

**ERCIYES UNIVERSITY
GRADUATE SCHOOL OF NATURAL AND
APPLIED SCIENCES
DEPARTMENT OF MECHANICAL ENGINEERING**

**DYNAMIC BEHAVIOR OF FUNCTIONALLY GRADED
PLATES UNDER IMPACT LOADS: MECHANICAL
CHARACTERIZATION, IMPACT TESTS, AND
NUMERICAL MODELING**

**by
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Ph.D. Thesis

**February 2022
KAYSERİ**

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COMPLIANCE OF SCIENTIFIC ETHICS

I hereby declare that all information in this document has been obtained and presented in accordance with academic rules and ethical conduct. I also declare that, as required by these rules and conduct, I have fully cited and referenced all material and results that are not original to this work.

Kemal ARSLAN



The Ph.D. thesis entitled **“Dynamic Behavior of Functionally Graded Plates under Impact Loads: Mechanical Characterization, Impact Tests, and Numerical Modeling”** has been prepared in accordance with the Graduate Thesis Proposal and Thesis Writing Guidelines of Erciyes University.

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Kemal ARSLAN

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ABSTRACT

This study presents a holistic effort including the production, mechanical characterization, impact tests, and numerical modeling of the layered Al6061/SiC FGM (Functionally Graded Material) plates. It is aimed to develop realistic numerical models for predicting the low- and high-velocity impact response of the FGM plates. Three different material compositions of the FGM plate as the metal-rich, linear, and ceramic-rich are considered representing the mechanical behavior transition from ductile to brittle, respectively. Drop-weight impact test method and a single-stage gas gun are utilized respectively for the low- and high-velocity impact experiments. Numerical modeling of the FGM plates is performed by the explicit finite element code, LS-DYNA®. For the constitutive modeling of the FGM plates, mechanical characterization of the FGM layers is performed using the quasi-static compression and SHPB (Split-Hopkinson Pressure Bar) test methods. An experimental-based layer-wise modeling approach that considers the actual mechanical behavior of the FGM plates by the quasi-static and high strain-rate mechanical characterization is implemented in the numerical models. Different constitutive modeling approaches are presented depending on the compositional gradient of the FGM plates and the loading rate (low- or high-velocity impact). For the metal-rich and linear FGM plates, a rate-

independent constitutive modeling approach using Piecewise Linear Plasticity Model is presented in the low-velocity impact analysis, while a rate-dependent constitutive modeling approach utilizing mainly Plasticity with Damage Model is developed in the high-velocity impact analysis. For the high ceramic content layers of the ceramic-rich FGM plate, a concrete damage model, Karagozian & Case (K&C) Concrete Model - Release III, is proposed for both low- and high-velocity impact analysis. The numerical results indicate that the developed numerical models predict the low- and high-velocity impact response of the FGM plates with a good agreement in general. Particularly, the proposed concrete damage-based constitutive modeling approach for the ceramic-rich FGM plate shows an excellent improvement for the numerical results compared to the elastoplastic modeling approach, and the improved model successfully captures the brittle damage modes of the ceramic-rich FGM plate such as cone crack, conoidal fracture, and comminution under the impact loads.

Keywords: Functionally Graded Material (FGM), Mechanical Characterization, Impact Analysis, Finite Element Method, Strain-Rate.

**FONKSİYONEL DERECELENDİRİLMİŞ PLAKALARIN ÇARPMA
YÜKLERİ ALTINDA DİNAMİK DAVRANIŞLARI: MEKANİK
KARAKTERİZASYON, ÇARPMA TESTLERİ VE SAYISAL MODELLEME**

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ÖZET

Bu tez, katmanlı Al6061/SiC FDM (Fonksiyonel Derecelendirilmiş Malzeme) plakaların üretimi, mekanik karakterizasyonu, darbe testleri ve sayısal modellemesini içeren bütüncül bir çalışma sunmaktadır. FDM plakaların düşük ve yüksek hızlı çarpma davranışını tahmin etmek için gerçekçi sayısal modellerin geliştirilmesi amaçlanmıştır. FDM plakaların, sünekten kırılana mekanik davranış geçişini temsil eden metal-zengin, doğrusal ve seramik-zengin olmak üzere üç farklı malzeme bileşimi dikkate alınmıştır. Düşük ve yüksek hızlı çarpma deneyleri için sırasıyla ağırlık düşürme test yöntemi ve tek kademeli gaz silah sistemi kullanılmıştır. FDM plakaların sayısal modellemesi LS-DYNA® eksplisit sonlu elemanlar kodu ile gerçekleştirilmiştir. FDM plakaların yapısal modellemesi için, FDM katmanlarının mekanik karakterizasyonu, yarı-statik basma ve Split-Hopkinson Basınç Çubuğu (SHPB, Split-Hopkinson Pressure Bar) test yöntemleri kullanılarak gerçekleştirilmiştir. Sayısal modellerde, yarı-statik ve yüksek şekil değiştirme hızlarında mekanik karakterizasyon ile elde edilen FDM plakaların gerçek mekanik davranışını dikkate alan katman bazında deneysel tabanlı bir modelleme yaklaşımı uygulanmıştır. FDM plakaların bileşimsel gradyanına ve yükleme hızına (düşük veya yüksek hızlı çarpma) bağlı olarak farklı yapısal modelleme yaklaşımları sunulmuştur. Metal-zengin ve

doğrusal FDM plakaları için, düşük hızlı çarpma analizinde Piecewise Linear Plasticity modeli kullanılarak şekil değiştirme hızından bağımsız bir yapısal modelleme yaklaşımı sunulurken, yüksek hızlı çarpma analizinde temel olarak Plasticity with Damage modelini kullanan şekil değiştirme hızına bağlı bir yapısal modelleme yaklaşımı sunulmuştur. Seramik-zengin FDM plakasının yüksek seramik içerikli katmanları için, hem düşük hem de yüksek hızlı çarpma analizlerinde, Karagozian & Case (K&C) Concrete Model - Release III beton hasar modeli önerilmiştir. Sayısal sonuçlar, geliştirilen sayısal modellerin, FDM plakaların düşük ve yüksek hızlı çarpma davranışını genel olarak iyi bir uyum ile tahmin ettiğini göstermiştir. Özellikle, seramik-zengin FDM plakası için önerilen beton-hasar tabanlı yapısal modelleme yaklaşımı, elastoplastik modelleme yaklaşımı ile karşılaştırıldığında sayısal sonuçlar için mükemmel bir iyileşme göstermiştir ve önerilen bu model, seramik-zengin FDM plakasının konik çatlak, konoid kırılma ve parçalanma gibi kırılğan hasar modlarını başarılı bir şekilde yakalamıştır.

Anahtar Kelimeler: Fonksiyonel Derecelendirilmiş Malzeme (FDM), Mekanik Karakterizasyon, Çarpma Analizi, Sonlu Elemanlar Metodu, Şekil Değiştirme Hızı.

TABLE OF CONTENTS

DYNAMIC BEHAVIOR OF FUNCTIONALLY GRADED PLATES UNDER IMPACT LOADS: MECHANICAL CHARACTERIZATION, IMPACT TESTS, AND NUMERICAL MODELING

COMPLIANCE WITH SCIENTIFIC ETHICS	ii
COMPLIANCE WITH GUIDELINES.....	iii
ACCEPTANCE AND APPROVAL	iv
ACKNOWLEDGEMENTS.....	v
ABSTRACT.....	vii
ÖZET	ix
TABLE OF CONTENTS	xi
LIST OF TABLES.....	xiv
LIST OF FIGURES.....	xvi

CHAPTER 1

INTRODUCTION

1.1. Motivation	1
1.2. Literature Review	2

CHAPTER 2

SPECIFICATIONS AND PRODUCTION OF FUNCTIONALLY GRADED MATERIALS FOR QUASI-STATIC AND DYNAMIC MECHANICAL CHARATERIZATION AND IMPACT TESTS

2.1. Introduction.....	12
2.2. Specifications of FGM Plates.....	12
2.3. Production of FGM Specimens	16

CHAPTER 3

CONSTITUTIVE MODELS FOR NUMERICAL MODELING OF FUNCTIONALLY GRADED PLATES UNDER IMPACT LOADS

3.1. Introduction.....	20
3.2. Plastic Kinematic Model.....	21
3.3. Isotropic Elastic Failure Model.....	22
3.4. Piecewise Linear Plasticity Model	23
3.5. Plasticity with Damage Model	24
3.6. Karagozian & Case (K&C) Concrete Model - Release III.....	25
3.7. TTO (Tamura–Tomota–Ozawa) Model	26

CHAPTER 4

QUASI-STATIC AND DYNAMIC MECHANICAL CHARACTERIZATION OF FUNCTIONALLY GRADED PLATES

4.1. Introduction.....	29
4.2. Quasi-Static Mechanical Characterization	29
4.2.1. Quasi-Static Compression Tests	30
4.2.2. Brazilian Tests (Splitting Tensile Tests)	33
4.3. Dynamic Mechanical Characterization.....	35
4.4. Other Mechanical and Physical Properties	41

CHAPTER 5

LOW-VELOCITY IMPACT TESTS AND NUMERICAL MODELING OF FUNCTIONALLY GRADED PLATES

5.1. Introduction.....	42
5.2. Low-Velocity Impact Tests	42
5.3. Experimental Results	44
5.4. Numerical Modeling	56

5.4.1. Verification of Elastic Modulus of Sintered Al6061 Plate	62
5.4.2. Strain-Rate Dependency of Low-Velocity Impact Analysis	68
5.5. Numerical Results	70
5.6. Comparison of the Present Numerical Model and the TTO Model.....	81

CHAPTER 6

HIGH-VELOCITY IMPACT TESTS AND NUMERICAL MODELING OF FUNCTIONALLY GRADED PLATES

6.1. Introduction.....	86
6.2. High-Velocity Impact Tests	86
6.3. Experimental Results	88
6.4. Numerical Modeling	106
6.4.1. Constitutive Modeling of the Al6061 Plate	109
6.4.2. Constitutive Modeling of the Metal-Rich FGM Plate.....	110
6.4.3. Constitutive Modeling of the Linear FGM Plate	111
6.4.4. Constitutive Modeling of the Ceramic-Rich FGM Plate	114
6.5. Numerical Results	116

CHAPTER 7

CONCLUSIONS

7.1. Introduction.....	130
7.2. Conclusions	131
7.3. Recommendations for Future Studies.....	135
REFERENCES	136
VITA	146

LIST OF TABLES

Table 4.1. The elastic modulus of the FGM layer specimens	33
Table 4.2. The theoretical and measured densities of the FGM layer specimens	41
Table 5.1. The compressive and tensile strength of the high ceramic content layers of the ceramic-rich FGM plate	62
Table 5.2. The mechanical properties of the Al6061-T6 sheet and Al6061 plate	64
Table 5.3. The comparison of the MAT_024-based and MAT_072R3-based models in terms of peak contact force, absorbed energy, and permanent displacement.....	72
Table 5.4. The experimental and numerical peak contact force, absorbed energy, and permanent displacement of the FGM plates	78
Table 5.5. The stress to strain transfer ratio (q) for different FGM layers	82
Table 5.6. The material parameters of the FGM plate constituents	82
Table 5.7. The comparative peak contact forces for the present numerical model and the TTO model with respect to the experimental results	85
Table 6.1. The measured projectile velocities	88
Table 6.2. The high-velocity impact test results in terms of penetration status	89
Table 6.3. The DOP measurements of the partially penetrated plates (mm)	96
Table 6.4. The material properties of the FSP (Steel 4340).....	108
Table 6.5. The material parameters of the 70%SiC layer	111

Table 6.6. The material parameters of the 50%SiC and 60%SiC layers	113
Table 6.7. The experimental and numerical DOP measurements and residual velocity of the projectile for the Al6061 plate.....	117
Table 6.8. The experimental and numerical DOP measurements and residual velocity of the projectile for the metal-rich FGM plate.....	118
Table 6.9. The experimental and numerical DOP measurements and residual velocity of the projectile for the linear FGM plate.....	122
Table 6.10. The experimental and numerical DOP measurements and residual velocity of the projectile for the ceramic-rich FGM plate	124

LIST OF FIGURES

Figure 1.1. A schematic illustration for an FGM plate	2
Figure 2.1. A schematic representation for the layered FGM plate	13
Figure 2.2. The distributions of ceramic volume fraction for different compositional gradients of the FGM plate	14
Figure 2.3. The volume fractions of the Al6061 and SiC constituents for each layer of the FGM plate for different compositional gradients	15
Figure 2.4. An internal view of the hot-press unit.....	17
Figure 2.5. The H13 hot-work tool steel dies for production of the FGM plates	17
Figure 2.6. A group of produced specimens for the high- and low-velocity impact and compression tests.....	19
Figure 2.7. A group of compression test specimens prepared by the EDM	19
Figure 2.8. The FGM layer specimens in terms of SiC fraction	19
Figure 3.1. The elastoplastic behavior definition of Plastic Kinematic Model with isotropic and kinematic hardening	22
Figure 3.2. A schematic representation of stress-strain curves for various strain- rates	23
Figure 3.3. The damage included stress-strain curve.....	24
Figure 3.4. A nonlinear damage curve	25
Figure 3.5. A schematic definition of the TTO model.....	28
Figure 4.1. The testing machine and a quasi-static compression test setup	30

Figure 4.2. The quasi-static true compressive stress-strain curves of the FGM layer specimens for different FGM plates.....	32
Figure 4.3. A Brazilian test setup	34
Figure 4.4. A schematic illustration of a SHPB test system.....	35
Figure 4.5. The SHPB test system.....	36
Figure 4.6. A dynamic compression test setup	37
Figure 4.7. A characteristic SHPB test result	37
Figure 4.8. The quasi-static and dynamic true compressive stress-strain curves of the FGM layer specimens	39
Figure 5.1. The drop-weight impact test equipment.....	43
Figure 5.2. The boundary conditions of the low-velocity impact tests	44
Figure 5.3. The after-impact front and rear face views of the Al6061 plate.....	45
Figure 5.4. The after-impact front and rear face views of the metal-rich FGM plate	46
Figure 5.5. The after-impact front and rear face views of the linear FGM plate	47
Figure 5.6. The after-impact front and rear face views of the ceramic-rich FGM plate	48
Figure 5.7. The cross-sectional deformation views of the Al6061 plate and the metal-rich and linear FGM plates for the impact energy of 48 J	49
Figure 5.8. The cross-sectional deformation and damage views of the ceramic-rich FGM plate for different impact energies.....	50
Figure 5.9. The diameter of indentation trace of the plates (mm).....	52

Figure 5.10. The comparative contact force histories, energy absorption histories, and force-displacement response of the plates for different impact energies.....	54
Figure 5.11. The numerical model of the FGM plate for the low-velocity impact	56
Figure 5.12. The constitutive models for the layers of the ceramic-rich FGM plate	59
Figure 5.13. The Brazilian test results for the 55%SiC, 68%SiC, and 70%SiC layers	61
Figure 5.14. The numerical models for the Al6061-T6 sheet and sintered Al6061 plate for the low-velocity impact	64
Figure 5.15. The stress-strain curves of the Al6061-T6 sheet and Al6061 plate ..	64
Figure 5.16. The experimental and numerical contact force histories of the Al6061-T6 sheet for different impact energies	65
Figure 5.17. The comparison of the theoretical and experimental elastic modulus of the sintered Al6061 plate in terms of contact force history.....	66
Figure 5.18. The experimental-numerical results of the Al6061 plate in terms of contact force and absorbed energy histories, and force-displacement response.....	67
Figure 5.19. The effect of strain-rate dependency for the low-velocity impact analysis of the Al6061 plate in terms of contact force history.....	69
Figure 5.20. The comparison of the MAT_024-based and MAT_072R3-based models in terms of contact force and absorbed energy histories, and force-displacement response of the ceramic-rich FGM plate	71

Figure 5.21. The experimental and numerical cross-sectional deformation and damage profiles of the ceramic-rich FGM plate.....	73
Figure 5.22. The crack distributions of the MAT_072R3-based model for the ceramic-rich FGM plate	73
Figure 5.23. The experimental and numerical external deformation and damage views of the ceramic-rich FGM plate (48 J).....	74
Figure 5.24. The experimental and numerical contact force histories of the FGM plates.....	76
Figure 5.25. The experimental and numerical force-displacement response of the FGM plates	77
Figure 5.26. The experimental and numerical absorbed energy histories of the FGM plates	79
Figure 5.27. The experimental and numerical energy absorption quantities and ratios of the FGM plates	80
Figure 5.28. The effect of q parameter on the numerical results ($n = 0.1$, 48 J)	83
Figure 5.29. The comparison of the present numerical model and the TTO model with respect to the experimental results.....	84
Figure 6.1. The single-stage gas-gun	87
Figure 6.2. The geometrical details of a .30 caliber FSP for sabot launch	87
Figure 6.3. The target fixture for the FGM plates	88
Figure 6.4. The front and rear face deformation and damage views of the Al6061 plate	90
Figure 6.5. The front and rear face deformation and damage views of the metal-rich FGM plate and the diameters of the frontal spall failure (mm)	92

Figure 6.6. The front and rear face deformation and damage views of the linear FGM plate and the diameters of the frontal spall failure (mm)	93
Figure 6.7. The front and rear face deformation and damage views of the ceramic-rich FGM plate	95
Figure 6.8. The post-impact cross-sectional views of the Al6061 plate	97
Figure 6.9. The post-impact cross-sectional views of the metal-rich FGM plate	98
Figure 6.10. The post-impact cross-sectional views of the linear FGM plate	99
Figure 6.11. The post-impact cross-sectional views of the ceramic-rich FGM plate	100
Figure 6.12. The high-speed camera setup for the high-velocity impact tests	102
Figure 6.13. The sequential damage frames of the Al6061 plate (950 m/s)	103
Figure 6.14. The sequential damage frames of the metal-rich FGM plate (950 m/s)	104
Figure 6.15. The sequential damage frames of the linear FGM plate (950 m/s)	104
Figure 6.16. The sequential damage frames of the ceramic-rich FGM plate (950 m/s)	105
Figure 6.17. The numerical model of the FGM plate and the FSP	106
Figure 6.18. The quasi-static and high strain-rate true compressive stress-strain curve of the FSP (Steel 4340)	108
Figure 6.19. The damage included true stress-strain curve of the Al6061 plate	110

Figure 6.20. The implemented constitutive models for the metal-rich FGM plate	111
Figure 6.21. The implemented constitutive models for the linear FGM plate ..	112
Figure 6.22. The quasi-static and dynamic true stress-strain curves of the 50%SiC and 60%SiC layers.....	113
Figure 6.23. The implemented constitutive models for the ceramic-rich FGM plate	114
Figure 6.24. The experimental and numerical cross-sectional deformation and damage views of the Al6061 plate	117
Figure 6.25. The experimental and numerical cross-sectional deformation and damage views of the metal-rich FGM plate	118
Figure 6.26. The experimental and numerical frontal spall failure of the metal- rich FGM plate	119
Figure 6.27. The experimental and numerical cross-sectional deformation and damage views of the linear FGM plate	122
Figure 6.28. The effect of strain-rate dependency of the 50%SiC and 60%SiC layers on the numerical results for the linear FGM plate (500 m/s)	123
Figure 6.29. The experimental and numerical cross-sectional deformation and damage views of the ceramic-rich FGM plate.....	124
Figure 6.30. The experimental and numerical sequential damage frames of the Al6061 plate (950 m/s).....	126
Figure 6.31. The experimental and numerical sequential damage frames of the metal-rich FGM plate (950 m/s)	126

Figure 6.32. The experimental and numerical sequential damage frames of the ceramic-rich FGM plate (950 m/s) 127

Figure 6.33. The temporal numerical damage views of the FGM plates (800 m/s) 129



CHAPTER 1

INTRODUCTION

1.1. Motivation

Functionally Graded Materials (FGMs) are heterogeneous media with a continuous and spatial variation of material composition from one point to another with respect to a prespecified rule. FGMs are multifunctional advanced composite materials comprising of two or more distinct constituents, generally metals and ceramics, that can be designed for a specific application by tailoring material composition through a desired direction (Fig. 1.1). FGM concept is initially proposed for space structures in 1984 by a group of material scientists as a thermal shield by means of their capability to maintain structural integrity under high-temperature environments [1]. This is due to excellent thermal resistance characteristics of ceramic constituent and superior fracture toughness of metal constituent. Furthermore, FGMs with a proper compositional gradient design could eliminate interfacial problems such as residual stresses, stress concentrations, and delamination that are frequently encountered in the conventional laminated composites. Therefore, FGMs present a great potential as a thermal or structural component for aerospace, nuclear, defense, and automotive industries where dynamic effects are highly important, and it is necessary to determine the dynamic behavior of FGM structures subjected to high-rate loadings.

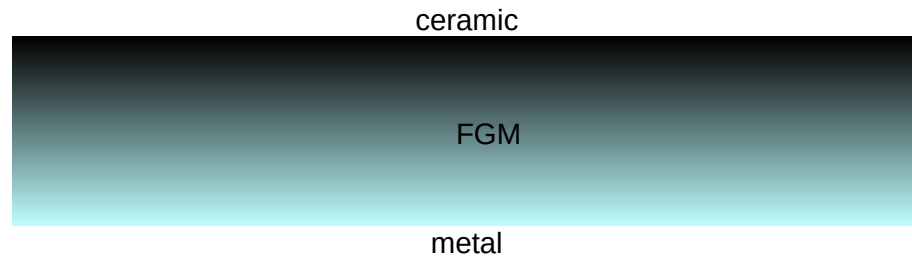


Figure 1.1. A schematic illustration for an FGM plate.

In recent years, FGMs have received a great interest for impact applications by means of their promising features provided by metal and ceramic constituents in a single volume. In impact applications of FGMs, ceramic constituent provides high penetration resistance, while metal constituent presents high fracture toughness and plastic deformation capability for structural integrity. Accordingly, various studies have been performed to investigate dynamic mechanical behavior of FGMs subjected to impact loads ranging from low- to high-velocity impact, and it is still-continuing research field especially for armor applications.

1.2. Literature Review

The literature review of FGM structures subjected to impact loadings is presented in two separate sections as the low- and high-velocity impact to discuss the studies more explicitly and to understand the importance and objective of this study.

Review on Functionally Graded Materials under Low-Velocity Impact

Gong et al. [2] proposed an analytical model for the low-velocity elastic impact behavior of FGM cylindrical shells. The influence of constituent volume fractions and various FGM shells configurations were studied.

Apetre et al. [3] presented a one-dimensional contact and low-velocity impact analysis of FGM core sandwich beams utilizing a nonlinear spring-mass model. It was stated that a reasonable FGM core design could prevent the impact damage decreasing maximum strains in the sandwich beams.

Etemadi et al. [4] investigated the low-speed impact response of sandwich beams with FGM core using finite element modeling. The effects of striker velocity, elastic modulus of face plates, core type, and geometrical dimensions of sandwich beam were examined. It was indicated that FGM core gives higher contact force and lower strains compared to homogeneous core.

Gunes et al. [5] performed a numerical study on the low-speed impact analysis of FGM plates considering the elastoplastic material behavior by means of TTO (Tamura–Tomota–Ozawa) model [6]. Mori–Tanaka micromechanics model [7,8] was implemented to estimate the effective elastic mechanical properties. The material composition of the plate and the striker velocity showed a remarkable effect on the impact response of FGM plates. Gunes et al. [9] extended their study with experimental tests to show the applicability of the TTO model for the elastoplastic impact modeling of FGM plates. The comparative results showed that the TTO model could give reasonable results for metal-rich composition, whereas higher differences occur for ceramic-rich composition.

Dai et al. [10] studied on the low-speed impact behavior of FGM plates employing the FSDT (first-order shear deformation theory) and a contact model. The results indicated that the plate composition, striker velocity, and geometric parameters exhibit a considerable influence on the impact response of FGM plates.

Yalamanchili and Sankar [11] explored the indentation and impact behavior of FGM beams subjected to hard and soft side impact under the same flexural

stiffness. It was found that the peak stresses and strains are lower in case of the soft side impact.

Khalili et al. [12] studied the low-speed impact behavior of FGM plates under thermal effects employing an analytical spring-mass model. The temperature rise causes decrement in the contact force and increment in the horizontal stress and indentation of FGM plates.

Kiani et al. [13] implemented a modified contact model to explore the low-speed impact behavior of FGM beams subjected to thermal loadings. The influence of compositional gradient, boundary conditions, and velocity, mass, and position of striker was evaluated as well as thermal effects. It was indicated that each parameter has a remarkable influence on the impact response of FGM beams.

Mao et al. [14] explored the low-speed impact damage of FGM coated shallow spherical shells. An analytical modeling based on interfacial damage formulations was performed. It was indicated that increasing striker velocity causes earlier damage initiation, and the damage level is higher at the contact region. Increasing FGM coating thickness increases stiffness of the surface which results in a shorter contact time and a higher contact force.

Shariyat and Jafari [15] proposed a refined contact model to investigate the low-speed impact behavior of radially prestressed bidirectional FGM plates. It was stated that both geometrical and material parameters exhibit a considerable effect on the impact behavior of FGM plates. Shariyat and Jafari [16] extended their study to low-speed impact analysis of two-directional FGM plates supported by an elastic foundation proposing a semi-analytical approach. The mechanical properties of FGM plates were described by a micromechanical model, and the refined contact model that considers the properties of plate and foundation was

employed. It was indicated that additional vibration modes that significantly influence the impact behavior of the plate occur in case of elastic foundation.

Shariyat and Farzan [17] presented the eccentric low-speed impact response of preloaded FGM plates, and Shariyat and Nasab [18] studied the noncentral low-speed impact behavior of FGM plates with an elastic foundation and partially or completely supported edges. In these studies, a refined contact model was employed considering the material distribution, plate thickness, and elastic foundation and support parameters. Both studies showed that the contact force and indentation increase in case of the eccentric impact.

Shariyat and Nasab [19] presented a micromechanics-based analytical model for the impact analysis of FGM plates considering viscoelastic behavior and employing an explicit shear-bending decomposition theory and a differential quadrature method. It was noted that considering the viscoelastic behavior of FGM plates causes increment in the plate stiffness.

Mao et al. [20] evaluated the dynamic and damage behavior of FGM spherical shallow shells subjected to low-speed impact based on a continuum damage law. It was found that the damage effect becomes more significant for the central displacement and contact force, and the damage initiates more earlier once the curvature radius of FGM shells increases.

Mohammadi et al. [21] presented the impact analysis of sandwich panels reinforced by FGM face plates developing an improved high-order theory with in-plane stresses of the core and implementing the FSDT for the face plates. The developed model exhibited a good correlation with finite element simulations, and it was found that there is a linear variation between impact velocity-peak contact force, impact velocity-central displacement, and the contact time is independent of the impact velocity.

The low-speed impact analysis of thermal loaded FGM plates [22], FGM doubly curved panels [23], and thermal loaded FGM beams [24] supported by an elastic foundation was performed employing a higher-order shear deformation theory. In these studies, the effect of different parameters such as temperature, foundation stiffness, compositional gradient, and striker velocity was discussed, and it was found that each parameter has a remarkable effect on the impact response of the FGM structures.

Arslan and Gunes [25] carried out a numerical study on the flexural impact behavior of FGM sandwich beams utilizing the finite element method. The elastic mechanical properties and elastoplastic material behavior of FGM sandwich beams were described respectively by the Mori–Tanaka model and the TTO model. The influence of material composition and number of layers of the beam, striker velocity, and impact side was investigated, and it was found that the material composition shows a significant effect, whereas the number of layers and impact side have minor effects on the impact response of FGM sandwich beams.

Zhu and Cai [26] developed a particle size-dependent finite element model for the low-speed impact analysis of FGM plates by incorporation of a dislocation-based continuum model and the TTO elastoplastic model. It was found that the particle size has a considerable effect on the impact behavior of FGM plates, and the size-dependent model gives closer results to the experiments than the size-independent model in terms of indentation diameter, especially for the higher volume fraction index and higher impact velocities. Zhu and Cai [27] extended their work to development of a strain rate-dependent enhanced continuum model in conjunction with a dislocation-based model and the Johnson–Cook plasticity model [28] for FGM plates subjected to a drop-weight impact to analyze the particle size-dependent behavior. The TTO model was applied for the

plasticity behavior of FGM plates. It was indicated that the prediction of strain-rate dependent model for the indentation diameter is better than the size-independent and strain-rate-independent models, especially for the higher impact velocities and lower volume fraction index. Additionally, the particle size-dependent behavior of FGM plates was significantly affected by the strain-rate even under a drop-weight impact.

Gunes et al. [29] carried out an experimental study on the low-speed impact behavior of Al6061/SiC FGM core sandwich plates with Al6061 face layers. The compositional gradient of the FGM core varied from metal-rich to ceramic-rich layer, and the impact tests were performed for both side of the sandwich plates. It was observed that the sandwich plate shows higher resistance to impact damage in case of impacted from ceramic-rich side.

Chaker et al. [30] developed a solid shell finite element in ABAQUS® user element interface to prevent locking problems that may be encountered in the shell and solid elements of ABAQUS®. To show the performance of the developed element, it was compared to existing studies from the literature, and it was implemented to estimate the low-speed impact response of FGM plates with the application of a single solid shell element across the plate thickness.

Review on Functionally Graded Materials under High-Velocity Impact

Petterson et al. [31] explored the ballistic resistance capability of Ti/TiB₂ FGM composites produced by spark plasma sintering (SPS) and hot isostatically pressing (HIP) methods. It was found that the FGM composite produced by SPS method shows better protection capability than that of produced by HIP method.

Eghtesad et al. [32] developed a mesh-free particle-based method, corrective smoothed particle algorithm (CSPM), for the high-speed impact analysis of metal/ceramic FGM plates. A combined strength model including the Johnson–

Cook plasticity model and the Johnson–Holmquist–Beissel brittle fracture model [33] was implemented for the constitutive modeling of FGM plates. The results showed that the developed meshless method with an appropriate strength model is capable of modeling both elastoplastic and brittle behavior of FGM plates under high-velocity impact.

Aydin and Apalak [34] studied the ballistic impact response of sandwich plates with Al6061 face layers and Al6061/SiC FGM core for different compositional gradients. It was found that the penetration resistance of the sandwich plate increases as the ceramic fraction increases except for ceramic-rich composition that undergoes brittle damage, and the frontal aluminum layer weakens the fragmentation effect of its adjacent ceramic-rich layer.

Huang and Chen [35-37] performed different experimental and experimental-numerical studies on the impact resistance capability of ceramic/ceramic ($\text{Al}_2\text{O}_3/\text{ZrO}_2$) FGM plates under ballistic impact. To show the ballistic performance, a four-layered FGM plate which is the base configuration was compared to monolithic ceramic plate (Al_2O_3) and adhesively bonded four-layered FGM plate [35], FGM plate with different number of layers (eleven-layered) and ceramic matrix composite plates with various volume fractions of the constituents [36], and FGM plates with different ZrO_2 fractions [37]. From these studies, it can be indicated as a main result that the FGM plate shows the highest ballistic performance compared to the other configurations, and the presence of gradient structure decreases the tensile wave reflection, prevents the circumferential and radial crack propagation due to toughened alumina, and delays the initiation and propagation of conoidal ceramic fracture.

Gunes et al. [38] carried out finite element simulations to investigate the high-velocity impact behavior and ballistic limit of honeycomb sandwich structures with Al6061/SiC FGM face plates implementing the Mori–Tanaka scheme and

the TTO model for the FGM plates. It was found that ballistic limit of the structure increases as the compositional gradient varies from metal-rich to ceramic-rich composition, and the existence of honeycomb core has a considerable influence on the impact behavior of the sandwich structure decreasing the penetration depth and residual projectile velocity compared to spaced FGM plates. Arslan et al. [39] extended their study performing ballistic impact experiments. The results indicated that the total ceramic fraction of the FGM plates depending on the material composition plays an important role on the damage mechanisms and energy absorption capability of the structure, and the numerical model has a limited ability to model the FGM plates; it shows increasing discrepancies especially for increasing ceramic fraction, whereas it could give reasonable results for metal-rich compositions.

Arslan and Gunes [40] performed an experimental damage analysis on honeycomb sandwich structures reinforced by Al6061/B₄C FGM plates subjected to high-speed impact. The experiments showed that the compositional gradient of the FGM plates ranging from metal-rich to ceramic-rich has a transition from ductile to brittle behavior and has a remarkable effect on the damage profiles, energy absorption capability, and impact resistance of the structure. Moreover, the honeycomb core provided a contribution for energy absorption due to buckling of the cell walls and transverse crushing of the cells.

Chao et al. [41] investigated the ballistic performance of three-layered Al2024/B₄C FGM plates produced by squeeze casting with the ceramic fraction of 25%, 47%, and 70% from bottom to top layer, respectively. Monolithic composite plates with the B₄C fraction of 47% and 70% were also considered for the comparison. It was indicated that the FGM plate presents a higher impact performance than the monolithic composite plates exhibiting a better structural integrity than the 70%B₄C plate and a lower penetration depth than the 47%B₄C plate, and the

presence of gradient structure enhances the fracture resistance of the plate by decreasing the reflected tensile wave magnitude.

Gunes et al. [42] carried out a numerical study on the performance of FGM plates under normal and oblique ballistic impact. The material behavior of FGM plates was described employing the Mori–Tanaka micromechanical model and the TTO model. The numerical results showed that both compositional gradient of the plate and obliquity of the projectile have a considerable effect on the ballistic performance of FGM plates.

Zhang et al. [43] produced Ti/TiB-TiB₂ composites having a continuous compositional gradient to investigate the ballistic penetration resistance. The composites were produced by SPS method in combination with tape casting. It was stated that the presence of rod-shape thin tape layers formed during sintering process successfully reduces the interface effects, and the produced composite has an excellent ballistic impact resistance exhibiting a capability of multiple impact. Zhong et al. [44] extended their work to design of Ti/TiB FGM composites as an armor considering the acoustic impedance matching between the layers. Two different types of composites, continuous volumetric gradient and continuous acoustic impedance gradient composites were produced, and the acoustic impedance gradient composite showed a better ballistic impact resistance capability than the continuous volumetric gradient composite.

Development of realistic numerical models has become a necessity for engineering designs and saving experimental costs. Therefore, it is important to develop realistic numerical models predicting the plasticity, damage, and fracture phenomena of FGM structures for impact engineering. On the other hand, numerical modeling of FGMs, particularly for nonlinear impact problems, is a challenging problem due to their complicated structure and behavior, and according to the reviewed literature, the studies were generally performed using

one of the experimental, numerical, or analytical approaches, and mostly elastic behavior of FGMs was considered. There are very limited studies on the numerical modeling of FGMs considering the plasticity, damage, and fracture behavior in the open literature. Based on this motivation, a holistic effort including the production, mechanical characterization, impact tests and numerical modeling of Al6061/SiC FGM plates is performed within the scope of this study. Namely, realistic numerical models for the low- and high-velocity impact behavior of FGM plates are developed in conjunction with experimental efforts using the explicit finite element code, LS-DYNA®. For this purpose, initially, the test samples are produced by powder metallurgy, then the quasi-static and dynamic mechanical characterization is carried out by the quasi-static compression test and a SHPB (Split-Hopkinson Pressure Bar) setup to consider the strain-rate effects and to implement the actual mechanical behavior of the FGM plates in the numerical models. To validate the numerical models, the low- and high-velocity impact tests are conducted using respectively drop-weight impact test method and a single-stage gas gun. Different constitutive modeling approaches including the effects of strain-rate, plasticity and plasticity damage, and brittle damage are presented depending on both compositional gradient of the FGM plates and impact velocity (low- or high-velocity impact).

CHAPTER 2

SPECIFICATIONS AND PRODUCTION OF FUNCTIONALLY GRADED MATERIALS FOR QUASI-STATIC AND DYNAMIC MECHANICAL CHARACTERIZATION AND IMPACT TESTS

2.1. Introduction

In this study, there are different specimens in terms of experiment type and corresponding specimen dimensions. The specimen types can be classified into three groups: i) quasi-static and dynamic compression test specimens, ii) low-velocity impact test specimens, and iii) high-velocity impact test specimens. The detailed information regarding to the specifications and production stages of the FGM specimens is given in this chapter.

2.2. Specifications of FGM Plates

The FGM plates consisting of metal (m) and ceramic (c) constituents have a compositional gradient from metal layer to ceramic-rich layer throughout the plate thickness (Fig. 2.1). The material composition of the FGM plates is described with respect to a power-law as

$$V_m(z) = \left(1 - \frac{z}{t}\right)^n \quad (2.1)$$

$$V_c(z) = 1 - V_m(z) \quad (2.2)$$

where V is the volume fraction, t is the thickness of plate, z is the coordinate variable in the thickness direction, and n is the compositional gradient exponent that determines the plate composition. Eq. (2.1) indicates a continuous variation, however, the FGM plates are produced in a stepwise variation (layer by layer) in this study. Hence, the variation of volume fractions is described as

$$V_m = \left(\frac{k-1}{N-1} \right)^n \quad (2.3)$$

$$V_c = 0.70(1 - V_m) \quad (2.4)$$

where k is the layer number from bottom to top of the plate, and N is the total number of layers. Fig. 2.2 represents the comparative continuous and stepwise (present state) variations of ceramic volume fraction for different n values.

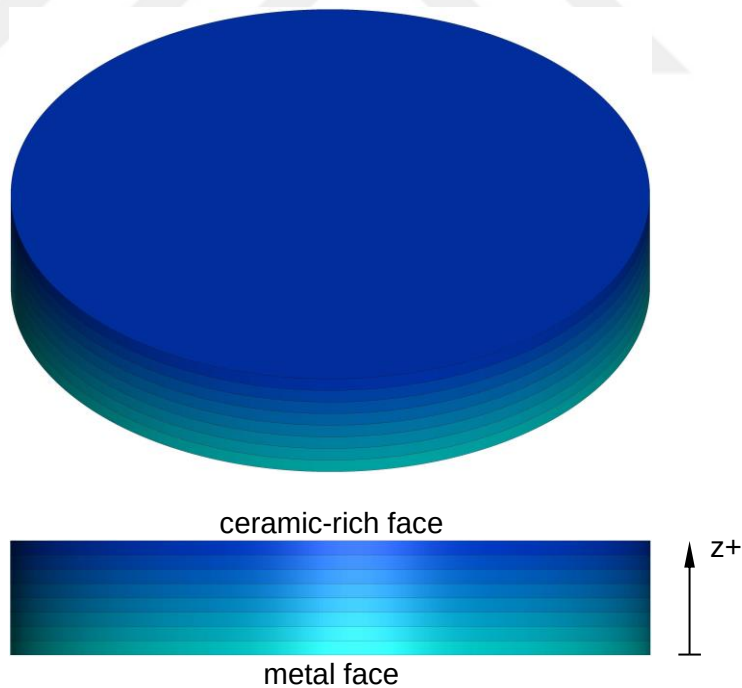


Figure 2.1. A schematic representation for the layered FGM plate.

In this study, the FGM plates consist of aluminum alloy 6061 (Al6061) metal and silicon carbide (SiC) ceramic constituents with an average particle size of 15 and

45 microns, respectively. The FGM plates comprise of eight sequential layers with metal bottom layer (100%Al6061) and ceramic-rich top layer (70%SiC-30%Al6061). The maximum ceramic fraction is selected as 70% since the higher ceramic content may cause some failures such as debonding and delamination just in the production stage for some specimens regarding to the previous experiences.

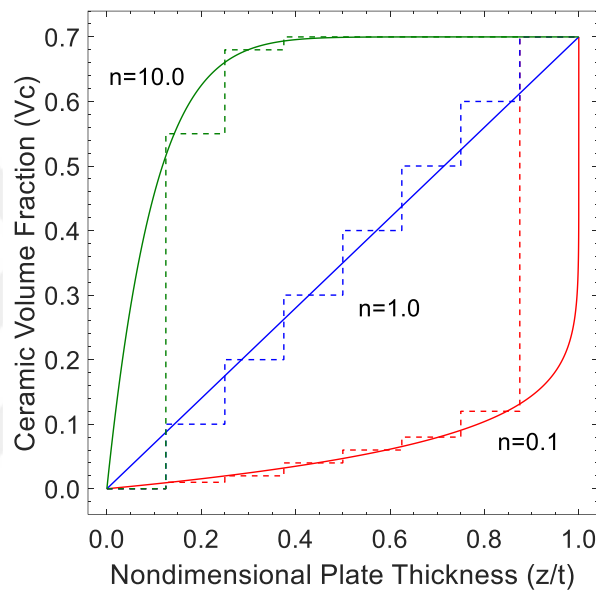


Figure 2.2. The distributions of ceramic volume fraction for different compositional gradients of the FGM plate (straight lines: continuous variation, dashed lines: stepwise variation): $n = 0.1$ (metal-rich), $n = 1.0$ (linear), $n = 10.0$ (ceramic-rich).

For the FGM plates, three different compositional gradients, $n = 0.1$ (metal-rich), $n = 1.0$ (linear), and $n = 10.0$ (ceramic-rich) compositions are considered representing the mechanical behavior transition from ductile to brittle, respectively. To illustrate the compositional gradient of the FGM plates, the volume fractions of the Al6061 and SiC constituents for each layer of the FGM plates are given in Fig. 2.3.

70%SiC – 30%Al6061
12%SiC – 88%Al6061
8%SiC – 92%Al6061
6%SiC – 94%Al6061
4%SiC – 96%Al6061
2%SiC – 98%Al6061
1%SiC – 99%Al6061
100%Al6061

(a) $n = 0.1$ (metal-rich)

70%SiC – 30%Al6061
60%SiC – 40%Al6061
50%SiC – 50%Al6061
40%SiC – 60%Al6061
30%SiC – 70%Al6061
20%SiC – 80%Al6061
10%SiC – 90%Al6061
100%Al6061

(b) $n = 1.0$ (linear)

70%SiC – 30%Al6061
70%SiC – 30%Al6061
70%SiC – 30%Al6061
70%SiC – 30%Al6061
70%SiC – 30%Al6061
68%SiC – 32%Al6061
55%SiC – 45%Al6061
100%Al6061

(c) $n = 10.0$ (ceramic-rich)

Figure 2.3. The volume fractions of the Al6061 and SiC constituents for each layer of the FGM plate for different compositional gradients.

The productions of the specimens are categorized in three groups in terms of experiment type and specimen dimensions:

- i. FGM layer specimens for quasi-static and dynamic compression tests
- ii. FGM plates for low-velocity impact tests
- iii. FGM plates for high-velocity impact tests

The first group specimens are the compression test specimens for quasi-static and dynamic mechanical characterization of the FGM plates. These specimens are named as FGM layer specimens, since this group consists of each layer of the FGM plates with different material compositions. Basically, the FGM layer specimens are metal matrix composites (MMCs) having certain volume fractions of the constituents and including both metal-rich and ceramic-rich compositions as seen from Fig. 2.3. These specimens have a cylindrical geometry with approximate dimensions of $\text{Ø}6 \times 6$ mm. The second and third group specimens are the FGM plates for low- and high-velocity impact tests. These specimens are cylindrical plates with a diameter of 50 mm and a thickness of 10 mm for the low-velocity impact tests and with a diameter of 90 mm and a thickness of 16 mm for the high-velocity impact tests.

2.3. Production of FGM Specimens

The FGM specimens for all experiments are produced by powder metallurgy technique (the powder stacking-hot pressing technique) using a vacuum and atmosphere controlled hot-press unit (Fig. 2.4). This technique generally includes four main processes: i) powder mixing, ii) green compaction (powder stacking), iii) sintering, and iv) cooling. Different sintering dies that are manufactured from heat-treated H13 hot-work tool steel are utilized for these processes (Fig. 2.5).

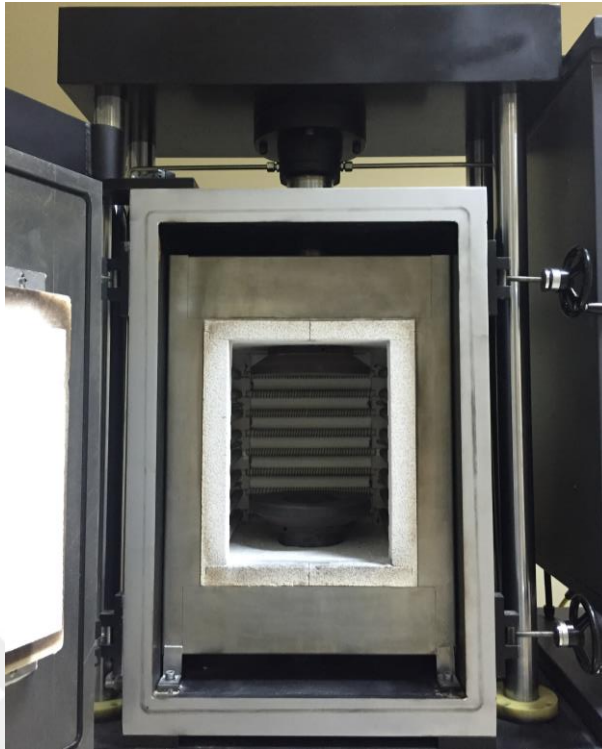
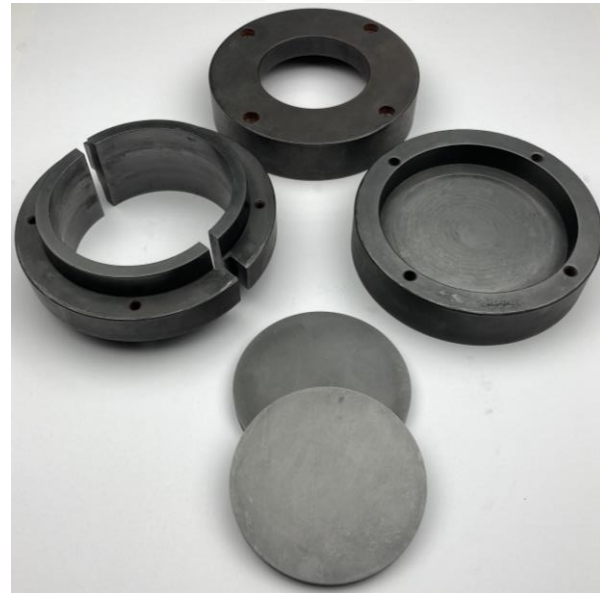


Figure 2.4. An internal view of the hot-press unit.



(a) Ø50 mm



(b) Ø90 mm

Figure 2.5. The H13 hot-work tool steel dies for production of the FGM plates.

In the first step, powder mixtures that have proper volumetric ratios of the constituents for each layer of the FGM specimens are blended in discrete mixing tubes by a powder mixer for 4-5 h to prevent unstable powder distribution and to obtain a homogeneous mixture. Then, inside of the die components are coated by a graphite lubricant to avoid fuse of the metal constituent to the die components during the sintering process. In the second step, the powder mixtures of each layer are stacked in the sintering die layer by layer in a convenient order applying unidirectional compaction under a low pressure. In the third step, the stacked and compacted specimen is sintered under a pressure of 50 MPa at a temperature of 620 °C with protective argon atmosphere for 1 h. In the fourth step, the sintering die is left to cool under controlled pressure and temperature to prevent thermal effects for the specimens. A group of produced specimens for the high- and low-velocity impact and quasi-static and dynamic compression tests from left to right is given in Fig. 2.6. The thickness of the specimens is slightly different from the planned thickness due to the production conditions. The thickness of the high-velocity impact test specimens is about 16-17 mm, while the thickness of the low-velocity impact test specimens is around 10 mm except for the ceramic-rich FGM plate, the thickness of the ceramic-rich FGM plate is approximately 11.5 mm.

The compression test specimens have an additional process to obtain their final dimensions. These specimens are prepared in two stages. The first stage is production of the specimens in the Ø50 mm die applying the above-mentioned processes. In the second stage, the compression test specimens are subtracted from the Ø50×6 mm specimens in the approximate dimensions of Ø6×6 mm by the EDM (electrical discharge machining) technique (Fig. 2.7). A group of FGM layer specimens for the compression tests with increasing volume fraction of SiC from left to right is shown in Fig. 2.8.

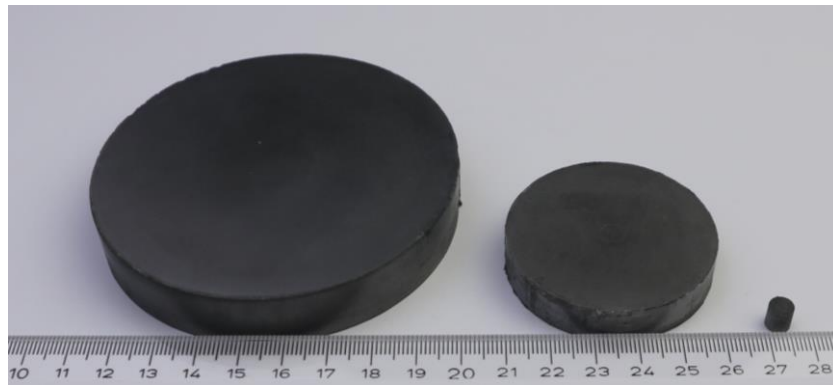


Figure 2.6. A group of produced specimens for the high- and low-velocity impact and compression tests.



Figure 2.7. A group of compression test specimens prepared by the EDM.

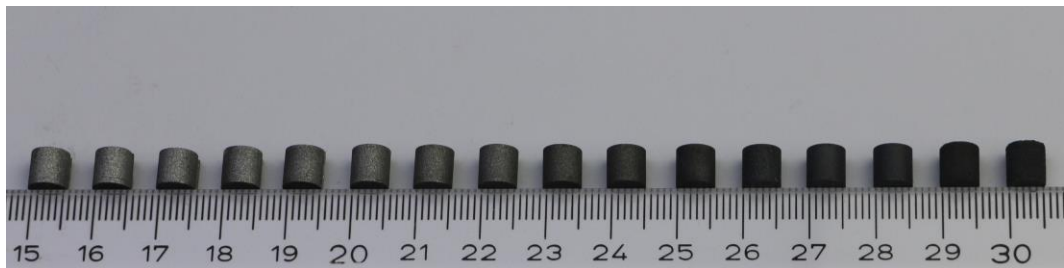


Figure 2.8. The FGM layer specimens in terms of SiC fraction.

(0%, 1%, 2%, 4%, 6%, 8%, 10%, 12%, 20%, 30%, 40%, 50%, 55%, 60%, 68%, 70%)

CHAPTER 3

CONSTITUTIVE MODELS FOR NUMERICAL MODELING OF FUNCTIONALLY GRADED PLATES UNDER IMPACT LOADS

3.1. Introduction

In this chapter, implemented constitutive models for numerical modeling of the FGM plates under impact loads are presented. In this study, the FGM plates have a transition from ductile (metal-rich) to brittle (ceramic-rich) behavior depending on the ceramic volume fraction of the layers. Namely, the FGM plates have both ductile and quasi-brittle/brittle layers according to the compositional gradient. Therefore, the constitutive modeling of the plates should be in a logical manner in terms of the ductility and brittleness of the layers. For this purpose, different constitutive models that are compatible to the mechanical behavior of the FGM layers are implemented to capture the realistic behavior of the FGM plates. Herein, a brief information about the constitutive models is given.

The numerical modeling procedure of the FGM plates is performed by the explicit finite element code, LS-DYNA®, and the implemented constitutive models from LS-DYNA® Keyword [45] are as follows:

1. Plastic Kinematic Model
(MAT_PLASTIC_KINEMATIC - MAT_003)
2. Isotropic Elastic Failure Model
(MAT_ISOTROPIC_ELASTIC_FAILURE - MAT_013)
3. Piecewise Linear Plasticity Model
(MAT_PIECEWISE_LINEAR_PLASTICITY - MAT_024)
4. Plasticity with Damage Model
(MAT_PLASTICITY_WITH_DAMAGE - MAT_081)
5. Karagozian & Case (K&C) Concrete Model - Release III
(MAT_CONCRETE_DAMAGE_REL3 - MAT_072R3)

Additionally, the TTO (Tamura–Tomota–Ozawa) model [6] that was used in the previous studies [25,38,39] is also utilized in the numerical analysis to show the improvement of the present numerical models.

3.2. Plastic Kinematic Model

Plastic Kinematic Model (MAT_PLASTIC_KINEMATIC or MAT_003) formulated by Krieg and Key [46] is an elastoplastic constitutive model that is suitable for modeling isotropic and kinematic hardening plasticity with an optional strain-rate dependent formulation. A schematic representation of the elastoplastic behavior definition of Plastic Kinematic Model is given in Fig. 3.1.

Strain-rate effects can be accounted for using the Cowper–Symonds model [47] which scales the yield stress with the factor of β as

$$\beta = 1 + \left(\frac{\dot{\epsilon}}{C} \right)^{1/p} \quad (3.1)$$

where $\dot{\epsilon}$ is the strain-rate ($\dot{\epsilon} = \sqrt{\dot{\epsilon}_{ij}\dot{\epsilon}_{ij}}$), and C and p are the strain-rate parameters.

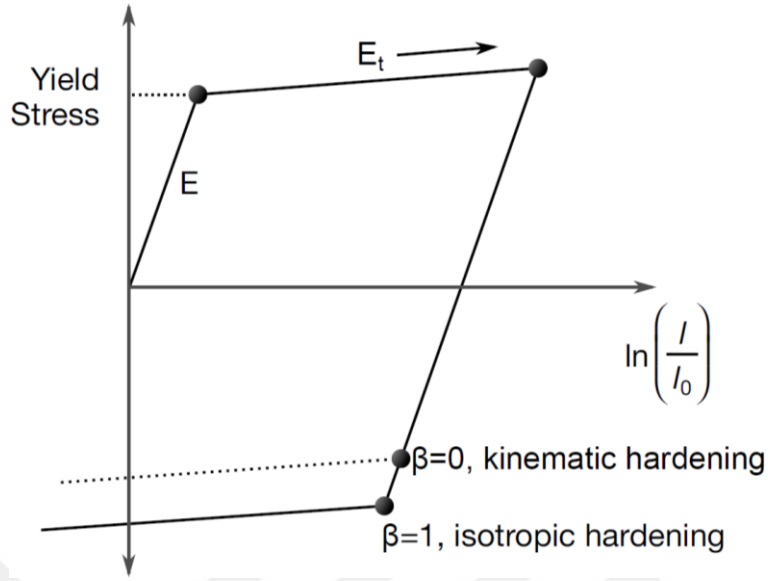


Figure 3.1. The elastoplastic behavior definition of Plastic Kinematic Model with isotropic and kinematic hardening [45].

3.3. Isotropic Elastic Failure Model

Isotropic Elastic Failure Model (MAT_ISOTROPIC_ELASTIC_FAILURE or MAT_013) is a non-iterative plasticity model. It has a failure option based on the plastic failure strain and failure pressure. Failure occurs when the following conditions are either satisfied.

$$\varepsilon_{eff}^p > \varepsilon_{max}^p \quad (3.2)$$

$$P^{n+1} < P_{min} \quad (3.3)$$

where ε_{max}^p and P_{min} are the plastic failure strain and failure pressure, respectively. Once either plastic strain or pressure reaches the failure criteria, the failed element is not able to carry tensile loads, and the deviatoric stresses are set to zero. The failed element can only carry compressive loads.

3.4. Piecewise Linear Plasticity Model

Piecewise Linear Plasticity Model (MAT_PIECEWISE_LINEAR_PLASTICITY or MAT_024) is an elastoplastic constitutive model that allows stress-strain curve input and strain-rate dependency. An effective plastic strain-based failure criteria can be defined to activate the failure option in the constitutive model. There are three different methods to consider the strain-rate effects:

1. The first method is the Cowper–Symonds model that is explained in the Plastic Kinematic Model.
2. A load curve that defines the scale factor versus strain-rate can be used to scale the yield stress instead of defining the strain-rate parameters.
3. Different stress-strain curves at various strain-rates can be described (Fig. 3.2).

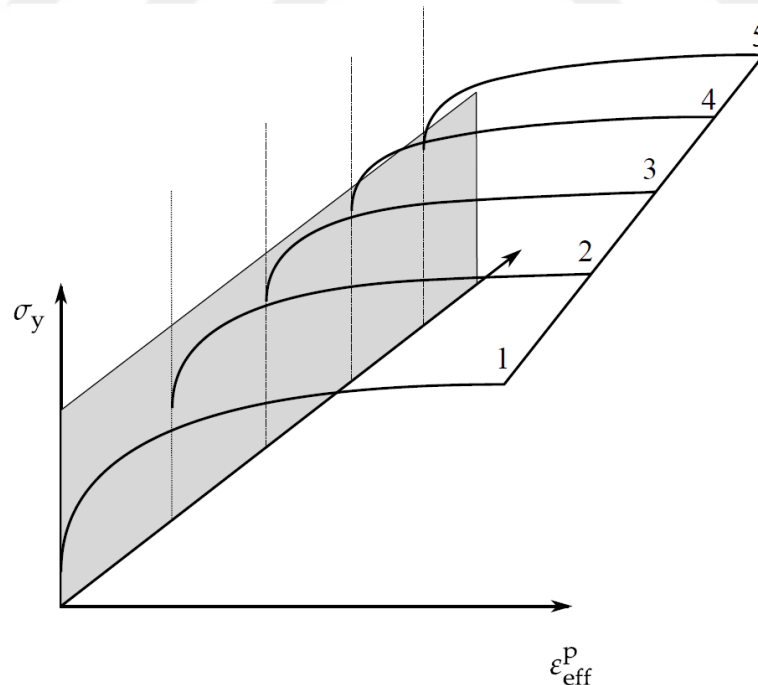


Figure 3.2. A schematic representation of stress-strain curves for various strain-rates [45].

3.5. Plasticity with Damage Model

Plasticity with Damage Model (MAT_PLASTICITY_WITH_DAMAGE or MAT_081) is an elastoplastic constitutive model that is similar to the MAT_024 model. In addition to the MAT_024, the MAT_081 considers the damage effects prior to rupture based on an effective plastic strain. The strain-rate effects are considered as is the case with the MAT_024. A nonlinear damage curve must be defined to consider the damage softening behavior. The illustrations of a damage included stress-strain curve and a nonlinear damage curve of the constitutive model are shown in Figs. 3.3 and 3.4, respectively.

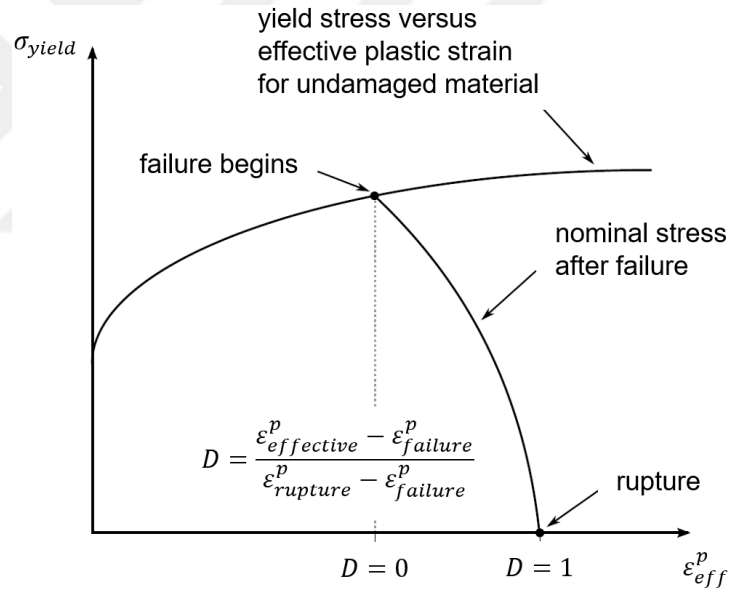


Figure 3.3. The damage included stress-strain curve [45].

In this constitutive model, the damage variable D which varies from zero (no damage) to unity (complete rupture) is defined in terms of the effective plastic strain after exceeding the failure strain:

$$D = \frac{\varepsilon_{effective}^p - \varepsilon_{failure}^p}{\varepsilon_{rupture}^p - \varepsilon_{failure}^p}, \quad \varepsilon_{failure}^p \leq \varepsilon_{effective}^p \leq \varepsilon_{rupture}^p \quad (3.4)$$

After the effective plastic strain exceeds the failure strain, damage softening begins and continues until the rupture strain.

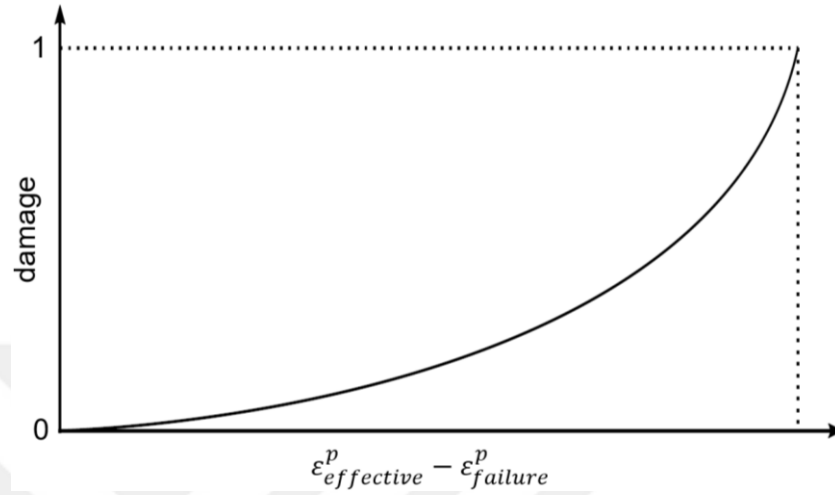


Figure 3.4. A nonlinear damage curve [45].

3.6. Karagozian & Case (K&C) Concrete Model - Release III

Karagozian & Case (K&C) Concrete Model - Release III (MAT_072R3 or MAT_CONCRETE_DAMAGE_REL3) developed by Malvar et al. [48] is a three-invariant model that consists of three independent failure surfaces (Eqs. 3.5-3.7) and considers the damage and strain-rate effects. Here, a brief information about the model is given, more detailed description of the model can be found in the Refs. [48,49].

$$\Delta\sigma_m = a_0 + \frac{p}{a_1 + a_2 p} \quad (\text{maximum failure surface}) \quad (3.5)$$

$$\Delta\sigma_r = a_0 + \frac{p}{a_{1f} + a_{2f} p} \quad (\text{residual failure surface}) \quad (3.6)$$

$$\Delta\sigma_m = a_0 + \frac{p}{a_{1y} + a_{2y} p} \quad (\text{yield failure surface}) \quad (3.7)$$

where p is the pressure, and a_0, a_1, a_2 are the material parameters.

This constitutive model is very useful for modeling concrete structures subjected to high-rate loadings, and has an automatic model parameter generation capability based on only the unconfined compressive strength. Although it is a concrete damage model, it was implemented for modeling a ceramic material (alumina) by Rahbek et al. [50], and the model successfully captured the damage mechanisms of the alumina subjected to high-velocity impact. Therefore, the MAT_072R3 constitutive model can be applicable for modeling other brittle materials. Accordingly, this constitutive model is utilized in this study for modeling some FGM layers that show quasi-brittle or brittle response.

3.7. TTO (Tamura–Tomota–Ozawa) Model

The TTO (Tamura–Tomota–Ozawa) model proposed by Tamura et al. [6] describes the elastoplastic behavior of two-constituent composite materials, especially metal/ceramic composites. In this study, the elastoplastic behavior of the FGM plates is also modeled by the TTO model for the purpose of comparison between the present numerical model and the TTO-based numerical model. In the TTO-based numerical model, the TTO model is implemented collaborating with the Mori–Tanaka micromechanical model [7,8]. The Mori–Tanaka model is utilized for describing the elastic mechanical properties of the FGM plates. A brief description of the TTO model and the Mori–Tanaka model is given below knowing that the relation between volume fractions of the constituents and their variations from Eqs. (2.3) and (2.4) given in the previous chapter.

TTO Model

Considering a metal/ceramic composite, the elastoplastic deformation of the composite is controlled by the metal constituent according to the TTO model (Fig. 3.5). Therefore, it is assumed that the plastic flow in the composite initiates once

the metal constituent yields. The yield strength of the composite, σ_y is described by

$$\sigma_y = \sigma_0 \left(V_m + \frac{q + E_m}{q + E_c} \frac{E_c}{E_m} (1 - V_m) \right) \quad (3.8)$$

where σ_0 is the yield stress of the metal constituent, and q is the stress to strain transfer ratio which is necessary to estimate the stress-strain curve of the composite (Fig. 3.5) and defined by

$$q = \frac{\sigma_c - \sigma_m}{|\varepsilon_c - \varepsilon_m|} \quad (3.9)$$

The q parameter varies from 0 to ∞ depending on the microstructural interaction and mechanical properties of the constituents. The stress-strain behavior of the composite is determined by Eqs. (3.10) and (3.11).

$$\frac{\varepsilon}{\varepsilon_y} = \frac{V_c E}{q + E_c} \frac{\sigma_m}{\sigma_y} + \frac{q + V_m E_c}{q + E_c} \frac{E}{E_m} \frac{\sigma_0}{\sigma_y} \left(\frac{\sigma_m}{\sigma_0} \right)^{n_0} \quad (3.10)$$

$$\frac{\sigma}{\sigma_y} = \frac{V_m q + E_c}{q + E_c} \frac{\sigma_m}{\sigma_y} + \frac{V_c q}{q + E_c} \frac{E_c}{E_m} \frac{\sigma_0}{\sigma_y} \left(\frac{\sigma_m}{\sigma_0} \right)^{n_0} \quad (3.11)$$

where ε_y is the yield strain of the composite, and n_0 is the hardening exponent of the metal constituent determined by

$$\frac{\varepsilon_m}{\varepsilon_0} = \left(\frac{\sigma_m}{\sigma_0} \right)^{n_0} \quad (3.12)$$

where ε_0 is the yield strain of the metal constituent.

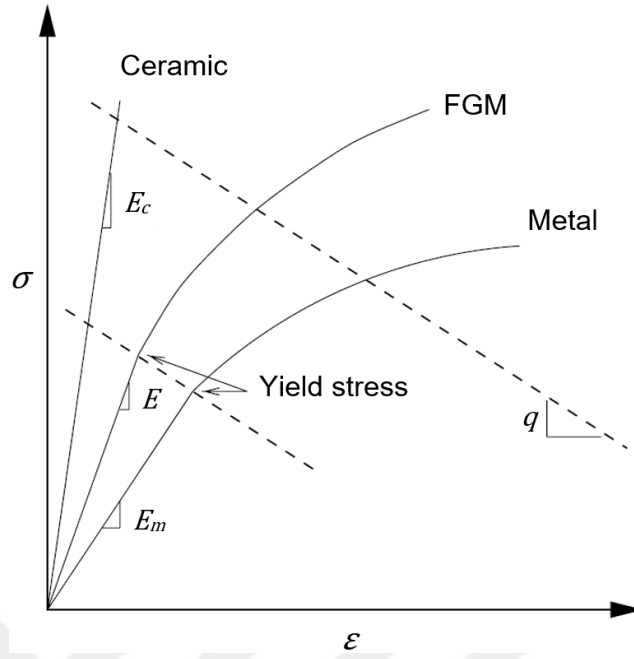


Figure 3.5. A schematic definition of the TTO model [51].

Mori–Tanaka Model

The Mori–Tanaka micromechanical model predicts the effective elastic modulus, E and the Poisson's ratio, ν by

$$E = \frac{9KG}{3K + G} \quad (3.13)$$

$$\nu = \frac{3K - 2G}{2(3K + G)} \quad (3.14)$$

where the effective bulk modulus, K and shear modulus, G are defined as

$$\frac{K - K_m}{K_c - K_m} = \frac{V_c}{1 + (1 - V_c) \frac{3(K_c - K_m)}{3K_m + 4G_m}} \quad (3.15)$$

$$\frac{G - G_m}{G_c - G_m} = \frac{V_c}{1 + (1 - V_c) \frac{G_c - G_m}{G_m + \frac{G_m(9K_m + 8G_m)}{6(K_m + 2G_m)}}} \quad (3.16)$$

CHAPTER 4

QUASI-STATIC AND DYNAMIC MECHANICAL CHARACTERIZATION OF FUNCTIONALLY GRADED PLATES

4.1. Introduction

In this chapter, quasi-static and dynamic mechanical characterization of the FGM plates is carried out to determine the strain-rate effects and to develop compatible numerical models using the characterized behavior for the low- and high-velocity impact tests. For this purpose, each layer of the FGM plates (the FGM layer specimens) is subjected to compression tests at a quasi-static strain-rate and various high strain-rates. The compression tests are repeated three times for each specimen, and the average results are presented. The test results are evaluated in terms of true stress-strain curves of the FGM layer specimens, and the strain-rate effect is presented for each FGM layer specimens by comparing the quasi-static and high strain-rate response.

4.2. Quasi-Static Mechanical Characterization

The quasi-static mechanical characterization includes two different experiments, the quasi-static compression test, and the splitting tensile or Brazilian test. The splitting tensile tests are carried out to determine the tensile strength of some FGM layers with high ceramic content, while the quasi-static compression tests are performed to obtain the mechanical properties such as the elastic modulus, yield strength, failure strain, and rupture strain, etc.

4.2.1. Quasi-Static Compression Tests

The quasi-static compression tests are carried out by an MTS® electromechanical universal testing machine (Fig. 4.1). Cylindrical specimens having a length to diameter ratio of $l/d = 1.0$ ($\text{Ø}6 \times 6$ mm) are utilized for the quasi-static compression tests of the FGM layer specimens. To minimize the interfacial friction, petroleum jelly is applied between the platens and specimens. The specimens are tested at a crosshead speed of 1 mm/min which corresponds to an approximate strain-rate of $2.8 \times 10^{-3} \text{ s}^{-1}$.



Figure 4.1. The testing machine and a quasi-static compression test setup.

The engineering compressive stresses, σ and strains, ε are respectively calculated as

$$\sigma = \frac{F}{A_o} \quad (4.1)$$

$$\varepsilon = \frac{\Delta l}{l_o} = \frac{l_o - l_i}{l_o} \quad (4.2)$$

where F , A_o , l_o , and l_i are respectively the instantaneous force, original cross-sectional area of the specimen, original length of the specimen, and instantaneous length of the specimen. Assuming that a uniform deformation occurs with

constant volume change during the test, the true stresses, σ_t and strains, ε_t are determined as

$$\sigma_t = \sigma(1 - \varepsilon) \quad (4.3)$$

$$\varepsilon_t = -\ln(1 - \varepsilon) \quad (4.4)$$

To present the stress-strain behavior of each FGM layer and to illustrate the gradation of the stress-strain response of the FGM plates, the true compressive stress-strain curves of the FGM layer specimens are given in Fig. 4.2 for the metal-rich, linear, and ceramic-rich FGM plates. It is obviously seen from Fig. 4.2 that the plastic strain capacity of the layer specimens decreases with increasing ceramic fraction. On the other hand, the strength of the layer specimens increases as the ceramic fraction increases up to a certain limit. The 55%SiC layer specimen has the highest compressive strength. The layer specimens with higher ceramic fraction than 55% show an unexpected behavior. Namely, strength weakening begins to occur over the 55%SiC fraction depending on weak bond between the ceramic particles due to decrement of the metal fraction. A detailed discussion on this state is presented in Chapter 5, Section 5.3 with the deformation and damage evaluation of the ceramic-rich FGM plate which comprises of mostly the layers with high ceramic fraction. Furthermore, the layer specimens with the SiC fraction from 0% to 40% exhibit elastoplastic behavior, and especially the specimens with relatively lower ceramic fraction show superior ductility. On the other hand, the stress-strain curves of the layer specimens with the SiC fraction from 50% to 70% have a quasi-linear response with lower ductility compared to the other layer specimens. Consequently, it can be expected that the metal-rich and linear FGM plates undergo ductile deformation, whereas the ceramic-rich FGM plate shows quasi-brittle or brittle behavior under impact loads.

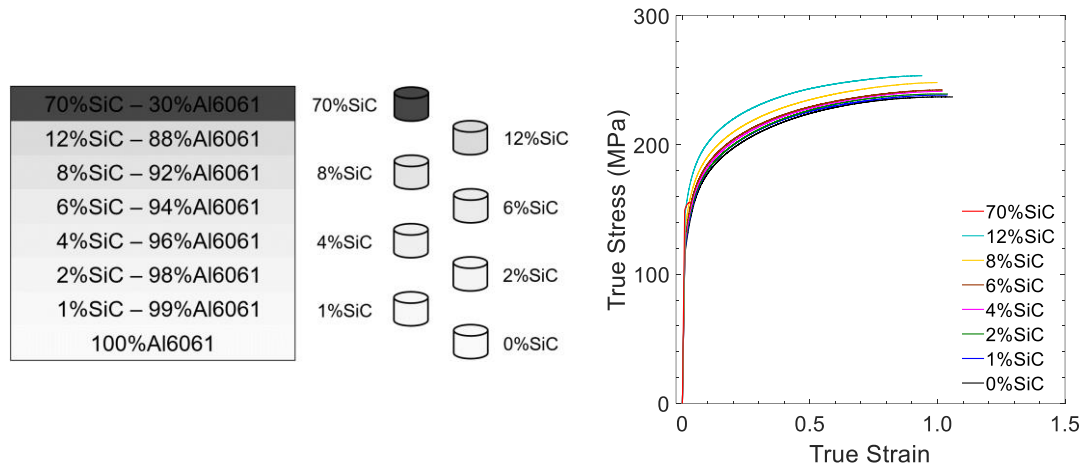
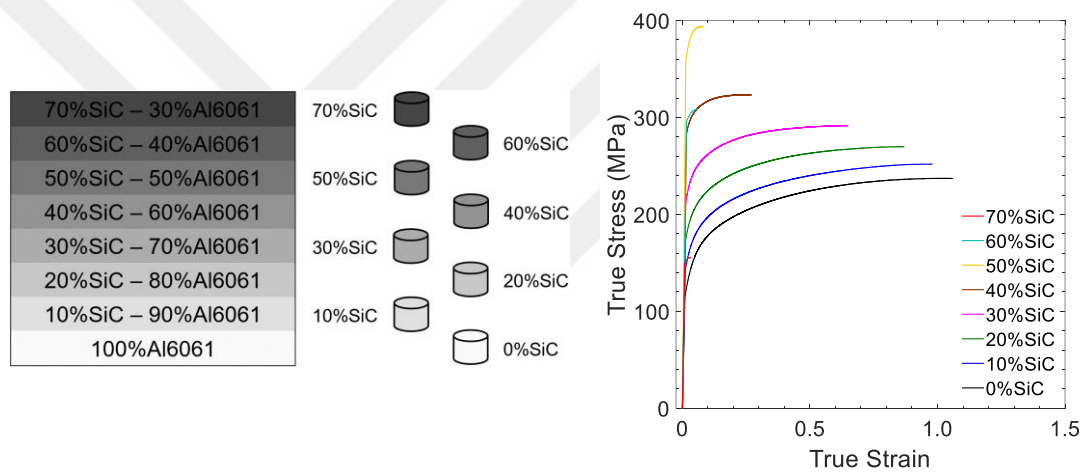
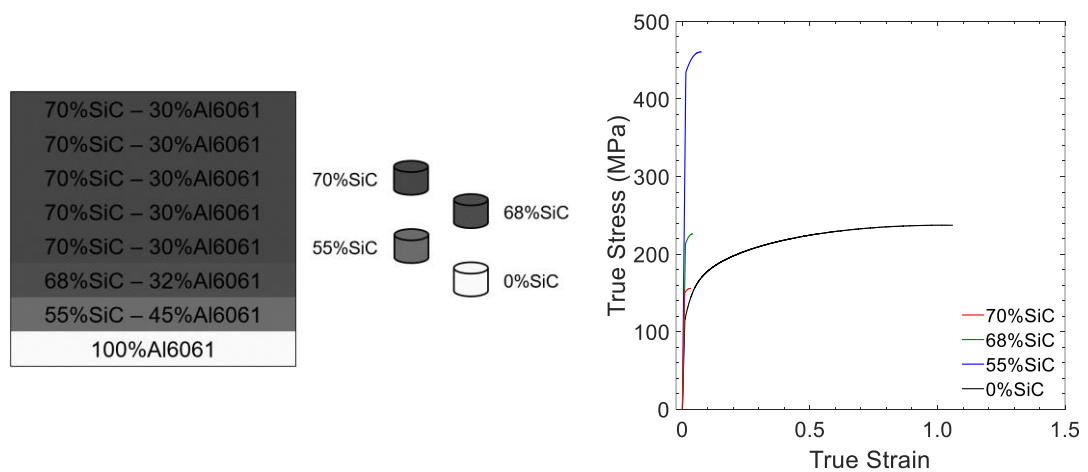
(a) $n = 0.1$ (metal-rich)(b) $n = 1.0$ (linear)(c) $n = 10.0$ (ceramic-rich)

Figure 4.2. The quasi-static true compressive stress-strain curves of the FGM layer specimens for different FGM plates.

From the quasi-static compression tests, elastic modulus of the FGM layer specimens which has a significant effect on the accuracy of the numerical results is determined. For a correct measurement, the elastic modulus is determined by considering the compliance of testing machine. Namely, the machine compliance is determined and subtracted from the specimen displacement. The elastic modulus of each FGM layer specimen is listed in Table 4.1. It is obviously seen that the pure Al6061 layer (0%SiC) has an unexpected elastic modulus comparing the elastic modulus of aluminum and its alloys. The reason of this unusual elastic modulus is explained in detail with its verification in Chapter 5, Section 5.4.1.

Table 4.1. The elastic modulus of the FGM layer specimens.

Specimen	Elastic modulus (GPa)	Specimen	Elastic modulus (GPa)
0%SiC	12.0	20%SiC	14.0
1%SiC	12.1	30%SiC	16.2
2%SiC	12.1	40%SiC	18.1
4%SiC	12.2	50%SiC	27.0
6%SiC	12.4	55%SiC	32.3
8%SiC	12.6	60%SiC	18.6
10%SiC	12.9	68%SiC	17.6
12%SiC	13.2	70%SiC	17.0

4.2.2. Brazilian Tests (Splitting Tensile Tests)

Tensile strength of brittle materials can be determined conducting both direct tensile test and Brazilian test (also known as the splitting tensile test or indirect tensile test). The Brazilian test is an experiment that is generally used in rock mechanics to determine the tensile strength of rocks indirectly. It is also commonly carried out for other brittle materials, especially concrete. Due to

difficulties in specimen preparation and generally invalid failure of specimen during test, the direct tensile test is not usually preferred for rocks, concrete, and other brittle materials. On the other hand, the Brazilian test is widely used since it is a simple and efficient method to determine the tensile strength of brittle materials.

In this study, the Brazilian test is utilized for some FGM layer specimens consisting of high ceramic fraction. Fig. 4.3 shows a Brazilian test setup for an FGM layer specimen. These specimens are the 55%SiC, 68%SiC, and 70%SiC layer specimens which are the layers of the ceramic-rich FGM plate. These specimens have ceramic-rich composition, and their mechanical behavior is quasi-brittle or brittle as seen from Fig. 4.2. The purpose of conducting the Brazilian test to these specimens is being a necessary input of the tensile strength in the MAT_072R3 constitutive model. The Brazilian test results are given in the numerical modeling procedure of Chapter 5.

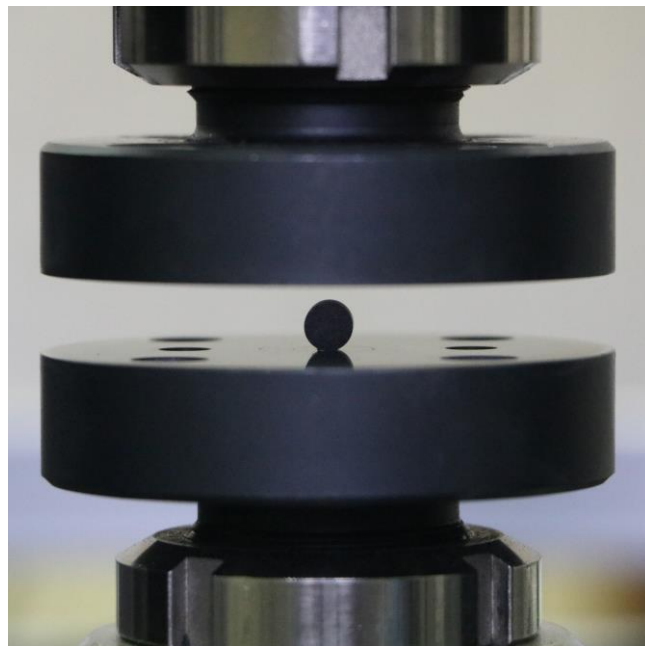


Figure 4.3. A Brazilian test setup.

4.3. Dynamic Mechanical Characterization

The dynamic mechanical characterization of the FGM plates is performed by a split-Hopkinson pressure bar (SHPB) also known as Kolsky bar [52] setup. The SHPB is an experimental technique to characterize the dynamic behavior of materials at high strain-rates. A schematic representation of a SHPB setup is shown in Fig. 4.4. A SHPB setup generally consists of three main units, a loading unit, bar components, and a data acquisition and recording system.

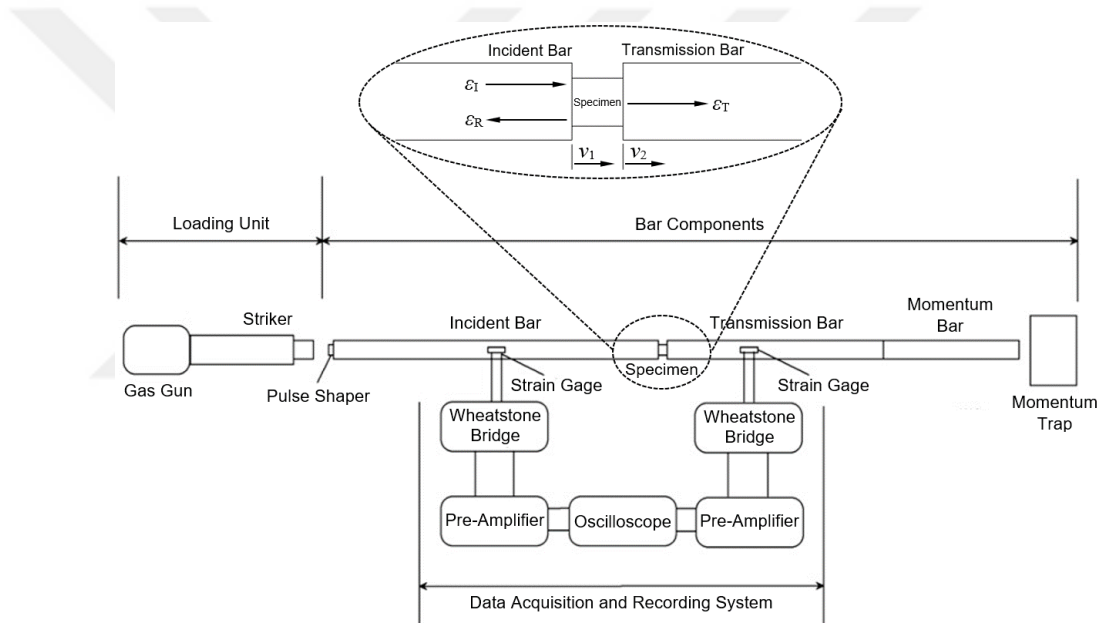


Figure 4.4. A schematic illustration of a SHPB test system [53].

In this study, a 19 mm (bar diameter) SHPB test system manufactured by Physics Applications Inc. is used for the dynamic compression tests (Fig. 4.5). The system has a striker bar with a length of 400 mm, and incident and transmission bars with a length of 2 m. Steel bars are commonly used in SHPB setups, however the aluminum alloy 7075-T651 is selected as the bar material instead of steel bars, since the acoustic or wave impedance (The acoustic impedance, Z is defined as the product of density, ρ and elastic wave speed, C ; $Z = \rho C$ where $C = \sqrt{E/\rho}$.) of bar materials and specimen should be close for the accuracy of test results. As

seen from Table 4.1, elastic modulus of the FGM layer specimens is even lower than that of the 7075-T651 bar, and the acoustic impedance of the FGM layer specimens is closer to the 7075-T651 bar than a steel bar. Moreover, the impedance mismatch in case of higher impedance of bar material causes nearly full reflection of the incident wave, and this makes stress equilibrium verification almost impossible [54].



Figure 4.5. The SHPB test system.

In this section, the dynamic mechanical characterization is carried out for the FGM layer specimens with the SiC fraction from 0% to 40%, since the strain-rate effects for the other specimens, 50%SiC to 70%SiC, is considered by a different method in the numerical modeling procedure. Four different strain-rates are considered for the dynamic compression tests of the layer specimens. A dynamic compression test setup of a layer specimen is given in Fig. 4.6. As is the case with the quasi-static compression tests, the interfaces between bars and specimens are lubricated by petroleum jelly to minimize the interfacial friction.



Figure 4.6. A dynamic compression test setup.

A characteristic SHPB test result is given in Fig. 4.7. The recorded data are the incident, reflected, and transmitted waves (marked with dashed line) which are utilized to obtain the stress-strain curves of the layer specimens.

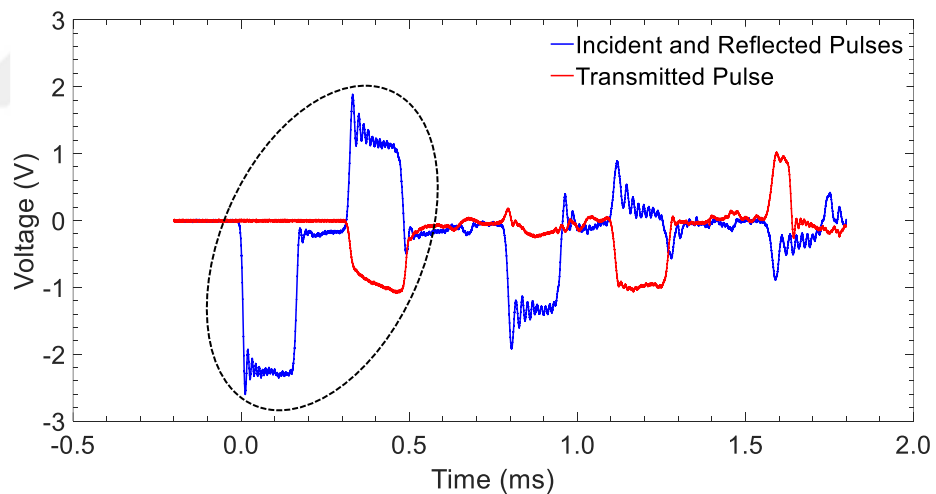


Figure 4.7. A characteristic SHPB test result.

A compressive wave is generated once the striker bar is impacted on the incident bar, and this compressive wave propagates through the incident bar. When the compressive wave reaches to the interface between the incident bar and specimen, a part of the compressive wave is reflected as a tensile wave into the incident bar, and the remaining part of the compressive wave is transmitted throughout the specimen to the transmission bar. The incident and reflected

waves are recorded by the strain gage on the incident bar, while the transmitted wave is recorded by the strain gage on the transmission bar. Based on the one-dimensional wave theory, the stress, strain, and strain-rate of the specimens are calculated respectively by

$$\sigma(t) = \frac{A_B}{A_s} E_B \varepsilon_T(t) \quad (4.5)$$

$$\varepsilon(t) = -2 \frac{C_B}{L_s} \int_0^t \varepsilon_R(t) dt \quad (4.6)$$

$$\dot{\varepsilon}(t) = -2 \frac{C_B}{L_s} \varepsilon_R(t) \quad (4.7)$$

where A_B , E_B , and C_B are the cross-sectional area, elastic modulus, and elastic wave speed of the bar material, A_s and L_s are the cross-sectional area and length of the specimen, and $\varepsilon_T(t)$ and $\varepsilon_R(t)$ are the time-dependent transmitted and reflected strains, respectively.

To show the strain-rate effect, the quasi-static and high strain-rate true compressive stress-strain curves of each FGM layer specimen are given in Fig. 4.8. As seen from Fig. 4.8, a significant increase in the stress levels (strain-rate hardening) is observed with increasing strain-rate from quasi-static to high strain-rates. On the other hand, the strain-rate hardening effect decreases at high strain-rates. At high strain-rates, maximum strain of the specimens increases with increasing strain-rate by means of plastic deformation capability of the specimens except for the 40%SiC layer specimen. For the 40%SiC layer specimen, the maximum strain or the plastic deformation capability begins to decrease with increasing strain-rate beyond the strain-rate of 1763 s^{-1} due to mechanical behavior transition from ductile to quasi-brittle depending on having higher ceramic fraction than the other layer specimens.

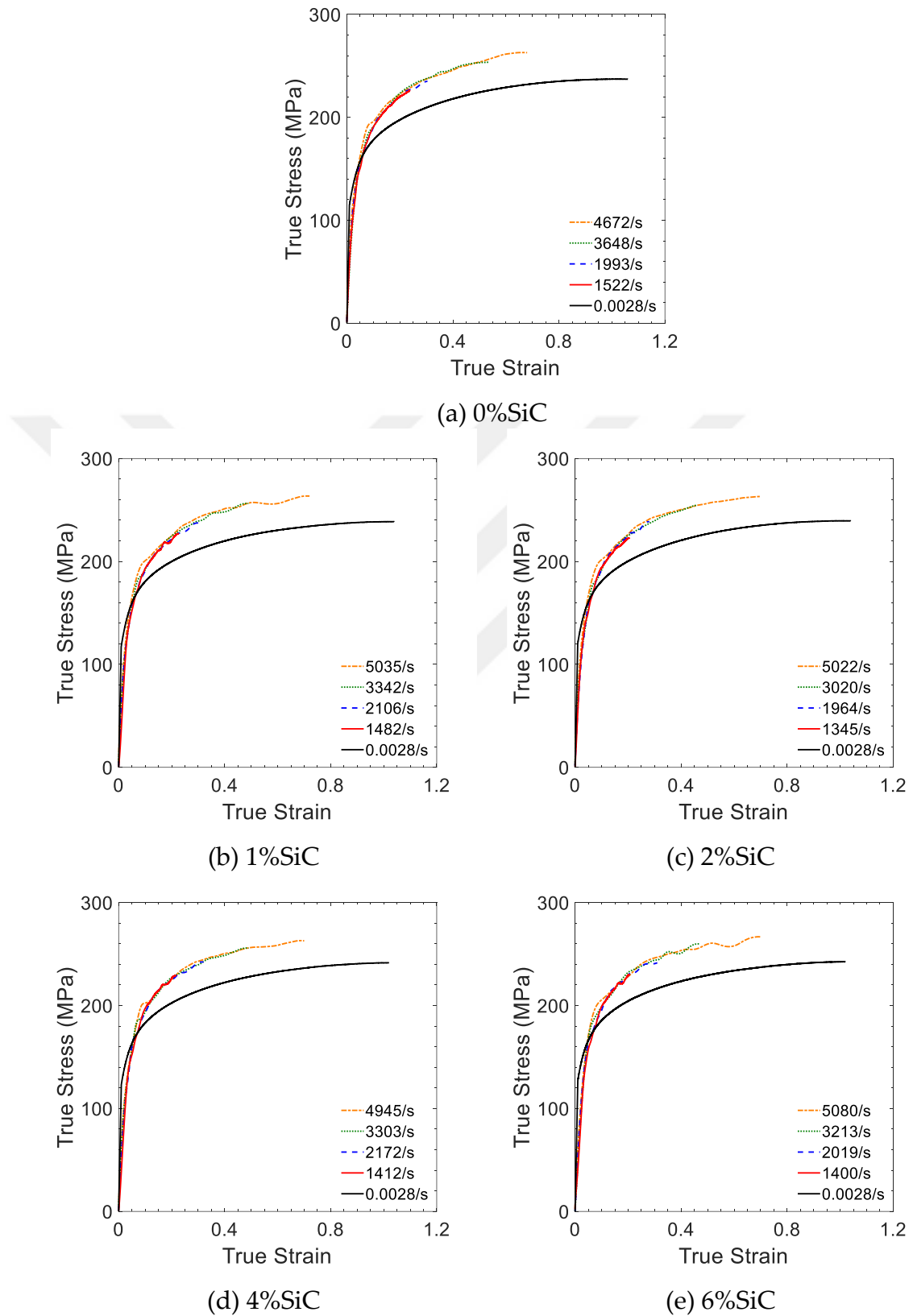
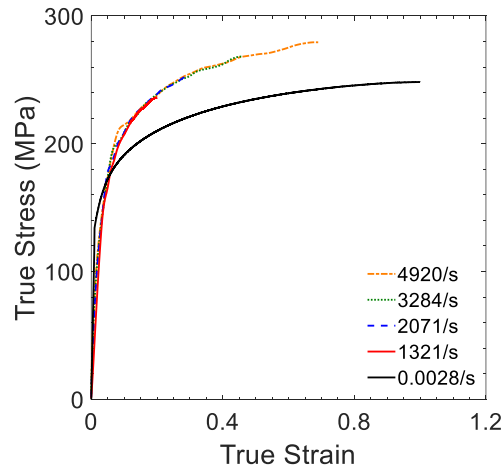
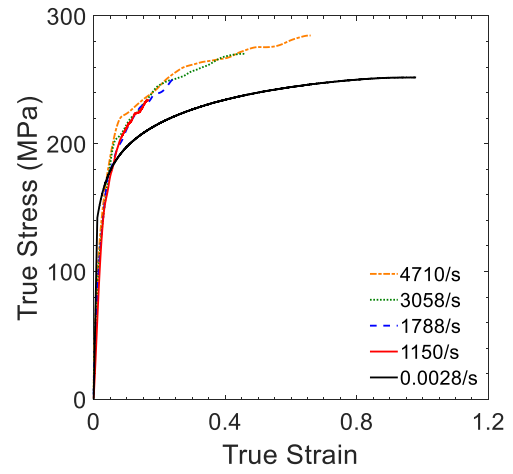


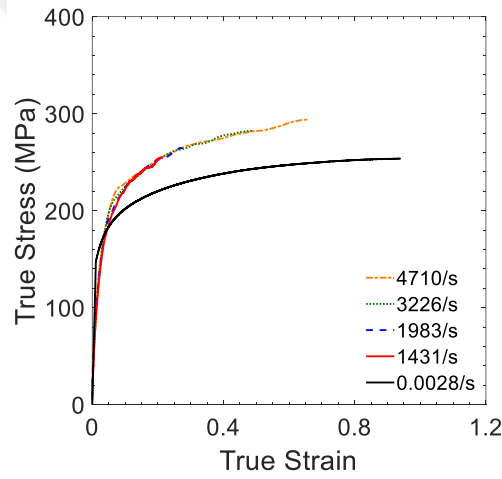
Figure 4.8. The quasi-static and dynamic true compressive stress-strain curves of the FGM layer specimens.



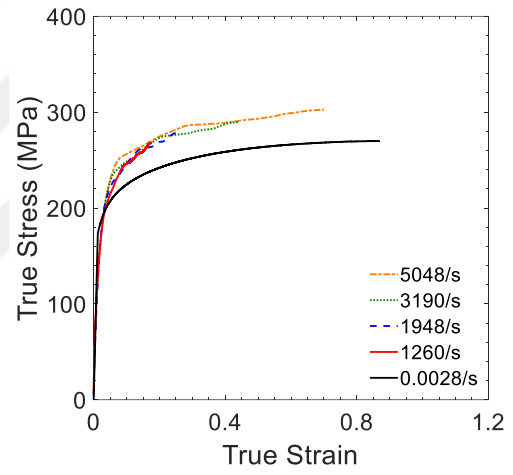
(f) 8%SiC



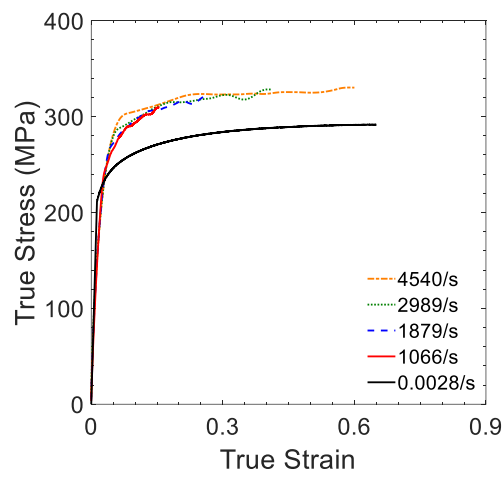
(g) 10%SiC



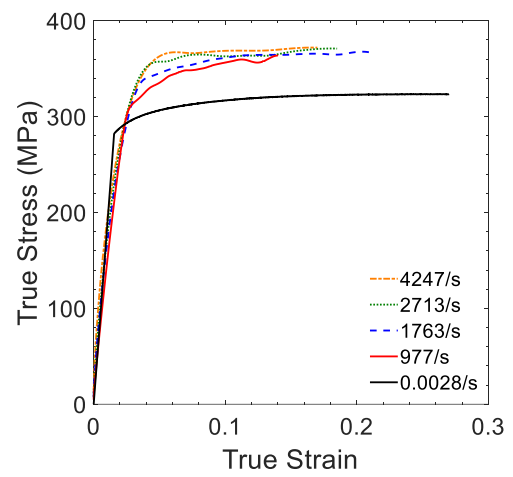
(h) 12%SiC



(i) 20%SiC



(j) 30%SiC



(k) 40%SiC

Figure 4.8. (continued)

4.4. Other Mechanical and Physical Properties

Other mechanical and physical properties of the FGM layer specimens are the Poisson's ratio and density that are necessary inputs for the impact analysis. The Poisson's ratio of the specimens is calculated by the linear rule of mixtures, since it is difficult to determine the Poisson's ratio experimentally in small dimensions. The theoretical Poisson's ratio of the layer specimens is determined considering the Poisson's ratio of the Al6061 and SiC constituents as 0.33 and 0.17, respectively. On the other hand, the densities are measured experimentally by an analytical balance based on the Archimedes' principle. The density measurement is repeated three times for each layer specimen, and the average of repeated measurements is reported. The densities of the layers which have relatively high porosity are determined by considering the measured mass and volume of these specimens. The theoretical (considering the density of the Al6061 and SiC constituents as 2700 kg/m³ and 3210 kg/m³, respectively) and measured densities of the FGM layer specimens are given in Table 4.2.

Table 4.2. The theoretical and measured densities of the FGM layer specimens.

Specimen	Theoretical density (kg/m ³)	Measured density (kg/m ³)	Specimen	Theoretical density (kg/m ³)	Measured density (kg/m ³)
0%SiC	2700.0	2670.0	20%SiC	2802.0	2770.0
1%SiC	2705.1	2680.0	30%SiC	2853.0	2820.0
2%SiC	2710.2	2680.0	40%SiC	2904.0	2870.0
4%SiC	2720.4	2690.0	50%SiC	2955.0	2920.0
6%SiC	2730.6	2700.0	55%SiC	2980.5	2940.0
8%SiC	2740.8	2710.0	60%SiC	3006.0	2720.0
10%SiC	2751.0	2720.0	68%SiC	3046.8	2520.0
12%SiC	2761.2	2730.0	70%SiC	3057.0	2440.0

CHAPTER 5

LOW-VELOCITY IMPACT TESTS AND NUMERICAL MODELING OF FUNCTIONALLY GRADED PLATES

5.1. Introduction

In this chapter, low-velocity impact tests and numerical modeling of the Al6061/SiC FGM plates are performed. Drop-weight impact test method is utilized to investigate the low-velocity impact behavior of the FGM plates, and an experimental-based numerical model that considers the actual mechanical behavior of the FGM plates is developed to predict the low-velocity impact response. To show the accuracy and improvement of the present numerical model, it is compared to the experimental results and an existing elastoplastic constitutive model from the literature.

5.2. Low-Velocity Impact Tests

The low-velocity impact experiments of the Al6061/SiC FGM plates are carried out by a CEAST Fractovis Plus drop-weight impact test equipment shown in Fig. 5.1-a. The test equipment has a hemispherical striker having a diameter of 20 mm and a mass of 5.045 kg (Fig. 5.1-b). Additionally, there is an anti-rebound system in the equipment to avoid repetitive impacts (Fig. 5.1-c).



Figure 5.1. The drop-weight impact test equipment.

Boundary conditions are provided by the pneumatic clamping rings that have an inner diameter of 40 mm (Fig. 5.2). Four different plates, the Al6061 plate and the metal-rich ($n = 0.1$), linear ($n = 1.0$), and ceramic-rich ($n = 10.0$) FGM plates, are tested in the low-velocity impact experiments. The plates are subjected to three different impact energy levels as 12 J, 24 J, and 48 J, and the striker is impacted on the ceramic-rich face of the FGM plates.



Figure 5.2. The boundary conditions of the low-velocity impact tests.

5.3. Experimental Results

The low-velocity impact test results are assessed in terms of deformation and damage mechanisms, contact force histories, energy absorption capacities, and force-displacement response of the FGM plates.

For the evaluation of external deformation and damage profiles of the FGM plates, the after-impact front and rear face views of the plates are given in Figs. 5.3-5.6. It is observed that there is macroscopically no failure or damage at the front and rear face of the FGM plates for all impact energies except for only the ceramic-rich FGM plate subjected to the 48 J impact energy. Excepting for only the ceramic-rich FGM plate under the 48 J impact energy, only elastoplastic deformation is observed with indentation and indentation trace at the impacted surface for all other plates and all considered impact energies.

FRONT FACE**REAR FACE****(a) 12 J****(b) 24 J****(c) 48 J**

Figure 5.3. The after-impact front and rear face views of the Al6061 plate.

FRONT FACE



REAR FACE



(a) 12 J



(b) 24 J



(c) 48 J

Figure 5.4. The after-impact front and rear face views of the metal-rich FGM plate.

FRONT FACE



REAR FACE



(a) 12 J



(b) 24 J



(c) 48 J

Figure 5.5. The after-impact front and rear face views of the linear FGM plate.

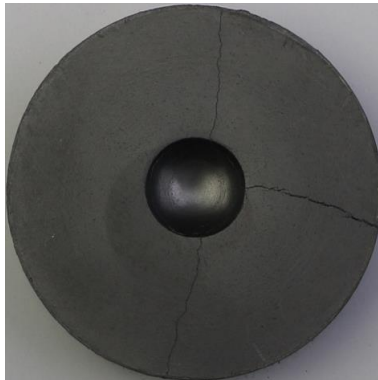
FRONT FACE**REAR FACE****(a) 12 J****(b) 24 J****(c) 48 J**

Figure 5.6. The after-impact front and rear face views of the ceramic-rich FGM plate.

For the ceramic-rich FGM plate under the 48 J impact energy, a combination of ductile deformation and brittle failure is observed as elastoplastic indentation and radial cracks, respectively. Furthermore, the ceramic-rich FGM plate exhibits a transition from ductile to quasi-brittle/brittle behavior with increasing impact energy. Namely, the ductile behavior is dominating for relatively low impact energies, whereas it shows quasi-brittle response for higher impact energies as seen from Fig. 5.6.

The impacted plates are cut into half section to evaluate the cross-sectional deformation and damage mechanisms of the Al6061 and FGM plates (Figs. 5.7 and 5.8). Herein, the ceramic-rich FGM plate is evaluated separately from the other plates, since it exhibits a distinctive response with increasing impact energy. On the other hand, the Al6061 plate and the metal-rich and linear FGM plates have a joint evaluation due to having similar deformation mechanisms. Besides, the cross-sectional views of these plates are given for only the impact energy of 48 J (Fig. 5.7) since these plates do not show a different deformation profile for other impact energies. As seen from Fig. 5.7, these plates exhibit also elastoplastic deformation in terms of the cross-sectional deformation.

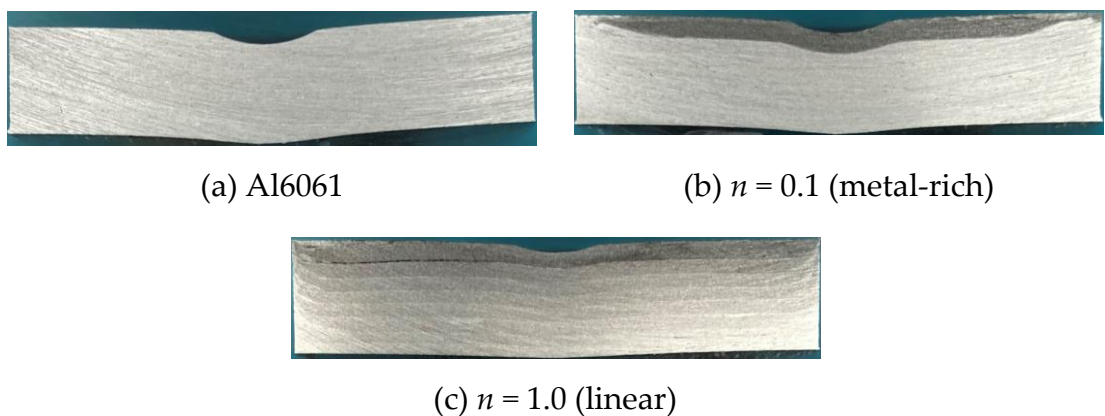


Figure 5.7. The cross-sectional deformation views of the Al6061 plate and the metal-rich and linear FGM plates for the impact energy of 48 J.

Considering the cross-sectional views of the ceramic-rich FGM plate (Fig. 5.8), only elastoplastic indentation takes place, and it is not observed any crack or brittle failure in the macroscopical scale for the 12 J impact energy. On the other hand, a conoidal crack occurs for the impact energy of 24 J, even though it is not observed any brittle failure in terms of the external examination of the plate. For the 48 J impact energy, the conoidal crack propagates and causes partial conoidal fracture in the plate. The conoidal fracture is the result of a cone crack which develops and propagates through the plate thickness by reason of Hertzian stress distribution on the periphery of the contact region, and it is a typical damage mode for brittle materials under indentation or impact loads. There are two different approximations on the reason of conoidal or Hertzian fracture. Frank and Lawn [55] proposed an approximation that is related to the principal stresses. Namely, cracking propagates perpendicular to the maximum principal stress following the minimum principal stress trajectory. Another approach was presented by Kocer and Collins [56] indicating that cracking propagates through the path of the maximum strain energy release. For these impact energies (24 J and 48 J), a complete conoidal fracture does not occur, since the metal constituent

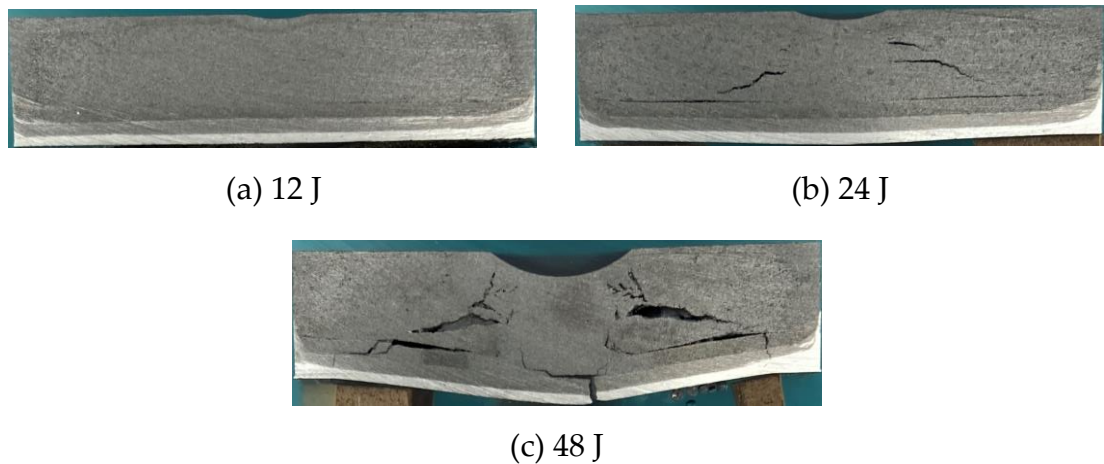


Figure 5.8. The cross-sectional deformation and damage views of the ceramic-rich FGM plate for different impact energies.

in the plate prevents the crack propagation and provides the structural integrity. Consequently, it can be stated that the ceramic-rich FGM plate shows quasi-brittle response for the impact energies of 24 J and 48 J, since it shows both elastoplastic indentation and partial conoidal fracture.

For a clear comparison of the indentation trace, comparative front face images of the plates are given in Fig. 5.9. As seen from Fig. 5.9, it is obviously seen that the diameter of indentation trace decreases with increasing total ceramic fraction in the FGM plate up to a certain limit. Namely, it decreases as the plate composition varies from metal-rich to linear, whereas it increases from linear composition to ceramic-rich composition, and the indentation trace of the ceramic-rich FGM plate is even larger than the indentation trace of the Al6061 plate and the metal-rich FGM plate. For the 12 J impact energy, the indentation trace of the ceramic-rich FGM plate is close to those of the Al6061 plate and the metal-rich FGM plate, while it is larger for the impact energies of 24 J and 48 J. This opposite or unexpected situation is related to the strength and the damage state of the plate. Considering the stress-strain curves of the FGM layer specimens in Fig. 4.2 (Chapter 4), it is obviously seen that the strength of the specimens increases as the ceramic fraction increases up to a certain limit, 55%SiC, and a further increment in the ceramic fraction causes decrement in the strength. This strength decrement is related to weak bond between the ceramic particles with decreasing volume fraction of the metal constituent since the ceramic particles are not sintered in the production. The bond between the ceramic particles and the structural integrity of the plate are provided by sintering the metal constituent, and decrement of the metal fraction results in weakening in the strength of the plate. Therefore, the overall strength of the ceramic-rich FGM plate is lower than its expected strength, since it mostly consists of the 70%SiC ceramic-rich layer whose strength is even lower than that of the Al6061 layer. In addition to the strength weakening, the damage state of the ceramic-rich FGM plate also plays

an important role for the larger indentation trace. It is obvious that the combination of elastoplastic deformation and brittle damage occur in the ceramic-rich FGM plate for the impact energies of 24 J (Fig. 5.8-b) and 48 J (Figs. 5.6-c and 5.8-c), while it can be explained with the strength weakening and the pore collapse due to having porosity of the 70%SiC layer (explained in detail in the energy absorption assessment) for the 12 J impact energy.

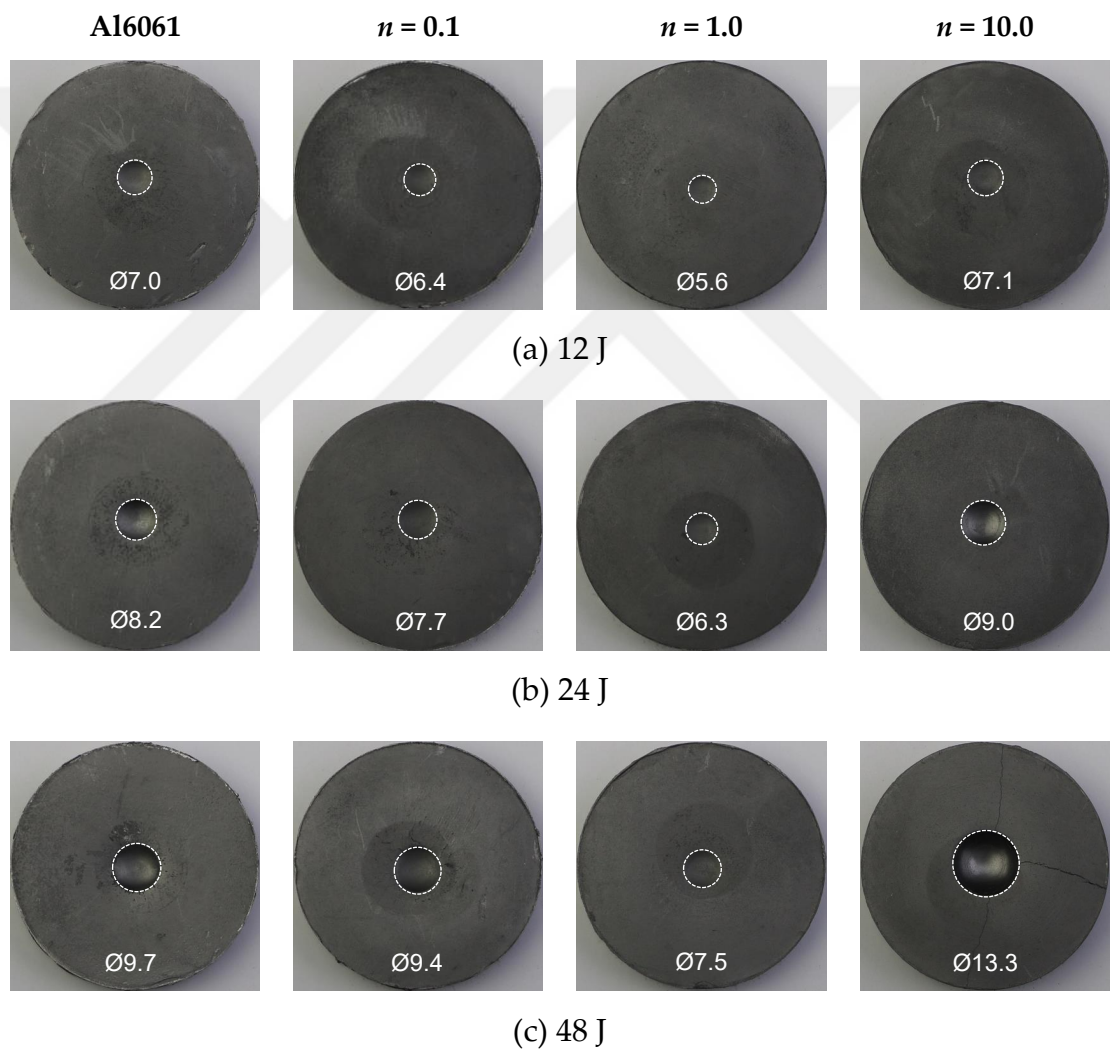


Figure 5.9. The diameter of indentation trace of the plates (mm).

To assess the contact force histories, energy absorption capabilities, and force-displacement response of the Al6061 and FGM plates, comparative graphs for these parameters are given in Fig. 5.10.

Fig. 5.10-a illustrates the contact force histories of the plates. A similar trend with the deformation and damage evaluation is observed in the contact force histories. Namely, the contact force increases, and the contact duration decreases in general with increasing total ceramic fraction in the plate up to ceramic-rich composition. Once the plate composition varies from linear to ceramic-rich, a decrement results in the contact force by reason of the strength weakening that is explained in the deformation and damage assessment section. Considering the contact force histories of the ceramic-rich FGM plate, it reaches an initial peak value, then decreases at a certain level. The magnitude of this decrement increases as the impact energy increases depending on the damage state of the plate. In analogy to the indentation trace variation, excepting for the initial peak value (for the ceramic-rich FGM plate), the contact force level of the ceramic-rich FGM plate is close to the Al6061 plate and the metal-rich FGM plate, whereas it is lower than those of the other plates for the impact energies of 24 J and 48 J. Especially for the 24 J and 48 J impact energies, the contact force of the ceramic-rich FGM plate drastically decreases after the initial peak value due to the first crack initiation, then it fluctuates by reason of the successive crack initiations and propagations.

Fig. 5.10-b shows the absorbed energy histories of the plates. As seen from Fig. 5.10-b, the absorbed energy decreases as the total ceramic fraction in the plate increases due to the decrement in the plastic deformation capability of the plate except for the ceramic-rich FGM plate. The ceramic-rich FGM plate is the most energy absorbing plate for all impact energies, and this excepted state is related to the strength, stiffness, and the damage state of the ceramic-rich FGM plate that is explained in detail for the indentation trace. For the Al6061 and FGM plates,

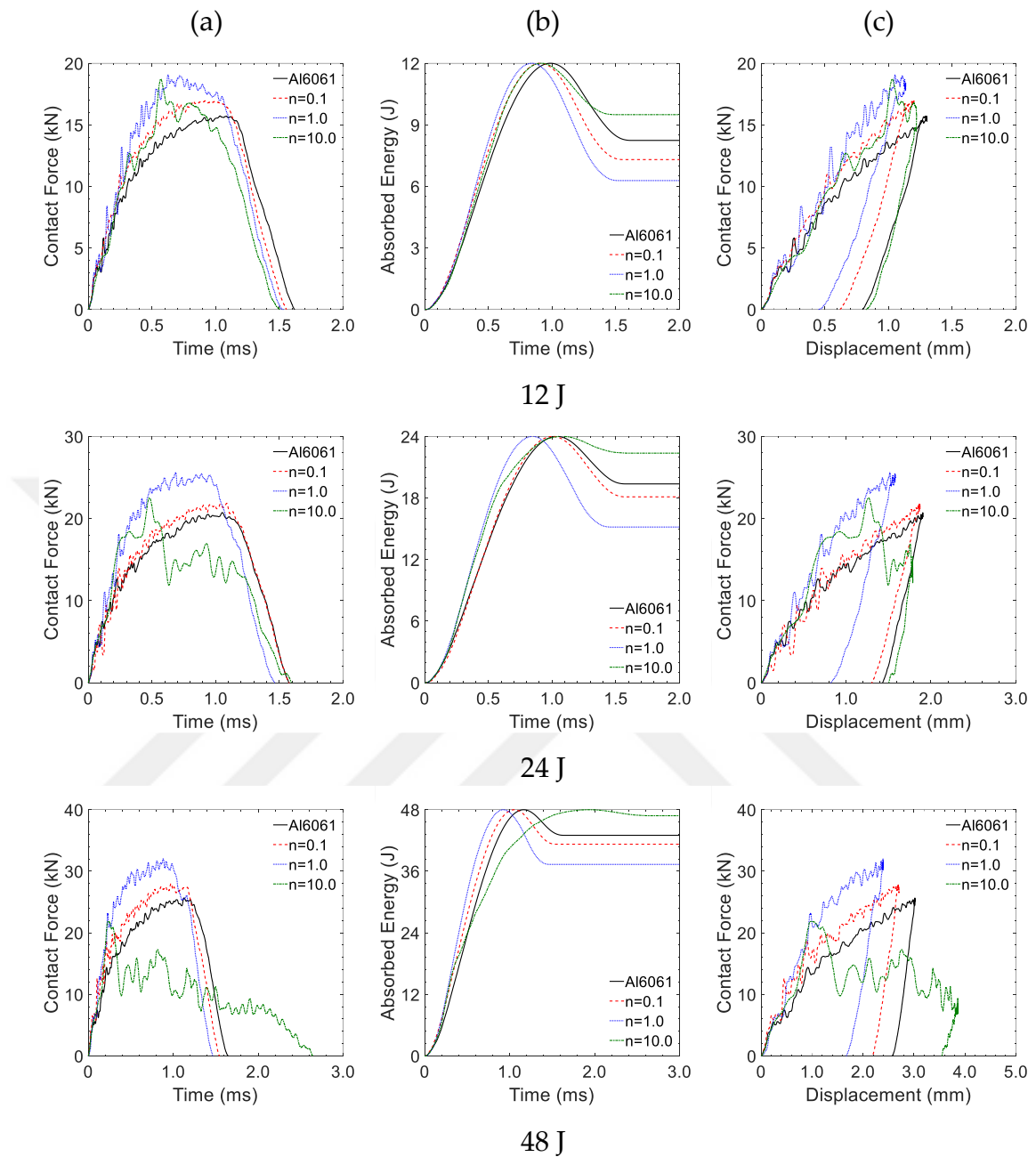


Figure 5.10. The comparative contact force histories, energy absorption histories, and force-displacement response of the plates for different impact energies.

the major energy absorption mechanisms are the elastic deformation, plastic deformation, and damage of the plates. Once the striker velocity reaches zero, the absorbed energy approaches its peak value. After this point, the elastic energy is retransmitted to the striker. Therefore, the energy absorption capacities are

determined by the plastic deformation capability and damage state of the plates. Namely, the impact energy is absorbed by mostly the plastic deformation for the Al6061 plate and the metal-rich and linear FGM plates, whereas the plastic deformation, brittle damage (radial cracks and conoidal fracture), and delamination are effective on the energy absorption of the ceramic-rich FGM plate. Additionally, the pore collapse is another energy absorption mechanism for the FGM plates. It is effective especially for the ceramic-rich FGM plate, since the ceramic-rich FGM plate mostly consists of the 70%SiC layer that has the maximum porosity among the layer specimens. Namely, the theoretical and measured densities of the 70%SiC layer are 3057 kg/m^3 and 2440 kg/m^3 , respectively. Porosity occurs especially in the high ceramic content layers since the ceramic constituent cannot be sintered in the present production method. The pores in the ceramic-rich layers absorb the impact energy by collapsing or crushing similar to cellular structures. Thus, the reason of being the most energy absorbing plate of the ceramic-rich FGM plate is exhibiting brittle damage and pore collapse in addition to plastic deformation compared to the other FGM plates.

Fig. 5.10-c presents the force-displacement response of the plates. In analogy to the other parameters, it is obvious that the stiffness and mechanical impedance of the FGM plate increases due to increasing total ceramic fraction in the plate exhibiting increment in the contact force and decrement in the permanent displacement except for the ceramic-rich FGM plate. The ceramic-rich FGM plate shows higher permanent displacement than the other FGM plates, since the radial cracks, conoidal fracture, and pore collapse occur in addition to the plastic deformation.

5.4. Numerical Modeling

The low-velocity impact modeling of the FGM plates is performed by the explicit finite element code, LS-DYNA®. The numerical model consists of the FGM plate, striker, and clamping rings considering their actual dimensions in the experiments. The FGM plates have eight sequential layers from metal bottom layer (100%Al6061) to ceramic-rich top layer (70%SiC-30%Al6061) through the thickness direction. The striker has a hemispherical geometry with a diameter of 20 mm and a mass of 5.045 kg, and the clamping rings have an inner diameter of 40 mm. The numerical model of the low-velocity impact experiments is shown in Fig. 5.11. It is determined that two elements are satisfactory for each layer of the FGM plate performing successive analyses and considering the computational efficiency and prediction of the numerical model. All components in the numerical model are meshed using eight-node solid element with reduced integration rule and Lagrangian formulation.

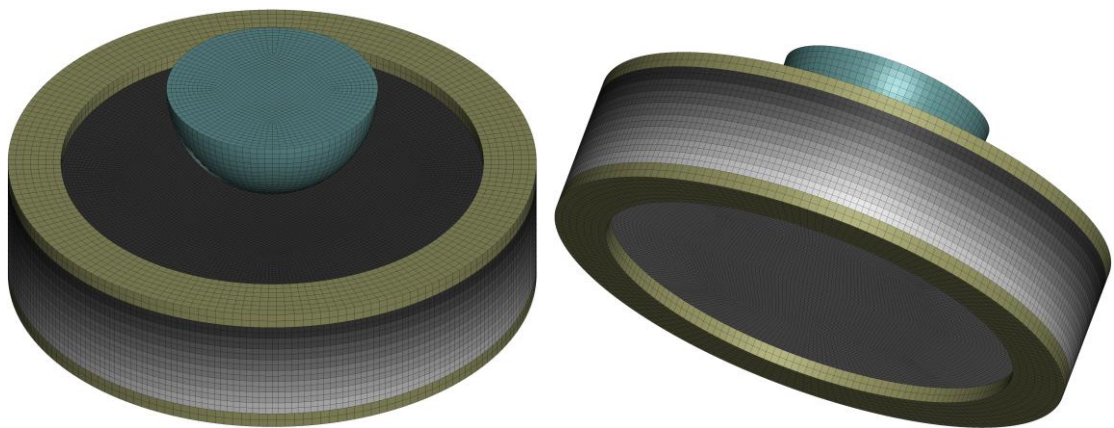


Figure 5.11. The numerical model of the FGM plate for the low-velocity impact.

Implementing compatible constitutive modeling in impact analysis is a critical step in terms of the accuracy of numerical results. To capture the realistic behavior of the FGM plates, an experimental-based constitutive modeling

procedure that considers the actual mechanical behavior of the FGM plates is performed. The elastoplastic constitutive model described in Chapter 3, MAT_PIECEWISE_LINEAR_PLASTICITY (MAT_024), is utilized to define the plasticity behavior of the FGM plate. The stress-strain behavior of each layer of the FGM plate (Fig. 4.2) that is characterized by the quasi-static compression tests is implemented in the MAT_024 model. According to the experimental results, the Al6061 and FGM plates except for the ceramic-rich FGM plate show only elastoplastic deformation. In other words, it is not observed any failure or damage in these plates. Therefore, the MAT_024 constitutive model is used without failure option. For the ceramic-rich FGM plate, the same procedure and additionally an improved modeling approach that is explained in the next subsection is implemented in the numerical model, and these two modeling approaches are compared. The behavior of the striker and clamping rings are assumed to be rigid and described by a rigid material model, MAT_RIGID (MAT_020), since these components do not plastically deform during the impact tests.

Other important parameters in the low-velocity impact analysis are contact algorithm and hourglass definitions. The contact definitions between the striker and plate and between the clamping rings and plate are represented by AUTOMATIC_SURFACE_TO_SURFACE contact algorithm. To avoid nonphysical deformation modes, the Belytschko–Bindeman [57] stiffness-based hourglass option with the hourglass coefficient of 0.03 is implemented in the numerical model, since the reduced integration rule is used in the solid element formulation.

Boundary conditions are applied by the rigid clamping rings according to the nature of the experiments. Namely, the motion of the bottom clamping ring is constrained in all directions, and the motion of the top clamping ring is allowed

only in the impact direction. Moreover, the clamping force (approx. 2.5 kN [58]) acting on the plates is applied to the top clamping ring. The ceramic-rich face of the FGM plates is placed as the strike face in the numerical model as is the case with the experiments, and the plates are subjected to impact loads with the impact energies of 12 J, 24 J, and 48 J.

An Improved Constitutive Modeling Approach for the Ceramic-Rich FGM Plate

In addition to the elastoplastic constitutive modeling (MAT_024) given in the previous section, an improved constitutive modeling approach including both MAT_024 elastoplastic model and a brittle damage model is proposed for the ceramic-rich FGM plate. Except for the constitutive modeling, the numerical modeling procedure is the same as in the previous section. The ceramic-rich FGM plate has mostly the 70%SiC layer that has quasi-brittle/brittle behavior, and it shows an unexpected behavior according to the experimental results. Namely, it is expected that the ceramic-rich FGM plate has the maximum contact force, whereas a decrement in the contact force is observed as the plate composition varies from linear to ceramic-rich due to the strength weakening and the damage state of the plate. The MAT_024 elastoplastic constitutive model is not capable of modeling the strength weakening and the damage state of the ceramic-rich FGM plate. Based on this motivation, an improved constitutive modeling approach that has combined strength model is implemented for the ceramic-rich FGM plate.

For the constitutive modeling of the ceramic-rich FGM plate, two different constitutive models given in Fig. 5.12 are utilized in the numerical model. The behavior of the metal bottom layer (100%Al6061) is described by the MAT_024 elastoplastic constitutive model, whereas the behavior of the layers with high ceramic content is defined using a nonconventional constitutive model for FGMs, MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3) constitutive model

originally known as the Karagozian & Case (K&C) Concrete Model - Release III. To model the damage of brittle materials, there are several constitutive models such as MAT_110 (JH-2), MAT_111, MAT_241 (JH-1), etc. in the LS-DYNA® Keyword. However, there is lack of knowledge about the input parameters of these constitutive model for FGMs, and additionally, the determination of the model parameters is not a straightforward process. Therefore, the MAT_072R3 constitutive model is selected to model the high ceramic content layers of the ceramic-rich FGM plate. This constitutive model is very useful for modeling concrete structures and has an automatic parameter generation capability based only on the unconfined compressive strength of the material to be modeled.

70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
68%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
55%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
0%SiC	MAT_PIECEWISE_LINEAR_PLASTICITY (MAT_024)

Figure 5.12. The constitutive models for the layers of the ceramic-rich FGM plate.

Another reason of selecting the MAT_072R3 constitutive model can be explained as the structure of the FGM plate is similar to the concrete. The concrete basically comprises of cement and aggregate constituents. Namely, the matrix material (Al6061) of the FGM plate can be resembled with the cement, while the reinforcement material (SiC) can be imitated with the aggregate. In the production of the FGM plates, the sintering process is applied with respect to the sintering temperature of the Al6061 matrix material. Namely, the SiC particles are not sintered during the production, they are distributed in the Al6061 matrix,

and the structural integrity is provided by sintering the Al6061 matrix. Therefore, the distribution of the SiC particles in the Al6061 matrix is similar to the distribution of the aggregate in the cement. Consequently, the MAT_072R3 constitutive model can be applicable to model metal matrix composites or metal/ceramic FGM composites.

As mentioned above, the MAT_072R3 constitutive model is capable of automatic model parameter generation using solely the unconfined compressive strength. Namely, there are seven cards in the LS-DYNA® keyword input to describe the complete model parameters for the MAT_072R3 constitutive model. However, it is sufficient to know only the unconfined compressive strength of the material, and the remaining parameters are generated automatically. Moreover, these parameters can also be modified by the user. For this constitutive model, an Equation-of-State (EOS), EOS_TABULATED_COMPACTION (EOS_008), is required to describe the pressure-volumetric strain response. The parameters of the EOS_008 are also generated automatically once the unconfined compressive strength is defined.

For the 55%SiC, 68%SiC, and 70%SiC layers, the compressive strength for the automatic parameter generation is utilized from the quasi-static compression test. Additionally, two parameters are modified in the constitutive model, the tensile strength and localization width (LOCWID). The LOCWID parameter is recommended as three times the maximum aggregate diameter. Therefore, the LOCWID is considered as 0.135 mm, since the particle size of the SiC powders is 45 microns. The tensile strength of the layers is determined by conducting the Brazilian test as explained in Chapter 4. The Brazilian test results are given in Fig. 5.13 in terms of force-displacement variation. Using these results, the tensile strength of the layers, f_t is calculated by Eq. (5.1) as

$$f_t = \frac{2P}{\pi dl} \quad (5.1)$$

where P , d , and l are respectively the applied force, diameter of the specimen, and length of the specimen. The compressive and tensile strength of the layers are listed in Table 5.1.

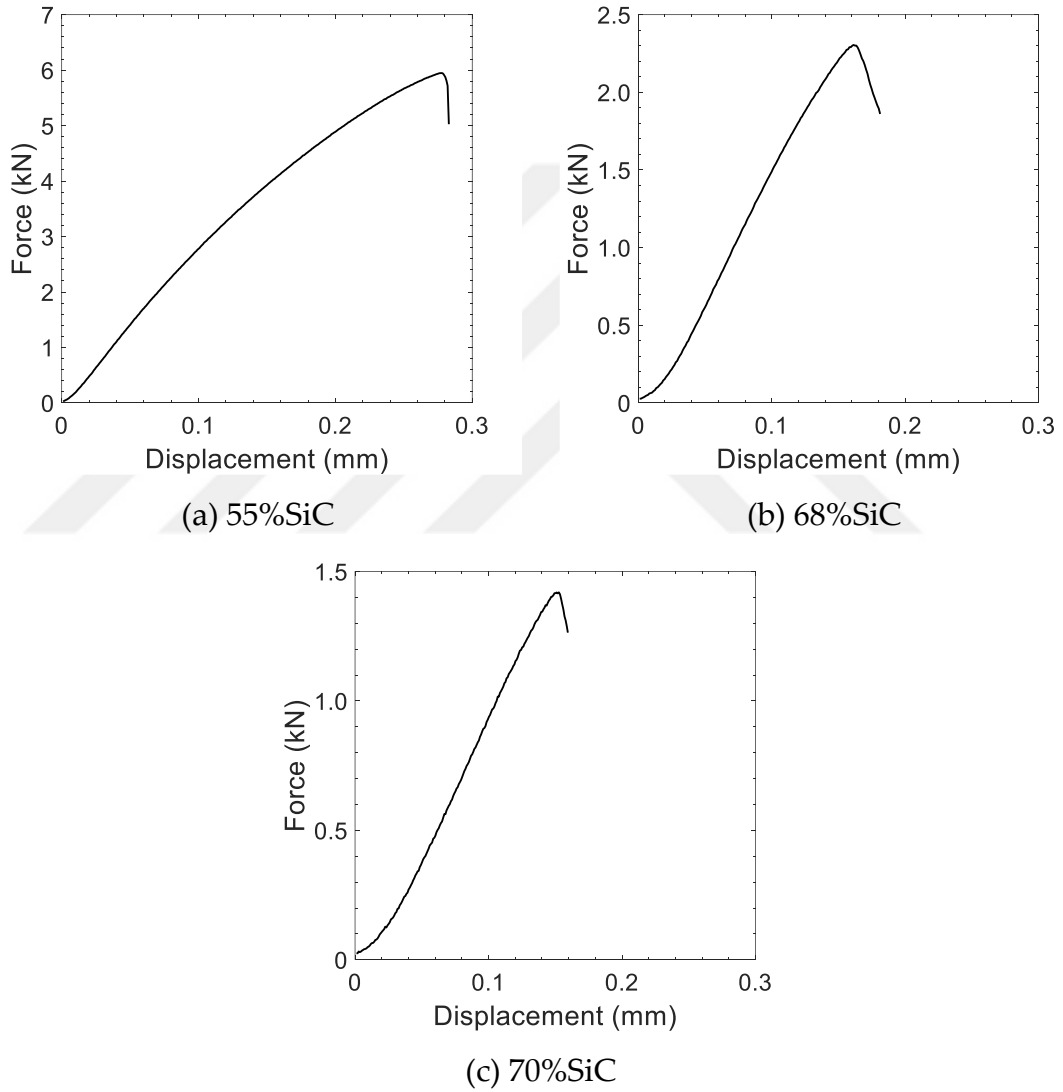


Figure 5.13. The Brazilian test results for the 55%SiC, 68%SiC, and 70%SiC layers.

Table 5.1. The compressive and tensile strength of the high ceramic content layers of the ceramic-rich FGM plate.

Specimen	Compressive strength (MPa)	Tensile strength (MPa)
55%SiC	460.8	109.1
68%SiC	226.1	41.0
70%SiC	155.8	27.1

Another issue for the MAT_072R3 constitutive model is erosion. The MAT_072R3 constitutive model does not allow erosion. Therefore, MAT_ADD_EROSION keyword is defined for the high ceramic content layers of the ceramic-rich FGM plate to simulate the physical damage and fracture of the plate. The maximum principal strain is one of the most applied erosion criteria for this constitutive model in the literature [59-68]. Therefore, a maximum principal strain is defined for each corresponding layer of the FGM plate. The maximum principal strain values are determined by the trial-and-error method since the erosion criteria depends on the problem type. The maximum principal strain values are respectively 0.40, 0.37, and 0.36 for the 55%SiC, 68%SiC, and 70%SiC layers.

5.4.1. Verification of Elastic Modulus of Sintered Al6061 Plate

In Chapter 4, an elastic modulus that is not a familiar value (12 GPa) for the sintered Al6061 alloy is determined experimentally. It is known that the elastic modulus of the aluminum and its alloys generally varies in the range of 67-73 GPa. On the other hand, there are some different values for the elastic modulus of aluminum alloys produced by powder metallurgy technique in the literature. It is stated that the elastic modulus for most sintered aluminum alloys falls in the range of 50-65 GPa in a patented study by Bishop et al. [69]. Additionally, Rao and Upadhyaya [70] studied on 2014 and 6061 aluminum alloy-based sintered

composites including SiC reinforcement. The effect of SiC particle volume fraction on the elastic modulus of the composites was investigated and it was reported that the elastic modulus of both aluminum alloys is lower than its usual value. Namely, the elastic modulus of 6061 and 2014 alloys were determined as about 16.7 and 20.1 GPa, respectively. It can be deduced from these studies that the elastic modulus is dependent on the production method and production conditions.

In this study, the elastic modulus of the sintered Al6061 alloy is determined as about 12 GPa. This value is relatively low compared to the usual value of aluminum alloys. On the other hand, it is similar to the study by Rao and Upadhyaya [70]. In this section, the accuracy of this value is controlled by performing an experimental-numerical study. First, a numerical model is developed for a control group including commercial Al6061-T6 sheet to verify the accuracy of the developed numerical model. Then, numerical modeling is performed for the sintered Al6061 plate. The numerical modeling procedure given in the previous section is same for the Al6061-T6 sheet and the sintered Al6061 plate except for their geometries. The Al6061-T6 sheet has a square geometry with the dimensions of 100×100×2.5 mm, while the sintered Al6061 plate is circular with the dimensions of Ø50×10 mm. The numerical models of these materials that consider the realistic test conditions for the low-velocity impact analysis are shown in Fig. 5.14. The implemented stress-strain curves of the aluminum plates for describing the elastoplastic behavior, and the mechanical properties are given respectively in Fig. 5.15 and Table 5.2.

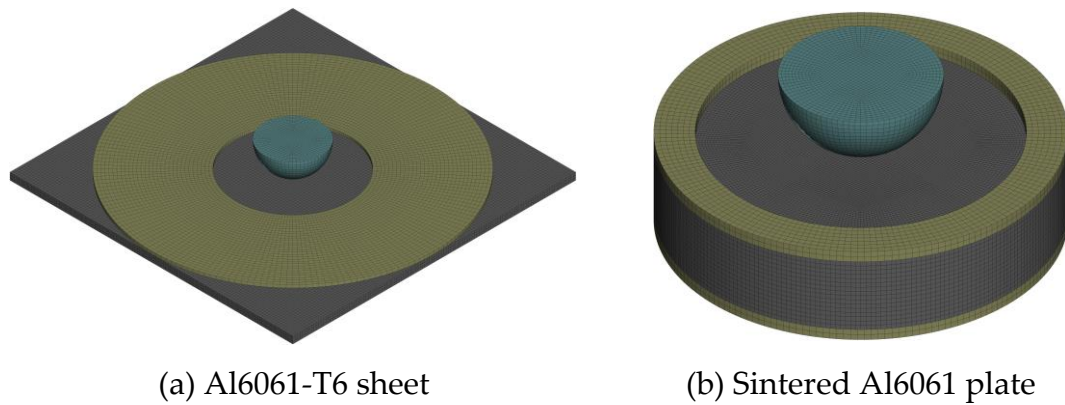


Figure 5.14. The numerical models for the Al6061-T6 sheet and sintered Al6061 plate for the low-velocity impact.

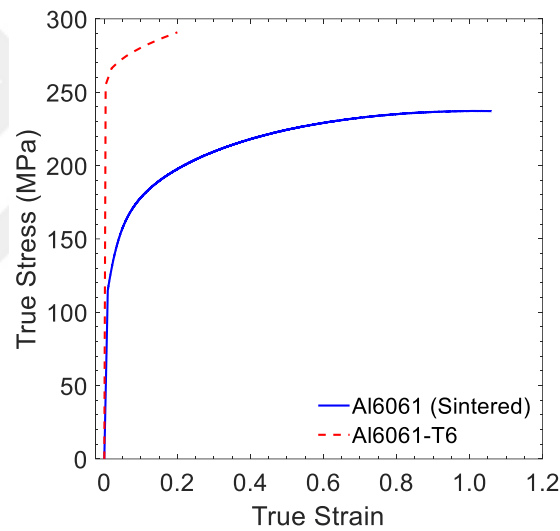


Figure 5.15. The stress-strain curves of the Al6061-T6 sheet and Al6061 plate.

Table 5.2. The mechanical properties of the Al6061-T6 sheet and Al6061 plate.

Material	Elastic modulus (GPa)	Yield strength (MPa)	Poisson's ratio
Al6061-T6 sheet	68.9	255.0	0.33
Al6061 plate	12.0	98.1	0.33

Three different impact energies are considered for each aluminum plate as 10 J, 20 J, and 40 J for the Al6061-T6 sheet and 12 J, 24 J, and 48 J for the sintered Al6061 plate. For the sintered Al6061 plate, the numerical analyses are carried out for

both experimentally determined (12 GPa) and theoretical (67 GPa) elastic modulus to show the accuracy of the determined elastic modulus.

The numerical results are evaluated in terms of the contact force histories of the plates. First, the contact force histories of the Al6061-T6 sheet are presented in Fig. 5.16. As seen from Fig. 5.16 that the developed numerical model with the elastic modulus of 68.9 GPa (its known value) shows a very good agreement with the experimental results. Thus, it is obviously stated that the accuracy of the numerical model is confirmed.

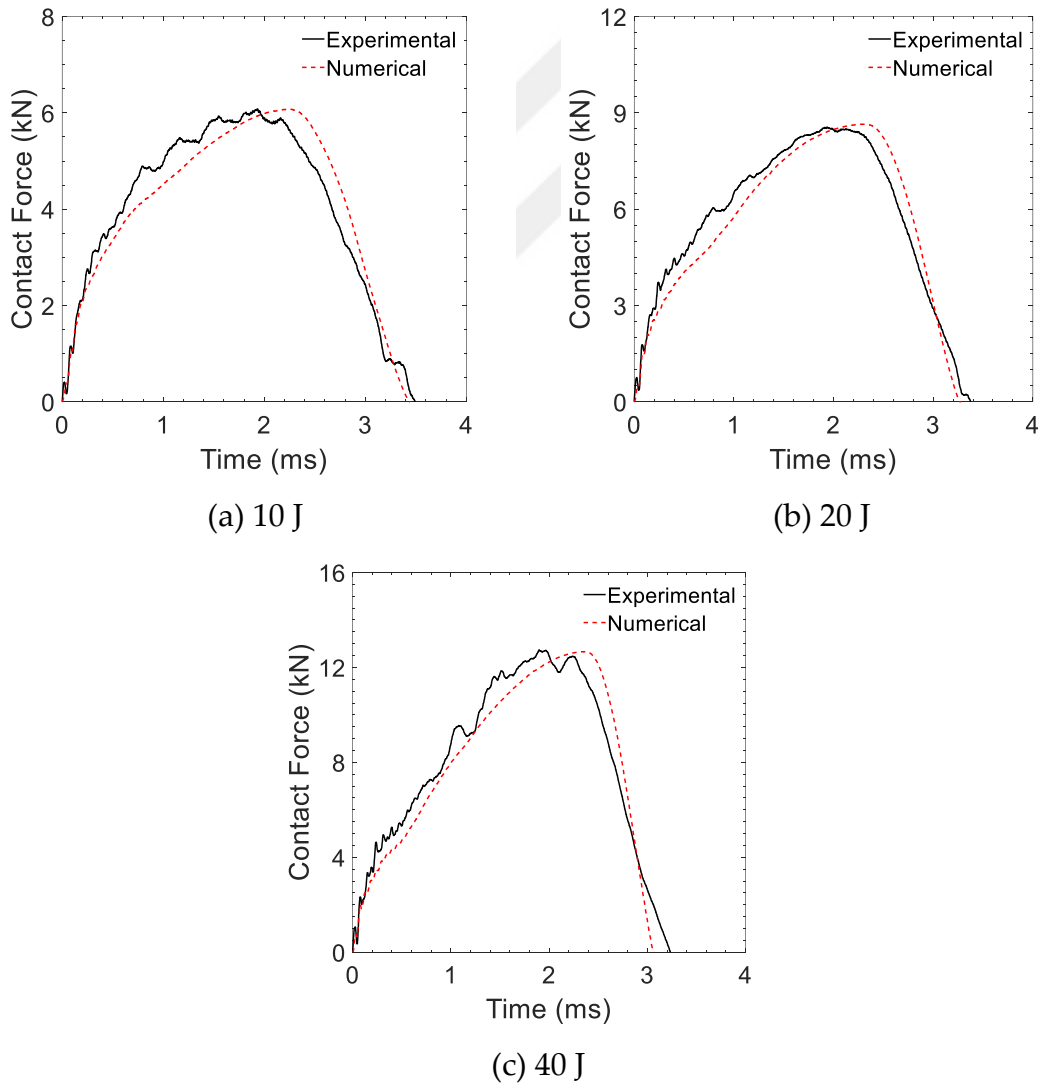


Figure 5.16. The experimental and numerical contact force histories of the Al6061-T6 sheet for different impact energies.

Accordingly, the contact force histories of the sintered Al6061 plate are given in Fig. 5.17 for the experimental and theoretical elastic modulus. It is obviously seen that the numerical results with the experimental elastic modulus (12 GPa) show a good agreement with the experimental results, whereas the plate with the theoretical elastic modulus (67 GPa) has a stiffer response exhibiting higher contact forces and shorter contact durations compared to the experimental results. Therefore, the experimental elastic modulus is verified reflecting the realistic behavior of the sintered Al6061 plate.

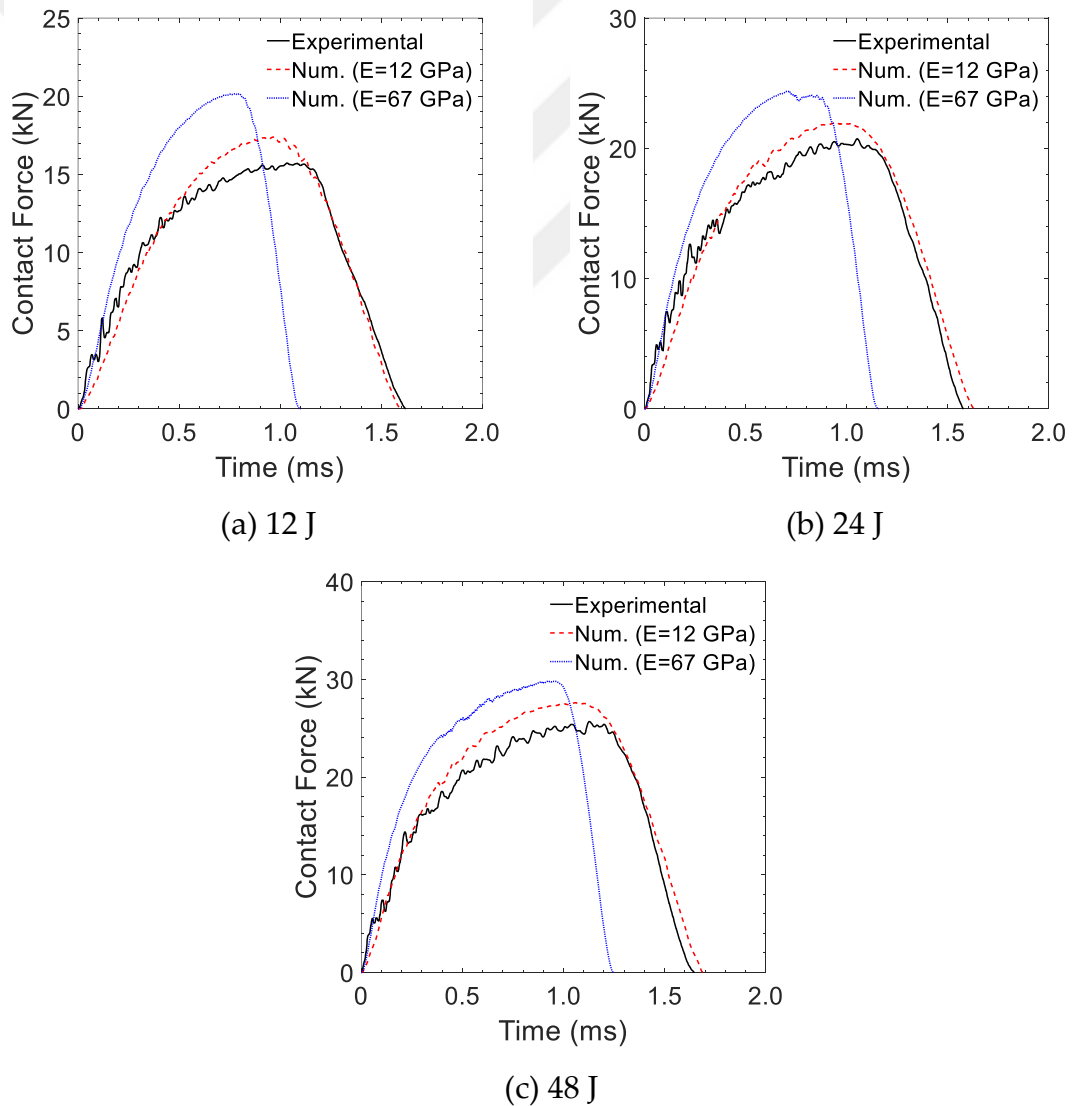
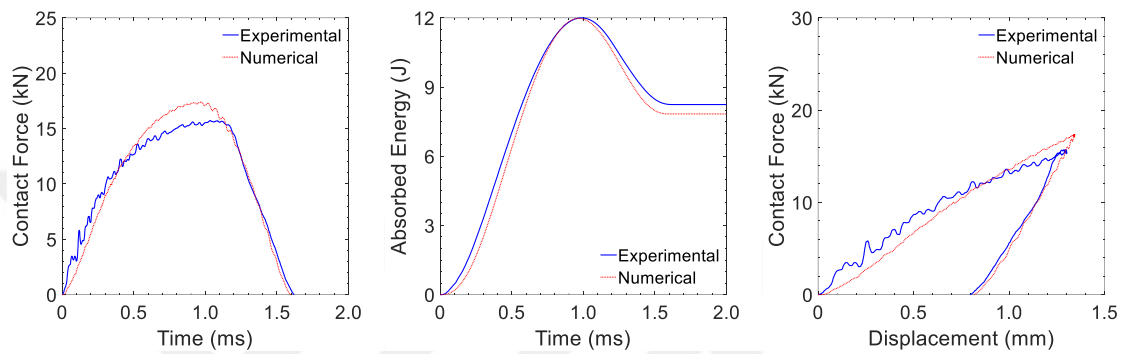
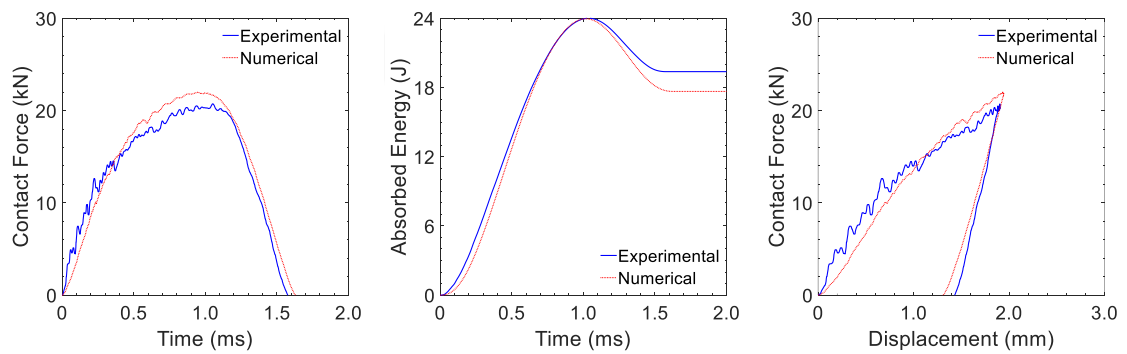


Figure 5.17. The comparison of the theoretical and experimental elastic modulus of the sintered Al6061 plate in terms of contact force history.

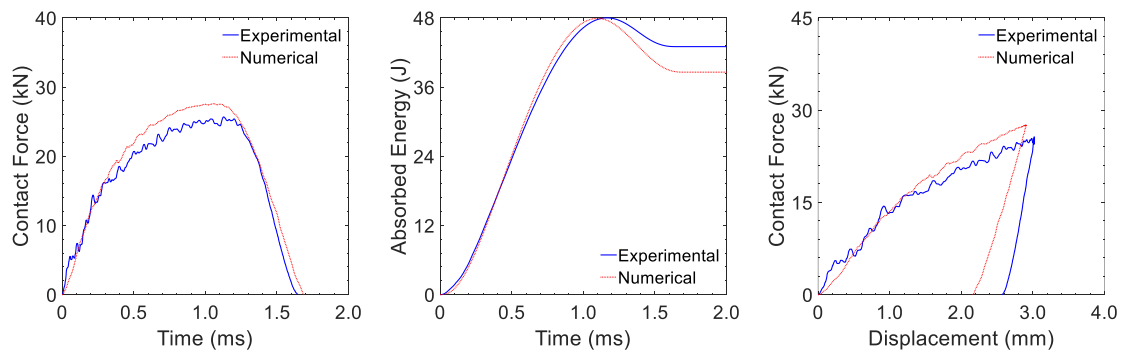
Additionally, this verified elastic modulus is also evaluated in terms of the absorbed energy history and force-displacement response of the Al6061 plate in addition to the contact force history (Fig. 5.18), and it is obviously seen that the numerical model with the experimental elastic modulus successfully predicts the experimental results in general for all parameters.



(a) 12 J



(b) 24 J



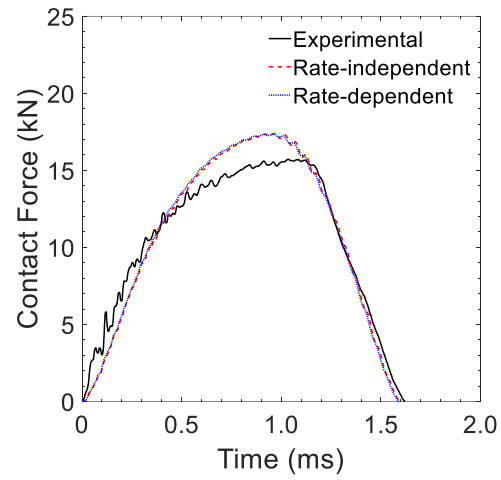
(c) 48 J

Figure 5.18. The experimental-numerical results of the Al6061 plate in terms of contact force and absorbed energy histories, and force-displacement response.

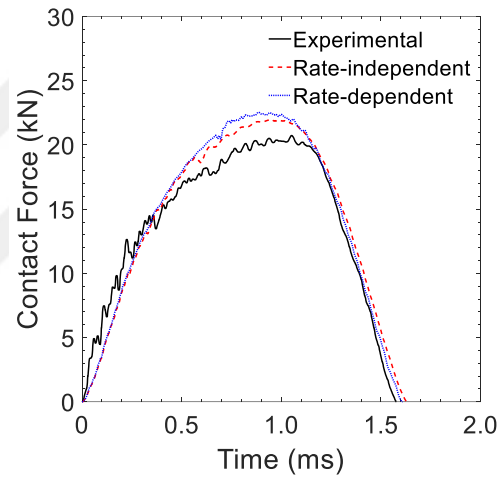
5.4.2. Strain-Rate Dependency of Low-Velocity Impact Analysis

In this section, the strain-rate dependency of the low-velocity impact analysis is investigated. An experimental-numerical study is performed to determine whether the high strain-rate behavior of the plates is necessary for the low-velocity impact analysis. The study is carried out for the Al6061 plate which is the matrix material of the FGM plates. Two different numerical models, rate-independent and rate-dependent, are developed to present the effect of considering high strain-rate behavior of the plate. To describe the material plasticity, the MAT_024 constitutive model is implemented in the numerical model. In the rate-dependent model, the quasi-static compressive behavior (Fig. 4.2), and the high strain-rate compressive behavior at four different strain-rates (Fig. 4.8-a) are described, while only the quasi-static compressive behavior is defined in the rate-independent model. The impact analyses are performed for three different energy levels as 12 J, 24 J, and 48 J to investigate the strain-rate effects depending on the impact velocity/energy, and the numerical results are evaluated according to the experimental results.

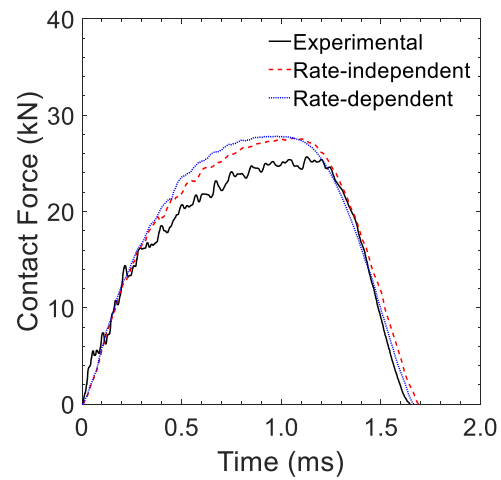
The assessment of the experimental-numerical results is performed in terms of the contact force history of the Al6061 plate. The contact force histories of the plate are given in Fig. 5.19 for different impact energies. As seen from Fig. 5.19, there is no significant difference between the rate-independent and rate-dependent model, and both numerical models show good correlation with the experimental results. Consequently, considering the quasi-static or rate-independent behavior is sufficient for the low-velocity impact analysis of the Al6061 plate, and therefore, the constitutive modeling of the FGM plates is also performed using the rate-independent modeling approach according to this result.



(a) 12 J



(b) 24 J



(c) 48 J

Figure 5.19. The effect of strain-rate dependency for the low-velocity impact analysis of the Al6061 plate in terms of contact force history.

5.5. Numerical Results

The numerical results are firstly presented for the ceramic-rich FGM plate to show the difference between the MAT_024-based and MAT_072R3-based models and the improvement of the MAT_072R3-based modeling approach. For the comparison of these two modeling approaches, the contact force and absorbed energy histories and force-displacement response of the ceramic-rich FGM plate are illustrated in Fig. 5.20. It is obviously seen that the MAT_072R3-based model shows an excellent agreement with the experimental results compared to the MAT_024-based model. The MAT_024-based model shows stiffer response than the MAT_072R3-based model exhibiting higher contact forces and lower absorbed energies and permanent displacements. The only exception is the permanent displacement of the MAT_024-based model for the 12 J impact energy. For this impact energy, the permanent displacement of the MAT_024-based model is slightly more than the MAT_072R3-based model.

To show the differences between the experimental and numerical results, the peak contact force, absorbed energy and permanent displacement values are listed in Table 5.3 for the 12 J, 24 J, and 48 J impact energies. In Table 5.3, the absolute differences of the model results compared to the experimental results are also given. It can be stated that the difference of the MAT_024-based model results is higher than the MAT_072R3-based model results except for the permanent displacement of the MAT_024-based model for the 12 J impact energy, and it drastically increases with increasing impact energy for the peak contact force and permanent displacement, while it remains almost constant for the absorbed energy.

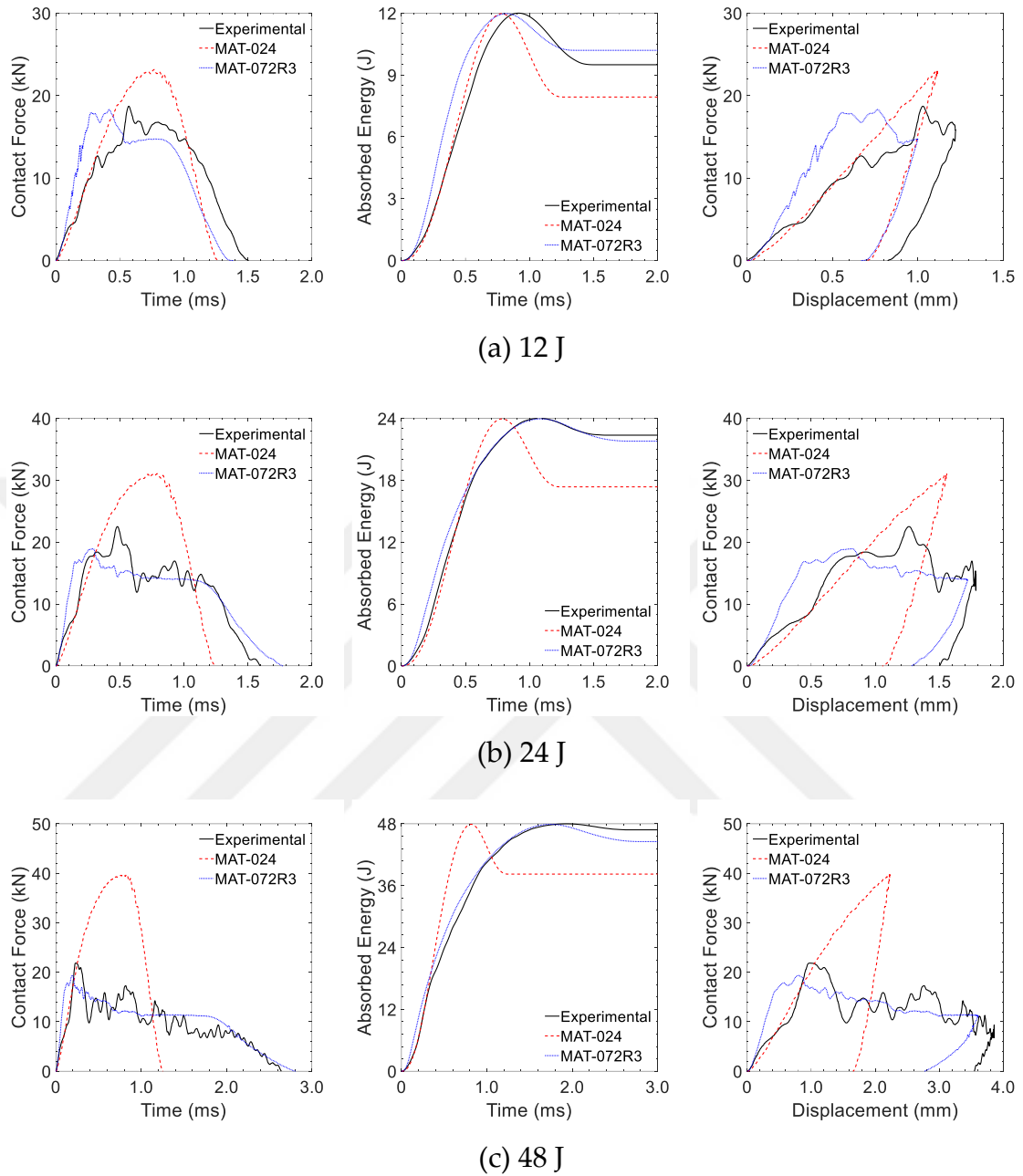


Figure 5.20. The comparison of the MAT_024-based and MAT_072R3-based models in terms of contact force and absorbed energy histories, and force-displacement response of the ceramic-rich FGM plate.

Table 5.3. The comparison of the MAT_024-based and MAT_072R3-based models in terms of peak contact force, absorbed energy, and permanent displacement.

		F_{peak} (kN)	Diff. (%)	E_{absorbed} (J)	Diff. (%)	δ_{permanent} (mm)	Diff. (%)
	Experimental	18.71	-	9.50	-	0.82	-
12 J	MAT_024	23.15	23.7	7.95	16.3	0.70	14.6
	MAT_072R3	18.31	2.1	10.22	7.6	0.67	18.3
	Experimental	22.51	-	22.37	-	1.50	-
24 J	MAT_024	31.07	38.0	17.41	22.2	1.06	29.3
	MAT_072R3	18.93	15.9	21.84	2.4	1.28	14.7
	Experimental	21.84	-	46.83	-	3.55	-
48 J	MAT_024	39.75	82.0	38.34	18.1	1.65	53.5
	MAT_072R3	19.34	11.4	44.65	4.7	2.76	22.3

To represent the comparative deformation and damage profiles of the ceramic-rich FGM plate, firstly, the cross-sectional deformation and damage views of the experiments and numerical model are given in Fig. 5.21 for the 24 J and 48 J impact energies that cause major deformation and damage in the plate. The MAT_024-based model does not have the capability of modeling brittle damage, therefore, it is able to model only the elastoplastic indentation in the plate. On the other hand, the MAT_072R3-based model shows a good prediction capturing the conoidal cracks and fracture of the plate. Furthermore, the radial crack initiations and propagations based on the erosion criteria are given in Fig. 5.22. For the 12 J impact energy, two directional radial cracks are observed, and increasing impact energy causes multiple crack initiations and additionally circumferential cracks around the impact region. For the 48 J impact energy that causes radial fracture in the experiments, the comparative external views of the experiment and numerical model are illustrated in Fig. 5.23. Although a complete fracture does not physically occur in the MAT_072R3-based model, the radial crack initiations and propagations are successfully captured.

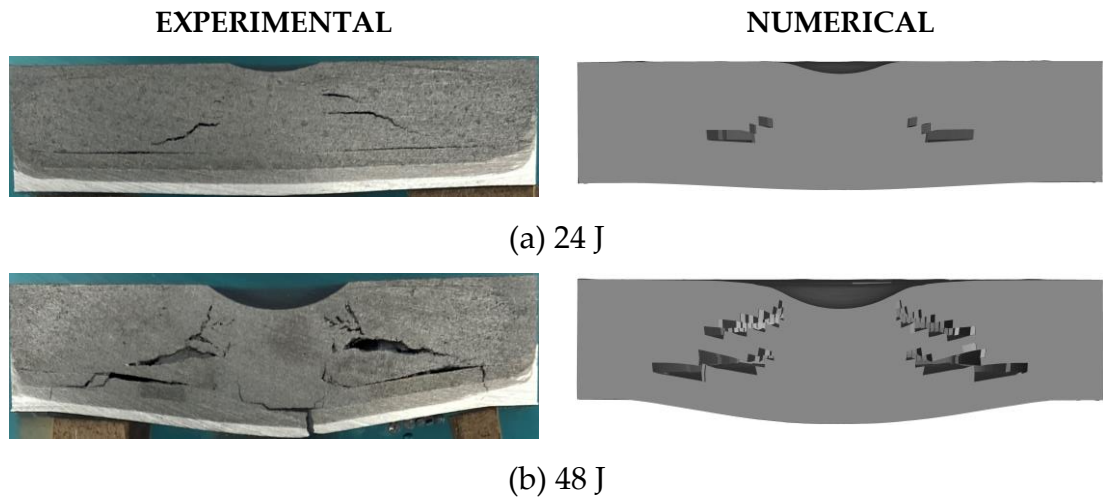


Figure 5.21. The experimental and numerical cross-sectional deformation and damage profiles of the ceramic-rich FGM plate.

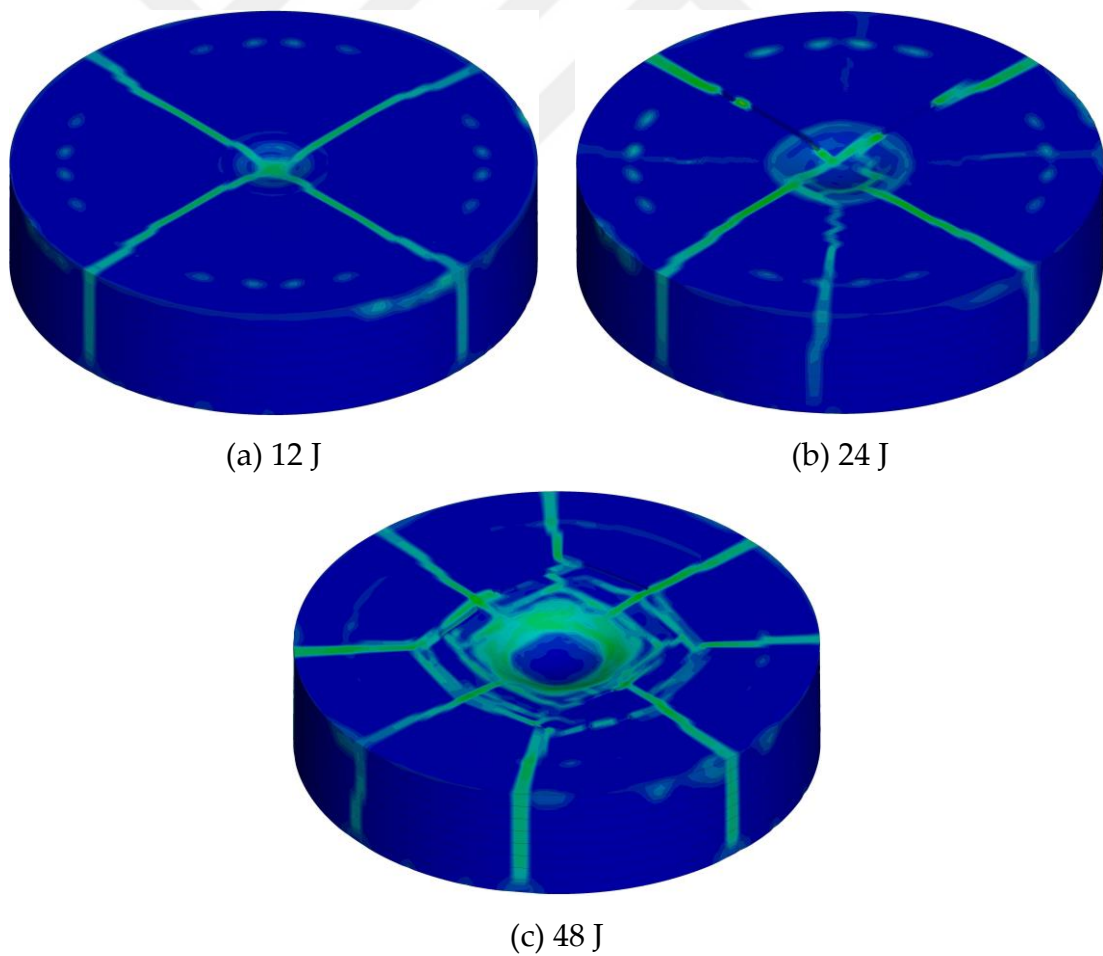


Figure 5.22. The crack distributions of the MAT_072R3-based model for the ceramic-rich FGM plate.

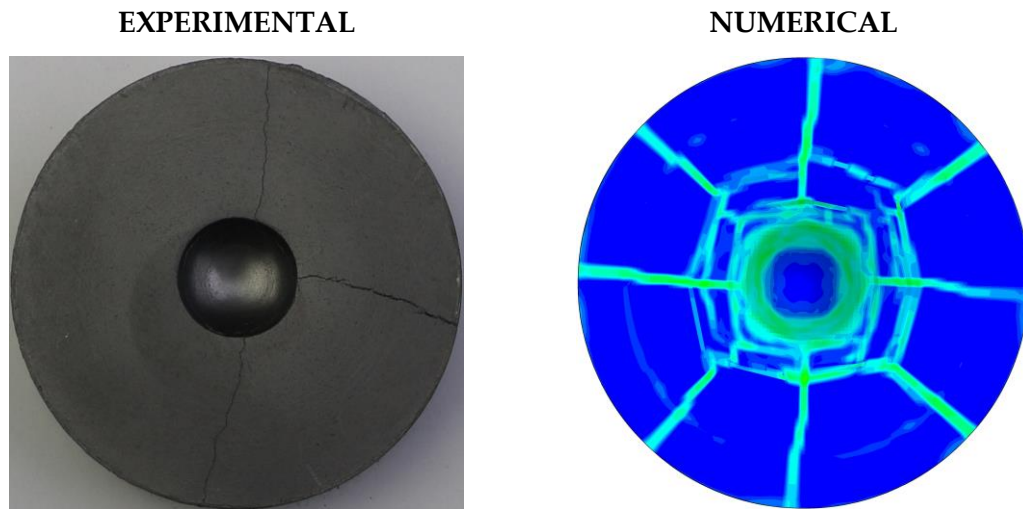


Figure 5.23. The experimental and numerical external deformation and damage views of the ceramic-rich FGM plate (48 J).

Consequently, it can be stated that the proposed MAT_072R3-based constitutive modeling presents an excellent prediction compared to the MAT_024-based constitutive modeling according to the contact force and absorbed energy histories, force-displacement response, and the deformation and damage profile of the ceramic-rich FGM plate.

After the evaluation of the numerical modeling results of the ceramic-rich FGM plate, the numerical results for all FGM plates are presented comparatively with the experimental results. The comparative contact force and absorbed energy histories and force-displacement response of the FGM plates are given in Figs. 5.24-5.26, and the peak contact forces, the absorbed energies, and the permanent displacements of the plates are summarized in Table 5.4.

Figs. 5.24 and 5.25 respectively show the comparative experimental-numerical contact force histories and force-displacement response of the plates. In analogy to the experimental results, the numerical results indicate that the contact force increases, and the contact duration and permanent displacement decreases as the total ceramic fraction in the plate increases up to ceramic-rich composition. The decrement in the contact force and the increment in the permanent displacement from linear composition to ceramic-rich composition is successfully predicted by the MAT_072R3-based constitutive modeling by means of its material softening and damage modeling capabilities. Additionally, the MAT_072R3-based model captures the decrement of the contact force after the initial peak value reflecting the crack initiations and propagations and the formation of conoidal fracture especially for the 24 J and 48 J impact energies. On the other hand, the MAT_024-based constitutive modeling of the metal-rich and linear FGM plates also shows a good agreement with the experimental results successfully predicting the force-time and force-displacement trends in general. Although the metal-rich and linear FGM plates have high ceramic content layers such as 50%SiC, 60%SiC, and 70%SiC, implementing their plasticity behavior is acceptable, since these layers exhibit ductile behavior under the low-velocity impact.

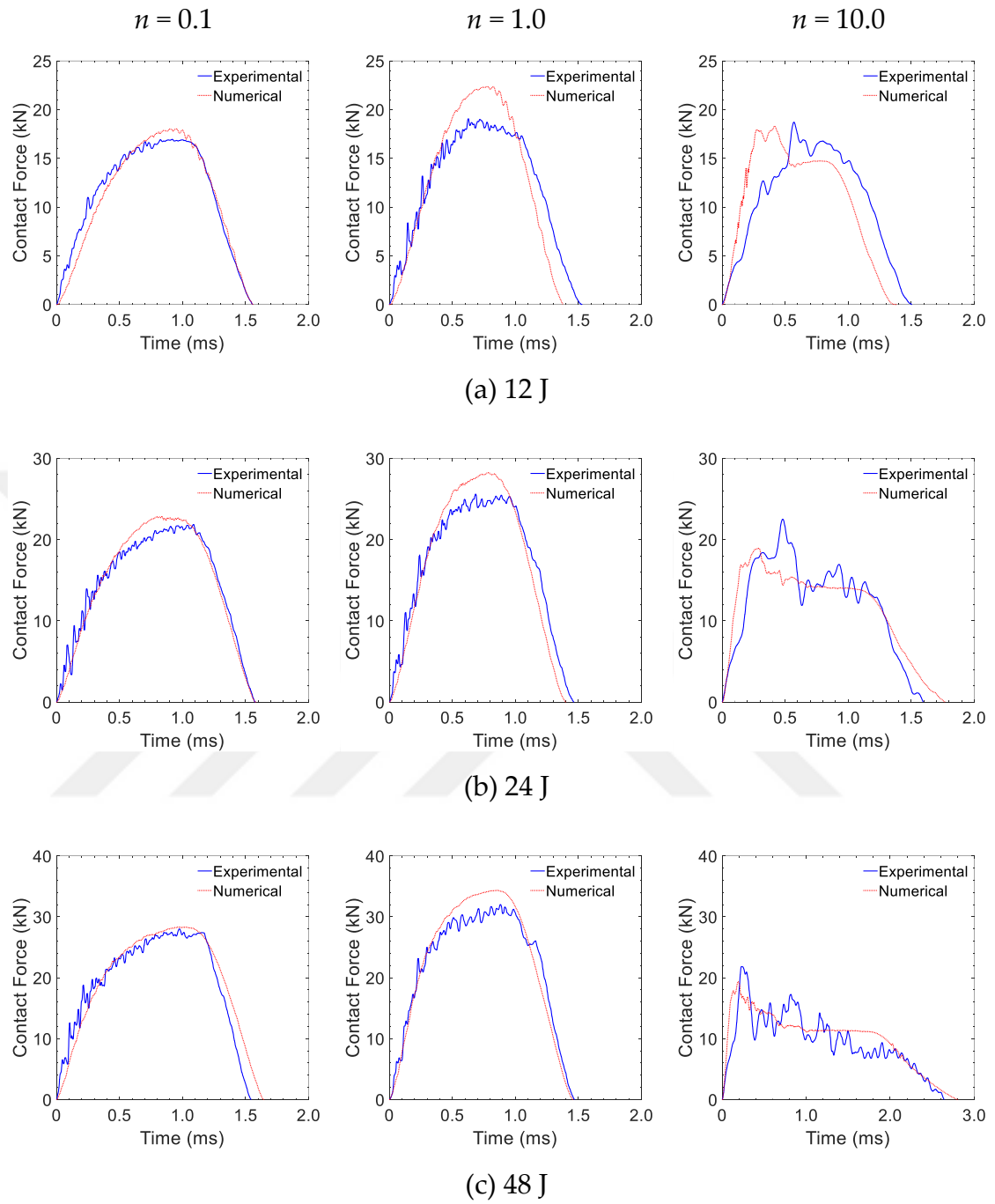


Figure 5.24. The experimental and numerical contact force histories of the FGM plates.

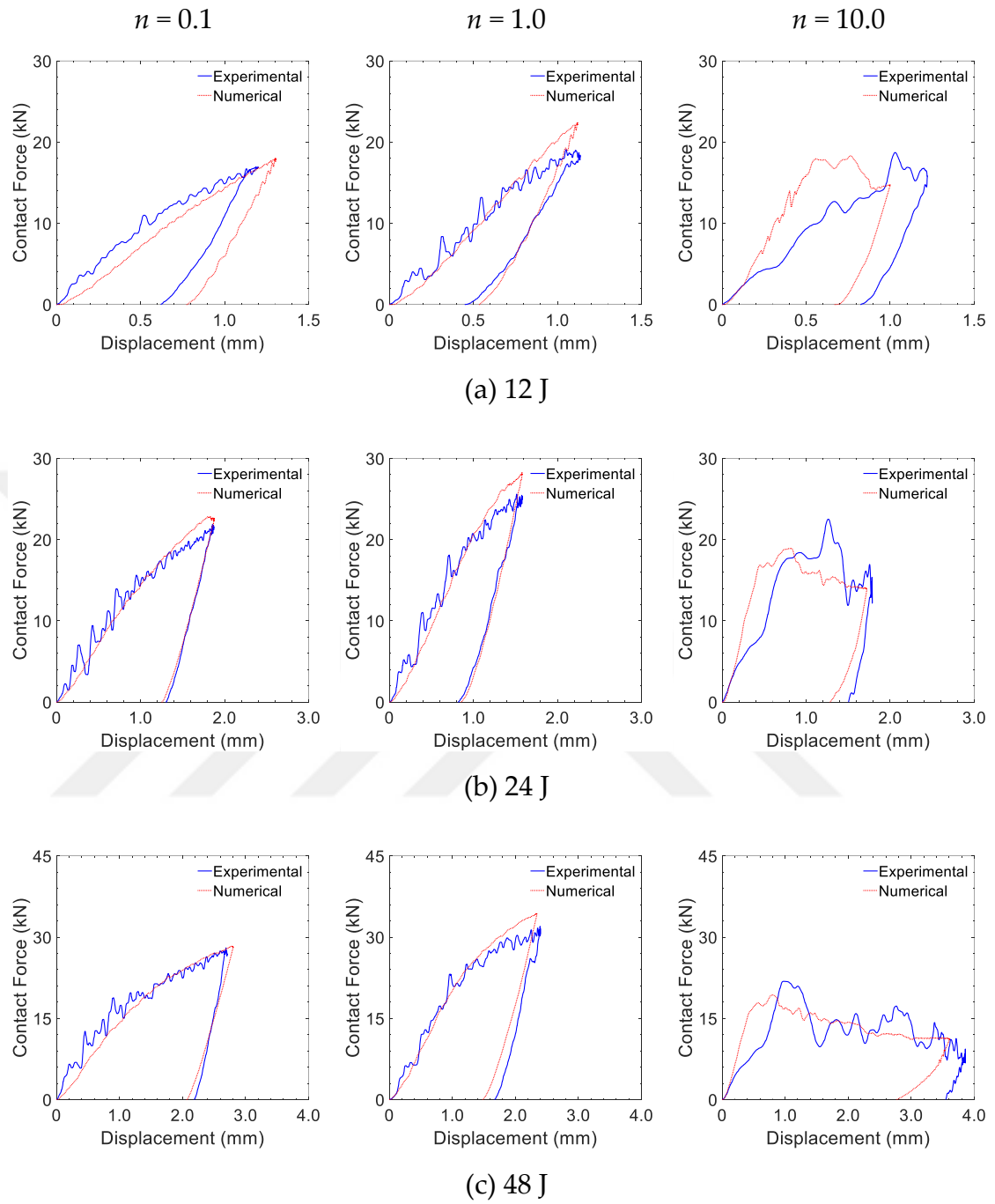


Figure 5.25. The experimental and numerical force-displacement response of the FGM plates.

Table 5.4. The experimental and numerical peak contact force, absorbed energy, and permanent displacement of the FGM plates.

		F_{peak} (kN)			E_{absorbed} (J)			$\delta_{\text{permanent}}$ (mm)		
		Exp.	Num.	Diff. (%)	Exp.	Num.	Diff. (%)	Exp.	Num.	Diff. (%)
12 J	Al6061	15.72	17.39	10.62	8.24	7.86	4.61	0.80	0.81	1.25
	$n = 0.1$	16.95	18.02	6.31	7.31	7.76	6.16	0.62	0.77	24.19
	$n = 1.0$	19.05	22.36	17.38	6.29	6.36	1.11	0.45	0.53	17.78
	$n = 10.0$	18.71	18.31	2.14	9.50	10.22	7.58	0.82	0.67	18.29
24 J	Al6061	20.73	22.00	6.13	19.37	17.70	8.62	1.43	1.30	9.09
	$n = 0.1$	21.86	22.87	4.62	18.11	17.75	1.99	1.30	1.25	3.85
	$n = 1.0$	25.60	28.25	10.35	15.17	15.38	1.38	0.82	0.85	3.66
	$n = 10.0$	22.51	18.93	15.90	22.37	21.84	2.37	1.50	1.28	14.67
48 J	Al6061	25.67	27.61	7.56	43.00	38.68	10.05	2.57	2.16	15.95
	$n = 0.1$	28.02	28.33	1.11	41.28	38.61	6.47	2.18	2.06	5.50
	$n = 1.0$	32.01	34.36	7.34	37.35	35.39	5.25	1.67	1.47	11.98
	$n = 10.0$	21.84	19.34	11.45	46.83	44.65	4.66	3.55	2.76	22.25

Figs. 5.26 and 5.27 present the comparative absorbed energy histories and energy absorption quantities and ratios of the plates, respectively. The most compatible results between the experiments and numerical model are obtained for the absorbed energy histories compared to the contact force histories and permanent displacements. In addition to the experimental results, Fig. 5.27 shows that the energy absorption ratio increases as the impact energy increases for all plates due to increasing plastic deformation and damage level of the plates.

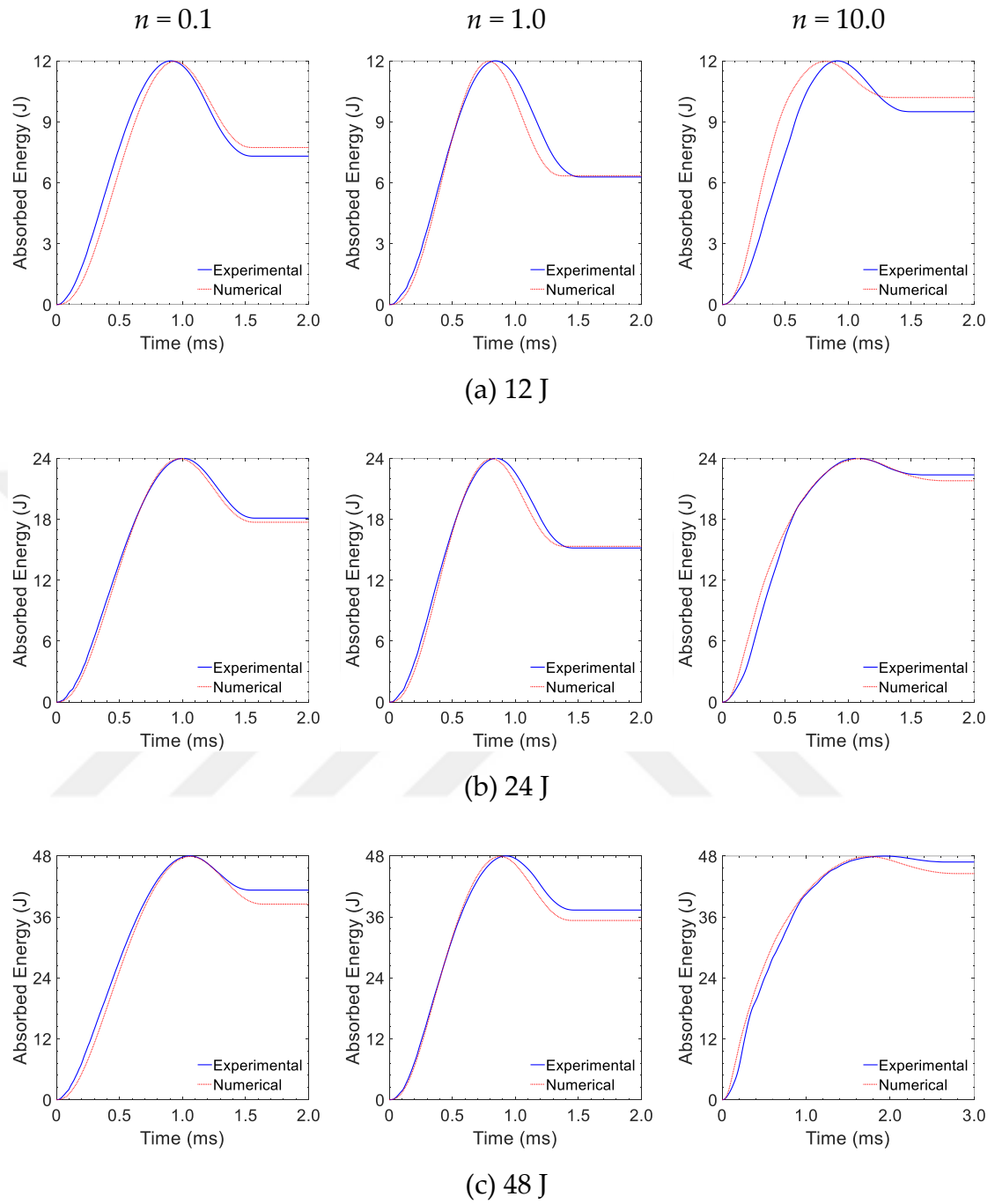
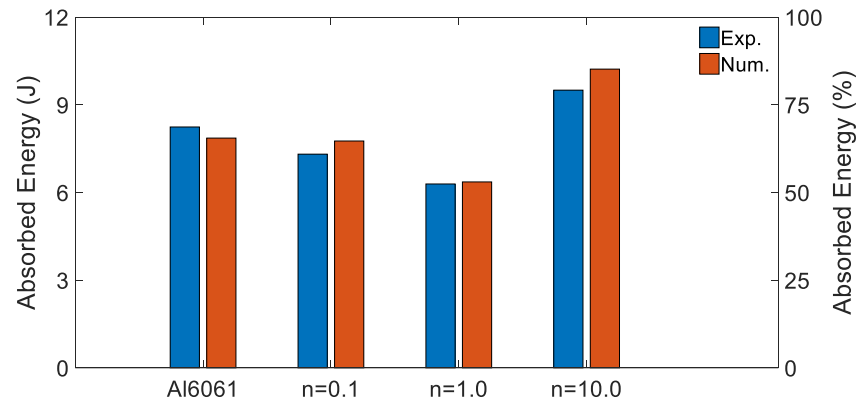
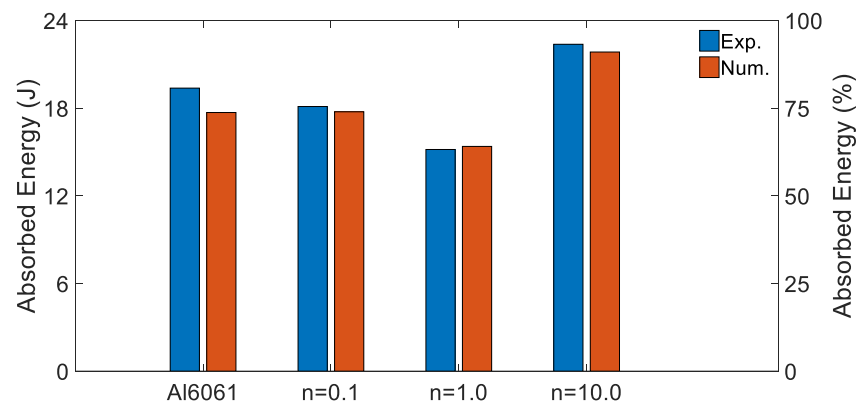


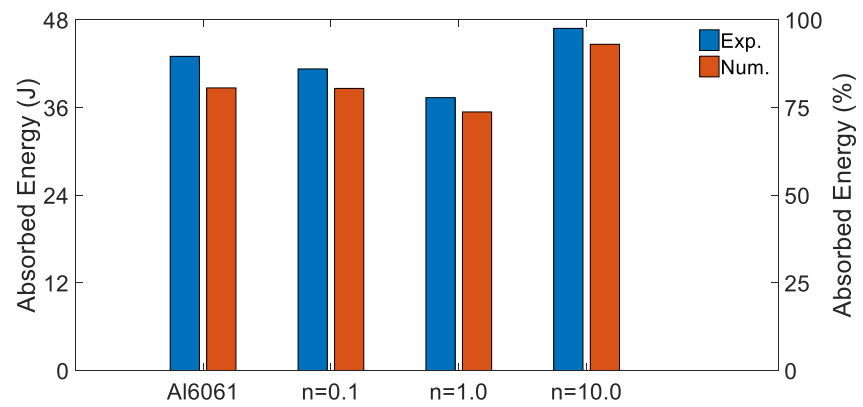
Figure 5.26. The experimental and numerical absorbed energy histories of the FGM plates.



(a) 12 J



(b) 24 J



(c) 48 J

Figure 5.27. The experimental and numerical energy absorption quantities and ratios of the FGM plates.

According to the comparative experimental-numerical results, there is a good agreement between the present numerical models and experiments in general. On the other hand, there are some deviations between the model and experimental results as seen from Figs. 5.24-5.26 and Table 5.4. The reason of these deviations can be explained that the layers of the FGM plates are assumed to be both isotropic and homogeneous in the numerical models. However, the actual structures of the plates do not have an idealized isotropy and homogenization. Additionally, it is assumed that the mechanical behavior of the layers is same in both tension and compression, and the compressive behavior of the layers is considered in the constitutive modeling (for the metal-rich and linear FGM plates), since the implemented elastoplastic constitutive models do not have a capability of considering both states. However, the tensile behavior of the layers may show discrepancies from the compressive behavior in the actual state.

5.6. Comparison of the Present Numerical Model and the TTO Model

To show the improvement of the present numerical model, it is compared to the TTO (Tamura–Tomota–Ozawa) model (explained in detail, Chapter 3) that was used for modeling of FGM structures in the previous studies [25,38,39]. In the TTO model, there are some important parameters to estimate the stress-strain behavior of the FGM layers, the stress to strain transfer ratio, q and the hardening exponent of the metal constituent, n_0 . These parameters are experimentally determined considering the quasi-static true stress-strain curves of the layers.

The q parameter is determined by using Eq. (3.9) between the stress-strain curves of Al6061 layer (0%SiC) and different FGM layers. Namely, the Al6061 layer is the reference layer, and other layers are changed to determine the variation of q parameter. For this purpose, two groups of layers, layers with relatively higher ceramic content (the 50%SiC and 55%SiC layers) and layers with intermediate

ceramic content (the 20%SiC, 30%SiC, and 40%SiC layers), are considered. The reason of selecting these groups can be explained that the q parameter is close for the layers within the group itself. The calculated q parameters for each group are listed in Table 5.5, and the average q values are considered for the groups. Using the average q values, the effect of q parameter on the numerical results is investigated.

Table 5.5. The stress to strain transfer ratio (q) for different FGM layers.

	20%SiC	30%SiC	40%SiC	50%SiC	55%SiC
q	21.0	28.7	27.7	57.1	66.9
Average q	25.8			62.0	

Additionally, the hardening exponent of the Al6061 constituent is determined from its stress-strain curve implementing Eq. (3.12), and the material parameters of the Al6061 and SiC constituents are given in Table 5.6.

Table 5.6. The material parameters of the FGM plate constituents.

Constituent	Density (kg/m ³)	Elastic modulus (GPa)	Poisson's ratio	Yield strength (MPa)	Hardening exponent
Al6061	2670	12	0.33	98.1	4.94
SiC	3210	302	0.17	-	-

The numerical modeling procedure is same as the previous section. The MAT_024 constitutive model is utilized for the FGM plates implementing the Mori–Tanaka scheme and the TTO model for the elastic and elastoplastic material definition, respectively.

Initially, the effect of q parameter is explored using the q values of 25.8 GPa and 62.0 GPa. A numerical analysis is performed for the metal-rich FGM plate under the impact energy of 48 J, and the result is given in Fig. 5.28 in terms of the contact force history. As seen from Fig. 5.28, the contact force increases, and the contact duration decreases with increasing q value. The lower q value gives closer results to the experiments. Therefore, $q = 25.8$ GPa is used for the TTO model for the comparative numerical analysis.

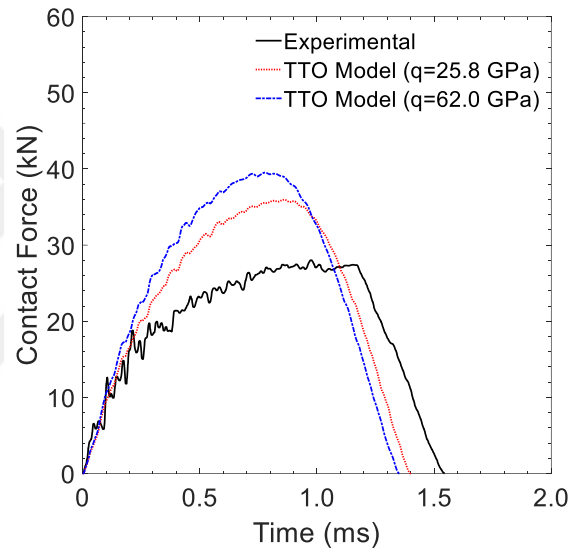


Figure 5.28. The effect of q parameter on the numerical results ($n = 0.1$, 48 J).

The comparative contact force histories of the present numerical model and the TTO model are given in Fig. 5.29 with the experimental results, and the peak contact forces are listed in Table 5.7. It is obviously seen from Fig. 5.29 and Table 5.7 that the deviation between TTO model and experiments increases with increasing impact energy and increasing total ceramic fraction of the FGM plate (from metal-rich to ceramic-rich). Especially for the ceramic-rich FGM plate, the contact force is predicted quite higher than the experiments. The reason of this difference can be explained that there is a continuous strength increment in the TTO model with increasing ceramic fraction of the FGM plate, however, strength

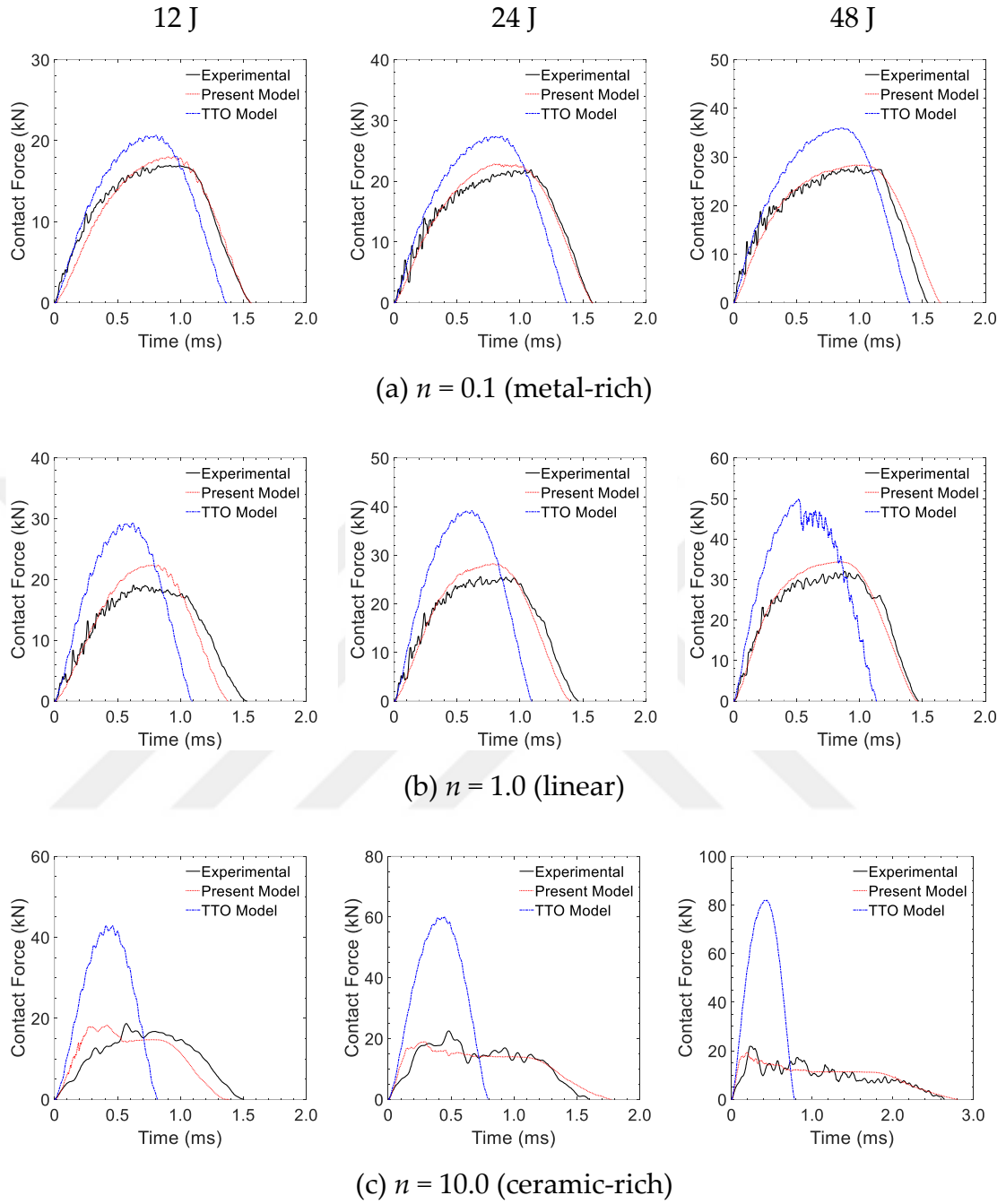


Figure 5.29. The comparison of the present numerical model and the TTO model with respect to the experimental results.

weakening results in the layers of the FGM plates over the 55%SiC fraction in the actual state. The TTO model is not capable of modeling the strength weakening. Additionally, the prediction of elastic modulus by the Mori–Tanaka scheme deviates significantly from the actual value especially for higher ceramic content.

For instance, the predicted elastic modulus of the 60%SiC and 70%SiC layers is 42.5 GPa and 56.8 GPa, whereas the actual elastic modulus for these layers is 18.6 GPa and 17.0 GPa, respectively.

Table 5.7. The comparative peak contact forces for the present numerical model and the TTO model with respect to the experimental results.

		Exp.	Present	TTO	Diff. (%) (Present)	Diff. (%) (TTO)
$n = 0.1$	12 J	16.95	18.02	20.66	6.31	18.70
	24 J	21.86	22.87	27.45	4.62	21.00
	48 J	28.02	28.33	35.96	1.11	22.38
$n = 1.0$	12 J	19.05	22.36	29.33	17.38	53.44
	24 J	25.60	28.25	39.17	10.35	52.77
	48 J	32.01	34.36	49.89	7.34	59.14
$n = 10.0$	12 J	18.71	18.31	42.85	2.14	129.66
	24 J	22.51	18.93	59.94	15.90	166.81
	48 J	21.84	19.34	81.89	11.45	275.41

Consequently, the present numerical models show an excellent improvement compared to the TTO model in conjunction with the Mori–Tanaka model. It can be stated that considering the actual stress-strain behavior successfully captures the realistic response of the FGM plates. On the other hand, the TTO model could give reasonable results for the metal-rich FGM plate at lower impact velocities.

CHAPTER 6

HIGH-VELOCITY IMPACT TESTS AND NUMERICAL MODELING OF FUNCTIONALLY GRADED PLATES

6.1. Introduction

In this chapter, high-velocity impact tests and numerical modeling of the Al6061/SiC FGM plates are carried out. A single-stage gas gun is used to determine the high-velocity impact behavior of the FGM plates, and strain-rate dependent numerical models based on the dynamic mechanical behavior of the FGM plates are developed to predict the high-velocity impact response.

6.2. High-Velocity Impact Tests

The high-velocity impact tests of the Al6061/SiC FGM plates are performed by a single-stage gas gun manufactured by Physics Applications Inc. (Fig. 6.1). In the high-velocity impact experiments, .30 caliber FSPs (Fragment Simulating Projectiles) specified in the MIL-DTL-46593B (MR) w/AMENDMENT 1 [71] are utilized. The launcher of the gas gun is suitable for sabotaged projectiles, and the geometrical dimensions of a .30 caliber FSP for sabotaged launch are given in Fig. 6.2. The FSPs used in the tests are produced from 4340H steel and quenched and tempered to a Rockwell C hardness value of 30 ± 2 in accordance with the MIL-DTL-46593B (MR) w/AMENDMENT 1.

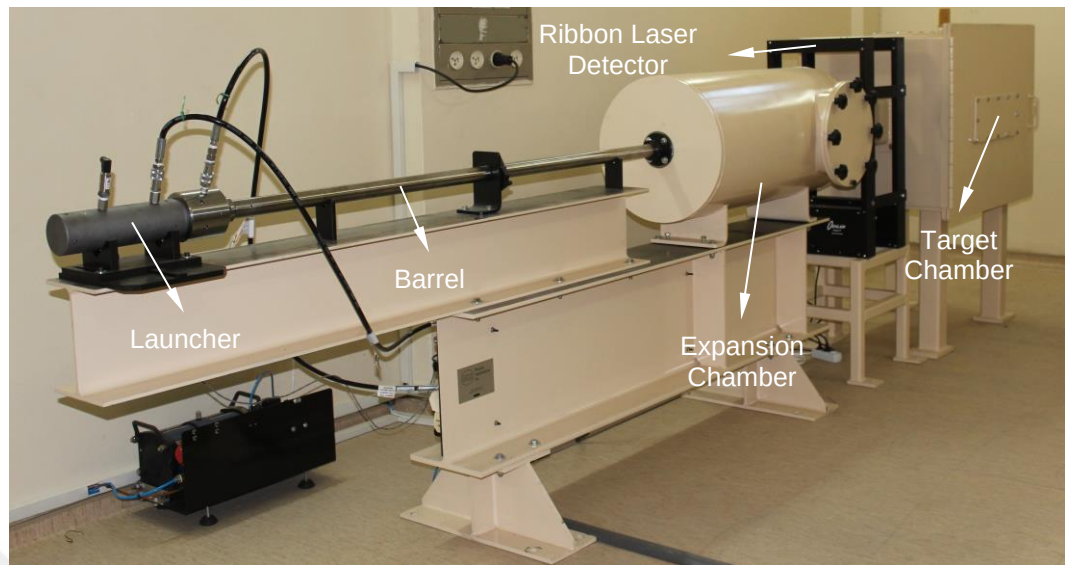


Figure 6.1. The single-stage gas-gun.

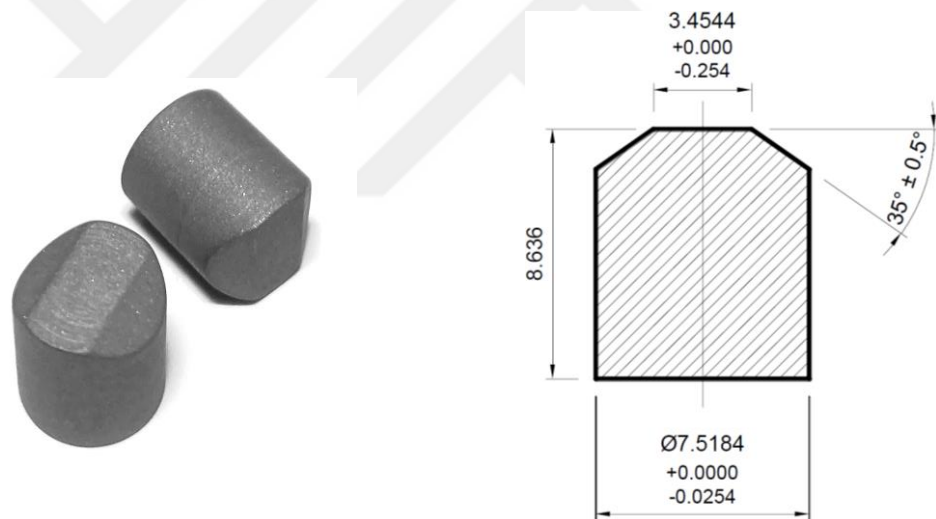


Figure 6.2. The geometrical details of a .30 caliber FSP for sabot launch.

For fixing the FGM plates in the shot area, a target fixture having an inner diameter of 70 mm is used (Fig. 6.3). Four different plates, the Al6061, $n = 0.1$ (metal-rich), $n = 1.0$ (linear), and $n = 10.0$ (ceramic-rich), are tested in the high-velocity impact experiments. The plates are tested at four different impact velocities as 500 m/s, 650 m/s, 800 m/s, and 950 m/s. The FGM plates are subjected to FSP impact from the ceramic-rich side. The projectile velocity is measured using a ribbon laser detector system which exists in the gas gun.

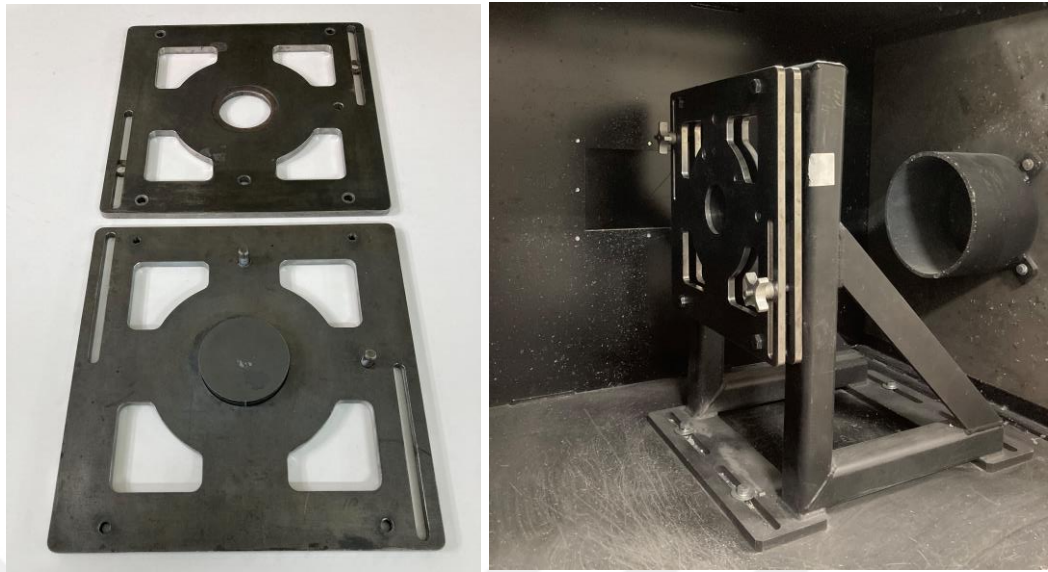


Figure 6.3. The target fixture for the FGM plates.

6.3. Experimental Results

The high-velocity impact tests are carried out under the same conditions for the impact velocities of 500 m/s, 650 m/s, 800 m/s, and 950 m/s. Expectedly, there are some deviations for the velocities due to experimental factors. The projectile velocities measured by the ribbon laser detector are listed in Table 6.1. For a joint evaluation in the results, the impact velocities are referred as 500 m/s, 650 m/s, 800 m/s, and 950 m/s.

Table 6.1. The measured projectile velocities.

Projected velocities	Measured velocities			
	Al6061	$n = 0.1$	$n = 1.0$	$n = 10.0$
500 m/s	515.3	515.8	514.1	509.3
650 m/s	659.1	653.8	651.6	658.1
800 m/s	815.2	809.7	828.1	817.8
950 m/s	964.3	974.4	963.3	946.3

The high-velocity impact test results of the FGM plates are evaluated in two different sections as the external (front and rear face) deformation and damage assessment and the cross-sectional deformation and damage evaluation. The summary of the impact test results is listed in Table 6.2. In Table 6.2, the status of the plates is indicated as partial or complete in terms of penetration. The plate status is indicated as partial penetration, if the projectile is embedded in the plate (at any location) and stated as complete penetration, if the projectile passes through the plate.

Table 6.2. The high-velocity impact test results in terms of penetration status.

	Penetration status			
	Al6061	$n = 0.1$	$n = 1.0$	$n = 10.0$
500 m/s	partial	partial	partial	partial
650 m/s	partial	partial	partial	complete
800 m/s	complete	complete	partial	complete
950 m/s	complete	complete	complete	complete

For the assessment of external deformation and damage mechanisms of the FGM plates, the post-impact front and rear face images of the plates are given in Figs. 6.4-6.7. The FGM plates show different deformation and damage mechanisms under the high-velocity impact depending on the mechanical behavior or the material composition. Namely, the deformation and damage behavior of the FGM plates has a transition from ductile (metal-rich) to brittle (ceramic-rich) according to the total ceramic fraction in the plates.

As expected, the Al6061 plate shows ductile deformation and damage. Namely, petaling at the front face and bulging at the rear face occurs for the partial penetration velocities (Figs. 6.4-a and 6.4-b), while frontal and rearward petaling results in for the complete penetration velocities (Figs. 6.4-c and 6.4-d).

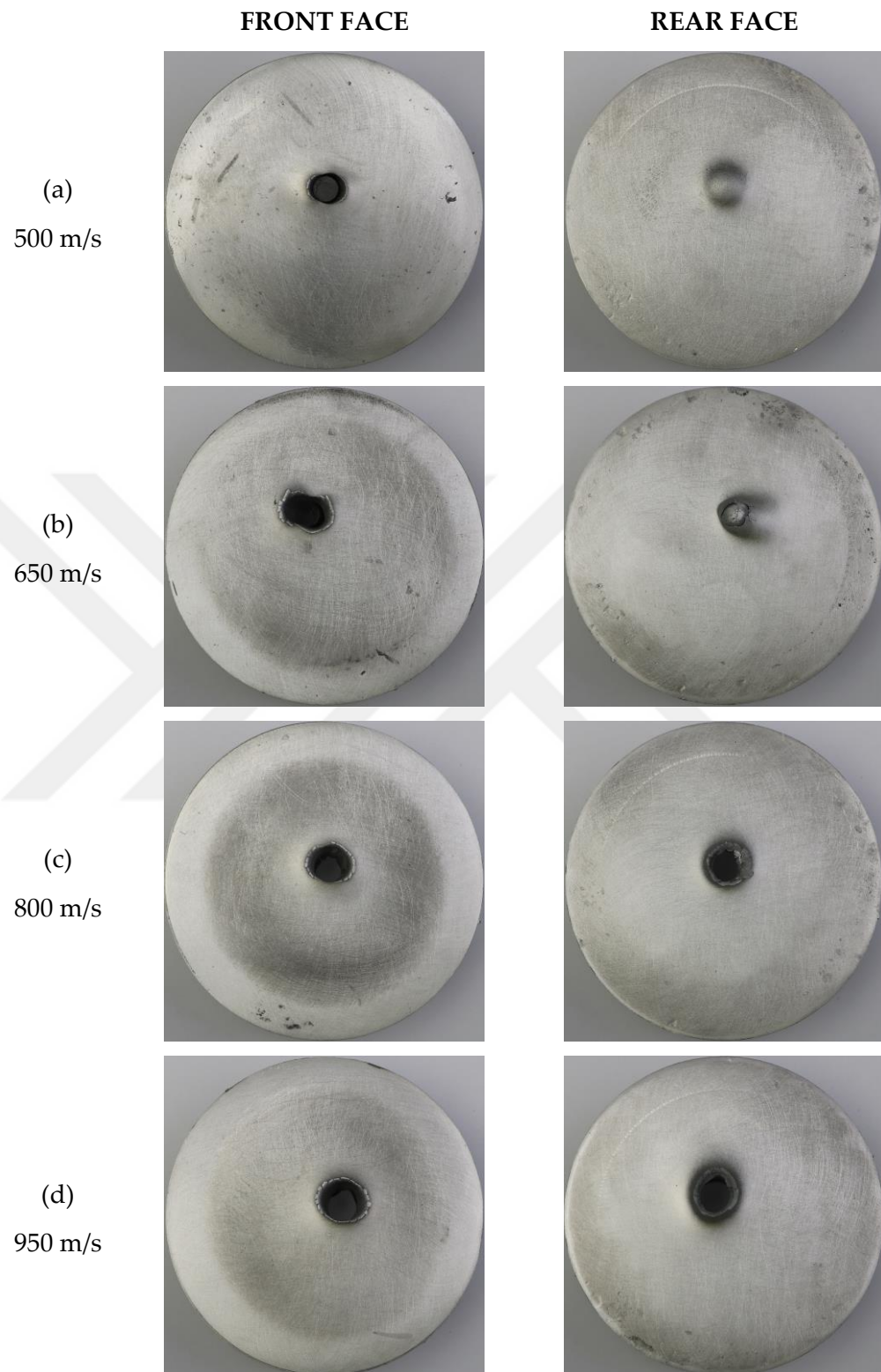


Figure 6.4. The front and rear face deformation and damage views of the Al6061 plate.

The metal-rich and linear FGM plates have a joint damage evaluation, since the general external damage mechanisms independent from the impact velocity are similar for these plates. For the rear face of these plates, bulging is observed for the partial penetration velocities, while ductile piercing is created by the projectile for the complete penetration velocities (Figs. 6.5 and 6.6). The rear face failure in these plates for the complete penetration velocities is not exactly petaling, since the top and intermediate layers of the FGM plates reduce the projectile penetration effect and accordingly affect the plate deformation. A shock wave occurs and propagates through target thickness in a high-velocity impact phenomenon, and accordingly this shock wave and forward motion of the projectile create bending moment in the target, and tensile stresses occur at the rear face. Failure initiates once the tensile stress exceeds the ultimate tensile strength of the target material. According to this failure formation, tensile cracks are observed in the metal-rich FGM plate under the 650 m/s impact velocity and in the linear FGM plate under the 800 m/s impact velocity.

The front face of the metal-rich and linear FGM plates is the ceramic-rich layer with 70%SiC fraction that has quasi-brittle/brittle behavior. Spalling failure due to the initial wave reflection from the interface between layers and distal side of the plate is observed at the front face of these plates. As seen from Figs. 6.5 and 6.6, the spalling material decreases with varying the plate composition from metal-rich to linear depending on the mechanical impedance of the layers underneath the 70%SiC layer, and it increases with increasing impact velocity due to higher wave amplitude. To illustrate this result, approximate spall diameters for the metal-rich and linear FGM plates are represented on the corresponding front face images. The reason of this state is explained in detail in the cross-sectional damage evaluation.

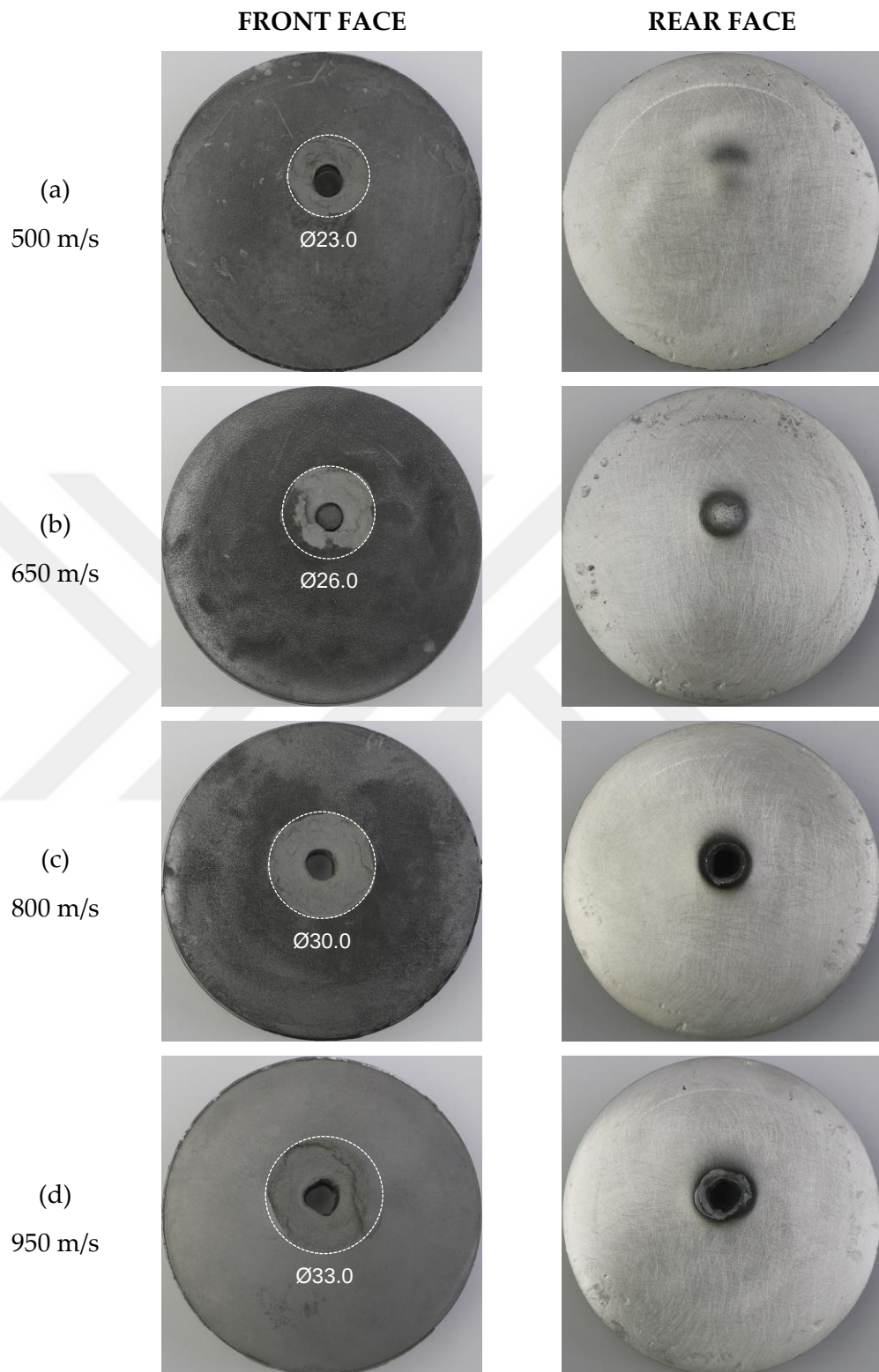


Figure 6.5. The front and rear face deformation and damage views of the metal-rich FGM plate and the diameters of the frontal spall failure (mm).

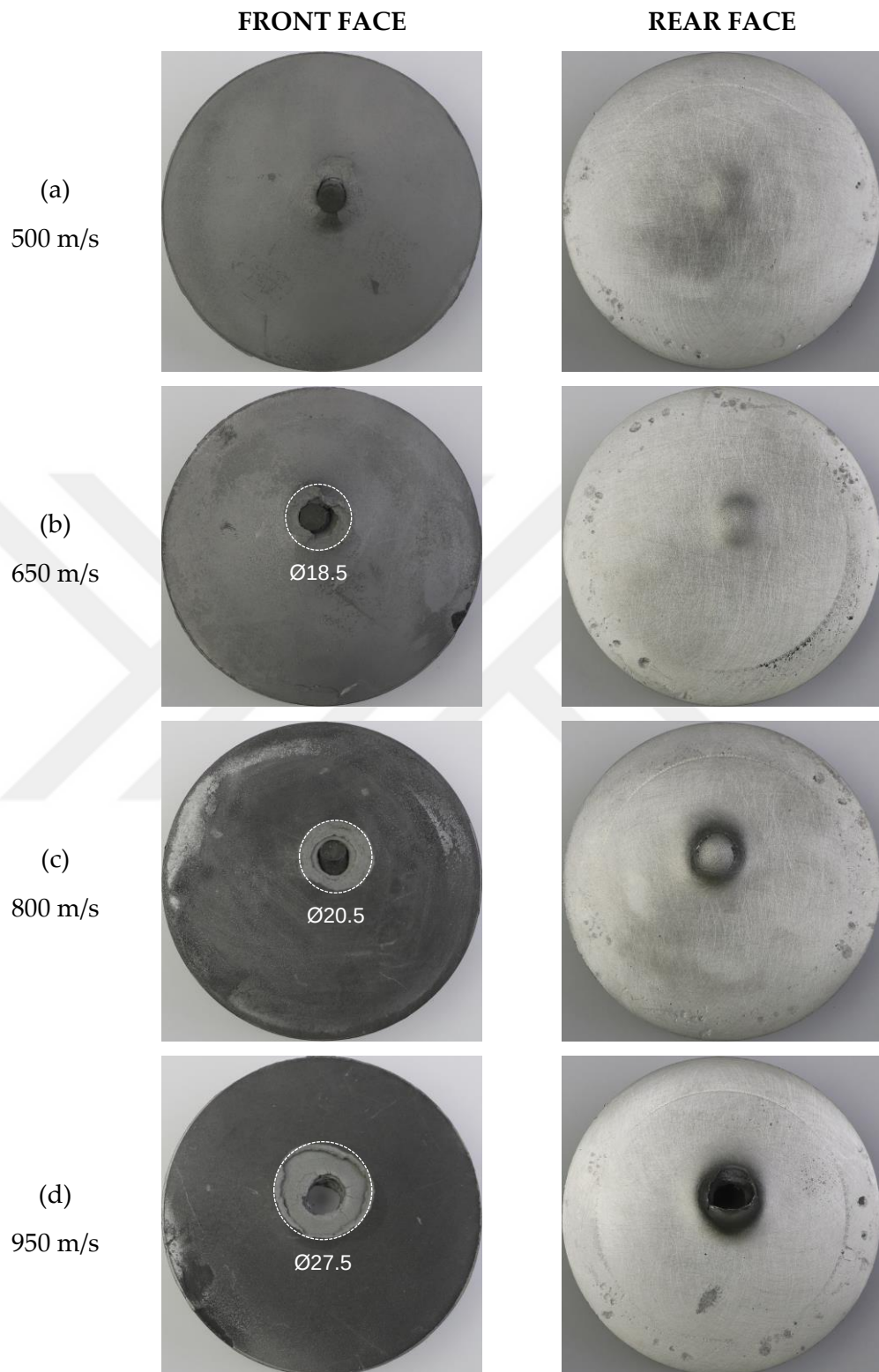


Figure 6.6. The front and rear face deformation and damage views of the linear FGM plate and the diameters of the frontal spall failure (mm).

The ceramic-rich FGM plate presents a distinctive response compared to the other FGM plates. As seen from the rear face views of the ceramic-rich FGM plate (Figs. 6.7-b, 6.7-c, and 6.7-d), it is obviously seen a typical brittle damage, conoidal fracture that is explicitly presented in the cross-sectional damage evaluation of the plates. The conoidal fracture occurs for all complete penetration velocities. The ceramic-rich FGM plate is not completely penetrated for only the impact velocity of 500 m/s, and it shows bulging at the rear face with a larger bulging region compared to the other plates (Fig. 6.7-a). Furthermore, a radial tensile crack that is caused by the bending moment (due to the initial shock wave and the forward motion of the projectile) is observed in the bulging region.

At the front face of the ceramic-rich FGM plate, spalling failure is not as effective as in the metal-rich and linear FGM plates. On the other hand, radial cracks are observed around the impact region for the impact velocities of 800 m/s and 950 m/s (Figs. 6.7-c and 6.7-d). The reason of having minor frontal spalling failure (the spallation of the 70%SiC front layer) of the ceramic-rich FGM plate is related to the general deformation and damage state depending on the mechanical behavior of the plate. Namely, the layers beneath the 70%SiC layer for the metal-rich and linear FGM plates show ductile deformation and damage under the initial wave reflections and the interactions of the initial and reflected waves, whereas most of the initial stress or shock wave is attenuated by the brittle damage modes such as crack initiations, crack propagations, and fractures. Additionally, considering the thickness of the 70%SiC front layer for the FGM plates, it exists as a thin layer with an approximate thickness of 2 mm for the metal-rich and linear FGM plates, whilst the ceramic-rich FGM plate has five 70%SiC layers with an approximate total thickness of 10 mm. The thickness of the 70%SiC layer is also effective on the frontal spallation failure of the FGM plates.

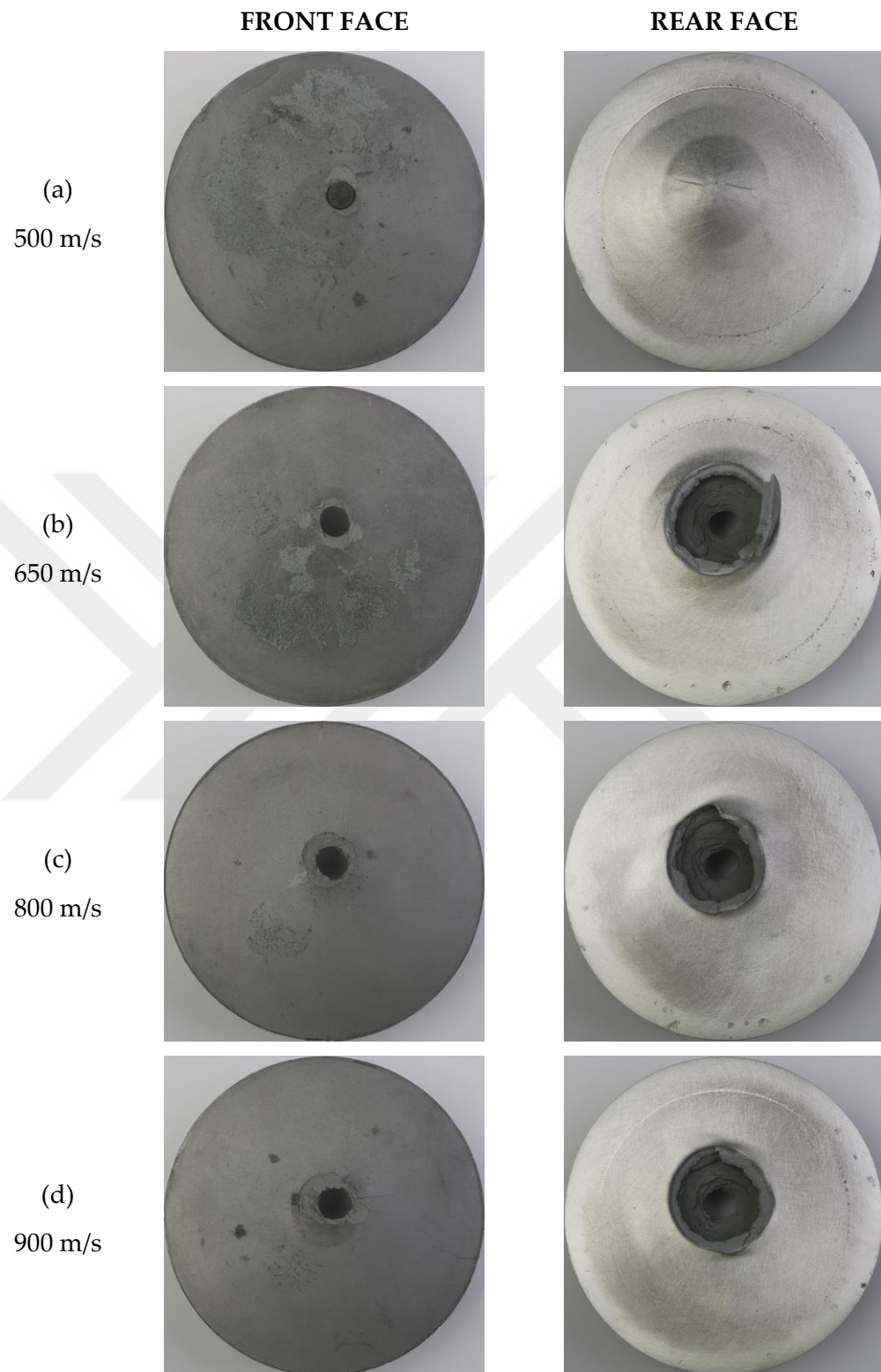


Figure 6.7. The front and rear face deformation and damage views of the ceramic-rich FGM plate.

To examine the cross-sectional deformation and damage mechanisms of the Al6061 and FGM plates, the impacted plates are cut into half section given in Figs. 6.8-6.11. The above-mentioned deformation and damage mechanisms of the plates are examined more clearly from the cross-sectional images. From the cross-sectional views of the plates, it can be clearly determined the depth of penetration (DOP) of the projectile for the partially penetrated plates, and the approximate DOP measurements are given in Table 6.3.

Table 6.3. The DOP measurements of the partially penetrated plates (mm).

	Al6061	$n = 0.1$	$n = 1.0$	$n = 10.0$
500 m/s	10.6	9.1	5.0	-
650 m/s	18.8	13.7	7.5	-
800 m/s	-	-	11.1	-
950 m/s	-	-	-	-

For the ceramic-rich FGM plate, there is only one partial penetration velocity (500 m/s). However, its DOP measurement is pointless and left out of this comparison, since the projectile is not at the location that the penetration ends. As seen from both Figs. 6.8-6.11 and Table 6.3, the DOP values decreases with increasing total ceramic fraction in the FGM plate except for the ceramic-rich FGM plate in analogy to the low-velocity impact test results. Namely, the linear FGM plate has the lowest projectile penetration, and shows the highest impact resistance.

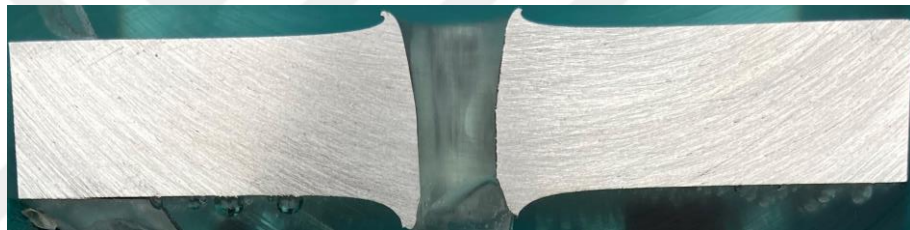
Considering the cross-sectional deformation and damage mechanisms of the Al6061 plate and the metal-rich and linear FGM plates, it is seen that these plates exhibit ductile behavior except for the failure of the top ceramic-rich layer of the metal-rich and linear FGM plates, and the projectile causes ductile hole formation through the plate thickness in these plates (Figs. 6.8-6.10).



(a) 500 m/s



(b) 650 m/s

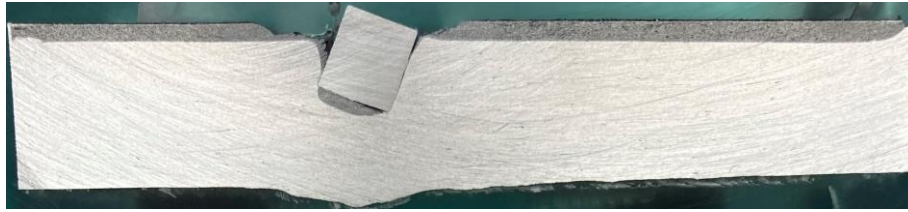


(c) 800 m/s



(d) 950 m/s

Figure 6.8. The post-impact cross-sectional views of the Al6061 plate.



(a) 500 m/s



(b) 650 m/s

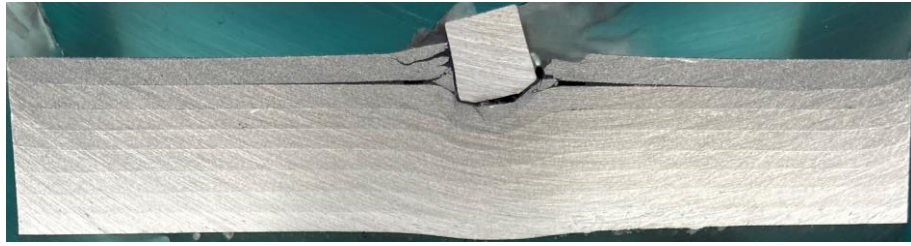


(c) 800 m/s



(d) 950 m/s

Figure 6.9. The post-impact cross-sectional views of the metal-rich FGM plate.



(a) 500 m/s



(b) 650 m/s

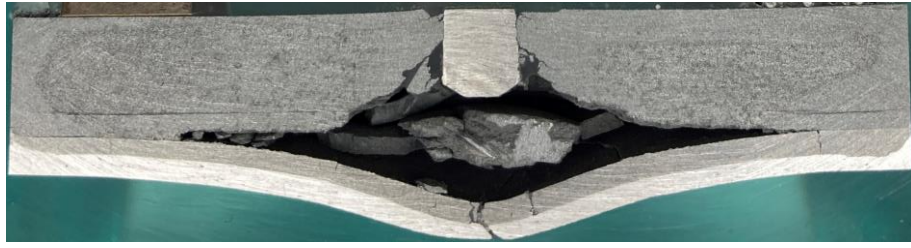


(c) 800 m/s



(d) 950 m/s

Figure 6.10. The post-impact cross-sectional views of the linear FGM plate.



(a) 500 m/s



(b) 650 m/s



(c) 800 m/s



(d) 950 m/s

Figure 6.11. The post-impact cross-sectional views of the ceramic-rich FGM plate.

In addition to the ductile hole formation in the metal-rich and linear FGM plates, the spalling failure (briefly mentioned in the external damage assessment) and delamination are observed. More explicitly, complete material removal (spallation) in a certain region occurs for the metal-rich FGM plate, whereas delamination is observed mainly between the 70%SiC and 60%SiC layers of the linear FGM plate with relatively minor material removal. The reason of the spall and delamination failures is related to the initial wave reflections in these plates. Namely, an initial compressive stress wave is generated by the projectile, and this stress wave is reflected as a tensile wave from the interface of the layers and the distal surface of the plates. The amplitude of the stress wave increases as the reflected waves interact with the initial compressive waves. Therefore, the delamination and spallation failures occur, since the magnitude of the final stress wave is greater than the normal failure strength of the interface between the 70%SiC and 60%SiC layers and the tensile strength of the 70%SiC layer, respectively. Furthermore, the reason of these different failure types is related to the mechanical impedance and the stress/shock wave propagation profile of the plates. The metal-rich FGM plate has a lower mechanical impedance than the linear FGM plate. Therefore, most of the initial compressive stress wave is reflected as a tensile wave from the distal surface of the plate and causes the spallation and a part of material removal from the frontal side of the plate. On the other hand, the linear FGM plate causes stress wave attenuation propagating a part of the initial stress wave in the transverse or lateral direction. The remaining part of the stress wave is reflected to the top of the plate; however, it does not have a sufficient effect for the spallation or material removal from the front side. Thus, the reflected stress wave causes delamination between the 70%SiC and 60%SiC layers.

As mentioned before, the ceramic-rich FGM plate shows brittle damage for all impact velocities. There are two different types of brittle damage in the ceramic-

rich FGM plate through the plate thickness, conoidal fracture and comminution. These are typical damage modes for brittle materials under impact loads as explained in detail in the low-velocity impact damage assessment of the ceramic-rich FGM plate (Hertzian fracture). The conoidal fracture occurs at the complete penetration velocities (Figs. 6.11-b, 6.11-c, and 6.11-d), while the comminution is observed at the partial penetration velocity (Fig. 6.11-a). The comminution damage is basically the same with the conoidal fracture. The difference can be explained that the comminuted material remains confined within the boundary of the conoidal fracture known as the Mescall zone reported by Crouch [72]. As seen from Fig. 6.11-a, the projectile creates the comminution in the plate and pushes forward the comminuted material. This causes bending and hinging in the Al6061 layer. Therefore, the Al6061 layer prevents the perforation of the plate by means of its plastic deformation capability.

To illustrate the damage mechanisms and damage sequence of the plates, and to determine the residual velocity of the projectile, high-speed photography method is performed during the impact tests using FASTCAM Mini AX200 digital camera manufactured by Photron (Fig. 6.12).

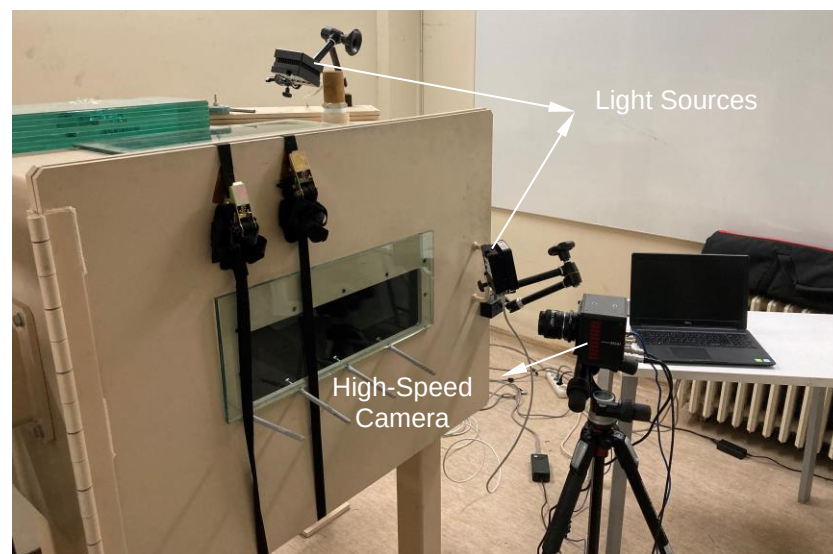


Figure 6.12. The high-speed camera setup for the high-velocity impact tests.

The sequential damage frames of the plates are given in Figs. 6.13-6.16. For a joint evaluation, the frames are presented for the impact velocity of 950 m/s that causes complete penetration for all plates. From Figs. 6.13-6.15, plugging failure is observed for the Al6061 plate and the metal-rich and linear FGM plates. Plugging failure is generally defined as an adiabatic shear failure that occurs in a narrow shear band under very high localized temperature (due to heat generated by plastic work), shear deformation and strain-rate. However, this failure is not only dependent on the material properties but also related with the geometry of projectile. Namely, blunt, or flat-nosed projectiles can cause plugging failure even when the adiabatic shear is not included [72]. The adiabatic shear failure is a complicated process, and there is still no agreement on the exact reason of adiabatic shear bands, even though phase transformation is still a popular hypothesis [72]. Therefore, the plugging failure may be caused by the adiabatic

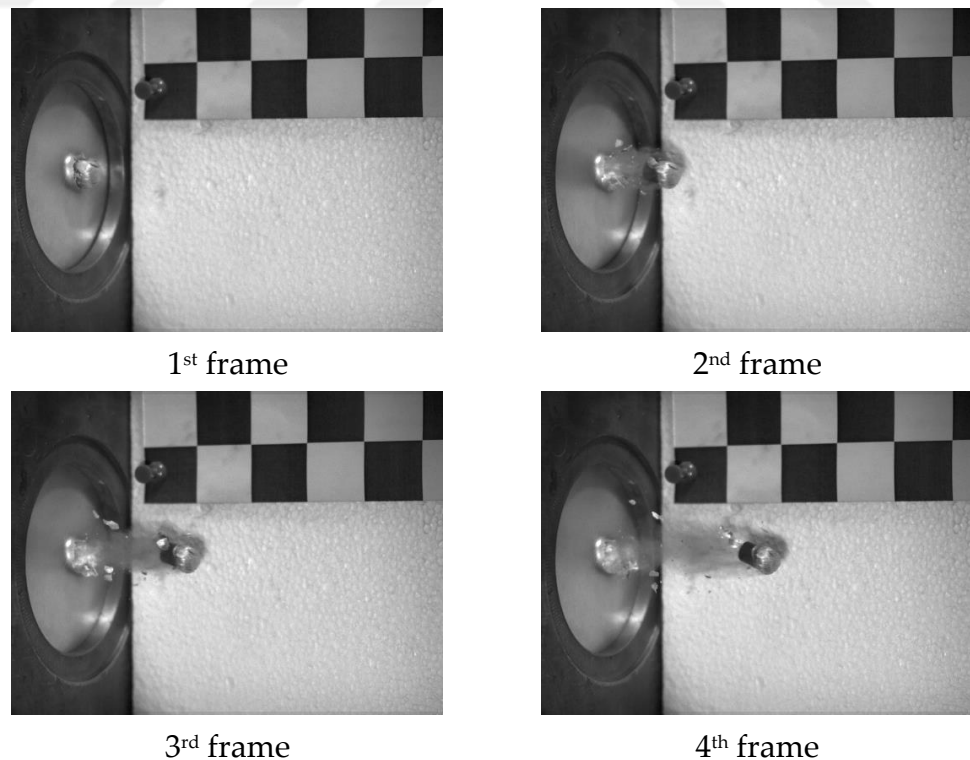


Figure 6.13. The sequential damage frames of the Al6061 plate (950 m/s).

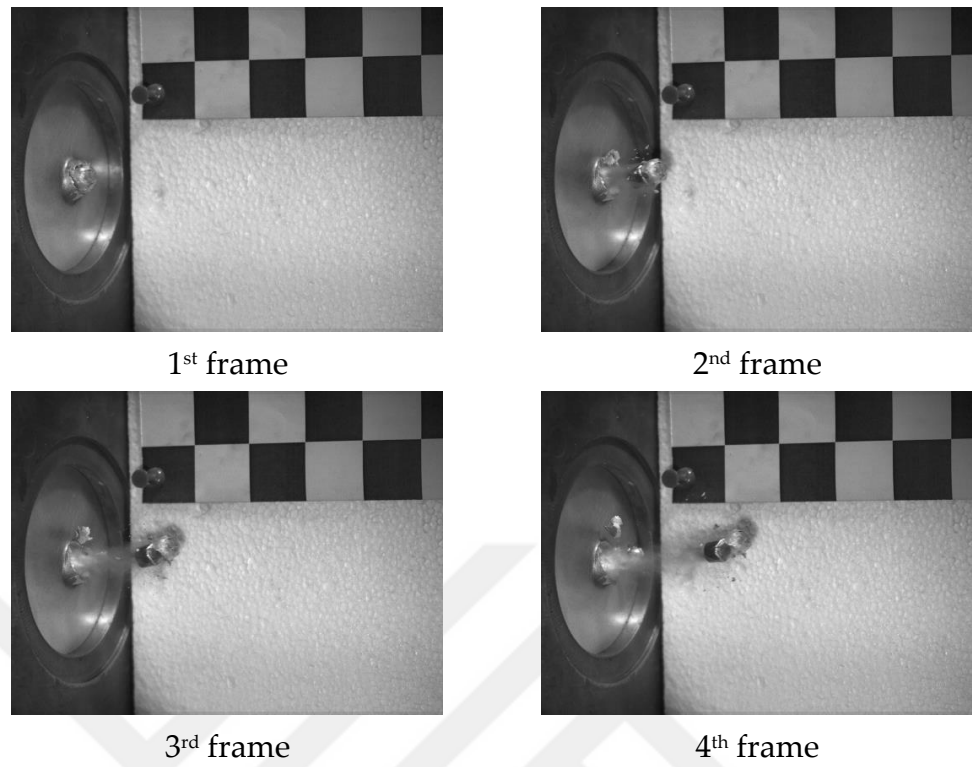


Figure 6.14. The sequential damage frames of the metal-rich FGM plate (950 m/s).

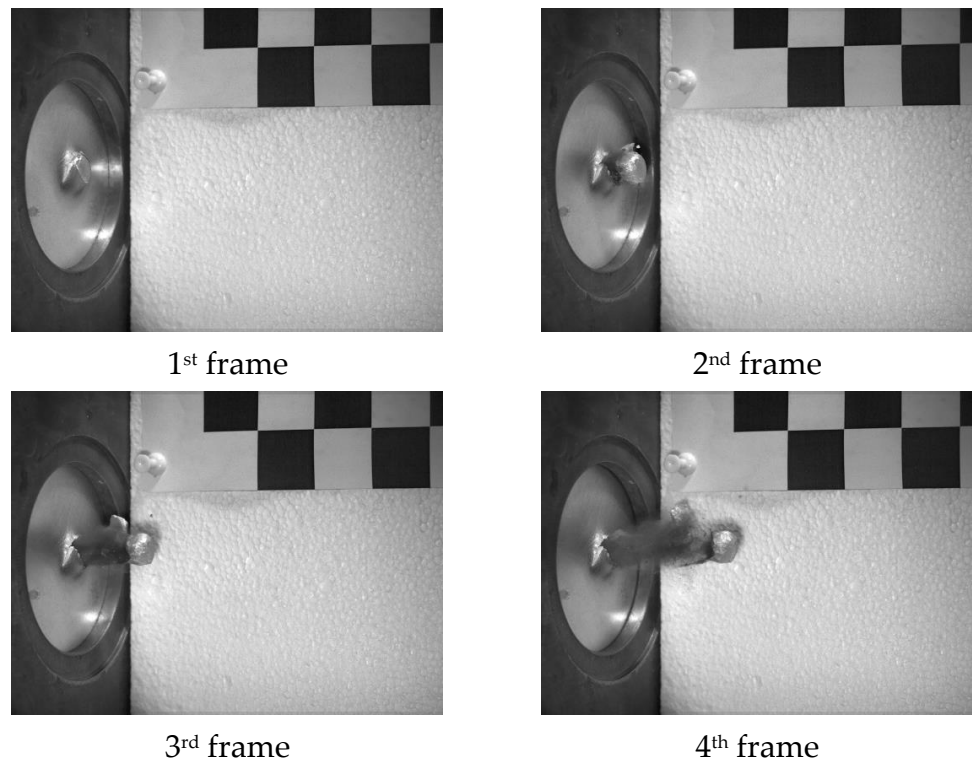


Figure 6.15. The sequential damage frames of the linear FGM plate (950 m/s).

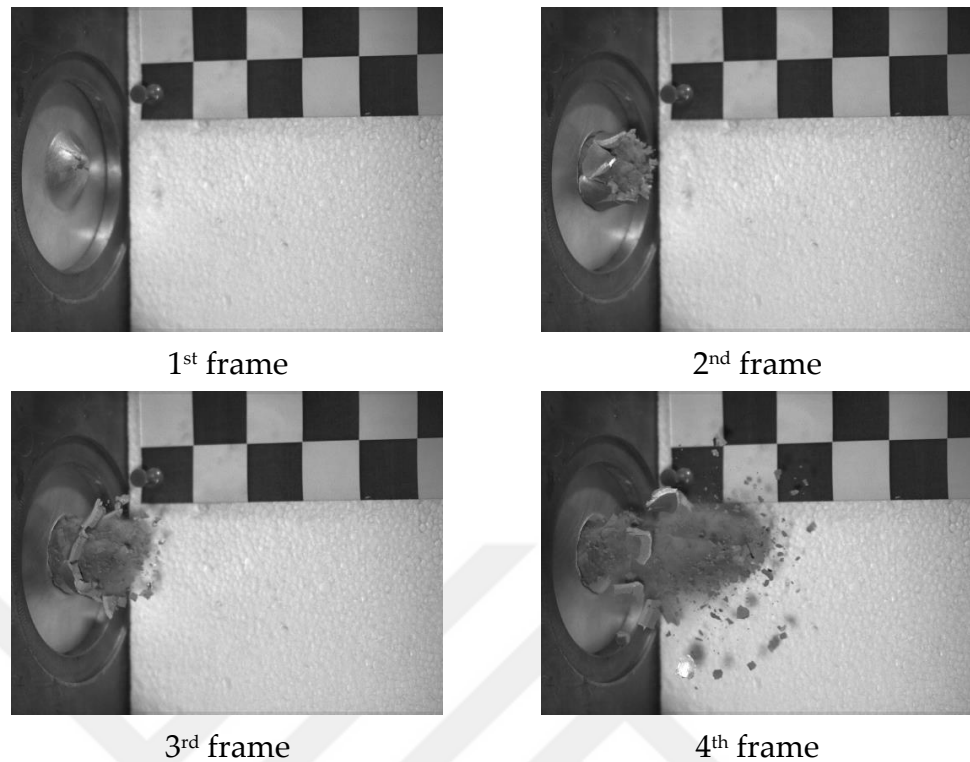


Figure 6.16. The sequential damage frames of the ceramic-rich FGM plate (950 m/s).

shear bands or the projectile geometry effect (The geometry of the FSP is close to flat-nosed shape.) or the combination of these effects in the Al6061 plate and the metal-rich and linear FGM plates. For the ceramic-rich FGM plate, it is seen from Fig. 6.16 that the Al6061 layer is subjected to bending deformation and plastically hinged in a certain region by reason of the forward momentum of the projectile and the fragmentations of the fractured conoid. Subsequently, a complete fracture occurs in the Al6061 layer due to the radial and circumferential tensile cracks.

Lastly, it can be roughly discussed on the ballistic limit velocities of the plates from the high-velocity impact test results. Namely, it can be deduced from Figs. 6.8-6.11 that the ballistic limit velocities of the Al6061 plate and the ceramic-rich FGM plate are respectively around 650 m/s and 500 m/s, and the approximate

ballistic limit velocities of the metal-rich and linear FGM plates are respectively in the range of 650-800 m/s and 800-950 m/s.

6.4. Numerical Modeling

The numerical model of the FGM plates under high-velocity impact loads is carried out by the explicit finite element code, LS-DYNA®. The numerical model consists of the FGM plate, fragment simulating projectile, and clamping fixtures considering their actual dimensions in the experiments. The FGM plate have eight sequential layers from metal (100%Al6061) bottom layer to ceramic-rich (70%SiC-30%Al6061) top layer through the plate thickness. The projectile has the dimensions in Fig. 6.2, and the clamping fixtures have an inner diameter of 70 mm. The numerical model of the high-velocity impact tests is shown in Fig. 6.17. In analogy to the low-velocity impact modeling, it is found that four elements for each layer of the FGM plate give satisfactory results performing successive analyses considering the prediction accuracy of the numerical model. All parts in the numerical model are meshed using eight-node solid element with reduced integration rule and Lagrangian formulation.

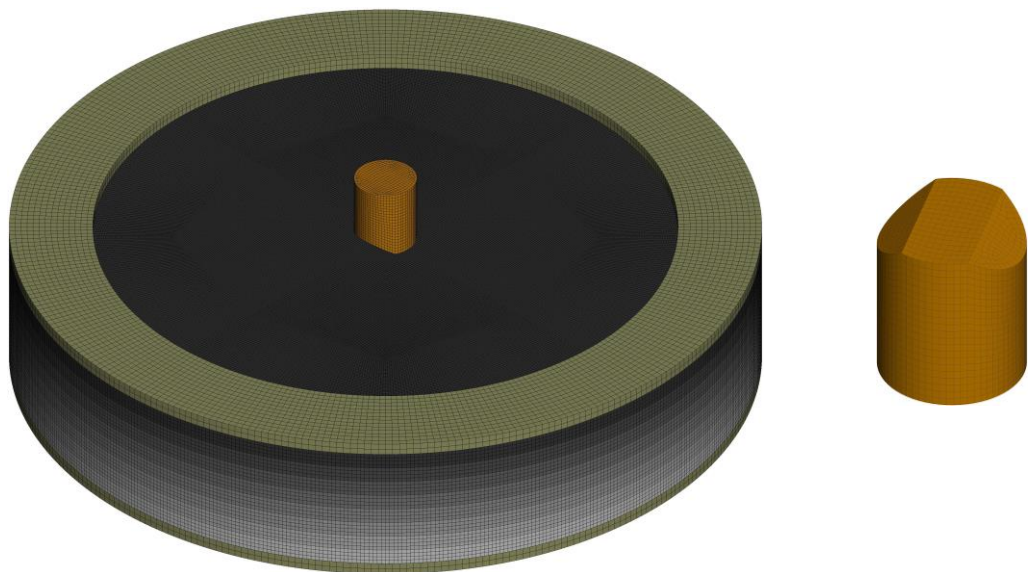


Figure 6.17. The numerical model of the FGM plate and the FSP.

Strain-rate is a highly important parameter for numerical modeling of materials subjected to high-rate loadings, and considering the strain-rate effects in the constitutive modeling is necessary to successfully predict the realistic behavior. Therefore, the actual dynamic mechanical behavior of the FGM plates is characterized by the high strain-rate compression tests (Chapter 4), and the high strain-rate compressive behavior of the layers are implemented in the numerical models to consider the strain-rate effects. The dynamic compression tests are performed for a strain-rate range of approximately 1000 s^{-1} to 5000 s^{-1} , since the gas gun is not capable of measuring the specific strain-rate of the plates during the high-velocity impact test.

The constitutive modeling procedure is different for each FGM plate, and therefore, it is given separately in subsections. Herein, the common parameters such as contact and hourglass definitions and boundary conditions are given. The contact definition in impact analysis is a critical step for the accuracy of numerical results. Therefore, `ERODING_SURFACE_TO_SURFACE` contact algorithm is defined between the projectile and each layer of the FGM plate with an approximate static and dynamic friction coefficients of 0.40 and 0.30, respectively, and `AUTOMATIC_SINGLE_SURFACE` contact algorithm is described to consider the possible self-contacting surfaces. The viscous form hourglass type, Flanagan–Belytschko viscous form [73] with exact volume integration, is implemented to avoid nonphysical deformation modes, and as an exception, the stiffness-based hourglass type, Flanagan–Belytschko stiffness form [73] with exact volume integration, is utilized for only the partial penetration velocities of the metal-rich and linear FGM plates, since it gives more satisfactory results. The FGM plates are clamped by the rigid annular rings in analogy to the experiments, and the plates are subjected to the FSP impact from their ceramic-rich face with the measured impact velocities.

The strain-rate dependent elastoplastic behavior of the projectile (FSP) is also considered in the numerical model. MAT_PIECEWISE_LINEAR_PLASTICITY (MAT_024) constitutive model is implemented to describe the material plasticity, and the quasi-static and high strain-rate compressive behavior given in Fig. 6.18 is utilized to consider the rate-dependent behavior of the projectile. A failure or erosion criteria is not defined, since the projectile undergoes only elastoplastic deformation during the impact tests as seen from the experimental results. The mechanical properties of the projectile are given in Table 6.4.

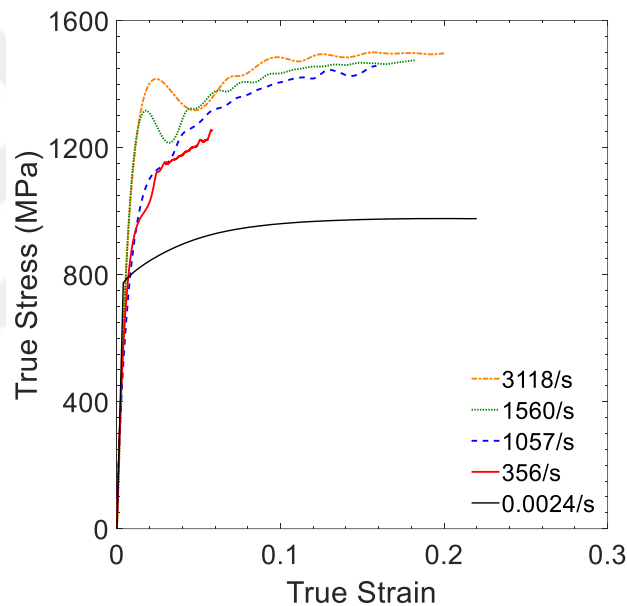


Figure 6.18. The quasi-static and high strain-rate true compressive stress-strain curve of the FSP (Steel 4340).

Table 6.4. The material properties of the FSP (Steel 4340).

Material parameters	FSP
Density (kg/m ³)	7850
Elastic modulus (GPa)	200
Poisson's ratio	0.29
Yield strength (MPa)	745

6.4.1. Constitutive Modeling of the Al6061 Plate

According to the low-velocity impact test results, it is observed that the Al6061 plate and the FGM plates except for the ceramic-rich composition exhibit only elastoplastic deformation. Namely, there is no ductile damage in these plates under the low-velocity impact. Therefore, the MAT_024 constitutive model is utilized without failure option to define the elastoplastic behavior of these plates. On the other hand, the Al6061 and FGM plates are partially or completely penetrated by the projectile under the high-velocity impact, and ductile, brittle, or the combination of ductile and brittle damage are observed in these plates according to their mechanical behavior. Thus, different constitutive models should be utilized for the plates under the high-velocity impact, and firstly, the constitutive modeling procedure is presented for the Al6061 plate which is the matrix material of the FGM plates.

Unlike using the MAT_024 constitutive model in the low-velocity impact modeling, MAT_PLASTICITY_WITH_DAMAGE (MAT_081) constitutive model is utilized to describe the material plasticity for the Al6061 plate under the high-velocity impact. This constitutive model is capable of modeling effective plastic strain-based ductile damage prior to the rupture. The damage is included by a nonlinear damage curve considering the damage softening behavior of material given in Fig. 3.4 (Chapter 3). Namely, the MAT_081 is selected instead of the MAT_024, since the MAT_024 does not have damage softening option and has an abrupt failure criterion based on an effective plastic failure strain. The compatible form of the stress-strain curve of the Al6061 plate for the MAT_081 constitutive model is given in Fig. 6.19. A second order nonlinear damage curve from the beginning of the softening behavior ($EPPF = 1.06$, effective plastic strain at which damage softening begins) to the rupture ($EPPFR = 2.50$, effective plastic strain at which material ruptures) is described with respect to Fig. 6.19. The

defined nonlinear damage curve for the Al6061 plate is applicable for the layers with the SiC fraction from 1% to 40%, since the mathematical difference between the failure and rupture strains is almost same for these layers.

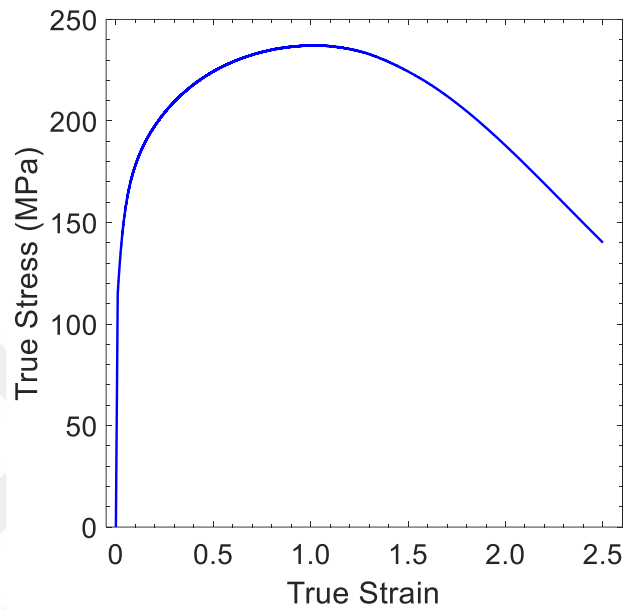


Figure 6.19. The damage included true stress-strain curve of the Al6061 plate.

6.4.2. Constitutive Modeling of the Metal-Rich FGM Plate

For the constitutive modeling of the metal-rich FGM plate, two different constitutive models given in Fig. 6.20 are employed in the numerical model. As seen from Fig. 6.20, the intermediate layers have low ceramic content, and these layers have ductile behavior and show elastoplastic deformation and damage under the high-velocity impact. Therefore, the MAT_081 constitutive model is used for these layers as is the case with the pure Al6061 layer. On the other hand, MAT_ISOTROPIC_ELASTIC_FAILURE (MAT_013) model, is implemented for the 70%SiC layer, since it exhibits an elastic brittle failure (the spall failure) under the high-velocity impact, and the stress-strain behavior of this layer is almost linear and shows very low ductility. The material parameters of the 70%SiC layer that is obtained from the quasi-static stress-strain curve are given in Table 6.5.

Here, the plastic failure strain is defined for the element erosion and is not the strain at the maximum stress. Considering the post-failure response of the layer to the rupture, the plastic failure strain is determined regarding to the compatibility between the experiments and numerical model results.

70%SiC	MAT_ISOTROPIC_ELASTIC_FAILURE (MAT_013)
12%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
8%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
6%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
4%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
2%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
1%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
0%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)

Figure 6.20. The implemented constitutive models for the metal-rich FGM plate.

Table 6.5. The material parameters of the 70%SiC layer.

Material parameters	70%SiC
Density (kg/m ³)	2440
Shear modulus (GPa)	7.0
Yield strength (MPa)	142.1
Tangent modulus (GPa)	0.534
Bulk modulus (GPa)	10.0
Plastic failure (erosion) strain	0.075
Failure pressure	155.8

6.4.3. Constitutive Modeling of the Linear FGM Plate

For the constitutive modeling of the linear FGM plate, three different constitutive models given in Fig. 6.21 are implemented in the numerical model. As seen from Fig. 6.21, the intermediate layers have a transition from low to high ceramic content, and according to the ceramic fraction, these layers have also a transition from ductile to quasi-brittle behavior. It is indicated in Chapter 4 that the layers

from 0%SiC to 40%SiC have ductile behavior, and the layers that have the SiC fraction of 50% and over have quasi-brittle behavior. On the other hand, these quasi-brittle layers show different response with respect to the behaviors of their adjacent layers and the general deformation and damage state of the plate under the high-velocity impact. For instance, the 55%SiC layer in the ceramic-rich FGM plate shows quasi-brittle response at higher velocities, whereas the 50%SiC and 60%SiC layers in the linear FGM plate do not exhibit quasi-brittle behavior. Therefore, the material behavior of the 50%SiC and 60%SiC layers of the linear FGM plate are described by a plasticity model, MAT_PLASTIC_KINEMATIC (MAT_003) constitutive model. The MAT_003 model is selected instead of the MAT_081 model, since it is difficult to determine the yield strength of these layers at high strain-rates, and these layers do not show certain plasticity at high strain-rates (Fig. 6.22). Therefore, the material behavior of these layers is defined using the bilinear plasticity model.

70%SiC	MAT_ISOTROPIC_ELASTIC_FAILURE (MAT_013)
60%SiC	MAT_PLASTIC_KINEMATIC (MAT_003)
50%SiC	MAT_PLASTIC_KINEMATIC (MAT_003)
40%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
30%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
20%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
10%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)
0%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)

Figure 6.21. The implemented constitutive models for the linear FGM plate.

The MAT_003 model allows to consider the strain-rate effects using the Cowper–Symonds model. For the 50%SiC and 60%SiC layers, the Cowper–Symonds parameters, C and p , are determined using the Cowper–Symonds relation and the compressive strength of the layers from the quasi-static and dynamic compressive stress-strain curves (Fig. 6.22) to approximately predict the strain-

rate-dependent behavior of the layers. Here, the dynamic compression tests of the 50%SiC layer are conducted by Maraging C350 steel bars differently from the other layers due to the stress levels of the 50%SiC layer. The determined Cowper–Symonds parameters and other material properties of the 50%SiC and 60%SiC layers are listed in Table 6.6. As is the case with the 70%SiC layer, the plastic failure strain of the 50%SiC and 60%SiC layers is determined according to the compatibility between the experiments and numerical model results considering

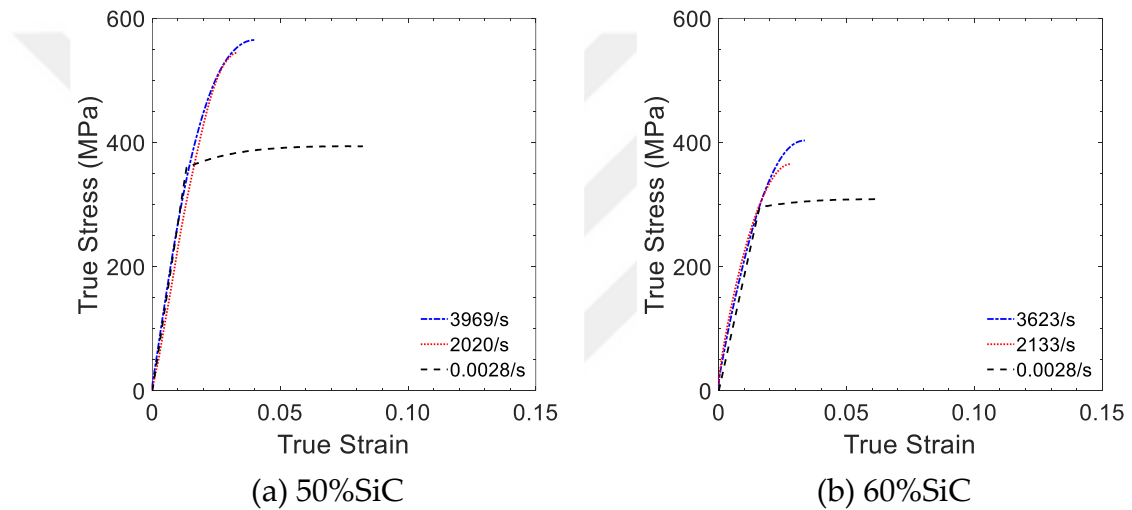


Figure 6.22. The quasi-static and dynamic true stress-strain curves of the 50%SiC and 60%SiC layers.

Table 6.6. The material parameters of the 50%SiC and 60%SiC layers.

Material parameters	50%SiC	60%SiC
Density (kg/m^3)	2920	2720
Elastic modulus (GPa)	27.0	18.6
Poisson's ratio	0.250	0.234
Yield strength (MPa)	330.6	280.7
Tangent modulus (GPa)	0.910	0.610
Hardening parameter	0	0
C (s^{-1})	302071	12283
p	5.190	1.031
Plastic failure (erosion) strain	0.40	0.35

the post-failure response of the layers to the rupture. For the constitutive modeling of the layers from 0%SiC to 40%SiC, the same modeling procedure is employed as is the case with the Al6061 plate. Namely, these layers are modeled using the MAT_081 constitutive model. Furthermore, the 70%SiC layer is modeled by the MAT_013 constitutive model.

6.4.4. Constitutive Modeling of the Ceramic-Rich FGM Plate

The constitutive modeling of the ceramic-rich FGM plate for the high-velocity impact is quite similar as is the case with the low-velocity impact modeling, there are only a few differences. First, the MAT_081 model is utilized instead of the MAT_024 model for the 0%SiC layer (Fig. 6.23). Additionally, the LOCWID and the erosion strains (the maximum principal strains) are modified in the MAT_072R3 model since the implemented values of these parameters in the low-velocity impact modeling cause some computational instabilities such as negative volume or complex sound speed in solid elements. Therefore, these parameters are numerically calibrated performing successive analyses, and it is determined that LOCWID = 16 mm (Default generated value is 25.4 mm in the MAT_072R3 model.) and the maximum principal strains of 0.50, 0.47, and 0.46 are the optimum values for the 55%SiC, 68%SiC, and 70%SiC layers, respectively.

70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
70%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
68%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
55%SiC	MAT_CONCRETE_DAMAGE_REL3 (MAT_072R3)
0%SiC	MAT_PLASTICITY_WITH_DAMAGE (MAT_081)

Figure 6.23. The implemented constitutive models for the ceramic-rich FGM plate.

Strain-rate effects can be also automatically considered in the MAT_072R3 model through the dynamic increase factor (DIF) that is internally defined in the constitutive model. The DIF modifies the failure surface to reflect the changes in the strength and is defined in the model separately for both compression and tension states.

The DIF for the compression is determined as [74]

$$r_{fc} = \begin{cases} \left(\frac{\dot{\epsilon}}{\dot{\epsilon}_s}\right)^{1.026\alpha}, & \dot{\epsilon} \leq 10^6 \text{ s}^{-1} \\ \gamma \left(\frac{\dot{\epsilon}}{\dot{\epsilon}_s}\right)^{1/3}, & \dot{\epsilon} > 10^6 \text{ s}^{-1} \end{cases} \quad 6.1$$

$$\gamma = 10^{6.156\alpha-2} \quad 6.2$$

$$\alpha = \frac{1}{5 + 0.9f'_c} \quad 6.3$$

The DIF for the tension is calculated as [75,76]

$$r_{ft} = \begin{cases} \left(\frac{\dot{\epsilon}}{\dot{\epsilon}_s}\right)^{\delta}, & \dot{\epsilon} \leq 1.0 \text{ s}^{-1} \\ \beta \left(\frac{\dot{\epsilon}}{\dot{\epsilon}_s}\right)^{1/3}, & \dot{\epsilon} > 1.0 \text{ s}^{-1} \end{cases} \quad 6.4$$

$$\beta = 10^{6\delta-2} \quad 6.5$$

$$\delta = \frac{1}{1 + 0.8f'_c} \quad 6.6$$

where $\dot{\epsilon}$ is the strain-rate, $\dot{\epsilon}_s$ is the reference or quasi-static strain-rate ($3 \times 10^{-5} \text{ s}^{-1}$ for compression and 10^{-6} s^{-1} for tension), and f'_c is the unconfined compressive strength (in MPa).

6.5. Numerical Results

The numerical results are given comparatively with the experimental results in terms of the deformation and damage state of the plates and the depth of penetration and residual velocity of the projectile.

The comparative experimental and numerical cross-sectional views of the Al6061 plate and the metal-rich FGM plate are given in Figs. 6.24 and 6.25, and the comparative depth of penetration and residual velocity of the projectile are listed in Tables 6.7 and 6.8, respectively. As seen from Figs. 6.24-6.25 and Tables 6.7-6.8, the developed numerical model shows a very good agreement with the experimental results for the Al6061 plate and the metal-rich FGM plate in terms of the deformation and damage state of the plates and the residual velocity of the projectile. For the Al6061 plate, the numerical model successfully captures and reflects the high-velocity impact response. Namely, the numerical model shows the frontal petaling failure and the bulging deformation at the rear face for the partial penetration velocities (Figs. 6.24-a and 6.24-b), and the frontal and rearward petaling and the ductile hole formation through the plate thickness occur for the complete penetration velocities (Figs. 6.24-c and 6.24-d) in analogy to the experiments. Furthermore, the numerical model also successfully predicts the high-velocity impact response of the metal-rich FGM plate exhibiting a similar trend for the deformation and damage profiles. The general deformation and damage state of the Al6061 plate and the metal-rich FGM plate is also similar exhibiting ductile behavior except for the frontal spalling failure of the metal-rich FGM plate. As seen from Fig. 6.25, a part of material removal due to the spall failure from the front side of the metal-rich FGM plate is observed in the numerical model results. For a more explicit comparison, the comparative front face views of the metal-rich FGM plate are given in Fig. 6.26. It is observed from Fig. 6.26 that the numerical model also predicts the frontal spalling failure with

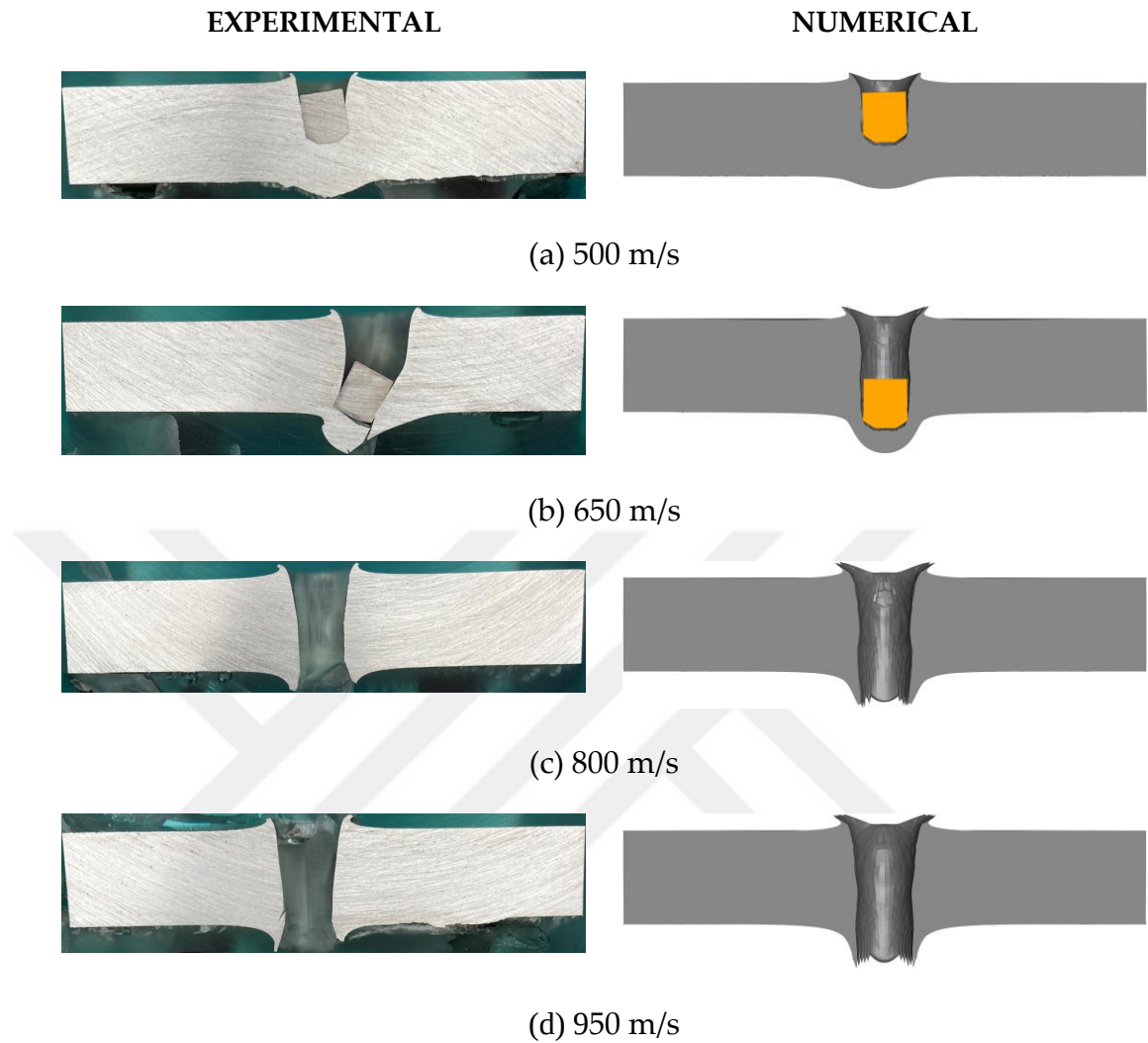


Figure 6.24. The experimental and numerical cross-sectional deformation and damage views of the Al6061 plate.

Table 6.7. The experimental and numerical DOP measurements and residual velocity of the projectile for the Al6061 plate.

	Status	Exp.	Num.	Diff. (%)
500 m/s	DOP (mm)	10.6	10.7	0.94
650 m/s	DOP (mm)	18.8	19.1	1.60
800 m/s	V_{residual} (m/s)	308.3	284.7	7.65
950 m/s	V_{residual} (m/s)	419.8	426.3	1.55

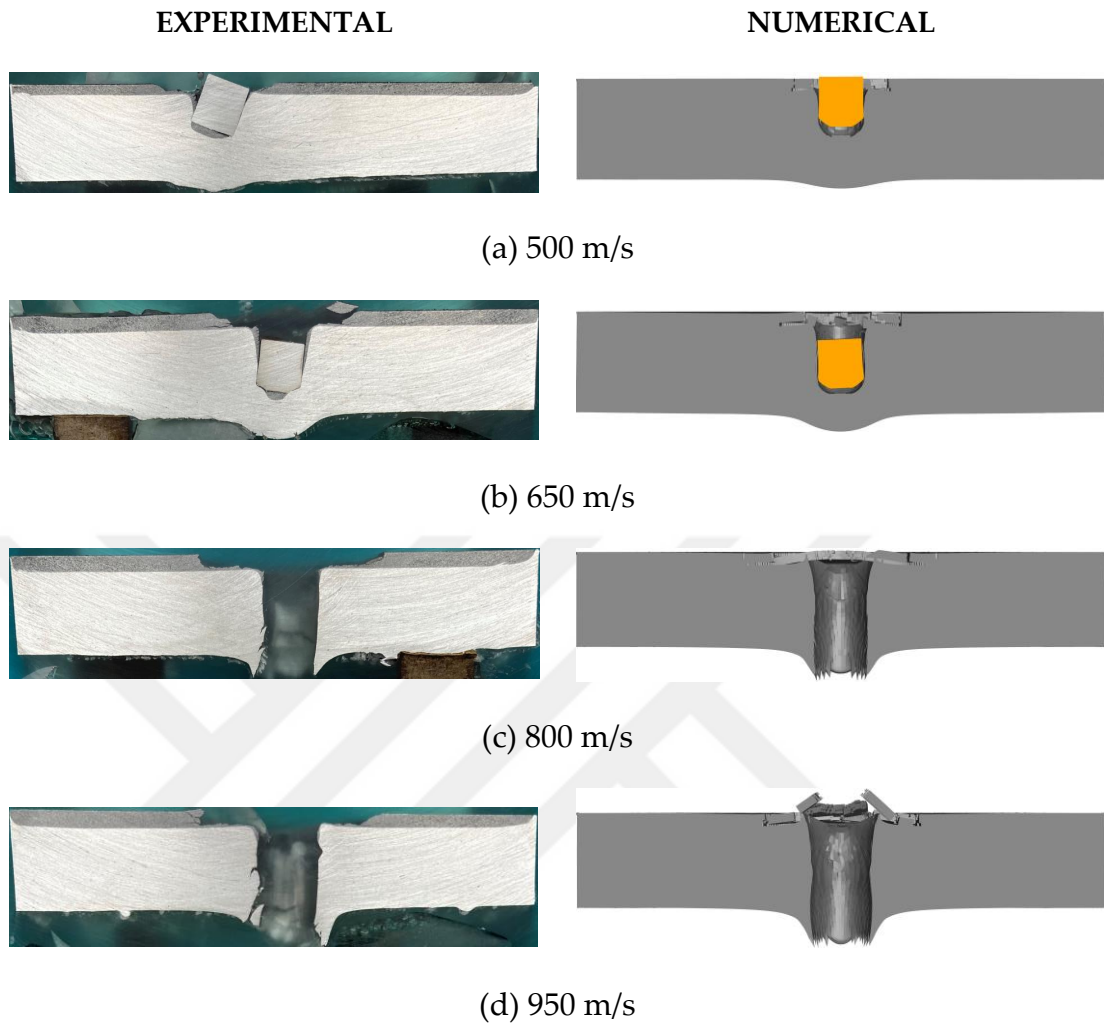


Figure 6.25. The experimental and numerical cross-sectional deformation and damage views of the metal-rich FGM plate.

Table 6.8. The experimental and numerical DOP measurements and residual velocity of the projectile for the metal-rich FGM plate.

	Status	Exp.	Num.	Diff. (%)
500 m/s	DOP (mm)	9.1	9.6	5.49
650 m/s	DOP (mm)	13.7	13.8	0.73
800 m/s	V_{residual} (m/s)	165.4	163.1	1.39
950 m/s	V_{residual} (m/s)	322.5	320.6	0.59

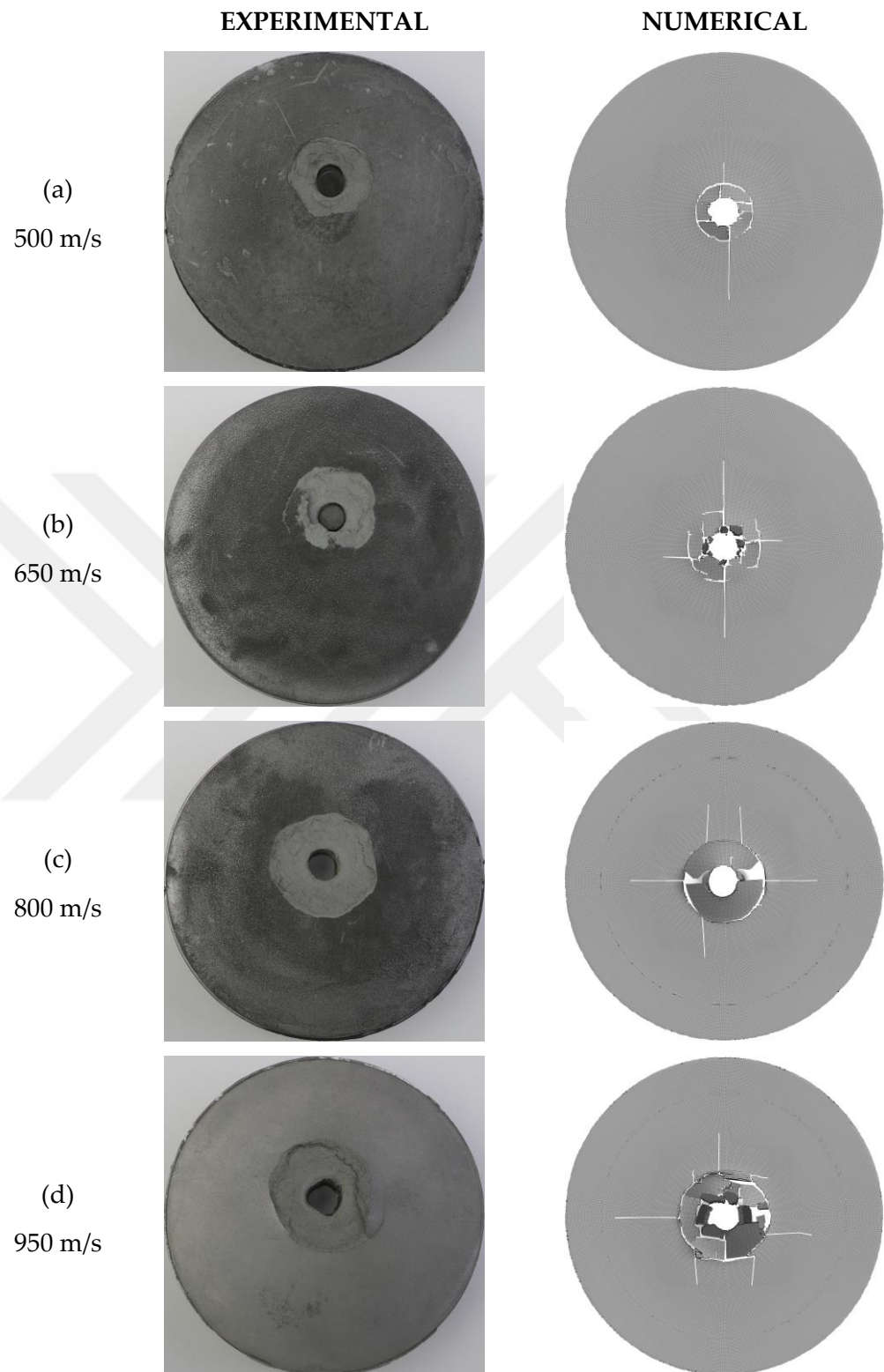


Figure 6.26. The experimental and numerical frontal spall failure of the metal-rich FGM plate.

a good agreement with the experiments. The frontal spalling failure is captured by implementing the isotropic elastic failure modeling by the MAT_013 constitutive model for the 70%SiC layer. It is mentioned in the previous sections that the 70%SiC layer has quasi-linear stress-strain response, and it also shows quasi-brittle or brittle behavior under impact loads. Therefore, implementing the isotropic elastic failure modeling provides a compatible frontal failure mode for the metal-rich FGM plate. In analogy to the experiments, the size of the frontal spall failure increases with increasing impact velocity in the numerical simulations. Differently from the experiments, the numerical model exhibits bidirectional radial cracks in addition to the spall failure. Considering the DOP and residual velocity of the projectile for the Al6061 plate and the metal-rich FGM plate (Tables 6.7 and 6.8), the numerical model gives very close results with the experiments, and the difference between the experimental and numerical results is below 8% for the Al6061 plate and below 6% for the metal-rich FGM plate.

Fig. 6.27 and Table 6.9 respectively show the comparative experimental and numerical cross-sectional views and the comparative depth of penetration and residual velocity of the projectile for the linear FGM plate. It is seen from Fig. 6.27 and Table 6.9 that the proposed numerical model has a limitation for predicting the high-velocity impact response of the linear FGM plate. Namely, it shows compatible response with the experiments for the impact velocities of 500 m/s and 650 m/s, whereas the difference between the experimental and numerical results begins to significantly increase above the impact velocity of 650 m/s. Although the difference between the experimental and numerical DOP results is 17.1% for the impact velocity of 800 m/s, the numerical model does not reflect the bulging effect that is observed in the experiment. Additionally, the plugging failure initiates in the experiment, however the numerical model also does not capture this failure initiation. A similar state is also observed for the impact velocity of 950 m/s. Namely, complete penetration occurs in the experiment,

whilst partial penetration is observed in the numerical analysis. The reason of these discrepancies is related to the constitutive modeling of the 50%SiC and 60%SiC layers. Namely, the Cowper–Symonds parameters, C and p , in the MAT_003 constitutive model are used to consider the strain-rate effects for these layers, and it successfully reflects the strain-rate hardening effect for the impact velocities of 500 m/s and 650 m/s. To show this state, a numerical analysis without the strain-rate parameters of the Cowper–Symonds model is performed for the impact velocity of 500 m/s, and the results are given in Fig. 6.28. As seen from Fig. 6.28, the rate-independent model (only for the 50%SiC and 60%SiC layers) gives higher depth of penetration compared to the rate-dependent model and experiment, and this result shows the strain-rate dependency of the problem. For the impact velocities of 800 m/s and 950 m/s, the difference between the experimental and numerical results can be explained by the adiabatic shear conditions, since the linear FGM plate may be sensitive to adiabatic shear failure. In this failure type, the rate of material softening due to the temperature increase can be major than the rate of strain-rate hardening especially for relatively high strain-rates, and this causes instabilities in material microstructure and subsequently a considerable decrement in material strength, especially in shear strength [72]. Therefore, the plugging failure occurs in material. On the other hand, the present numerical model is not capable of modeling the temperature-sensitive plasticity that exists in the Johnson–Cook plasticity model [28], and material softening due to adiabatic temperature increase does not occur in the plate. Therefore, the linear FGM plate shows higher impact resistance in the numerical model than in the experiment for the impact velocities of 800 m/s and 950 m/s. In addition to the above-mentioned states, the numerical model successfully captures the delamination failure of the 70%SiC layer without an interface model by elastic failure of the elements adjacent to the underneath layer due to the stress wave reflections.

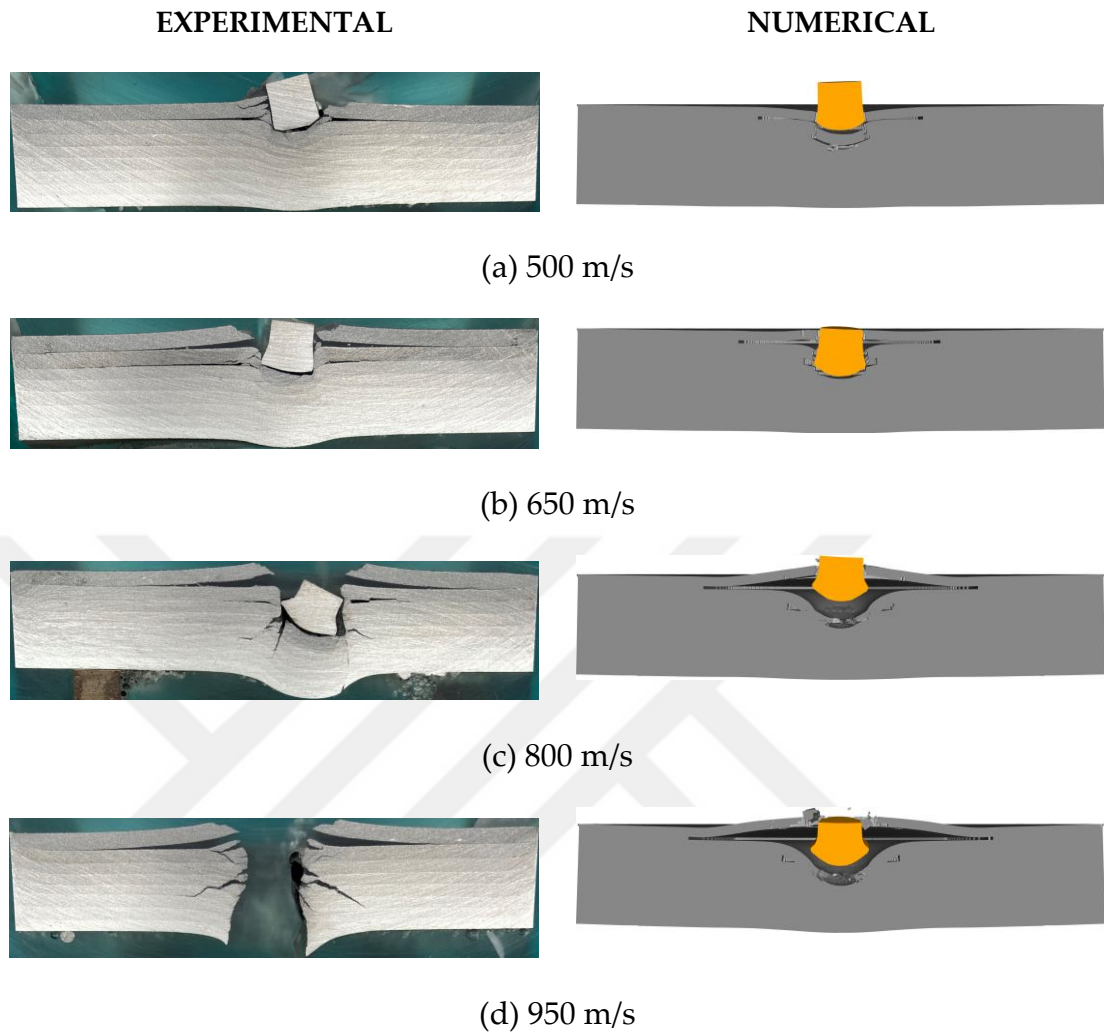


Figure 6.27. The experimental and numerical cross-sectional deformation and damage views of the linear FGM plate.

Table 6.9. The experimental and numerical DOP measurements and residual velocity of the projectile for the linear FGM plate.

	Status	Exp.	Num.	Diff. (%)
500 m/s	DOP (mm)	5.0	5.1	2.0
650 m/s	DOP (mm)	7.5	8.4	12.0
800 m/s	DOP (mm)	11.1	9.2	17.1
950 m/s	V_{residual} (m/s)	53.4	-	-



Figure 6.28. The effect of strain-rate dependency of the 50%SiC and 60%SiC layers on the numerical results for the linear FGM plate (500 m/s).

For the ceramic-rich FGM plate, the comparative experimental and numerical cross-sectional views and residual projectile velocities are given respectively in Fig. 6.29 and Table 6.10. In general, there is a good agreement between the experimental and numerical results successfully capturing the comminution at the impact velocity of 500 m/s, the conoidal fracture at the impact velocities of 800 m/s and 950 m/s, and the residual projectile velocities. The only difference is observed at the impact velocity of 650 m/s. For this velocity, complete penetration is observed in the experiment, whereas the projectile is embedded in the plate in the numerical simulations. However, this is not a complete disagreement between the experiment and modeling. Namely, the projectile is blocked at the bottom of the plate, and the plate is about to be completely penetrated in the numerical analysis. On the other hand, the residual velocity of the projectile is about 53.8 m/s that is very close to not causing a complete penetration of the plate in the experiment. Therefore, the numerical model does not completely fail for this velocity. Additionally, a slight difference is observed in the failure mode of the Al6061 bottom layer for the complete penetration velocities. Herein, it should be noted that an additional failure/erosion criterion, an equivalent stress at failure as the maximum tensile or tensile failure stress, is defined for the Al6061 layer, since the failure of the Al6061 layer is dominated by the tensile failure rather than the plastic strain failure as seen from Figs. 6.16 and 6.29. Namely, complete fracture in a certain region of the Al6061 layer due to the circumferential and

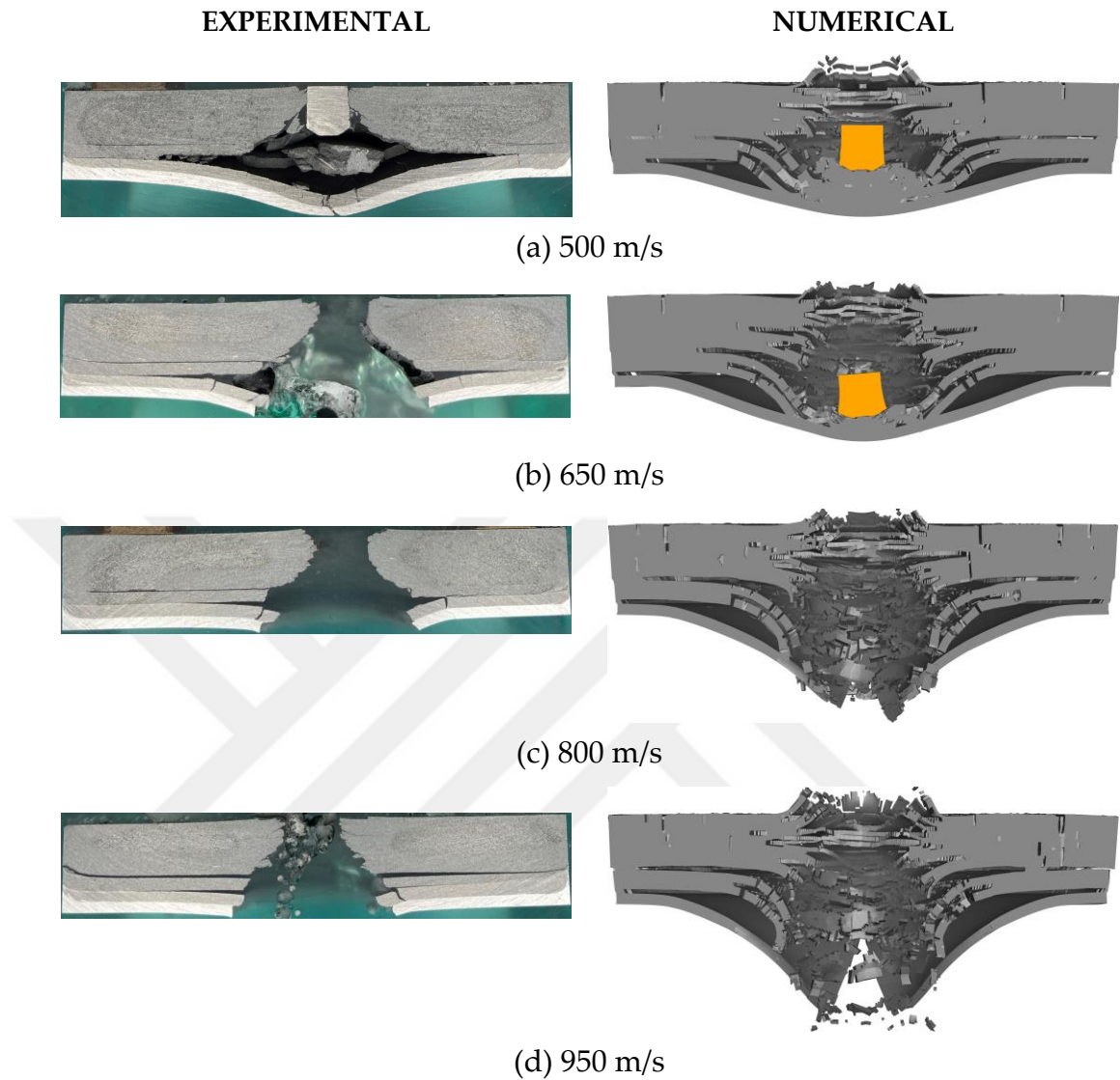


Figure 6.29. The experimental and numerical cross-sectional deformation and damage views of the ceramic-rich FGM plate.

Table 6.10. The experimental and numerical DOP measurements and residual velocity of the projectile for the ceramic-rich FGM plate.

	Status	Exp.	Num.	Diff. (%)
500 m/s	DOP (mm)	-	-	-
650 m/s	V_{residual} (m/s)	53.8	-	-
800 m/s	V_{residual} (m/s)	135.6	154.7	14.09
950 m/s	V_{residual} (m/s)	272.7	256.0	6.12

radial tensile cracks results in the experiments. Therefore, assuming that the stress-strain behavior of the Al6061 layer is the same in both tension and compression, the maximum compressive stress of the Al6061 layer from its quasi-static stress-strain curve, 237.3 MPa, is described as the tensile failure criterion to capture the failure mode of the Al6061 layer. As compared to the experiments, the radial tensile cracks are successfully captured, whilst the complete fracture in a certain region of the Al6061 layer does not occur. The reason of this state is given below with the comparison of the experimental and numerical sequential damage frames of the ceramic-rich FGM plate.

To show the compatibility of the numerical model results with the experiments in terms of the damage sequence, the comparative sequential damage frames for the Al6061 plate and the metal-rich and ceramic-rich FGM plates are given respectively in Figs. 6.30-6.32 for the impact velocity of 950 m/s. As seen from Figs. 6.30 and 6.31, the numerical model shows a good agreement with the experiments successfully reflecting the plugging failure for the Al6061 plate and the metal-rich FGM plate. As mentioned above, a slight difference between the numerical results and experiments of the ceramic-rich FGM plate is observed for the failure of the Al6061 bottom layer. As seen from Fig. 6.32, the numerical model successfully captures the tensile crack initiation and propagation in the Al6061 layer, whereas the complete fracture due to the circumferential tensile crack could not be modeled. The reason of this state can be explained that there is no separation between the Al6061 and 55%SiC layers in the experiments, and these layers are pushed down together by the forward momentum of the projectile and upper layers. This forward motion creates hinging in the Al6061 layer, and further movement causes higher tensile stresses than the tensile strength of the Al6061 layer at the circumferential boundary of the hinged region. On the other hand, separation between the Al6061 and 55%SiC layers occurs due to failure of the adjacent elements of the 55%SiC layer in the numerical analysis.

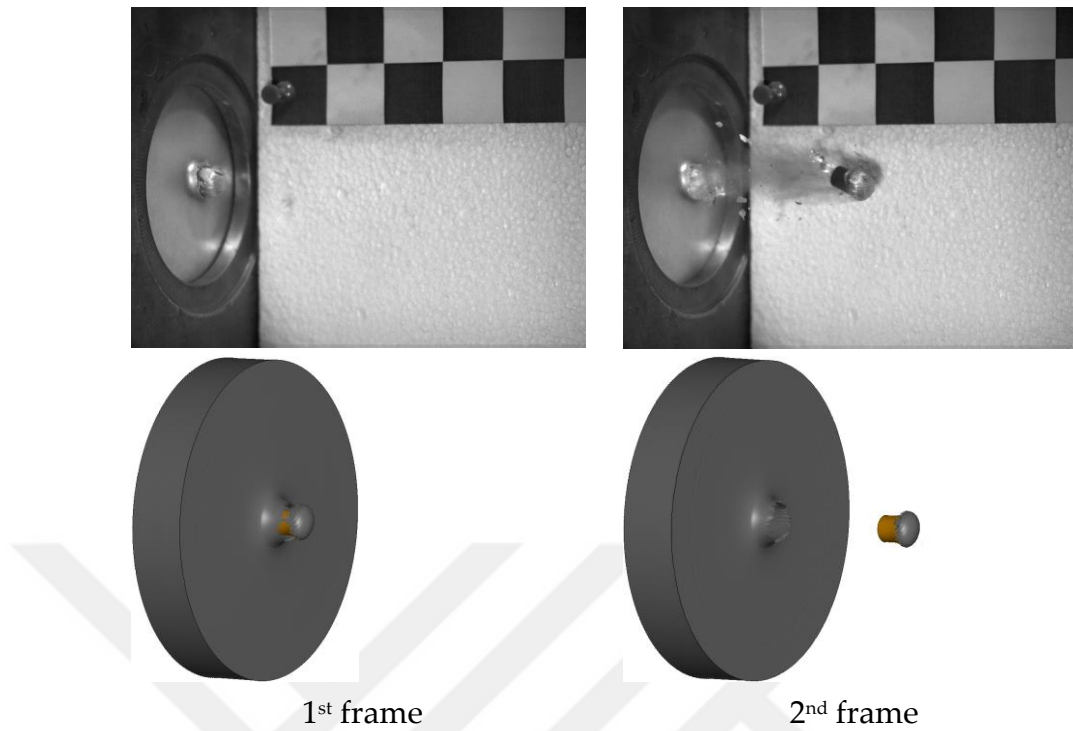


Figure 6.30. The experimental and numerical sequential damage frames of the Al6061 plate (950 m/s).

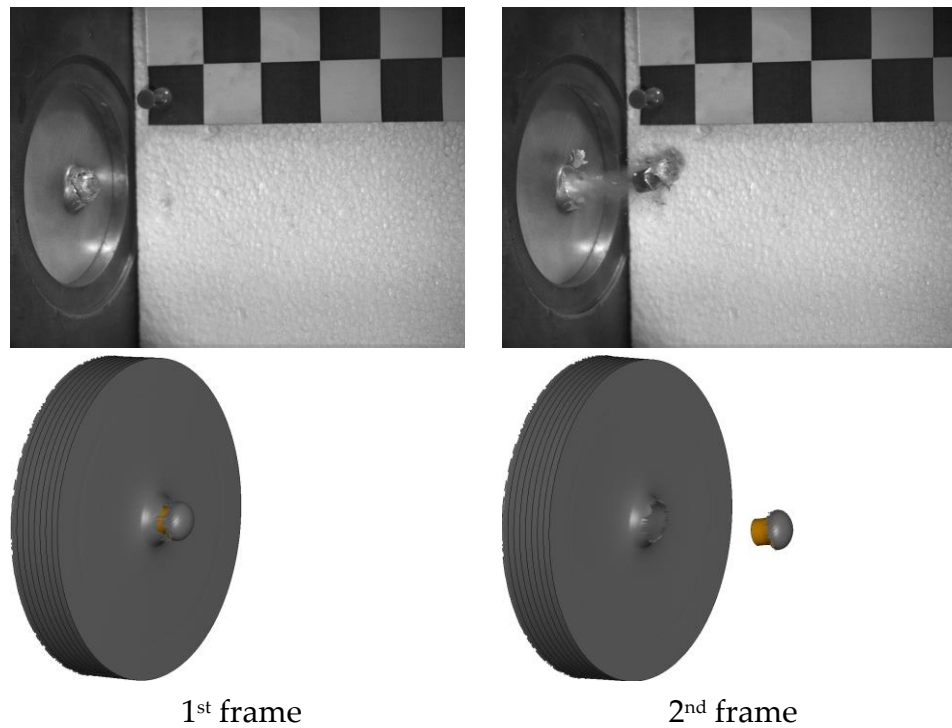


Figure 6.31. The experimental and numerical sequential damage frames of the metal-rich FGM plate (950 m/s).

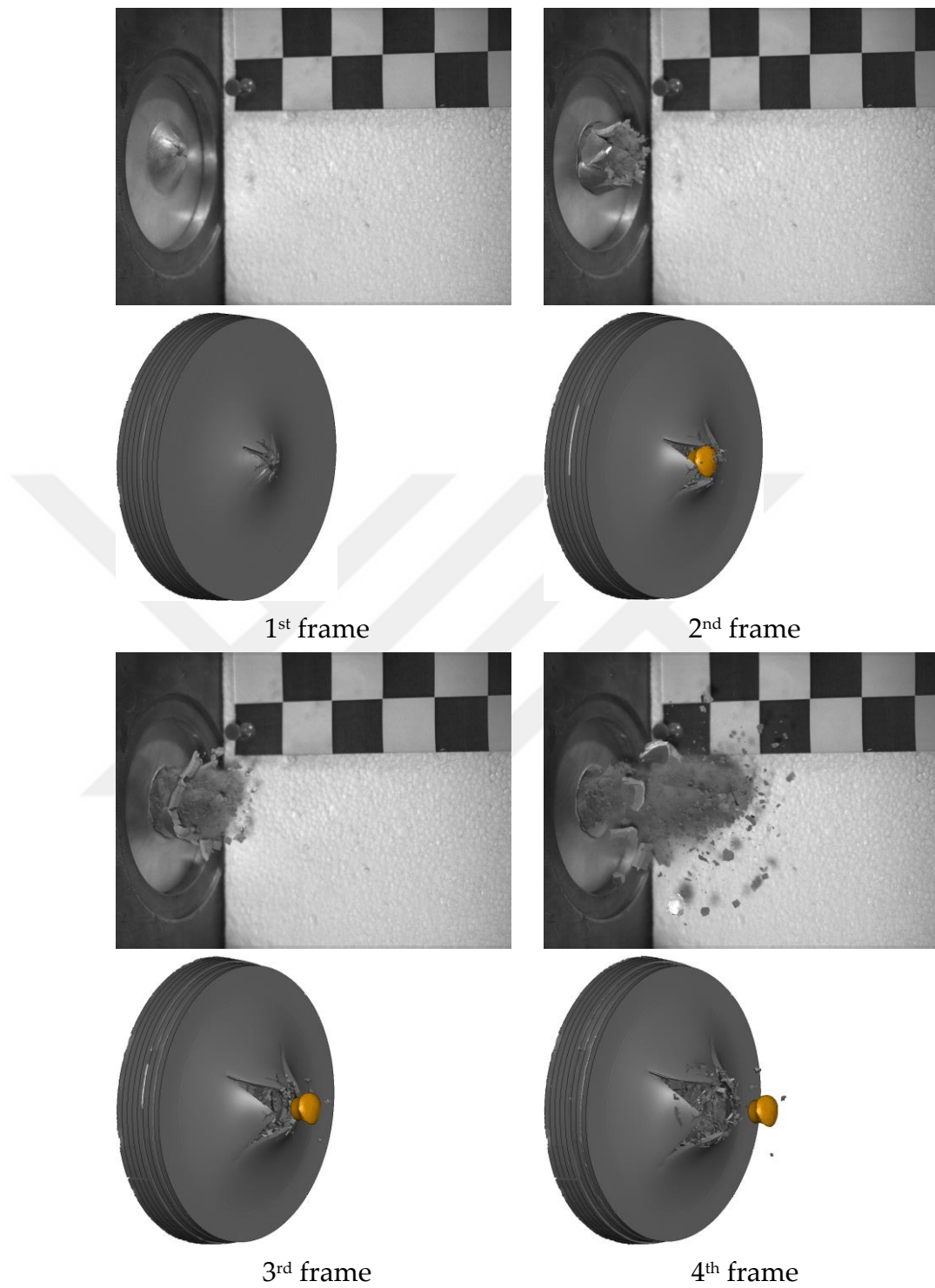


Figure 6.32. The experimental and numerical sequential damage frames of the ceramic-rich FGM plate (950 m/s).

This causes an independent deformation for the Al6061 layer from the upper layers. The projectile creates localized tensile stress at the rear face of the Al6061 layer, and the radial tensile cracks initiate and propagate by reason of the forward motion of the projectile. Therefore, a hinged region and accordingly circumferential tensile cracks do not result in the Al6061 layer.

Consequently, the present numerical models show a good agreement with the experiments in general. In Fig. 6.33, the numerical sequential damage frames of the FGM plates are illustrated for a general evaluation of the results and a clear comparison of the damage sequence. It can be concluded that the high-velocity impact resistance of the FGM plate increases with increasing ceramic fraction in the plate except for the ceramic-rich FGM plate. Thus, the highest impact resistant plate is the linear FGM plate having a lower depth of penetration and a lower residual projectile velocity. On the other hand, the impact resistance of the FGM plate exhibit a decrement as the plate composition varies from linear to ceramic-rich as is the case with the low-velocity impact. In the low-velocity impact, the contact force levels of the ceramic-rich FGM plate are even lower than the metal-rich FGM plate due to the strength weakening. In the high-velocity impact, the residual projectile velocities of the metal-rich FGM plate are higher than the ceramic-rich FGM plate for the impact velocities of 800 m/s and 950 m/s, even though the metal-rich FGM plate shows a better impact performance than the ceramic-rich FGM plate for the impact velocities of 500 m/s and 650 m/s. The reason of this result is related to the energy absorption mechanism of these plates. Namely, the projectile energy is absorbed by the combination of brittle failures (cracks and conoidal fracture), elastoplastic deformation, and delamination in the ceramic-rich FGM plate, whereas the metal-rich FGM plate absorbs the impact energy by mostly localized elastoplastic deformation and ductile damage. Therefore, the ceramic-rich FGM plate gives lower residual projectile velocities than the metal-rich FGM plate at the complete penetration velocities.

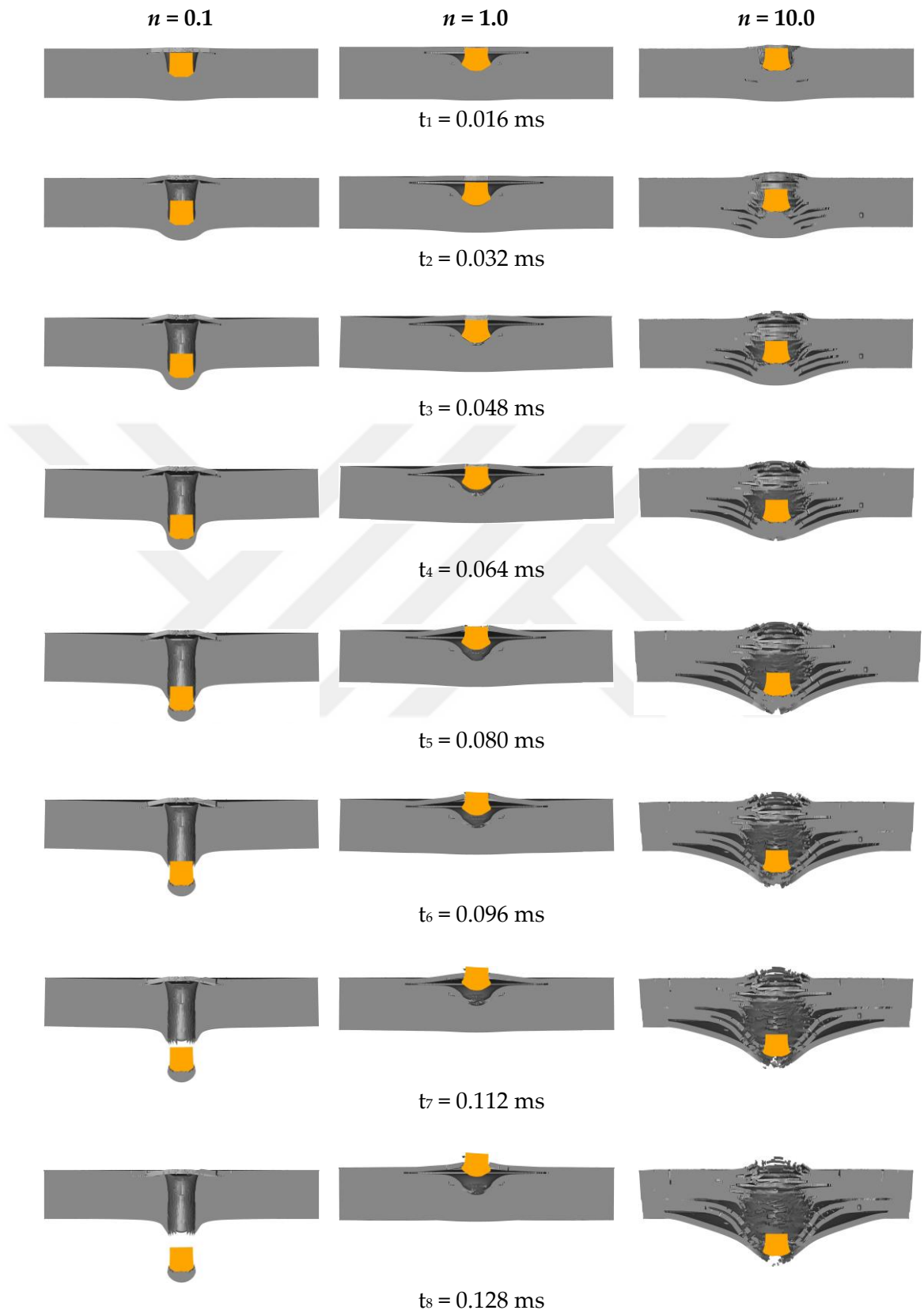


Figure 6.33. The temporal numerical damage views of the FGM plates (800 m/s).

CHAPTER 7

CONCLUSIONS

7.1. Introduction

In this study, experimental testing and numerical modeling studies are performed on the dynamic behavior of Al6061/SiC FGM plates subjected to low- and high-velocity impact loads. The main objective of this study is to develop realistic numerical models for predicting the impact response of the FGM plates. Drop-weight impact test method and a single-stage gas gun are used respectively for the low- and high-velocity impact experiments. Numerical modeling studies of the FGM plates are carried out by the explicit finite element code, LS-DYNA®. For the constitutive modeling of the FGM plates, quasi-static and dynamic mechanical characterization of the FGM layers is performed using the quasi-static compression test method and SHPB (Split-Hopkinson Pressure Bar) setup. An experimental-based layer-wise constitutive modeling procedure that considers the actual mechanical behavior of the FGM layers from the quasi-static and dynamic mechanical characterization is performed. Depending on both material composition of the FGM plates and impact velocity (low- or high-velocity impact), different constitutive modeling approaches including the effects of strain-rate, plasticity and plasticity damage, and brittle damage are proposed.

7.2. Conclusions

The main conclusions of this study can be indicated as follows:

- In the low-velocity impact, the metal-rich and linear FGM plates show elastoplastic deformation for all impact energies, whereas the ceramic-rich FGM plate undergoes both elastoplastic deformation and brittle failures. The ceramic-rich FGM plate exhibits a ductile-dominated behavior with no macroscopical failure or damage at lower impact velocities, while conoidal fracture and radial cracks initiates and propagates with increasing impact energy.
- In the high-velocity impact, the metal-rich and linear FGM plates exhibit ductile damage in general except for the frontal failure. At the front side of these plates, spalling failure due to the initial wave reflections is observed. The spallation effect decreases with varying the plate composition from metal-rich to linear depending on the mechanical impedance of the layers underneath the 70%SiC front layer. The ceramic-rich FGM plate shows brittle damage for all impact velocities. There are two different types of brittle damage in the ceramic-rich FGM plate through the plate thickness, conoidal fracture and comminution that are typical damage modes for brittle materials under impact loads.
- In the low-velocity impact, the impact resistance of the FGM plate increases with increasing total ceramic fraction in the plate exhibiting higher contact force, shorter contact duration, and lower permanent displacement except for the ceramic-rich FGM plate. As the plate composition varies from linear to ceramic-rich, these parameters show opposite variations and the impact resistance of the ceramic-rich FGM plate is even lower than the metal-rich FGM plate in general due to

strength weakening in the layers of the ceramic-rich FGM plate. This strength decrement is related to weak bond between the ceramic particles with decreasing volume fraction of the metal constituent. The absorbed energy decreases with increasing total ceramic fraction in the plate due to decrement in the plastic deformation capacity except for the ceramic-rich FGM plate. On the other hand, the ceramic-rich FGM plate is the most energy absorbing plate for all impact energies. This is due to differences in the energy absorption mechanisms depending on the mechanical behavior and the damage state of the plates. Namely, the impact energy is absorbed by mostly the plastic deformation for the metal-rich and linear FGM plates, whereas the plastic deformation, brittle failures (radial cracks and conoidal fracture), and pore collapse are effective on the energy absorption of the ceramic-rich FGM plate.

- In analogy to the low-velocity impact, the impact resistance of the FGM plate increases with increasing ceramic fraction in the plate except for the ceramic-rich composition in the high-velocity impact. Thus, the highest impact resistant plate is the linear FGM plate exhibiting a lower depth of penetration and a lower residual projectile velocity. On the other hand, the impact resistance of the FGM plate decreases as the plate composition varies from linear to ceramic-rich as is the case with the low-velocity impact. However, the residual velocities of the ceramic-rich FGM plate are lower than the metal-rich FGM plate for the impact velocities of 800 m/s and 950 m/s, even though the metal-rich FGM plate shows a better impact performance than the ceramic-rich FGM plate for the impact velocities of 500 m/s and 650 m/s. This state depends on the energy absorption mechanism of these plates. Namely, both brittle failures such as cracks and conoidal fracture and elastoplastic deformation are effective on the energy absorption mechanism of the ceramic-rich FGM plate, whereas the metal-

rich FGM plate absorbs the impact energy by mostly localized elastoplastic deformation and ductile damage.

- Determination of the actual elastic modulus is found to be highly important in terms of the accuracy of numerical results. Although the material is a known metallic alloy, the production method (powder metallurgy) significantly affects the elastic modulus. The actual elastic modulus of the FGM layers is determined by the quasi-static compression tests, and an unusual value for the elastic modulus of the sintered Al6061 plate which is the matrix material of the FGM plates is found as about 12 GPa, and this value is verified by performing numerical analysis.
- It is found that considering the quasi-static or rate-independent behavior of the plates gives satisfactory results, and therefore, the strain-rate effect is not important for the low-velocity impact analysis.
- In the low-velocity impact analysis, the rate-independent MAT_024-based constitutive modeling shows a good agreement with the experimental results for the metal-rich and linear FGM plates.
- The low-velocity impact analysis shows that the developed numerical models for the FGM plates present an excellent agreement with the experimental results compared to the TTO model, and the TTO model could give reasonable results for only the FGM plate with higher metal fraction.
- In the high-velocity impact analysis, excepting for the constitutive modeling of the 70%SiC front layer, the rate-dependent MAT_081-based constitutive modeling for the metal-rich FGM plate and the combination of MAT_003 (for the 50%SiC and 60%SiC layers) and MAT_081 constitutive models with the rate-dependent behavior for the linear FGM

plate give compatible results with the experiments except for the linear FGM plate under the impact velocities of 800 m/s and 950 m/s. Additionally, the isotropic elastic failure constitutive modeling (MAT_013) of the 70%SiC front layer successfully captures the frontal spalling failure of the metal-rich FGM plate.

- For the ceramic-rich FGM plate in case of both low- and high-velocity impact, a combined constitutive modeling approach is presented by elastoplastic material models and a concrete damage model. Namely, the constitutive behavior of the pure Al6061 layer is described by the MAT_024 model for the low-velocity impact analysis and by the MAT_081 model for the high-velocity impact analysis. On the other hand, the Karagozian & Case (K&C) Concrete Model - Release III (MAT_072R3) is proposed for the high ceramic content FGM layers (55%SiC, 68%SiC, and 70%SiC) to capture the quasi-brittle/brittle behavior of these layers. The proposed combined constitutive modeling approach of the ceramic-rich FGM plate significantly improves the numerical results exhibiting an excellent agreement with the low-velocity impact experiments in terms of contact force history, absorbed energy history, and force-displacement response of the plate compared to the pure MAT_024-based modeling approach. Besides, the K&C concrete damage-based model successfully predicts the high-velocity impact response of the ceramic-rich FGM plate in general capturing the brittle damage modes, the comminution and conoidal fracture, and residual projectile velocities.

7.3. Recommendations for Future Studies

In this study, the developed numerical models for the FGM plates show a good agreement with the experimental results in general for both low- and high-velocity impact. For the high-velocity impact model of the linear FGM plate, there is a disagreement between the experiments and model results at the impact velocities of 800 m/s and 950 m/s. This discrepancy can be explained by having a possible sensitivity of the linear FGM plate to adiabatic shear failure that occurs under very high localized temperature, shear deformation and strain-rate, and the developed numerical model for the linear FGM plate is not capable of modeling the temperature-sensitive plasticity that exists in the Johnson–Cook plasticity model. Therefore, material softening due to adiabatic temperature increase does not occur in the numerical model of the linear FGM plate. According to the above-mentioned statements, the first recommendation is to develop a temperature-sensitive plasticity model for the linear FGM plate using the Johnson–Cook dynamic plasticity model. Additional future studies with the validated realistic numerical models can be performed for blast loading and multiple impact performance of the Al6061/SiC FGM plates or various composite structures consisting of the FGM plates.

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FOREIGN LANGUAGE

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PUBLICATIONS

a. SCI, SCI-Expanded Indexes

1. **Arslan, K.**, Gunes, R., Apalak, M.K., Reddy, J.N., 2020. Evaluation of geometrically nonlinear and elastoplastic behavior of functionally graded plates under mechanical loading-unloading. **Mechanics of Advanced Materials and Structures**, (In Press).
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4. **Arslan, K.**, Gunes, R., 2018. Experimental damage evaluation of honeycomb sandwich structures with Al/B₄C FGM face plates under high velocity impact loads. **Composite Structures**, **202**: 304-312.
5. **Arslan, K.**, Gunes, R., Apalak, M.K., Reddy, J.N., 2019. Experimental tests and numerical modeling of ballistic impact on honeycomb sandwich structures reinforced by functionally graded plates. **Journal of Composite Materials**, **51** (29): 4009-4028.
6. Gunes, R., **Arslan, K.**, 2016. Development of numerical realistic model for predicting low-velocity impact response of aluminium honeycomb sandwich structures. **Journal of Sandwich Structures & Materials**, **18** (1): 95-112.

b. Other Indexes

1. **Arslan, K.,** Gunes, R., 2017. Ballistic impact simulation of ceramic/metal armor structures. **International Journal of Engineering Research and Development** 9 (3): 12-20.

c. International Conferences

1. **Arslan, K.,** Gunes, R., 2021. Evaluation of stress wave propagation in particle-reinforced metal matrix composites. *16th International Conference "Dynamical Systems – Theory and Applications"*, Łódź, Poland.
2. **Arslan, K.,** Gunes, R., 2021. Low-velocity impact response of metal-ceramic functionally graded plates: A novel numerical modelling approach. *16th International Conference "Dynamical Systems – Theory and Applications"*, Łódź, Poland.
3. **Arslan, K.,** Gunes, R., 2021. Computational modeling of strain-rate effects on the low-velocity impact response of sintered Al6061 plates. *2nd International Workshop on Plasticity, Damage and Fracture of Engineering Materials*, Ankara, Turkey.
4. **Arslan, K.,** Gunes, R., 2018. Effect of core orientation on low velocity impact response of honeycomb sandwich beams. *12th International Conference on Sandwich Structures*, Lausanne, Switzerland.
5. **Arslan, K.,** Gunes, R., 2017. Numerical simulations of functionally graded sandwich beams under low velocity impact loads. *20th International Conference on Composite Structures*, Paris, France.

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8. **Arslan, K.**, Gunes, R., Apalak, M.K., Reddy, J.N., 2016. Low-velocity impact performance of honeycomb sandwich structures reinforced by functionally graded face plates. *14th International Symposium on Functionally Graded Materials*, Bayreuth, Germany.
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