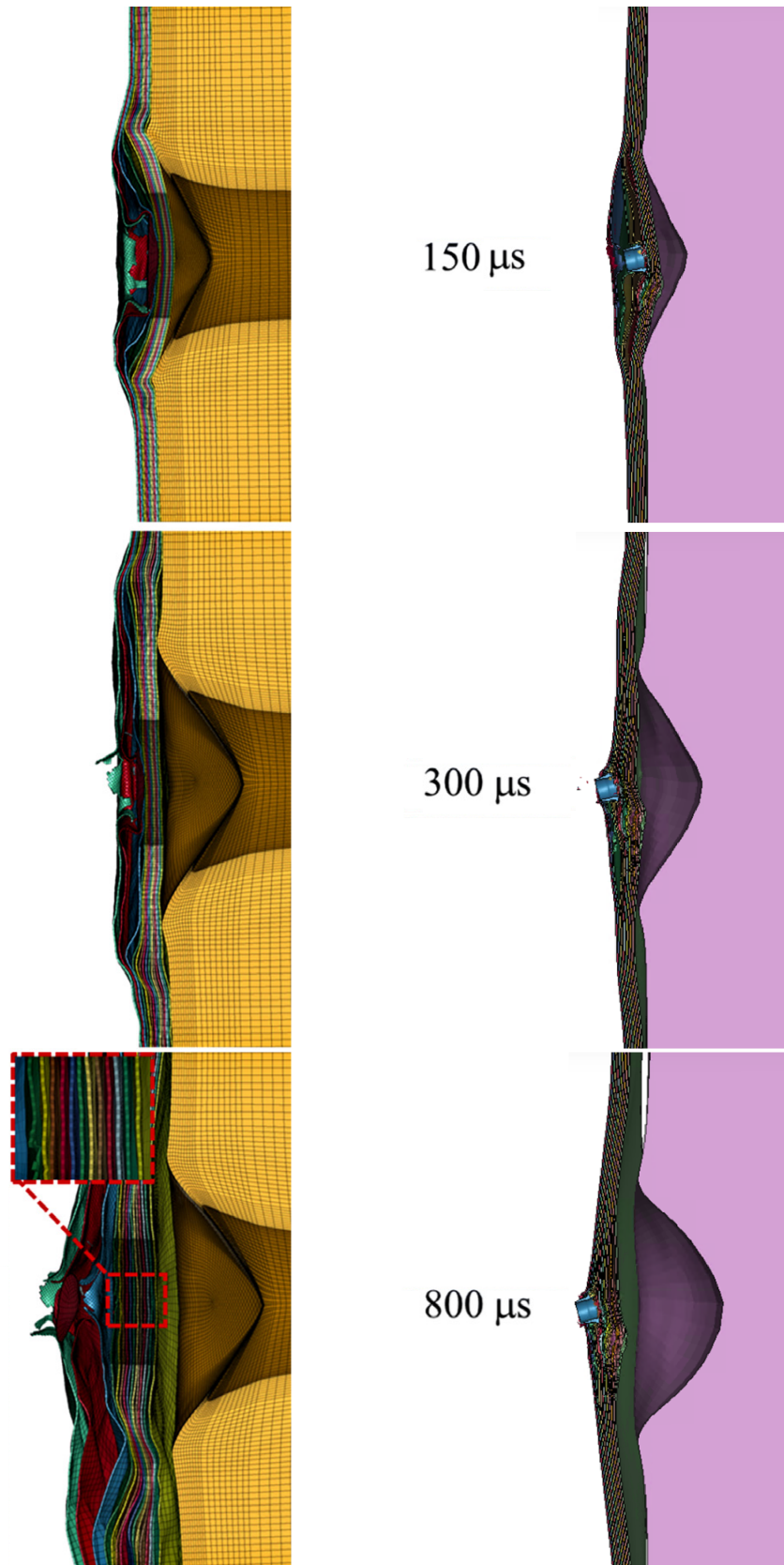


Figure 5.2: Impact sequence on the soft body armor comparison (second half), on the left the original work by Lin et al. [17], on the right the current work.

Figure 5.3 shows a side by side comparison of the final snapshot of the simulations, at $800 \mu\text{s}$; the delamination in Lin's model is more pronounced compared to the current work, but the final bulging in the plastilina block is consistent across the two models.

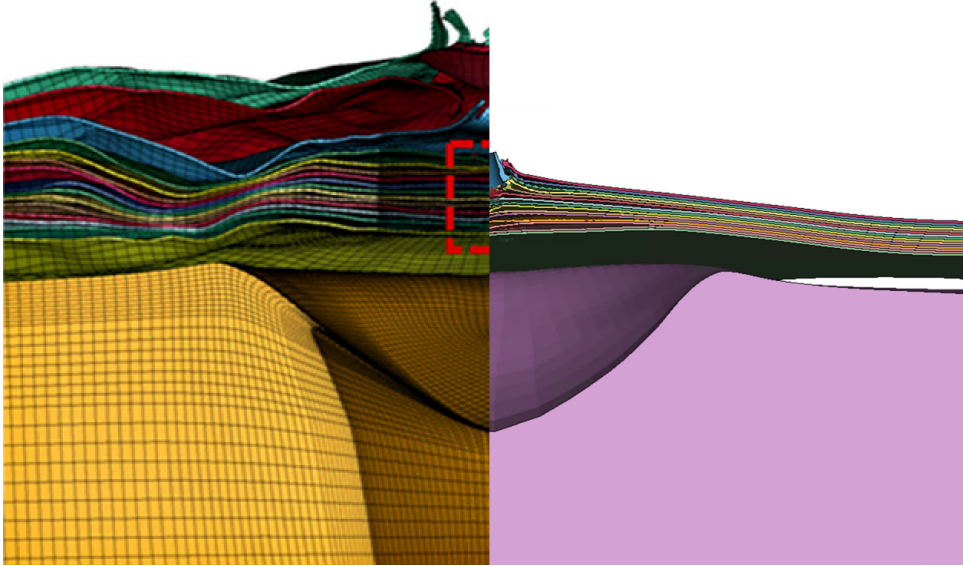


Figure 5.3: Detail of the simulation comparison at $800 \mu\text{s}$, on the left the original work by Lin et al. [17], on the right the current work.

The back-face signature registered by the plastilina block is shown in figure 5.4: Lin reported a maximum deformation in the plastilina of 24.5 mm, slightly below the 25.7 mm deformation measured in the current model.

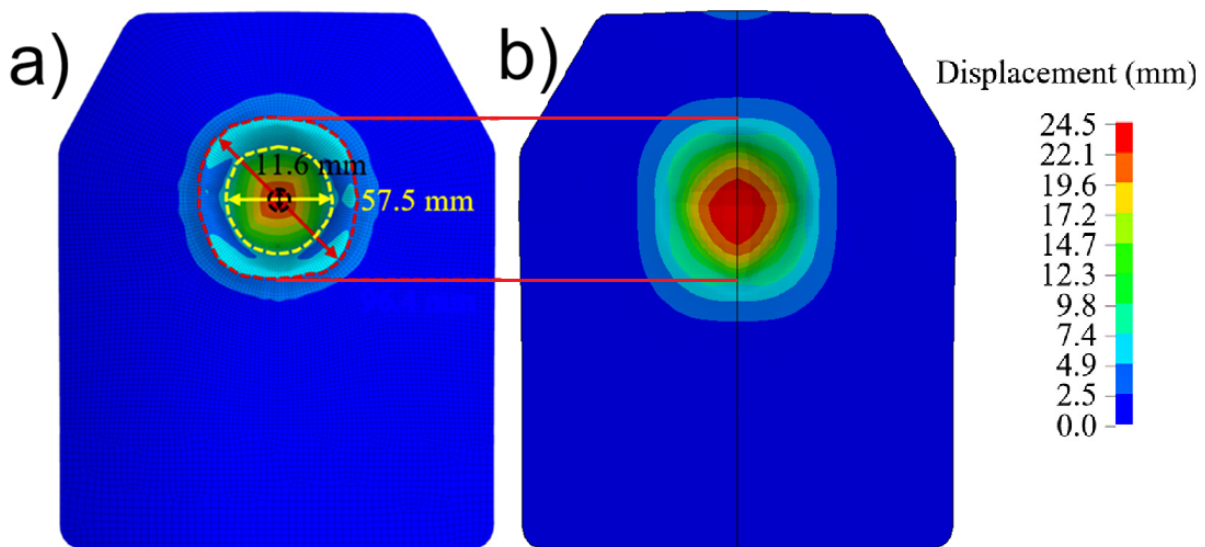


Figure 5.4: Plastilina final displacement comparison for the soft body armor: a) original work by Lin et al. [17], b) current model.

5.1.2. Composite ceramic SAPI

The second case taken into consideration to validate the finite element model (and then used as the foundation for subsequent simulations) is the ballistic impact of a 7.62 APM2 projectile against a alumina/UHMWPE SAPI. Compared to the previous case, this new model adds a 12 mm alumina tile in front of the UHMWPE laminate, to reproduce a common layout of personal protections.

The failure equivalent plastic strain for the alumina plate, FS , (mentioned in section 2.3.2) was not provided in the original reference, and given its strong mesh sensitivity [18] it was iteratively calibrated to closely match the results of Lin's work. A literature review confirmed that the selected value of FS , equal to 0.525, is comparable with values adopted by other scholars when modeling alumina with the Johnson-Holmquist II model [8, 13, 16, 18, 21, 24, 44, 84].

The impact sequence is presented in figures 5.5 and 5.6. This comparison shows how the model proposed in the current work is able to faithfully replicate all the features observed in Lin's simulation; the projectile is drastically eroded in the impact process, the interaction with the alumina tile generates the conventional conoid of comminuted ceramic characteristic of impacts on fragile materials. The steel core of the projectile is able to penetrate completely the ceramic layer of the armor; its residual kinetic energy is then absorbed by the UHMWPE laminate, that shows low to none signs of delamination. After the initial impact transient the armor assembly (consisting of ceramic tile and UHMPE sheet still bonded together) is rebounded from the plastilina block: since the SAPI is simply resting on top of it, the kinetic energy released by the impact is then transmitted to the plastilina as an impulse and this causes the two armor and plastilina to separate.

The ceramic conoid morphology is consistent between the two models where the mesh is refined in Lin's model: the model realized in this model has a larger section with refined mesh, that is able to capture a realistic fracture behavior even farther from the projectile.

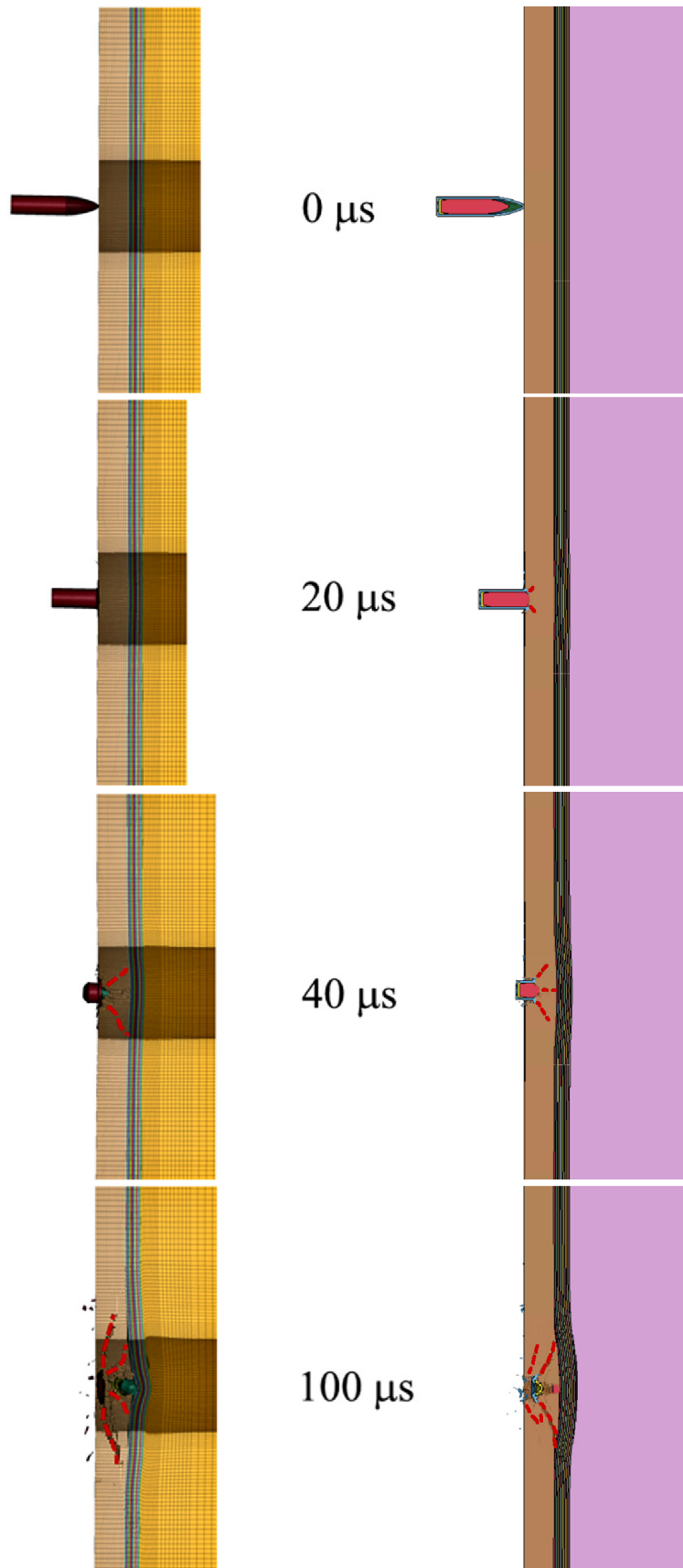


Figure 5.5: Impact sequence on the ceramic composite SAPI comparison (first half), on the left the original work by Lin et al. [17], on the right the current work.

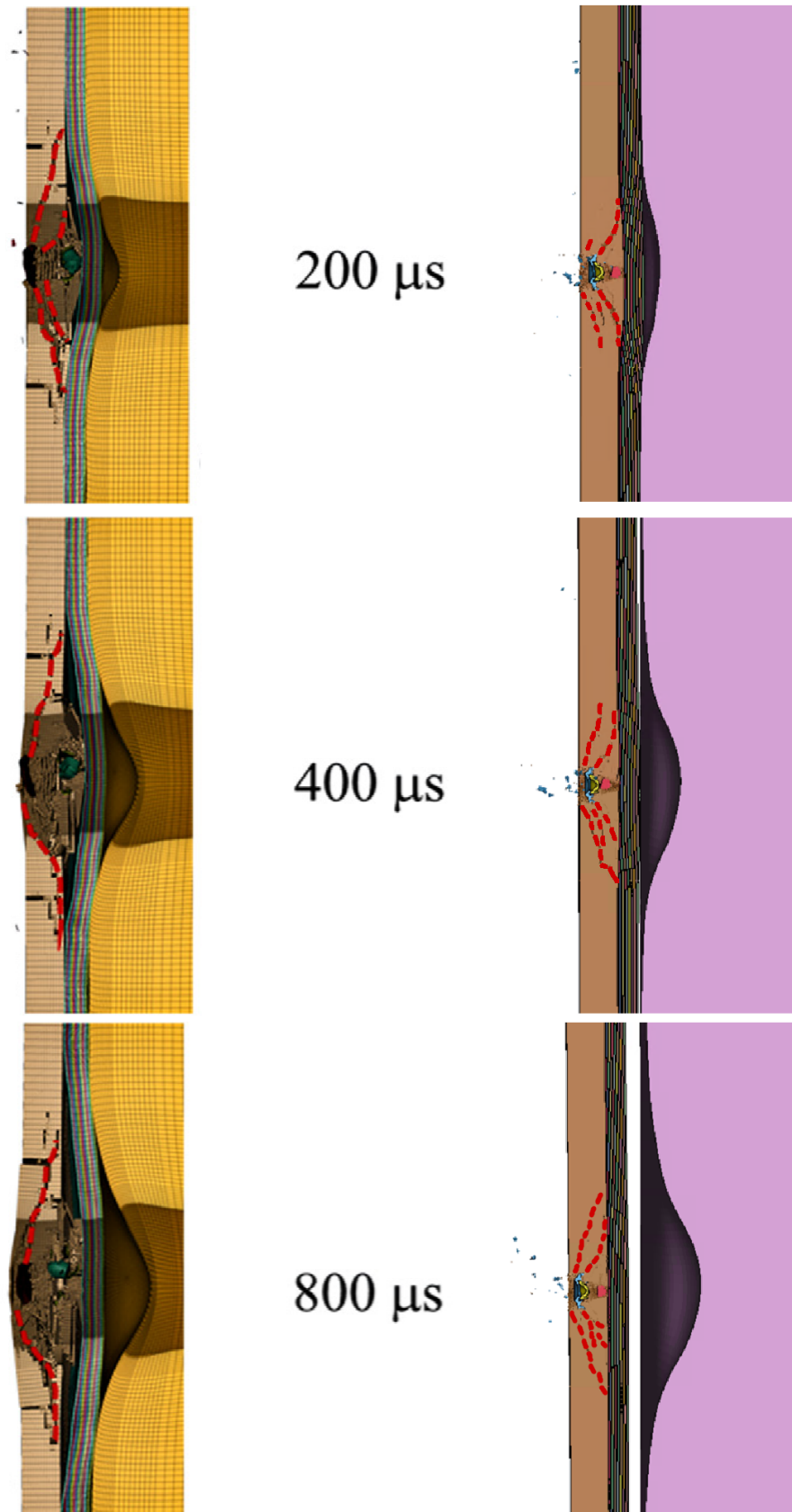


Figure 5.6: Impact sequence on the ceramic composite SAPI comparison (second half), on the left the original work by Lin et al. [17], on the right the current work.

Figure 5.7 shows a close-up comparison of the simulation results between the reference and the current work.

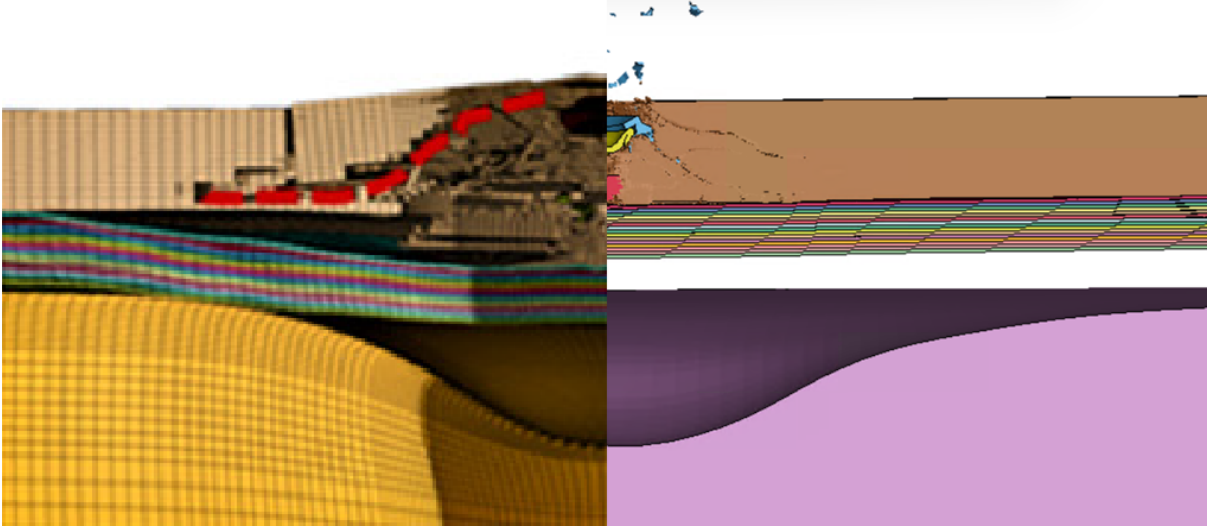


Figure 5.7: Detail of the simulation comparison at $800 \mu s$, on the left the original work by Lin et al. [17], on the right the current work.

The final deformation of the plastilina blocks is reported in figure 5.8. The displacement field obtained from the current model closely resembles Lin's results in terms of both morphology and peak value: the maximum displacement measured from the proposed model is 19.6 mm against the 20.1 mm reported in Lin's article. Figure 5.9 represents the history plot of the plastilina maximum displacement node for the reference and the current work.

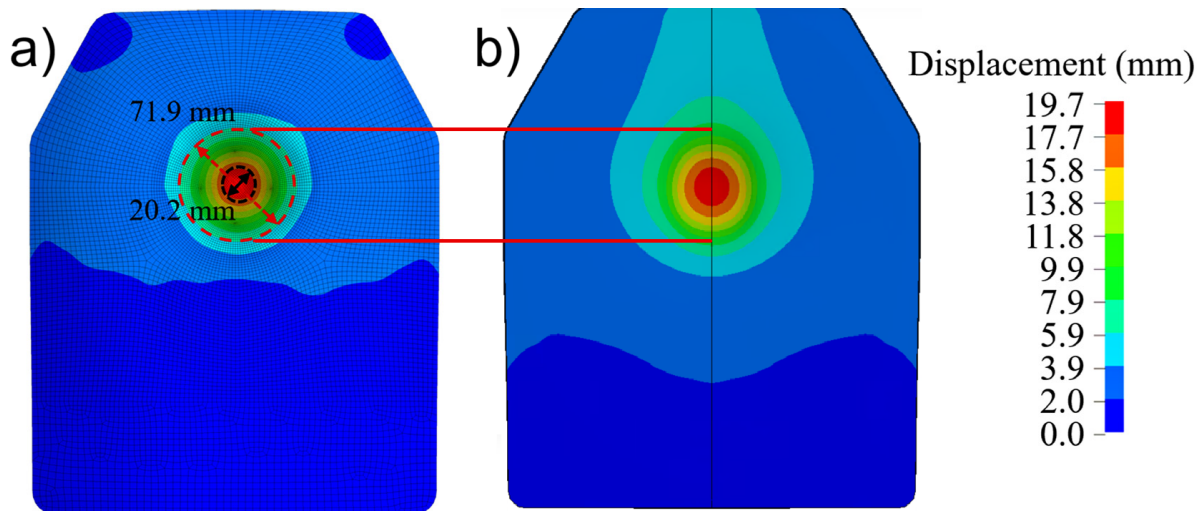


Figure 5.8: Plastilina final displacement comparison for the ceramic composite SAPI: a) original work by Lin et al. [17], b) current model.

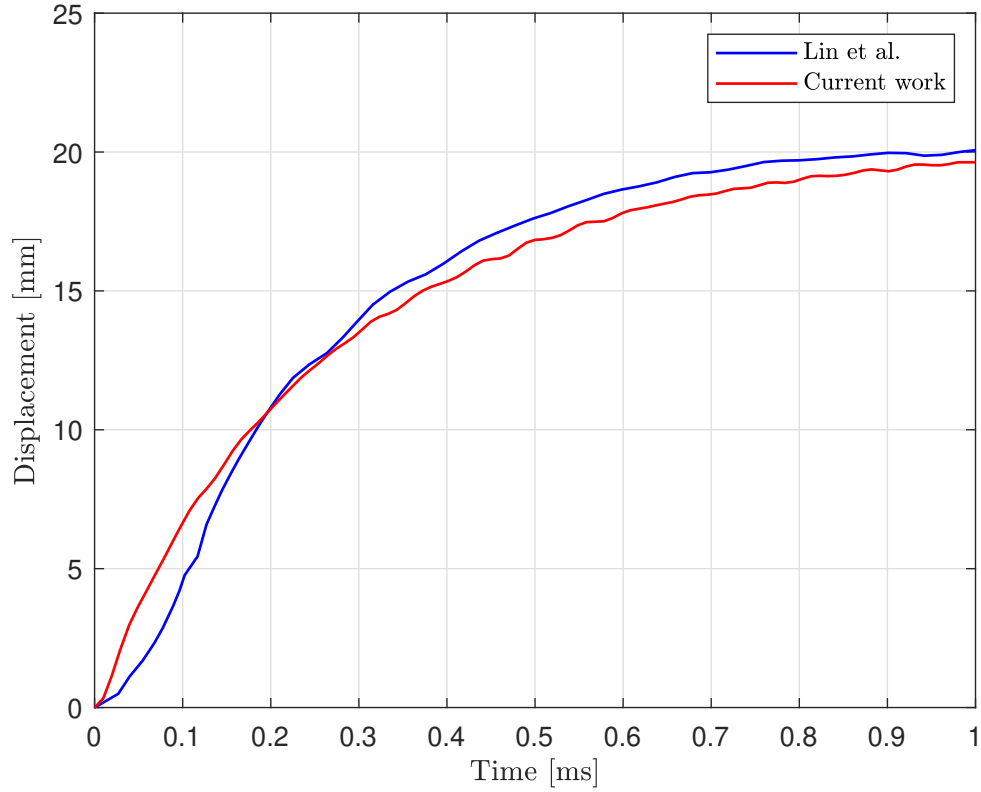


Figure 5.9: Plastilina displacement history comparison

Table 5.1 summarizes the final values of the plastilina displacement for the two protection systems comparing the Lin’s results with the data obtained with the current model, including the relative error. This data confirms that the proposed numerical simulation framework is able to consistently and accurately replicate Lin’s model, allowing it to be extended to the new impact scenarios previously hinted.

Impact case	Lin et al.[17] [mm]	Current model [mm]	Relative error
Soft body armor	24.5	25.7	4.9%
Composite SAPI	20.1	19.6	2.5%

Table 5.1: Back face deformation final values and error for the two cases

5.2. First impact simulation

The results of the simulation of the first impact (ballistic impact on the undamaged protection) will be presented in this section. The final state of this simulation is later used as the initial damaged condition for the second hit simulations. The plastilina has not been modeled in this simulation to reduce computational cost, since in this case the

back face deformation is not of interest. This allowed to reduce simulation run time to approximately 74 hours of computation parallelized on 24 cores. Figures 5.10 and 5.11 show the impact sequence.

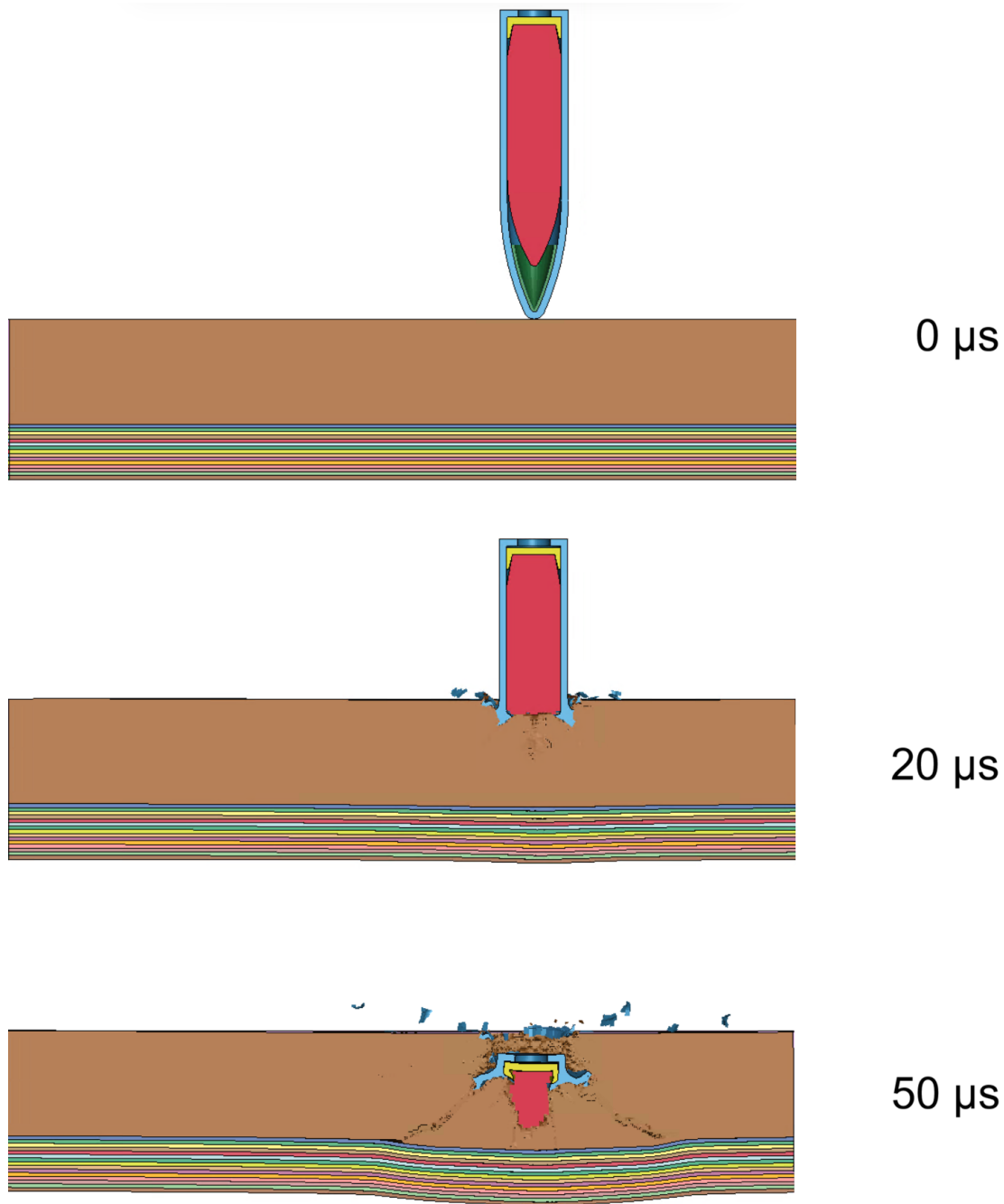


Figure 5.10: First impact sequence on the composite SAPI (first half)

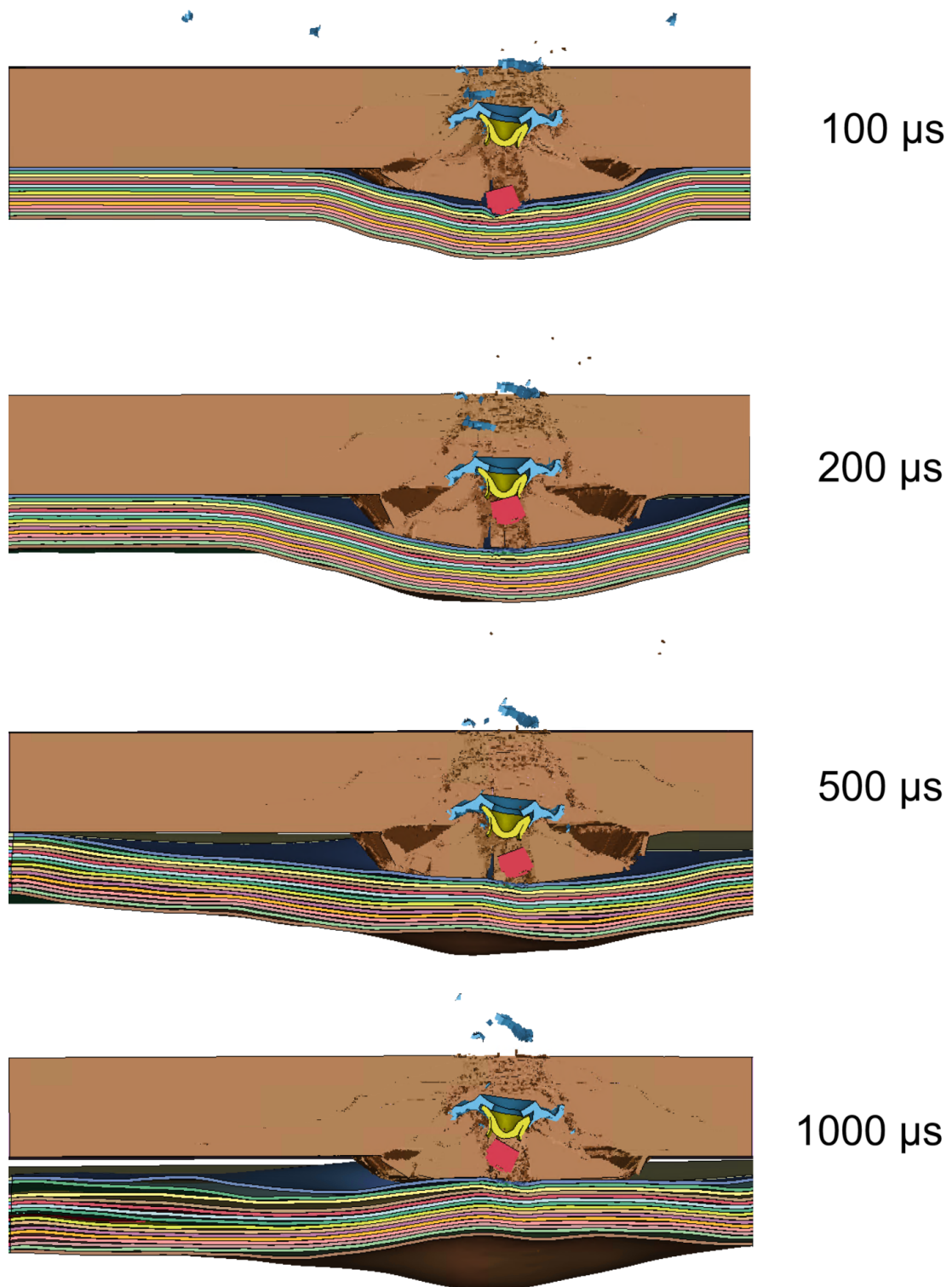


Figure 5.11: First impact sequence on the composite SAPI (second half)

Since an hourglass control has been employed to reduce zero energy artifacts, the UHMWPE

energies have been plot in figure 5.12. For a result to be meaningful a rule of thumb is that the hourglass energy should be less than 10% of the internal energy: this condition is satisfied for all the parts in the model; here is presented only the UHMWPE because it was the part with the most critical hourglassing, as shown in section 4.1.4.

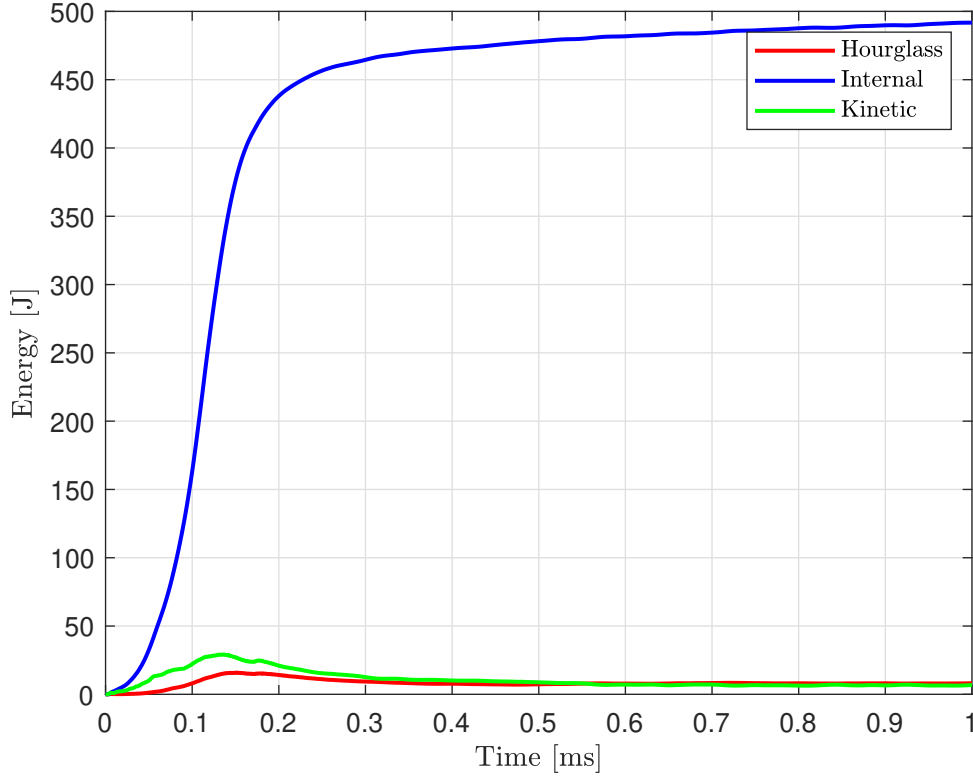


Figure 5.12: Energy history plots for the UHMWPE

The simulation results, similar to the ones presented in the previous section, show that the armor is able to stop the threat; the projectile core is greatly eroded in the interaction with the alumina tile, creating the typical conoid. The UHMWPE laminate exhibits damage exclusively in terms of delamination of the pies, without tearing or shearing.

5.3. Second hit simulation

To investigate the armor ballistic response to a double hit, 4 simulations have been run, sweeping the distance of the two projectiles from 30 to 60 mm with a step of 10 mm. The computational cost for each simulation is 130 hours on average, considering that to modify the impact distance both the first and the second impacts have to be simulated again. Figures 5.13 and 5.14 present the impact sequence for all the projectile distances taken into consideration. In all the simulated scenarios the projectile is able to defeat

the armor, but as the spacing between the impact points increases, more of the core mass is eroded and the core residual velocity reduces. The damage in the armor also varies significantly varying the distance between the impactors: the ceramic conoid development is limited by the pre-existing damage, so when the new impact location is farther from the initial damage the newly produced comminuted alumina is spread on a larger area.

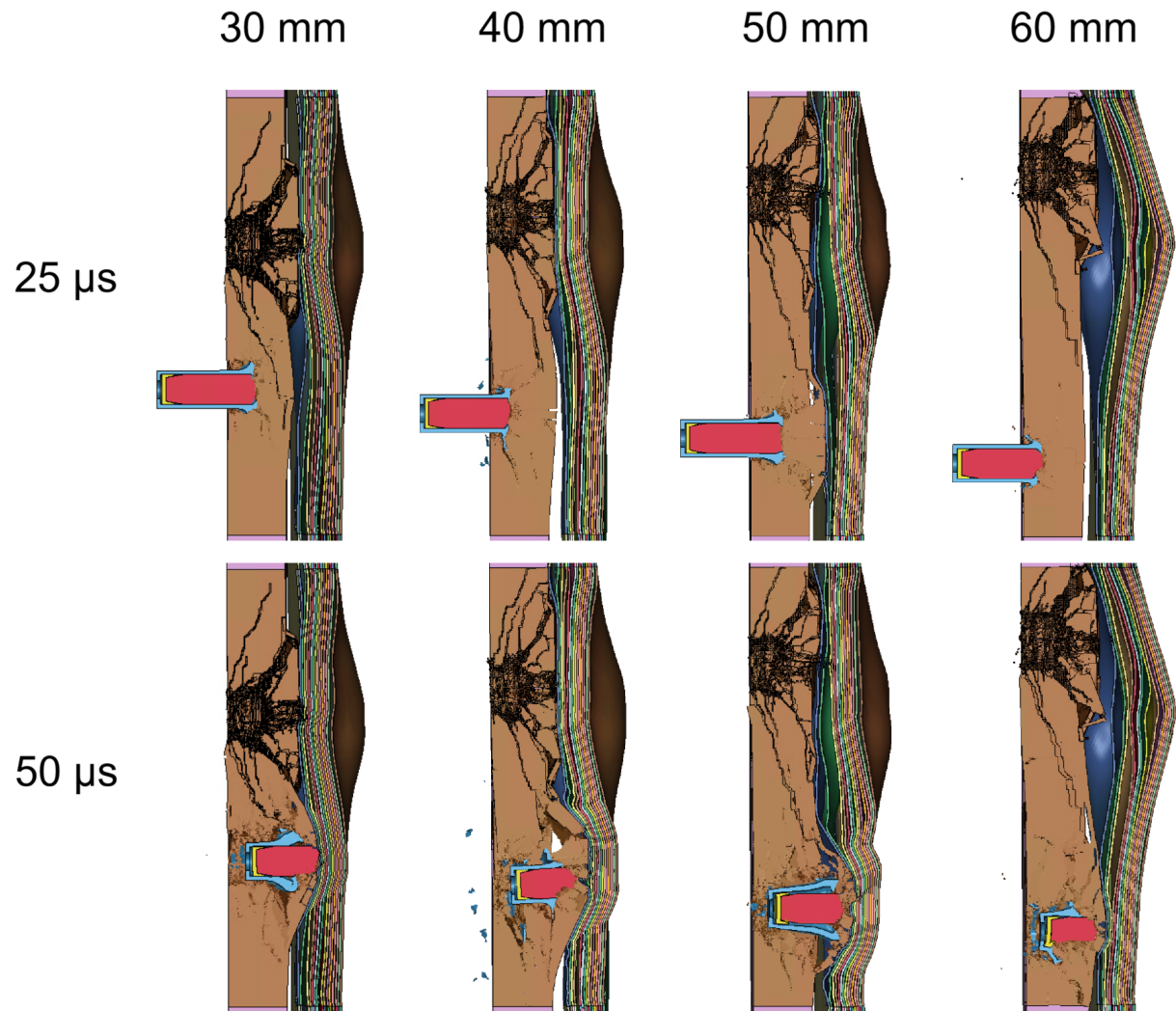


Figure 5.13: Second hit impact sequence on the composite SAPI (first half)

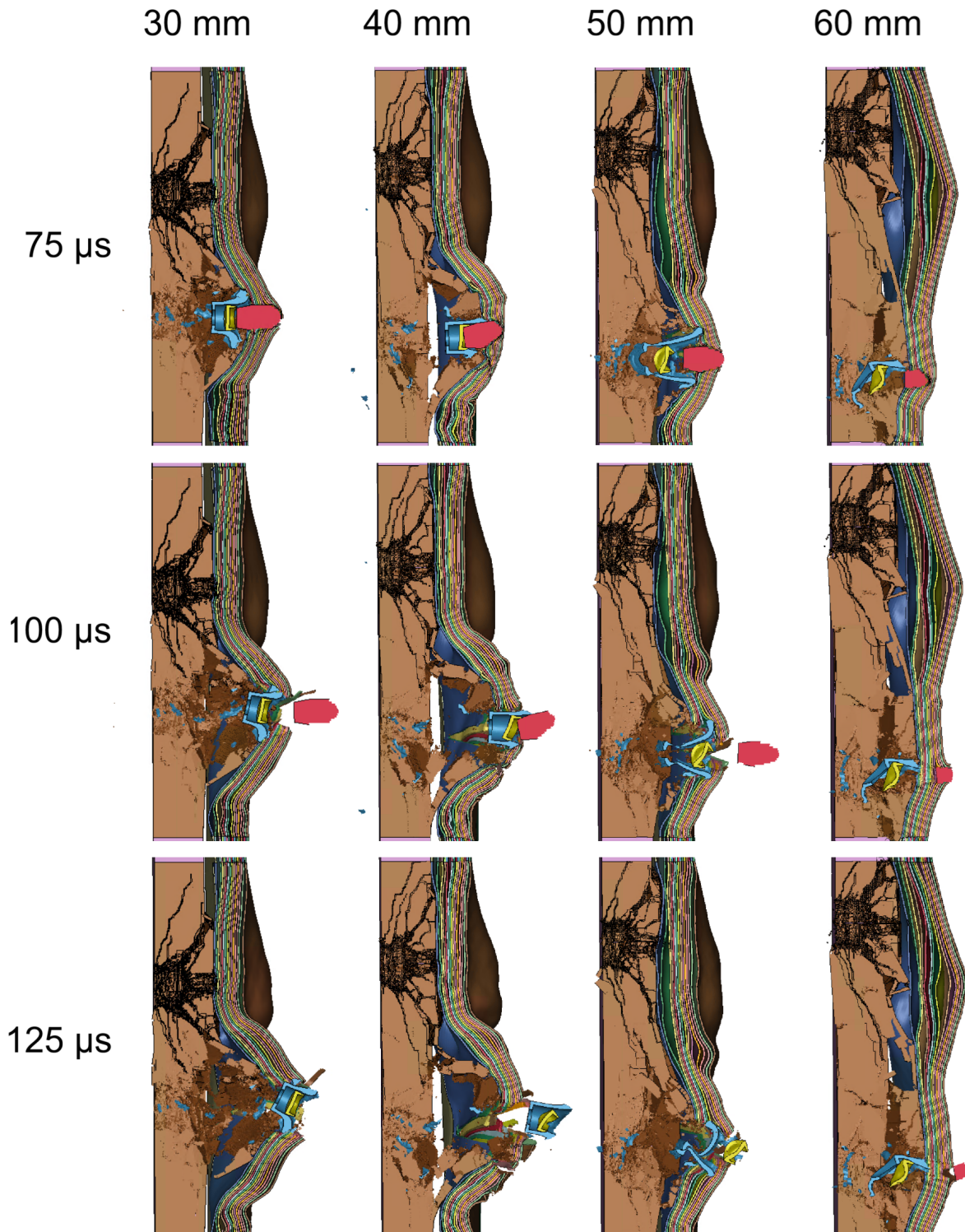


Figure 5.14: Second hit impact sequence on the composite SAPI (second half)

Figures 5.15 and 5.16 show a detail view of the simulation results at 100 μs of the two scenarios of the closest and farthest impact from the pre-existent damage location.

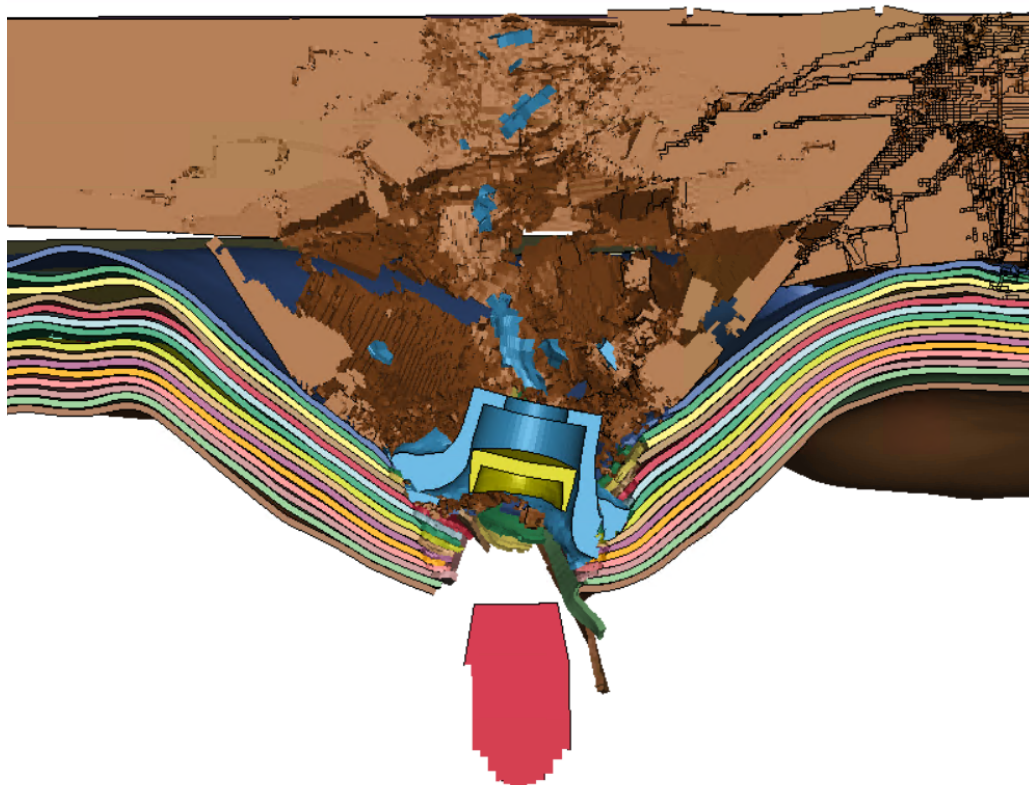


Figure 5.15: 30 mm spaced second impact simulation at $100 \mu s$

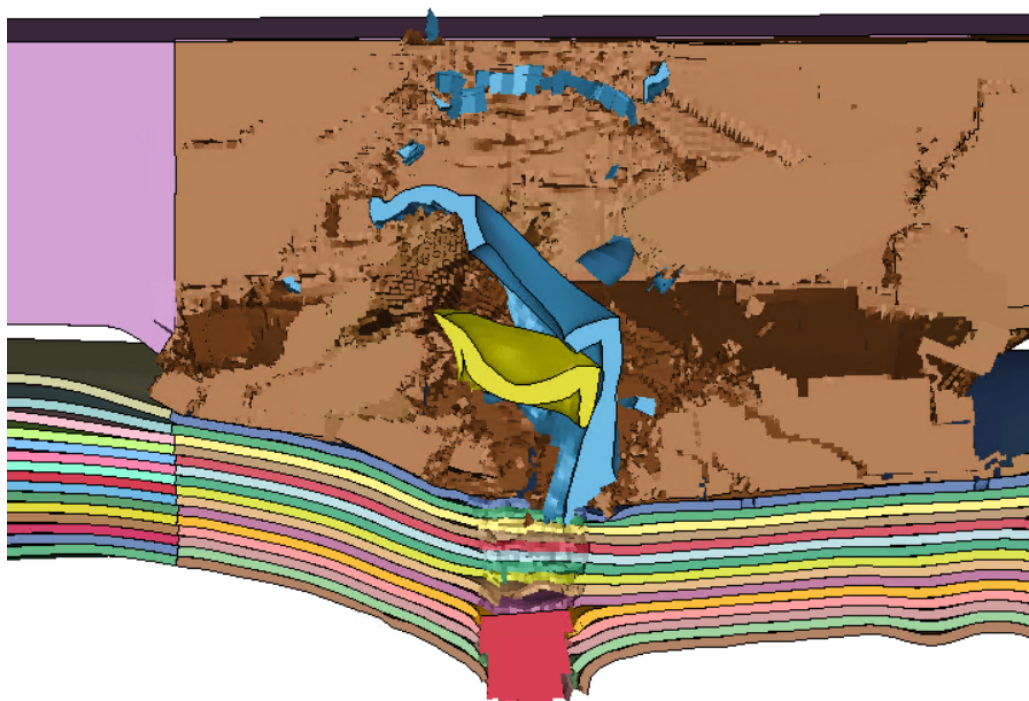


Figure 5.16: 60 mm spaced second impact simulation at $100 \mu s$

The interaction with the pre-existent damage is visible in figure 5.17 where two representative cases are showed; the crack propagation in the ceramic tile caused by the second impact is arrested when intercepting the fractures generated in the first impact. This affects how the ceramic layer is able to absorb energy with an extensive comminution; moreover the dimensions of ceramic conoid determine how the alumina tile is able to spread and transfer the kinetic energy to the UHMWPE layer and consequently how much strain energy is absorbed by the laminate.

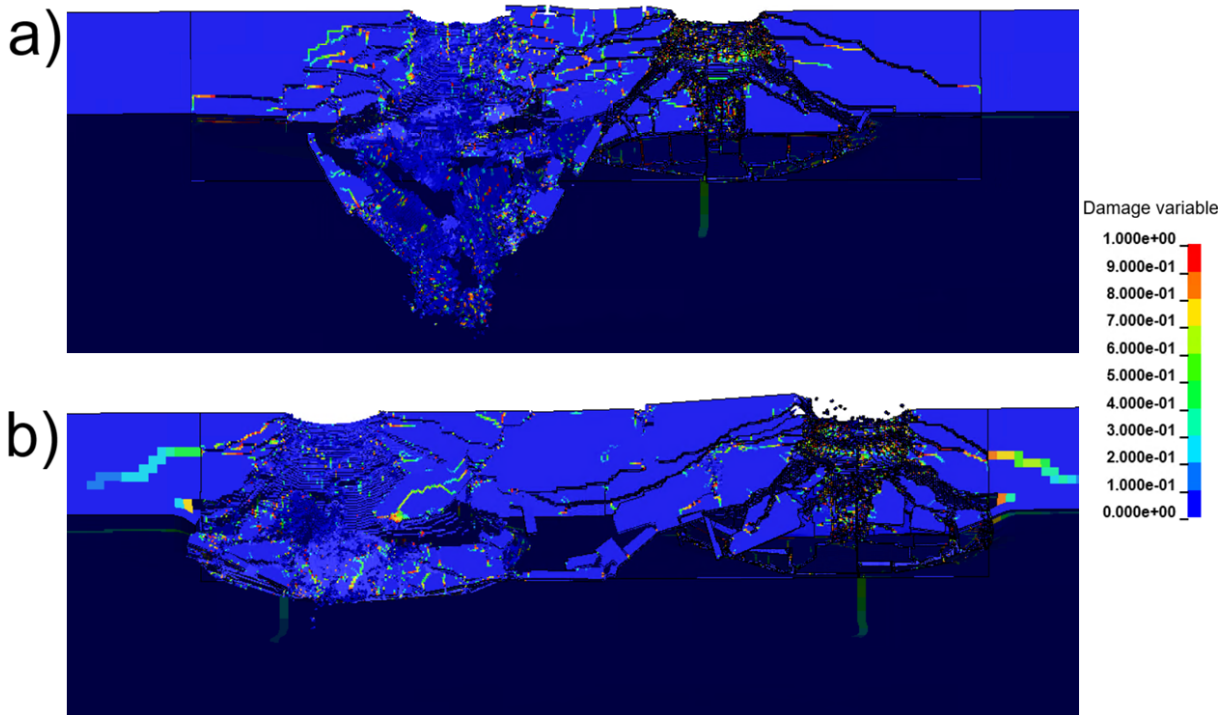


Figure 5.17: Damage in ceramic tile for: a) 30 mm projectile spacing, b) 60 mm projectile spacing

As the distance from the first impact damage location increases, the core is more blunted and eroded, this induces a shift in the damage mechanism of the UHMWPE layer from extensive tearing to soft plugging, as shown in fig. 5.18. Since this failure is highly localized and occurs rapidly, it prevents the laminate to absorb more energy engaging in large-scale deformation mechanism [63].

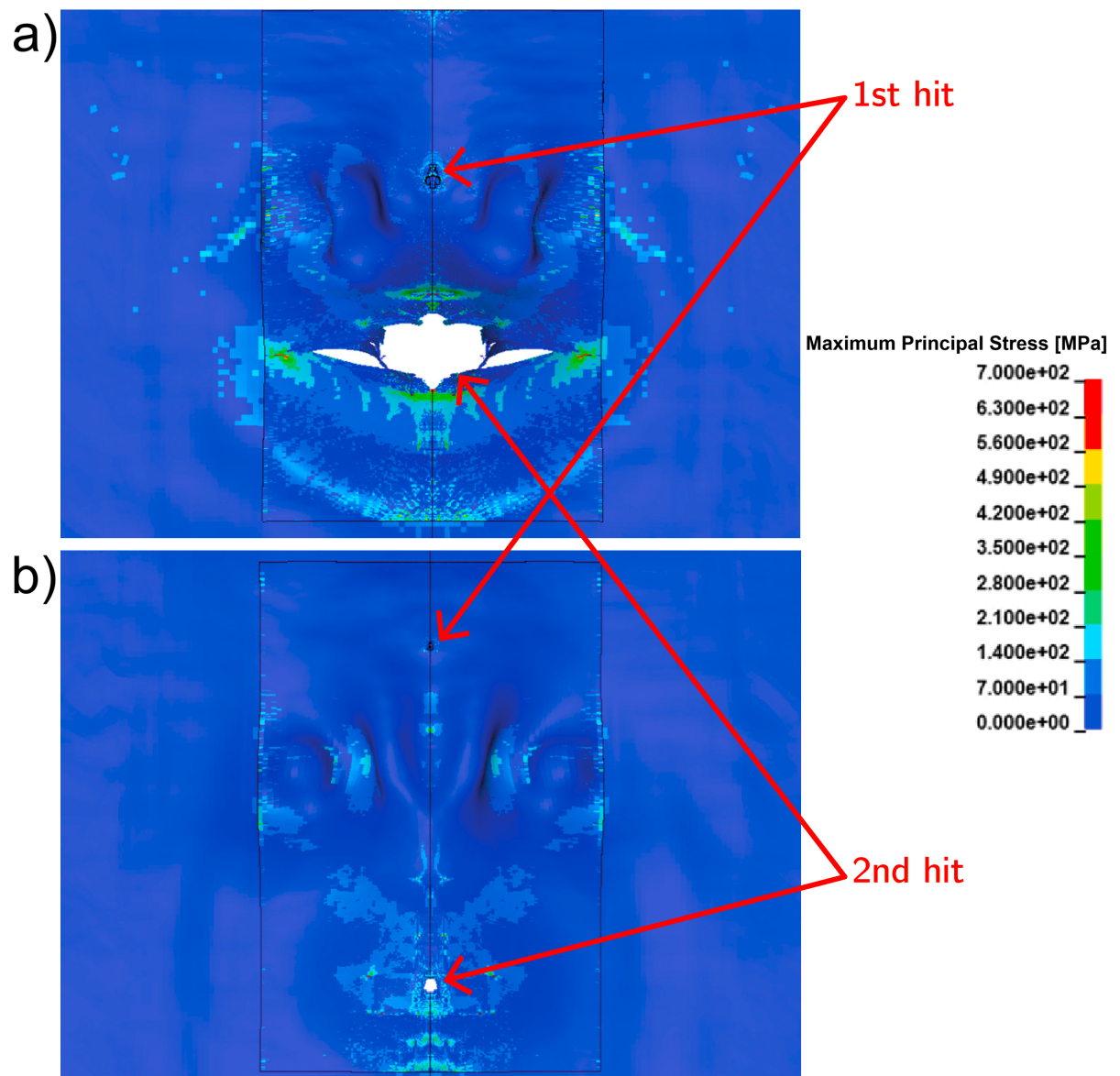


Figure 5.18: Damage comparison in UHMWPE laminates for different impact distances: a) 30 mm, b) 60 mm

The transition between failure modes (confirming that at 60 mm the armor is approaching the ballistic limit) can be also observed in the velocity history of the core of the projectile (shown in figure 5.19).

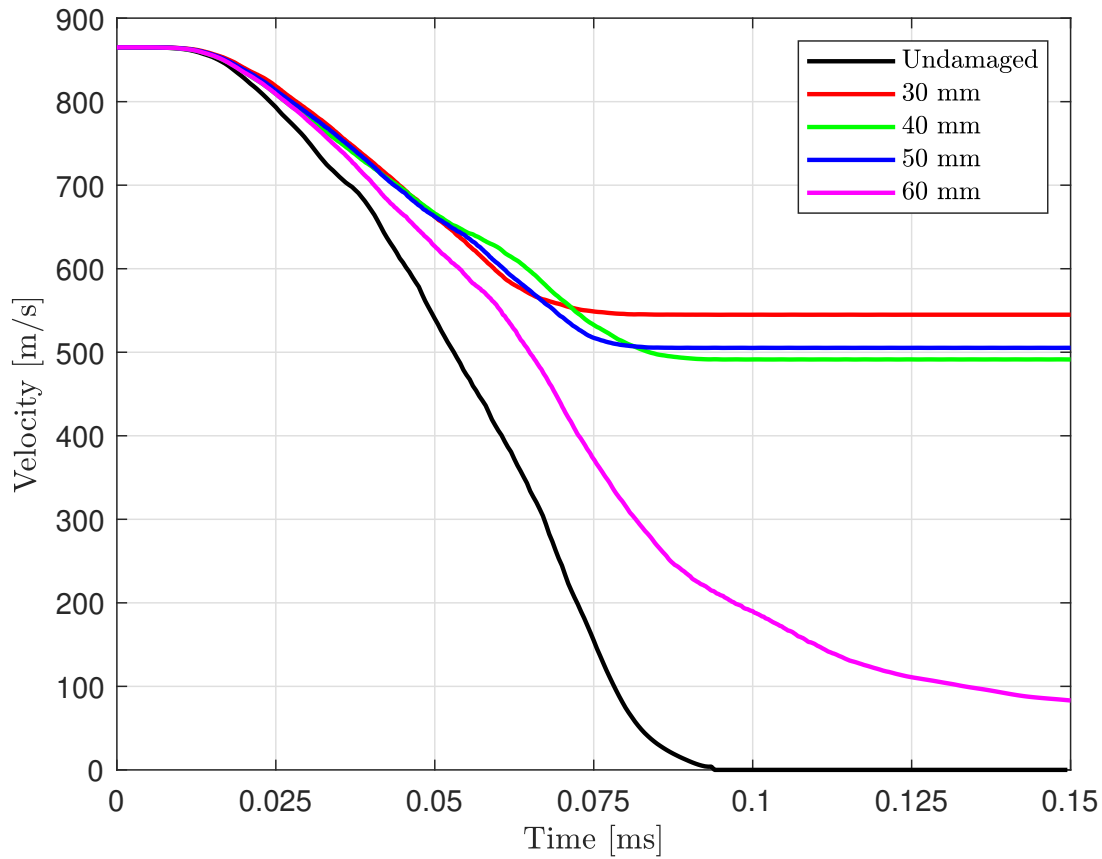


Figure 5.19: Core velocity history comparison between the undamaged (first hit) and second hit cases

While the velocity profiles for the 30, 40 and 50 mm distance are clustered between 500 and 600 m/s, the 60 mm profile (in magenta) exhibits a different behavior, symptom of a transition from a regime of complete penetration of the armor to a condition where the armor is able to fully stop the threat (such as in the case of the first impact, represented here as the black line).

The key quantitative indicators of the armor protective performance are reported in table 5.2: residual velocity of the projectile (calculated as the average velocity of the nodes of the eroded core), the ratio between the final and initial mass of the core and the kinetic energy of the whole projectile (calculated as $\frac{1}{2} m v^2$) expressed as a percentage of its initial kinetic energy (4057 J).

	First impact	30 mm	40 mm	50 mm	60 mm
Residual core velocity [m/s]	0	545	490	505	83
Residual core mass [%]	3.5	23.1	27.3	15.6	3.4
Residual projectile kinetic en. [%]	0	6.23	6.67	2.97	0.03

Table 5.2: Residual core velocity, core mass and projectile kinetic energy

The kinetic energy of the whole bullet (consisting of core, jacket, filler and cap), is a more representative metric for the residual threat, since it takes into account simultaneously the velocity reduction and the eroded mass [21, 35]. Figure 5.20 shows the residual kinetic energy in function of the impact distance.

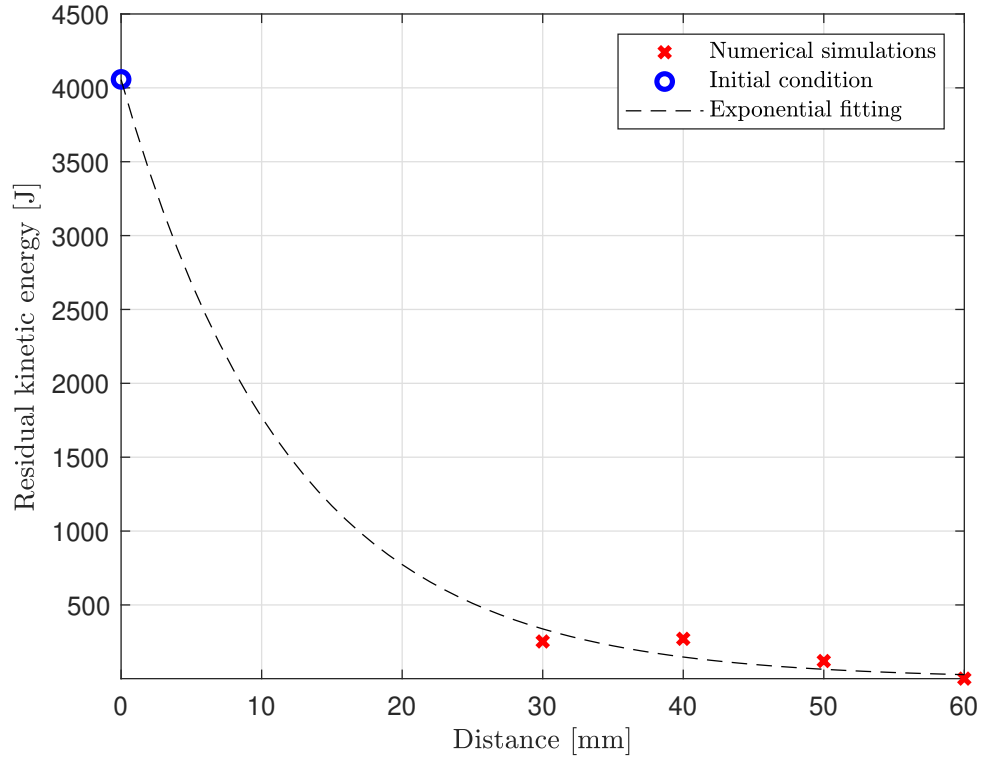


Figure 5.20: Residual kinetic energy in function of impact distance

A simple exponential function $f(x) = a \exp(bx)$ has been fit through the simulation data of all the cases considered. In addition it was supposed that a second hit in the same location as the first impact would result in a reduction of 17.5 J of kinetic energy (adding to the fit the blue circle in figure 5.20). This hypothesis was formulated considering that for the zero distance scenario the projectile would have to only pierce the UHMWPE layer: the energy that the laminate is able to subtract to the projectile was calculated as an average of the core kinetic energy reduction (in the cases of 30, 40 and 50 mm) related

to the core/UHMWPE interaction. For example, for the 30 mm case figure 5.13 shows that at 50 μ s the core has completely gone through the ceramic layer; this means that the energy reduction from that moment onwards is only caused by the UHMWPE.

The residual kinetic energy trend of the simulation results (red crosses in figure 5.20) confirms that as the spacing between the two impact points increases, the armor is able to absorb more of the projectile kinetic energy, up to the point that at 60 mm the protection approaches the nominal protective capability, absorbing the 99.97% of the projectile's kinetic energy.

5.4. Cracked armor case

The effect of a crack on the ceramic plate was studied simulating 6 impacts at various distances from the crack location, from 0 mm (projectile directly impacting the crack) to 20 mm. The ballistic performance of the armor was evaluated with the back-face signature on the plastilina block, comparing it with the baseline measurement in the case of the intact protection, already discussed in section 5.1.2. To correctly measure the final value of bfd, the simulation time was extended to 1.5 milliseconds, resulting on an average computational time of 91 hours for each simulation.

For the sake of brevity, the impact sequence of only 3 representative cases (0 mm, 5 mm and 20 mm) are reported in figures 5.21 and 5.22.

The cracked armor is able to stop the 7.62 APM2 threat in all the scenarios, but the back-face signature is affected significantly by the distance between the impact point and the crack. The numerical simulation results (listed in table 5.3) highlight how a sweet-spot of 4-5 mm distance from the crack maximizes the plastilina displacement; an hit in correspondence of the crack results in less clay deformation and increasing the distance over 5 mm reduces progressively the back face displacement.

	Undamaged SAPI	0 mm	2 mm	4 mm	5 mm	10 mm	20 mm
BFD [mm]	19.7	23.0	27.9	32.4	32.4	28.0	25.1

Table 5.3: Back-face signature comparison

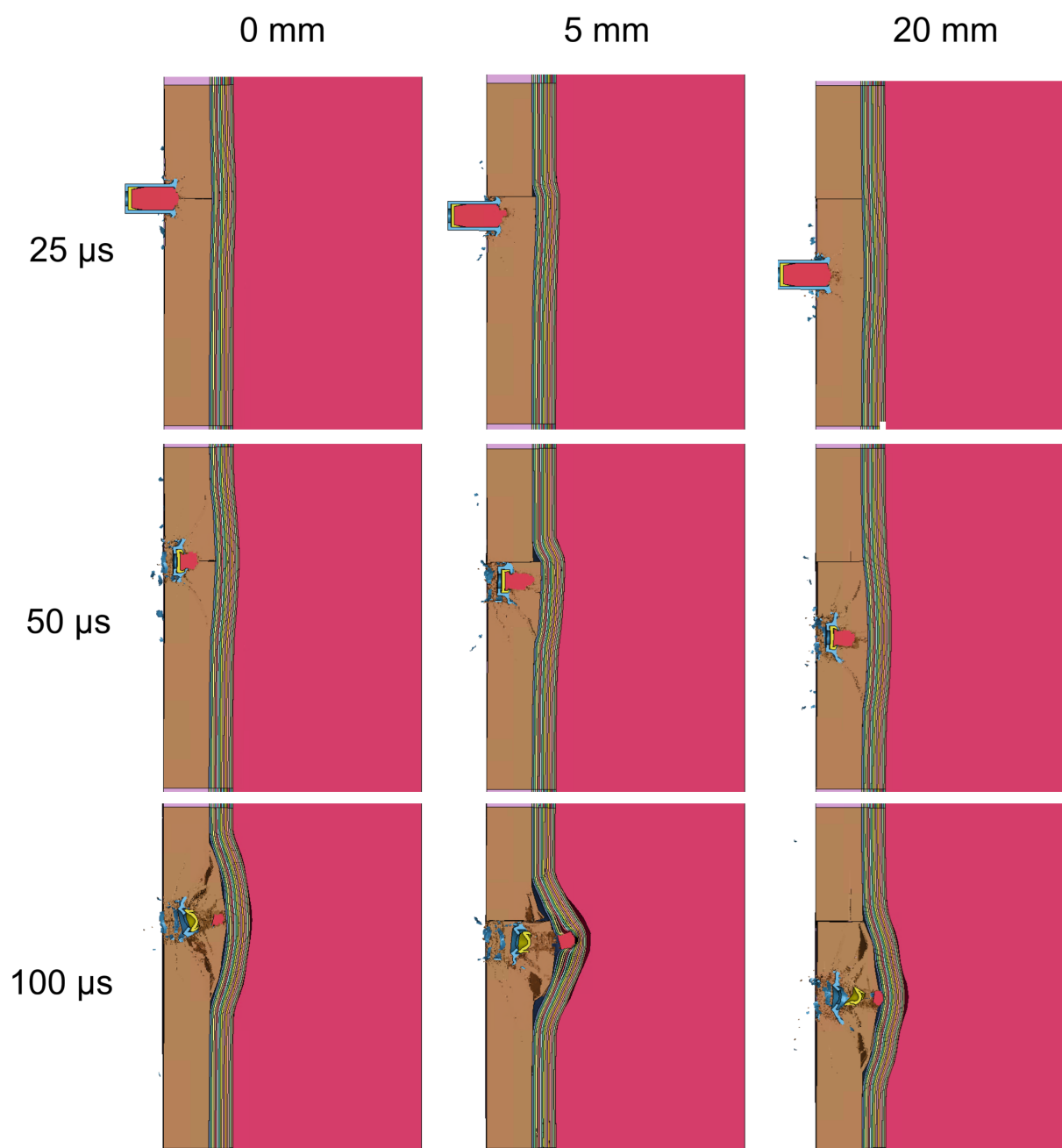


Figure 5.21: First impact sequence on the composite SAPI (first half)

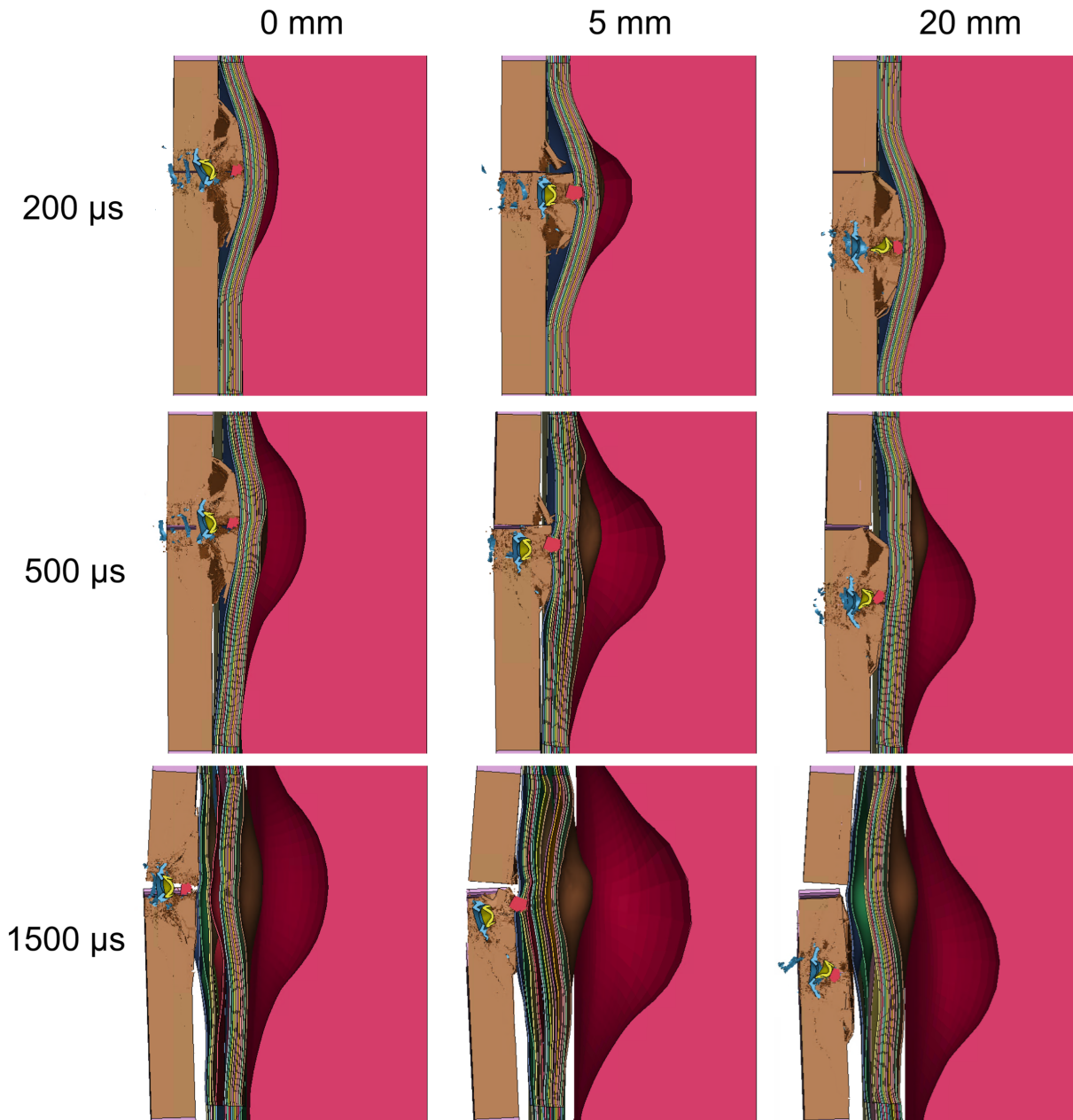


Figure 5.22: First impact sequence on the composite SAPI (second half)

With the variation of the impact distance the damage mechanism in the ceramic drastically changes: when the impact point is near the crack (i.e. the projectile directly interacts with both the halves of the cracked tile) the energy is distributed effectively to both sides of the crack and the ceramic conoid can develop in a fully symmetrical fashion, resulting only in a minor reduction of ballistic performance (when comparing back face deformations to the intact condition), as shown in figure 5.23.

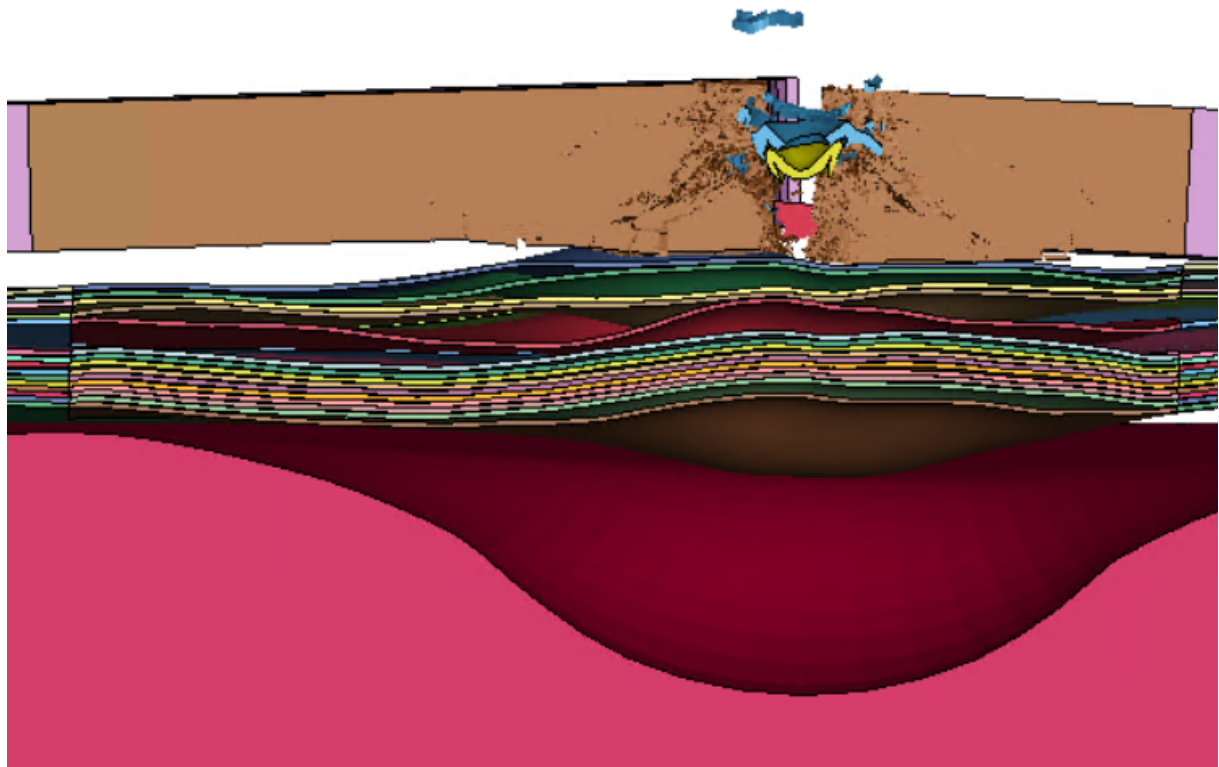


Figure 5.23: Projectile impact directly on the crack at 1.5 ms

For a distance that roughly corresponds to half of the projectile's caliber, the crack in the ceramic promotes the shear sliding of the material right below the impact point and confines the initial crack propagation only on one side of the crack (where the impact takes place), as shown in figure 5.24. This results in a drastic reduction of energy absorbed by the ceramic layer of the SAPI and a smaller conoid of fractured ceramic. This sensibly affects the protective capability of the armor system, increasing sensibly the back face signature.

At 20 mm distance from the crack (figure 5.25), the effects of the pre-existing damage are almost negligible: the failure morphology returns to be the usual symmetric conoid of comminuted alumina. The back face deformation measurement confirm that at 20 mm from the crack the armor approaches the nominal protective performance.

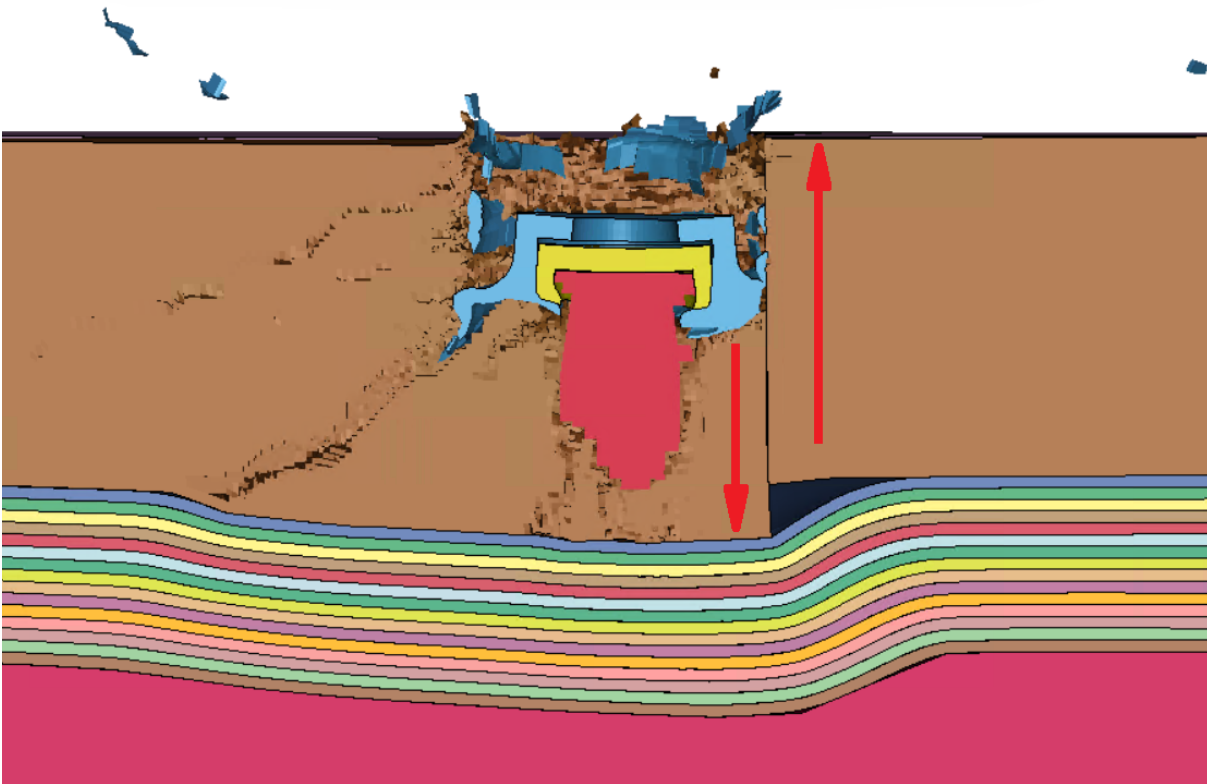


Figure 5.24: Projectile impact 5 mm from crack at $50 \mu s$

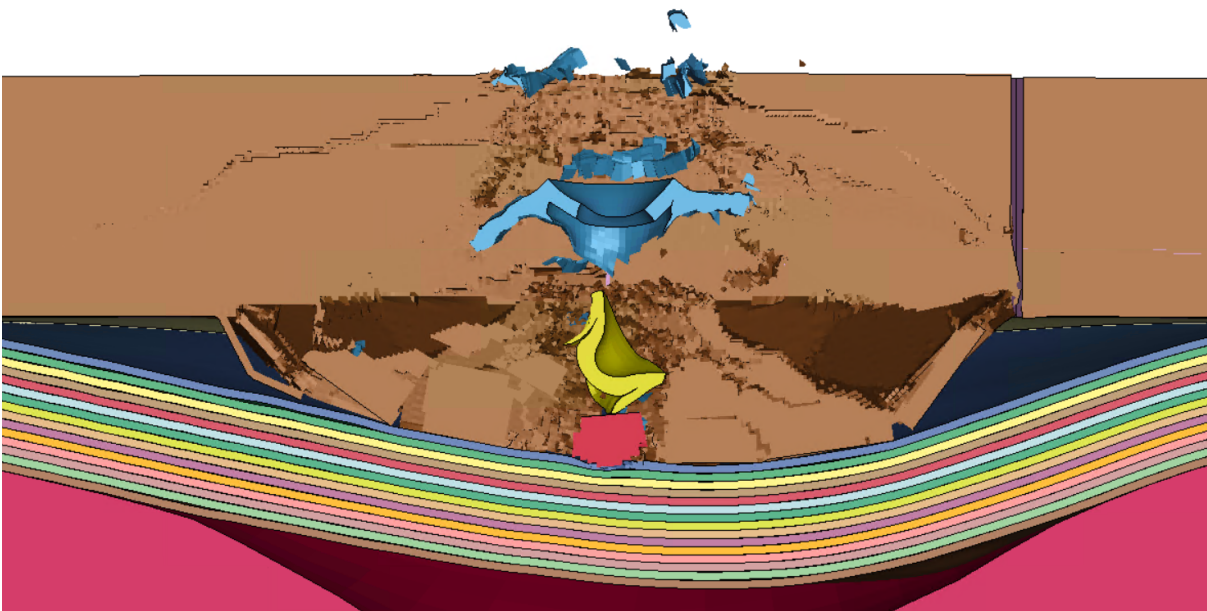


Figure 5.25: Projectile impact 20 mm from crack at $25 \mu s$

Figure 5.26 shows the final damage (at 1.5 ms) on the ceramic layer subjected to ballistic hits 5, 10 and 20 mm distant from the crack. It's clearly visible how the fracture propa-

gation inhibited by the crack and the right half of the tile presents almost no damage at all.

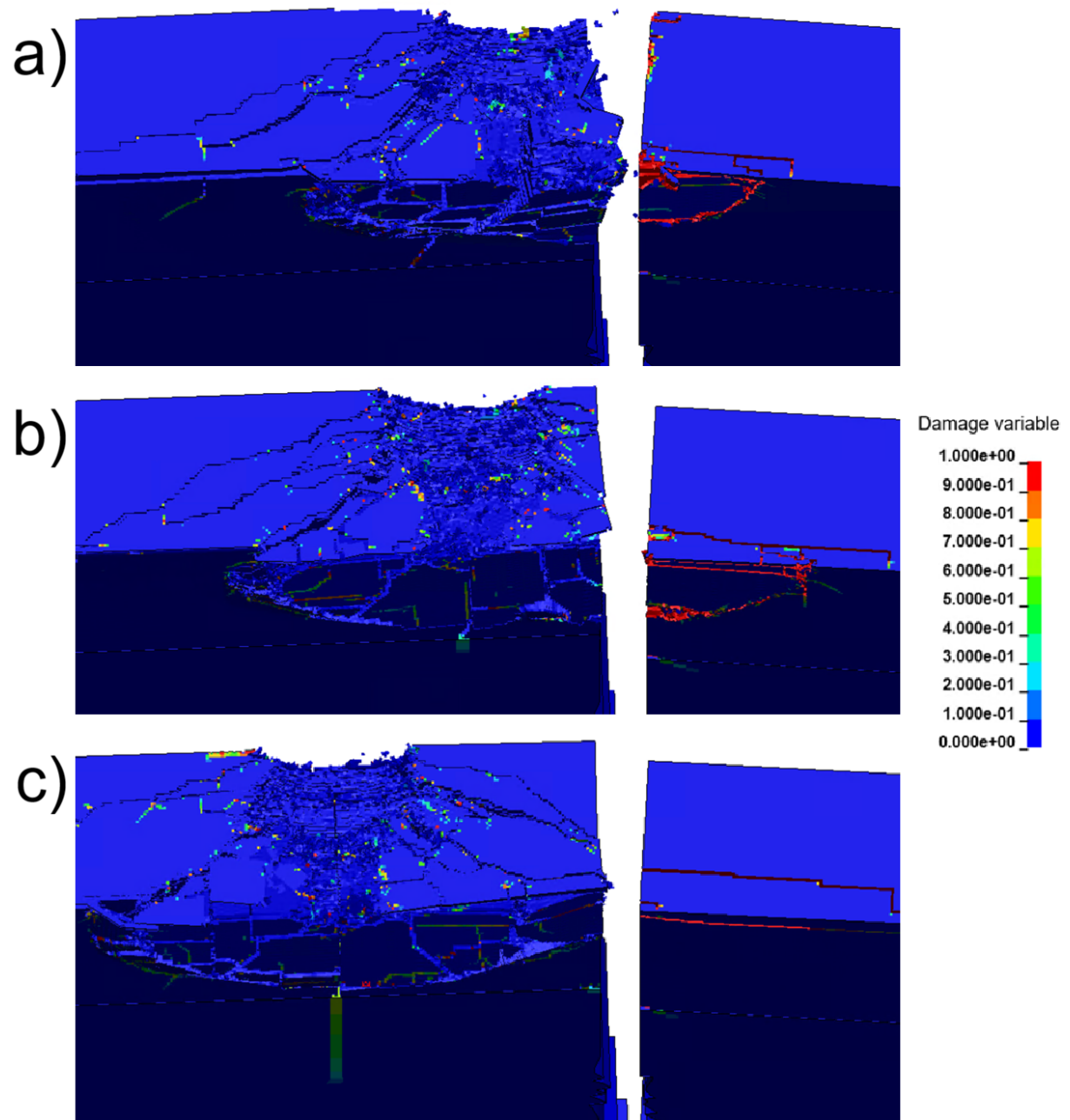


Figure 5.26: Damage comparison on ceramic layer for different impact distances: a) 5 mm, b) 10 mm and c) 20 mm

The different conoid dimensions and shape consequently affect the damage on the UHMWPE layer; figure 5.27 shows that the impact 5 mm distant from the crack results in a severe delamination and in an initial soft plugging of the core of the projectile, with a similar initial perforation morphology seen in figure 5.16.

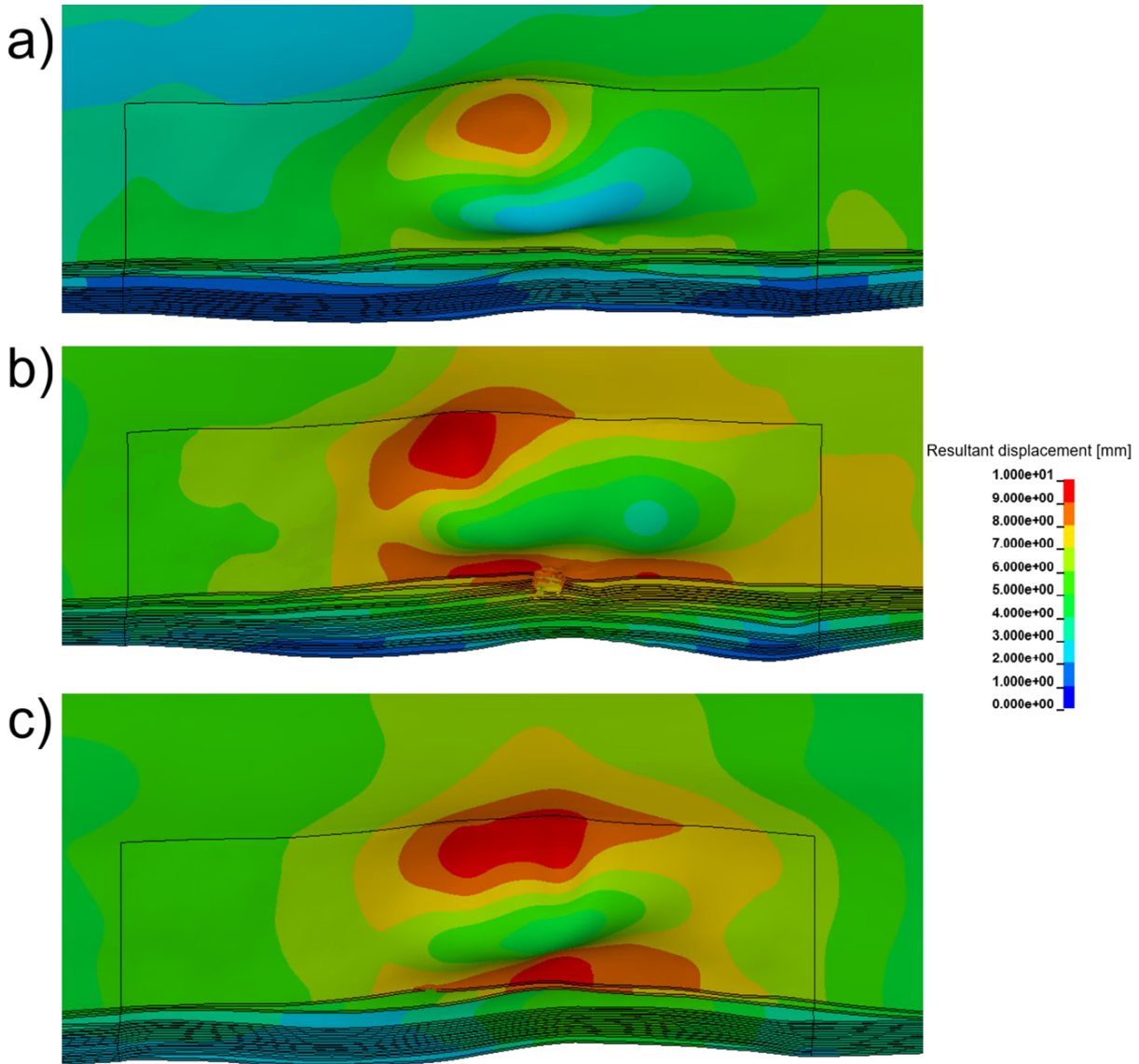


Figure 5.27: UHMWPE laminate displacement for different impact distances: a) 0 mm, b) 5 mm and c) 20 mm

Figure 5.28 shows the dynamic history evolution of the back face displacement of all the simulated cases, comparing them with the reference undamaged armor bfd (dashed black line)

Finally, back face deformation values listed in table 5.3 have been plot in figure 5.29. Considering the data distribution, a double term exponential equation (dotted line) has been fit to the experimental data. The equation has the form:

$$f(x) = a \exp(bx) + c \exp(dx) \quad (5.1)$$

where a , b , c and d are constants to be fitted. This model has been chosen because it's able to describe a peak in the domain and to capture non-zero asymptotic decay. Table 5.4 summarizes the equation parameters.

Parameter	a	b	c	d
Value	34.96	-0.01568	-12.22	-0.4929

Table 5.4: Equation 5.1 parameter values

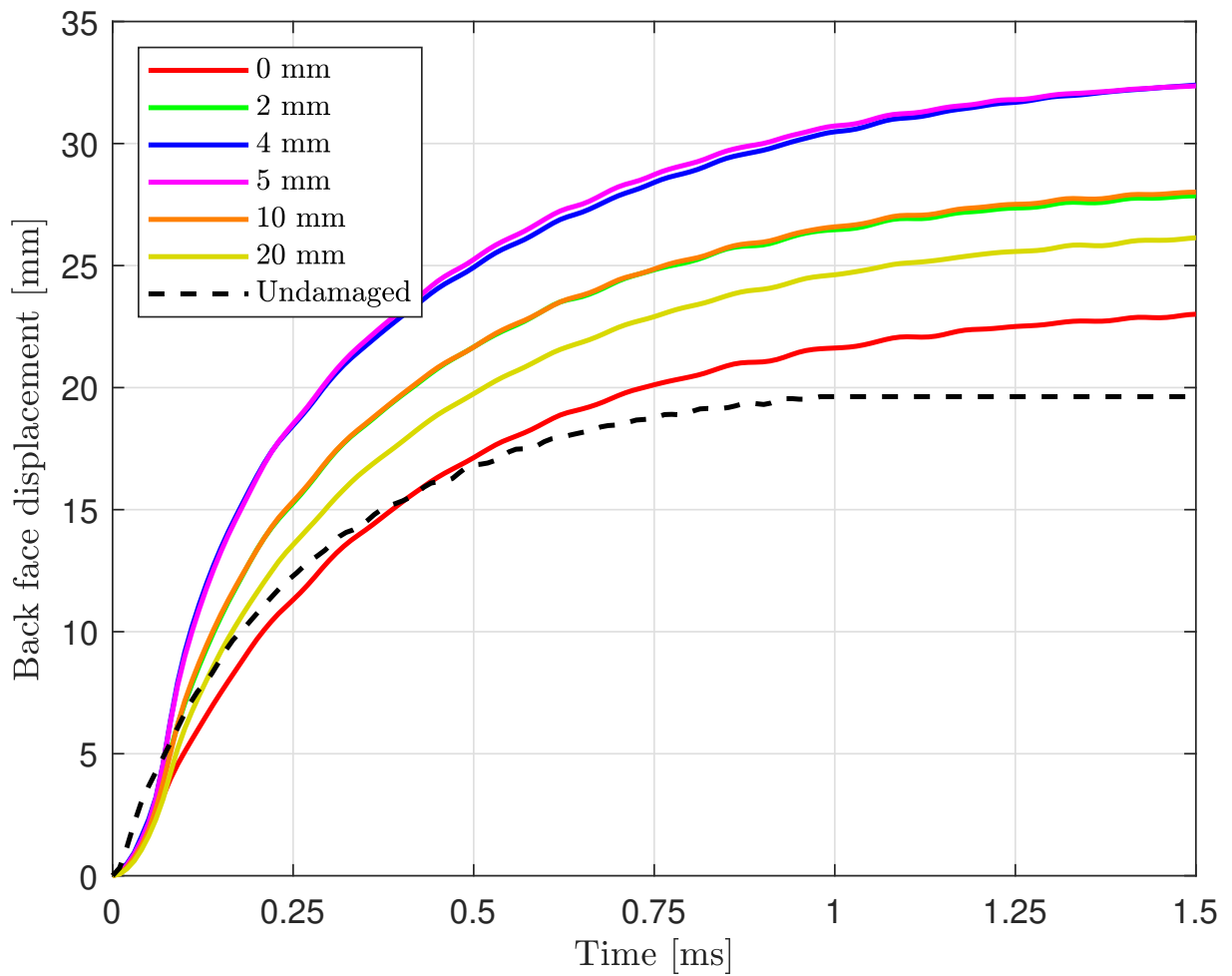


Figure 5.28: Back-face deformation history comparison for all the simulations

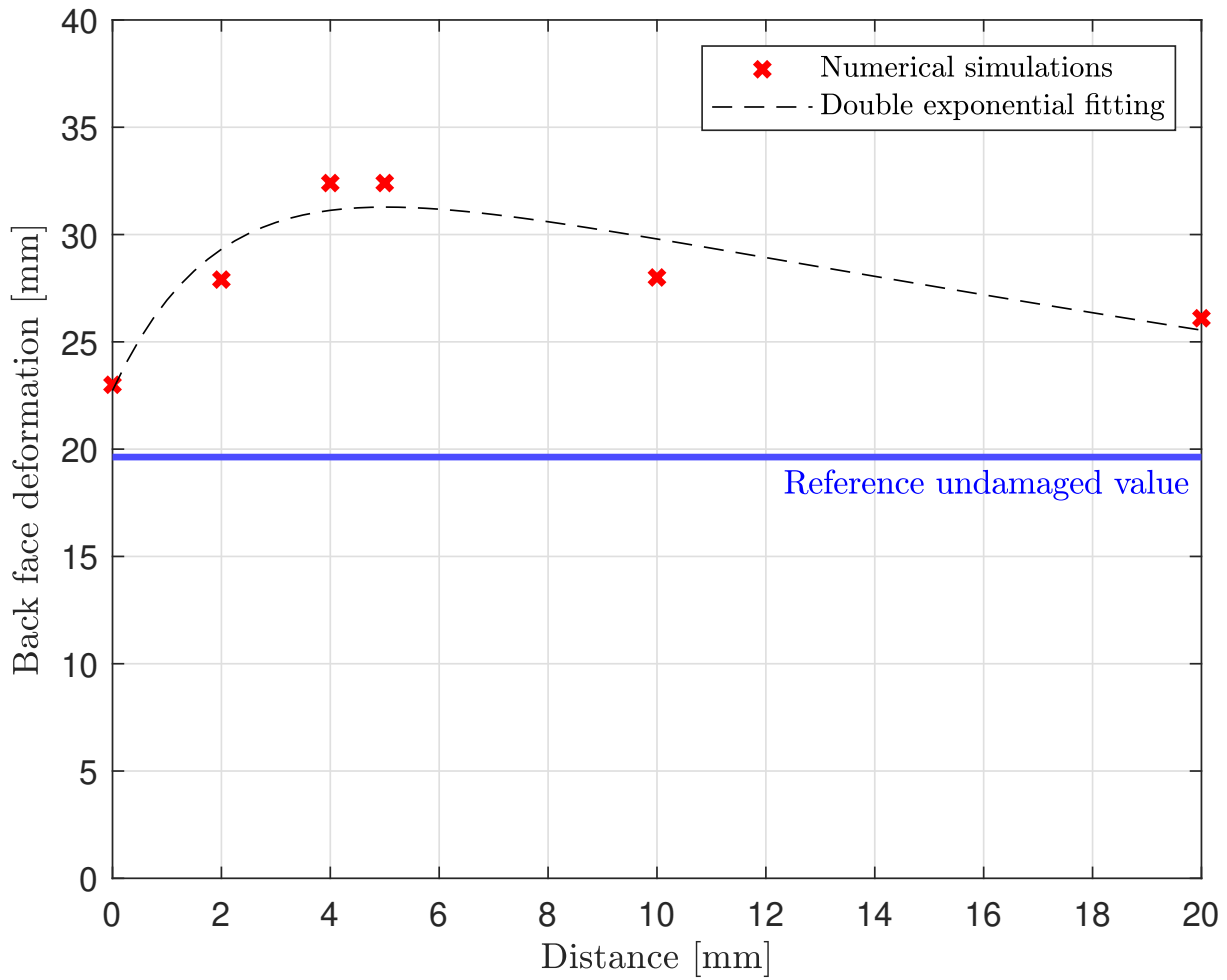


Figure 5.29: Back-face deformation in function of impact distance

To conclude, the back face signature measurements confirm that the ballistic performance of a cracked tile SAPI has a minimum at a distance equal to half of the projectile caliber and then gradually return to the nominal degree of protection.

6 | Conclusions

The scope of this work is to realize a numerical model to reproduce a ballistic impact on a composite Small Arms Protective Insert and to predict its residual protective capability after prior damage. This procedure is proposed as a complement to the current health monitoring solutions, namely ballistic testing or radiography/visual inspection, integrating the numerical simulation results with traditional acceptance/rejection criteria.

The advantage of the present approach is that it is a non destructive investigation (as opposed to ballistic testing) and provides quantitative performance indicators, such as back face deformation or residual projectile velocity (instead of a qualitative health estimation that conventional radiography screening is able to produce). Coupling high-fidelity numerical simulations with conventional non-destructive testing can support and improve the decision-making process of determining if a personal protection with variable degree of damage has to be decommissioned (because it no longer meets protection standards) or if its operating life can be extended despite the damage.

There are two main challenges of this new procedure; first all the materials composing the armor system and the projectile have to be modeled coherently with the actual specimens (this can be achieved through literature research and experimental data calibration) and second the pre-existent damage morphology on the SAPI has to be correctly modeled in the numerical simulation (starting from x-ray raw data). An important factor that must be kept in mind is the computational burden of this type of high-fidelity simulations: even with state-of-the-art hardware, computation time can range from tens to hundreds of hours, also depending on the duration of the phenomenon that is desired to be captured.

Numerical simulations have been conducted simulating a ballistic impact on a: (i) an undamaged SAPI, (ii) a previously hit SAPI and (iii) a cracked SAPI. Multiple simulations have been conducted at various distances from the pre-existent damages, to investigate the performance of the armor against the new threat.

Results confirm the correlation between the residual ballistic resistance and the distance from the damage location. In detail, when considering a protection damaged by a prior ballistic impact, the numerical simulations show that the penetration protection is com-

promised inside a 60 mm radius from the damage location. Furthermore, even where the armor would be able to stop the threat, the behind-armor blunt trauma could result in severe injury, suggesting that ulterior investigation in that regard should be carried out. On the other hand, the finite element analysis conducted on the cracked ceramic specimen shows that the armor is able to stop the projectile regardless of the distance between the crack and the projectile; the back-face deformation shows that the armor provides a sufficient degree of protection independently from the impact location, with a peak of 32.4 mm bfd at 5 mm distance from the crack, but nevertheless still in compliance with the NIJ standard 0101.06 (44 mm maximum displacement allowed).

Future work will include doing additional simulations increasing projectile distance in the second impact case, to assess the actual back face deformation when the armor is able to stop the bullet, and to do a mesh independency study to evaluate the possibility of modeling only a portion of the armor instead of the half model, to further reduce the computational burden.

Finally it will be studied the possibility of creating an artificial neural network framework trained on the results of finite element simulations to predict projectile residual velocity or back-face deformation starting from x-ray data. Other scholars [95, 96] recently investigated this approach to predict residual projectile velocity and damage of ballistic impacts on undamaged protection with promising results. Employing this surrogate model offers the advantages of greatly improved computational time and removes the need of a finite element expert analyst, being a stand alone system. The challenges of this approach will be creating a sufficiently large database to be used as a training set for the neural network.

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A | Material Parameters

In this appendix are listed the parameters for the material models (constitutive relations and equations of state) employed in the numerical simulations.

A.1. Alumina

MAT_110 - JOHNSON_HOLMQUIST_CERAMICS

Parameter	Value
Density RO [tonne/mm ³]	3.7e-9 [16]
Shear Modulus G [MPa]	9.016e4 [16]
Intact normalized strength parameter A [-]	0.93 [16]
Fractured normalized strength parameter B [-]	0.31 [16]
Strength parameter (for strain rate dependence) C [-]	0 [16]
Fractured strength (pressure exponent) m [-]	0.6 [16]
Intact strength (pressure exponent) n [-]	0.6 [16]
Reference strain rate $EPS0$ [s ⁻¹]	1 [97]
Maximum tensile pressure strength T [MPa]	200 [97]
Maximum normalized fractured strength $SFMAX$ [-]	0.2 [16]
Hugoniot Elastic Limit HEL [MPa]	1.9e4 [16]
Pressure at Hugoniot Elastic Limit $PHEL$ [MPa]	1460 [16]
Fracture of energy loss converted to hydrostatic energy $BETA$ [-]	1 [16]
Parameter for plastic strain to fracture $D1$ [-]	0.005 [16]
Parameter for plastic strain to fracture (exponent) $D2$ [-]	1 [16]
First pressure coefficient (bulk modulus) $K1$ [MPa]	1.31e5 [16]
Second pressure coefficient $K2$ [MPa]	0 [16]
Third pressure coefficient $K3$ [MPa]	0 [16]
Failure criterion (equivalent plastic strain at failure) FS [-]	0.525

A.2. UHMWPE

MAT_059 - COMPOSITE_FAILURE_SOLID_MODEL

Parameter	Value
Density RO [tonne/mm ³]	9.7e-10 [98]
Elastic Modulus - longitudinal direction EA [MPa]	3.426e4 [98]
Elastic Modulus - transverse direction EB [MPa]	3.426e4 [98]
Elastic Modulus - normal direction EC [MPa]	3620 [99]
Poisson's ratio ba $PRBA$ [-]	0 [100]
Poisson's ratio ca $PRCA$ [-]	0.013 [99]
Poisson's ratio cb $PRCB$ [-]	0.013 [99]
Shear modulus ab GAB [MPa]	173.8 [100]
Shear modulus ab GBC [MPa]	547.8 [100]
Shear modulus ab GCA [MPa]	547.8 [100]
Bulk modulus of failed material KF [MPa]	0 [17]
In plane shear strenght SBA [MPa]	120 [101]
Longitudinal shear strenght SCA [MPa]	574 [101]
Transverse shear strenght SCB [MPa]	574 [101]
Longitudinal compressive strenght XXC [MPa]	1e30 [17]
Transverse compressive strenght YYC [MPa]	1e30 [17]
Normal compressive strenght ZZC [MPa]	1900 [102]
Longitudinal tensile strenght XXT [MPa]	1150 [103]
Transverse tensile strenght YYT [MPa]	1150 [103]
Normal tensile strenght ZZT [MPa]	1e30 [17]

A.3. Hardened Steel

MAT_107 - MODIFIED_JOHNSON_COOK

Parameter	Value
Density RO [tonne/mm ³]	7.8e-9 [104]
Elastic Modulus E [MPa]	2.1e5 [104]
Poisson's ratio PR [-]	0.29 [104]
Damage coupling parameter $BETA$ [-]	0 [57]
Taylor-Quinney coefficient XSI [-]	0.9 [57]
Specific heat CP [Nmm/tonneK]	4.52e8 [57]
Thermal expansion coefficient $ALPHA$ [-]	1.2e-05 [57]
Reference strain rate $E0DOT$ [s ⁻¹]	1 [104]
Room temperature TR [K]	293 [104]
Melting temperature TM [K]	1790 [104]
Initial temperature $T0$ [K]	293 [104]
Plasticity parameter A [MPa]	1034 [104]
Plasticity parameter B [-]	1.81e4 [104]
Plasticity parameter C [-]	0.005 [104]
Plasticity parameter N [-]	0.64 [104]
Plasticity parameter M [-]	1 [104]
Damage parameter $D1$ [-]	0.01 [104]
Damage parameter $D2$ [-]	0 [104]
Damage parameter $D3$ [-]	0 [104]
Damage parameter $D4$ [-]	0 [104]
Damage parameter $D5$ [-]	0 [104]
Critical temperature TC [K]	1620 [104]

A.4. Brass

MAT_015 - JOHNSON_COOK

Parameter	Value
Density RO [tonne/mm ³]	8.94e-9 [104]
Shear Modulus G [MPa]	4.626e4 [104]
Elastic Modulus E [MPa]	1.249e5 [104]
Poisson's ratio PR [-]	0.35 [104]
Plasticity parameter A [MPa]	500 [104]
Plasticity parameter B [-]	0 [104]
Plasticity parameter C [-]	0.025 [104]
Plasticity parameter N [-]	1 [104]
Plasticity parameter M [-]	1 [104]
Melting temperature TM [K]	1360 [104]
Room temperature TR [K]	293 [104]
Reference strain rate $EPS0$ [s ⁻¹]	1 [104]
Specific heat CP [Nmm/tonneK]	3.667e8 [104]
Damage parameter $D1$ [-]	2 [104]
Damage parameter $D2$ [-]	0 [104]
Damage parameter $D3$ [-]	0 [104]
Damage parameter $D4$ [-]	0 [104]
Damage parameter $D5$ [-]	0 [104]

EOS_004 - GRUNEISEN

Parameter	Value
Speed of sound C [mm/s]	3.667e6 [104]
Coefficient $S1$ [-]	1.507 [104]
Coefficient $S2$ [-]	0 [104]
Coefficient $S3$ [-]	0 [104]
Gruneisen gamma $GAMMA0$ [-]	2.089 [104]
Coefficient A [-]	0.485 [104]

A.5. Lead

MAT_107 - MODIFIED_JOHNSON_COOK

Parameter	Value
Density RO [tonne/mm ³]	1.066e-8 [105]
Elastic Modulus E [MPa]	1.6e+04 [105]
Poisson's ratio PR [-]	0.42 [105]
Damage coupling parameter $BETA$ [-]	0 [57]
Taylor-Quinney coefficient XSI [-]	0.9 [57]
Specific heat CP [Nmm/tonneK]	1.24e8 [57]
Thermal expansion coefficient $ALPHA$ [-]	2.9e-05 [57]
Reference strain rate $E0DOT$ [s ⁻¹]	72.108 [105]
Room temperature TR [K]	293 [105]
Melting temperature TM [K]	525 [105]
Initial temperature $T0$ [K]	293 [105]
Plasticity parameter A [MPa]	0 [105]
Plasticity parameter B [-]	55.552 [105]
Plasticity parameter C [-]	0.126 [105]
Plasticity parameter N [-]	0.0987 [105]
Plasticity parameter M [-]	1 [105]
Damage parameter DC [-]	1 [105]
Damage parameter WC [-]	175 [105]
Critical temperature TC [K]	472.5 [57]

A.6. Plastilina

MAT_015 - JOHNSON_COOK

Parameter	Value
Density RO [tonne/mm ³]	1.57e-9 [106]
Shear Modulus G [MPa]	50 [106]
Elastic Modulus E [MPa]	168 [106]
Poisson's ratio PR [-]	0.49 [106]
Plasticity parameter A [MPa]	1e-5 [106]
Plasticity parameter B [-]	0.238 [106]
Plasticity parameter C [-]	0.25 [106]
Plasticity parameter N [-]	0.29 [106]
Plasticity parameter M [-]	0.502 [106]
Melting temperature TM [K]	373 [106]
Room temperature TR [K]	296 [106]
Reference strain rate $EPS0$ [s ⁻¹]	1 [106]
Specific heat CP [Nmm/tonneK]	1.28e9 [106]

EOS_001 - LINEAR_POLYNOMIAL

Parameter	Value
Coefficient $C0$ [-]	0 [106]
Coefficient $C1$ [-]	2804 [106]
Coefficient $C2$ [-]	4.07e+4 [106]
Coefficient $C3$ [-]	-3.6e4 [106]
Coefficient $C4$ [-]	1.7 [106]
Coefficient $C5$ [-]	1.7 [106]
Coefficient $C6$ [-]	0 [106]