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RESPONSE OF COMPOSITE MATERIALS TO DYNAMIC
AND LOW TEMPERATURE ENVIRONMENTS

BY

JAMES M. LEBLANC

A THESIS SUBMITTED IN PARTIAL FULFILLMENT OF THE
REQUIREMENTS FOR THE DEGREE OF

MASTER OF SCIENCE

IN

CIVIL ENGINEERING

UNIVERSITY OF RHODE ISLAND

2019

MASTER OF SCIENCE THESIS
OF
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UNIVERSITY OF RHODE ISLAND
2019

ABSTRACT

This dissertation is prepared using the manuscript format.

The dynamic response of composite materials subjected to underwater and air shock loading conditions has been studied. Additionally, the effects of low temperatures on the mechanical and fracture properties of these materials has been evaluated. The primary contribution of the author is on the computational modeling aspects of each of the dynamic loading studies conducted. The experimental work presented has been completed by the author's collaborators from the University of Rhode Island. The low temperature effects on the materials is solely an experimental investigation and was undertaken in collaboration with researchers at the Naval Undersea Warfare Center. The objective of the project is to develop a better understanding of the response of composite materials when subjected to shock loading conditions leading to more efficiently designed structures. The work is divided into five phases as described in the following overview.

In the first phase of the research the near field underwater explosion response of E-Glass / Epoxy plates was investigated. The study also included the effects of elastomeric polyurea coatings on both the transient response and damage characteristics. The computational models developed in the study were shown to simulate the testing accurately, and using the Russell Error measure, demonstrate model correlation that can be described as excellent. The models are able to accurately simulate the detonation of the explosive charge and the resulting pressure fields and plate deflections.

The objective of the second phase of the project was to investigate the effects of material ageing on the response of Carbon-Epoxy laminates when subjected to air blast

loading. The shock loading was induced through the use of an air driven shock tube and the effects of seawater exposure were quantified in terms of transient response and material failure onset. Computational models of the experiments were developed through the use of the Ls-Dyna code for both fully clamped and simply supported edge conditions. The models were shown to accurately capture both the timing and displacement magnitudes of the specimens as well as the onset of material failure.

The third phase of the project investigates the response of composite cylinders when subjected to near field underwater explosive loading, including the effects of polyurea coatings. The objective is to determine the influence of both charge standoff and coating thickness on the transient response as well as damage mechanisms / evolution during loading. Experiments with corresponding simulations were performed with good agreement between the two in terms of pressure loads and damage extents. The simulations were further utilized to examine the internal and kinetic energy levels and distributions during loading as well as the surface strain characteristics.

The effects of material ageing on the response of flat plates subjected to near field explosive loading is the focus of the fourth part of the research. In this investigation, bi-axial Carbon/Epoxy laminates with and without long term seawater immersion effects were subject to the explosive loading to determine the influence of material degradation on the panel response. A fully coupled Eulerian-Lagrangian computational approach was utilized for the modeling of the corresponding experiments. The simulations were used to demonstrate an increase of maximum strains with ageing as well as the characteristics of stress wave propagation as a function of laminate architecture.

The final aspect of the research presented is aimed at investigating the influence of temperature on the mechanical and fracture performance of composite laminates. The focus is on the low temperatures associated with seawater in the arctic regions and deep depths of the oceans. Mechanical characterization is in the form of tensile, compression, and short beam shear and fracture is evaluated in terms of Mode-I failure. The results show that for both E-Glass and Carbon Epoxy materials there is an influence of temperature on both mechanical and fracture performance of the material.

ACKNOWLEDGEMENTS

The completion of this degree has caused me to look back through my academic career at URI that started when I first enrolled as an undergraduate student in 1999 to the present. It seems impossible to imagine the experiences and opportunities I have been afforded through my relationship with the university. At the beginning I had my sights set simply on graduating in four years in preparation for the beginning of my career. Little did I know then, that I would be actively seeking opportunities to remain engaged with the university after 20 years, and that to a large extent research and academics would be the foundation of that career. Looking back, it is difficult to recall all of the professors from whom I have taken courses and all the staff that helped me get to where I am today. The number is no doubt larger than expected, and for this I am eternally grateful.

First and foremost, I would like to thank my advisor Dr. George Tsiatas for his guidance, assistance, and patience throughout the duration of this degree. Additionally, I would thank Dr. Shukla and Dr. Das for serving as members of my thesis committee.

I would like to thank my parents Ray and Holly, my siblings Chris, Jennifer, and Allison, and my grandparents for their understanding nature, and support through this degree program and all of my academic pursuits. Most importantly, I thank my children Sam and Sydney for keeping me calibrated and placing everything in perspective. Seeing them explore the scientific world as they enter grade school has been enlightening and I look forward to seeing them grow as they move forward. Revisiting the basic childhood experiences of planting lima beans and flipping over rocks in search of bugs has to no small extent re-grounded my appreciation of the sciences.

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*For all those professors whom I have had the privilege of taking courses from,
conducting collaborative research with, and received academic and life guidance from.*

***“TELL ME AND I FORGET, TEACH ME AND I MAY REMEMBER, INVOLVE
ME AND I LEARN.”***

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CHAPTER 1

INTRODUCTION AND LITERATURE REVIEW

Within the marine community there is growing interest in constructing new vehicles and structures from composite materials. The advantages of these advanced material systems include high strength to weight ratios, lighter structural components, and overall reduced maintenance costs. However, when structures manufactured from these materials are employed in certain applications they must also be designed in a manner such that they will be able to survive dynamic loading events. The static response of composite materials is well understood while there is less of an understanding in terms of what happens to the same composite material when subjected to high loading rates. This typically results in composite structures being conservatively designed with large safety factors. Due to these large safety factors the structures are often over designed, thus not fully utilizing the high strength to weight ratio afforded.

Studies on composite materials subjected to high loading rates have utilized both experimental and computational techniques. Work by Latourte et al utilized a scaled fluid structure method to study the failure modes and damage mechanisms in both monolithic and sandwich plates subjected to underwater impulsive loads. Schiffer and Tagarelli have compared the response of glass and carbon reinforced composites and found that the glass reinforced plates had larger blast resistance than the carbon plates due to their higher tensile ductility. Avachat and Zhou studied the effects of underwater shock loading on filament wound and sandwich composite cylinder and found that while both constructions exhibited similar damage mechanisms, including delamination, fiber failure and matrix cracking, the sandwich structure had overall better performance than a

monolithic cylinder with similar mass. The same authors also utilized an Underwater Shock Loading Simulator combined with digital image correlation to show that for sandwich constructions lower density cores yield higher blast performance than high density cores due to their larger core compression capability. LeBlanc and Shukla have studied the response of flat and curved composite plates to far field underwater explosive loading through experimental and computational methods. Franz et al. and Mouritz et al. studied the effects of an underwater explosion at different standoff distances on a glass composite laminate. Dear and Brown have conducted a detailed study on the damage mechanisms and energy absorption in composite plates when subjected to impact loading.

In recent years, the use of polyurea materials to enhance the failure resistance of structures subjected to explosive loading has become a topic of interest. Polyurea is a synthetic, high strength / high elongation coating that is typically spray cast onto existing structures to increase their performance under shock and ballistic loading events such as those of a bomb blast. Research efforts have recently studied the effectiveness of polyurea when used with composite materials. LeBlanc et al. showed that the transient response of UNDEX loaded composite plates is dependent upon both coating thickness as well as location. Tekalur et al investigated the response of E-Glass composites coated with polyurea subjected to air blast loading. This study indicated that the polyurea coating reduced the transient deflections and post mortem damage levels as compared to the uncoated material. Gardner et al studied the effect of location of the polyurea in relation to the foam core in sandwich composites. It was observed that when a layer of polyurea is placed between the foam core and the back-face of the sandwich the blast resistance is improved, while conversely if the polyurea is placed between the front face

and the foam core the performance is degraded. Furthermore, effects of polyurea coatings have been studied through the use of computational simulations. Amirkhizi et al have developed a visco-elastic constitutive material model that describes the behavior of polyurea materials under a broad range of strain rates, and includes pressure and temperature effects. Amini et al used LS-DYNA to simulate impact / impulsive loading experiments of polyurea coated steel plates.

The effects of low temperatures on the quasi-static properties of a variety of composite systems have been studied and reported in the literature. Particular interest was shown during the 1980's and 1990's in composite properties at cryogenic temperatures (approx.. -240°F and below) for the replacement of heavy, metallic cryogenic tanks with lighter weight composites. Nettles and Biss studied the quasi-static tensile (fiber dominated) and shear (matrix dominated) properties of a carbon/epoxy composite. They found that with decreasing temperature the response in the matrix was significantly affected. Material tested at low temperature became more brittle, and much less tough. Shear modulus and strength increased with decreasing temperature; however, tensile testing did not reveal any heavy temperature dependence.

Not nearly as much work has been done to characterize composite materials subjected to dynamic loads at low temperatures. Lopresto and Langella studied the damage of glass/epoxy laminates from impact loading at temperatures from standard room temperature to -58 °F. They found that the indentation on the surface of the laminates was reduced with decreasing temperature and that higher energies were required to penetrate the specimens. This was due to embrittlement of the specimens at lower temperatures. These trends held for both thin and thick laminates. They found,

however, that the total delamination area in the composite was not dependent upon the temperature. This result was echoed by Gomez-del Rio, et al., in their study of the tensile behavior of carbon/epoxy using a tensile split Hopkinson pressure bar.

Icten, et al., studied the response of a quasi-isotropic glass/epoxy composite to impact loading. They investigated the maximum contact load, deflection, and absorbed energy as a function of impact energy and temperature (68°F, -4°F, and -76°F). They found that at low impact energies the maximum damage and response of the composite was similar across the temperature range. At higher impact energies the temperature had a significant effect. The major damage mechanisms shifted from being matrix dominated to fiber dominated. Further, as temperature decreased the amount of energy absorbed by the composite also decreased, leading to higher perforation thresholds. Charpy impact tests were carried out by Khalid on aramid/epoxy and glass/epoxy specimens at temperatures ranging from -40°F to 104°F at regular intervals. He found that impact energy decreased with decreasing test temperature and that the failure mechanism was dependent upon temperature.

Gupta and Shukla investigated the air blast response of foam core sandwich composite panels at 5°F (facesheet)/-40°F(core). At these low temperatures they found that the performance of the panels was degraded as compared with those tested at room temperature. As observed in previous studies, the damage mechanisms were different at low temperatures. Further, the response of the structure was more severe due to shear cracking which developed in the foam core.

CHAPTER 2

NEAR FIELD UNDERWATER EXPLOSION RESPONSE OF POLYUREA COATED COMPOSITE PLATES

by

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Abstract

An experimental study with corresponding numerical simulations has been conducted to evaluate the response of E-Glass / Epoxy composite plates, including polyurea coating effects, subjected to near field underwater explosion (UNDEX) loading. Experiments are performed in a water filled blast tank in which the transient plate response during the UNDEX loading is measured utilizing high speed photography coupled with Digital Image Correlation. The experimental results show that the transient response of the plate is improved through the use of a thicker plate or through the application of a polyurea coating, although there is a weight penalty associated with the additional material which should be considered. Corresponding computational models of the experiments have been conducted with the commercial finite element code LS-Dyna. The simulations are shown to have a high level of correlation to the experimental data.

1. Introduction

Within the naval community there is an interest in constructing new vehicles and structures from composite materials. The advantages of these advanced material systems include high strength to weight ratios, lighter structural components, and overall reduced maintenance costs. However, when structures manufactured from these materials are employed in military applications they must also be designed in a manner such that they will be able to survive an underwater explosion (UNDEX) event. The static response of composite materials is well understood while there is less of an understanding in terms of what happens to the same composite material when subjected to high loading rates. This typically results in composite structures being conservatively designed with large safety factors. Due to these large safety factors the structures are often over designed, thus not

fully utilizing the high strength to weight ratio afforded by composite materials. In the current study, the response of E-Glass / Epoxy composite plates, with and without polyurea coatings, subjected to near field underwater explosion (UNDEX) loading has been analyzed. Two parameters are investigated in the study: (1) Effect of the thickness of the baseline uncoated plate, and (2) Effect of coating the back-face of the plate (side opposite of explosive charge) with polyurea. The investigation consists of experiments performed in an underwater blast tank including the use of Digital Image Correlation (DIC) to capture the transient response of the plates. Corresponding computational simulations were performed with the commercial finite element code LS-Dyna.

When a submerged structure is exposed to an underwater explosion, it undergoes a complex and highly transient loading condition including high peak pressures and spherical wave fronts. When explosions occur at sufficiently large standoff distances from a structure, the shock fronts are nearly planar and act over the entire structure in a nearly uniform manner. This loading results in structural responses consisting primarily of flexure with large center-point deflections. However, there tends to be low levels of material damage (primarily inter-laminar delaminations) and plate perforations / ruptures are minimal. In the absence of plate rupture, the shock wave is almost fully reflected away from the structure, shielding any occupants / internal equipment from the effects of the high pressure waves. Conversely, when an explosion occurs directly on, or very close to, the surface of a structure, the loading area is limited to the vicinity of the detonation itself. The result is highly localized pressure loadings and the structure sustains higher amounts of damage, oftentimes including plate penetration / complete rupture. Upon rupture of the plate the pressure waves enter the structure, subsequently exposing any

occupants to the adverse effects of high pressure gases as well as any shrapnel which may become dislodged from the blast area.

Studies on composite materials subjected to high loading rates have utilized both experimental and computational techniques. Work by Latourte et al [1] utilized a scaled fluid structure method [2] to study the failure modes and damage mechanisms in both monolithic and sandwich plates subjected to underwater impulsive loads. Schiffer and Tagarelli [3] have compared the response of glass and carbon reinforced composites and found that the glass reinforced plates had larger blast resistance than the carbon plates due to their higher tensile ductility. Avachat and Zhou [4] studied the effects of underwater shock loading on filament wound and sandwich composite cylinder and found that while both constructions exhibited similar damage mechanisms, including delamination, fiber failure and matrix cracking, the sandwich structure had overall better performance than a monolithic cylinder with similar mass. The same authors [5] also utilized an Underwater Shock Loading Simulator combined with digital image correlation to show that for sandwich constructions lower density cores yield higher blast performance than high density cores due to their larger core compression capability. LeBlanc and Shukla [6, 7] have studied the response of flat and curved composite plates to far field underwater explosive loading through experimental and computational methods. Franz et al. [8] and Mouritz et al. [9] studied the effects of an underwater explosion at different standoff distances on a glass composite laminate. Dear and Brown [10] have conducted a detailed study on the damage mechanisms and energy absorption in composite plates when subjected to impact loading.

In recent years, the use of polyurea materials to enhance the failure resistance of structures subjected to explosive loading has become a topic of interest. Polyurea is a synthetic, high strength / high elongation coating that is typically spray cast onto existing structures to increase their performance under shock and ballistic loading events such as those of a bomb blast. Research efforts have recently studied the effectiveness of polyurea when used with composite materials. LeBlanc et al. [12, 13] showed that the transient response of UNDEX loaded composite plates is dependent upon both coating thickness as well as location. Tekalur et al [14] investigated the response of E-Glass composites coated with polyurea subjected to air blast loading. This study indicated that the polyurea coating reduced the transient deflections and post mortem damage levels as compared to the uncoated material. Gardner et al [15] studied the effect of location of the polyurea in relation to the foam core in sandwich composites. It was observed that when a layer of polyurea is placed between the foam core and the back-face of the sandwich the blast resistance is improved, while conversely if the polyurea is placed between the front face and the foam core the performance is degraded. Furthermore, effects of polyurea coatings have been studied through the use of computational simulations. Amirkhizi et al [16] have developed a visco-elastic constitutive material model that describes the behavior of polyurea materials under a broad range of strain rates, and includes pressure and temperature effects. Amini et al [17, 18] used LS-DYNA to simulate impact / impulsive loading experiments of polyurea coated steel plates.

2. Materials

In the current study, E-Glass Epoxy bi-axial laminate composite plates, with and without polyurea coatings are studied. The following section details the materials utilized in the investigation.

2.1 Composite

The composite material used in this investigation is Cyply® 1002, a reinforced plastic manufactured by Cytec Engineered Materials. The material is a cured epoxy composite which utilizes a non-woven, parallel fiber construction with continuous E-Glass filaments. A cross-ply construction has been utilized in this study that has alternating plies of 0° and 90° with each ply having a thickness of 0.254 mm (0.01 in.). The cured material has an areal weight of 0.46 kg/m² (0.85 lb/yd²) per ply (0.254 mm) and a specific gravity of 1.85. The resin content is 36 ± 3%. Two plate thicknesses, 0.762 mm and 1.524 mm, have been utilized in the study. The 0.762 mm plates have 3 lamina of alternating 0/90 plies such that the laminate schedule is [0/90/0] while the 1.524 mm plate has a schedule of [0/90/0/90/0/90]. The properties for a single unidirectional ply of the material are provided in Table 1.

Table 1 - Cyply 1002 - Mechanical Properties (Uni-Directional)

	MPa
Tensile Modulus (0°)	39.3e3
Tensile Modulus (90°)	9.65e3
Tensile Strength (0°)	965
Tensile Strength (90°)	20
Compressive Strength (0°)	883
Compressive Strength (90°)	193

2.2 Polyurea

The composite laminate serves as the baseline substrate to which the polyurea coatings are applied. In the current study the 0.762 mm composite plate has been coated with a polyurea coating having a thickness of 0.762 mm. This results in a plate with a combined thickness of 1.524 mm, equal to that of the thicker composite plate. The coating is applied to the back side of the plate after manufacturing and is not integrated into the composite itself. This construction is chosen to represent what would typically be found in a real world application where structures are retrofitted (spray coated) with this material as opposed to being incorporated into the original design. The polyurea is sprayed onto the plates and then post cured for 48 hours at a temperature of 160°F.

The polyurea material used is Dragonschild-BC available from Specialty Products, Inc. of Lakewood, WA. This is a 2 part material that can be spray cast to a wide range of surfaces and materials. The polyurea has been characterized in both tension and compression for strain rates from 0.01 s^{-1} to 2000 s^{-1} . Characterization up to 100 s^{-1} was performed using standard material testing machine whereas a split Hopkinson pressure bar was used to characterize the response of the material at 2000 s^{-1} . The response of the material at 2000 s^{-1} was only characterized in compression and is assumed to be similar in tension. At the lower strain rates unique tests were conducted for both tension and compression. The full material characterization is shown in Figure 1. From this figure it is seen that the material exhibits strong strain rate dependence and becomes stiffer with increasing loading rate. Furthermore, the material displays a stiffening effect in compression above 300% whereas in tension the response exhibits a stress plateau like behavior.

A summary of the plate thicknesses and areal weights is provided in Table 2, and a schematic of the laminate designs are shown in Figure 2.

Figure 1 - Dragon Shield BC Polyurea Stress-Strain Behavior

Table 2 – Thickness and Areal Weight of Laminates

	Thickness, mm (in)	Areal Weight, kg/m ² (oz/yd ²)
Thin Baseline Laminate	0.762 (0.03)	1.45 (42.7)
Thick Baseline Laminate	1.524 (0.06)	2.91 (85.5)
Thin Baseline with Polyurea coating	1.524 (0.06)	2.28 (67.4)

end of the tunnel. The outer dimensions of the flange are 381 mm x 381 mm. The flange has a series of through holes around the perimeter which allow for bolting of the test plates to the flange. The test plates are sandwiched between the flange and a second steel frame and are secured to the flange with a series of 1.59 mm diameter through bolts spaced at 38.1 mm. The use of the tunnel and mounting flange provide a water tight seal around the test plate and allows for the plates to be air backed, Figure 3. The composite plate and mounting fixture geometrical details are provided in Figure 4.

Figure 3 - UNDEX Test Tank

Figure 4 - Specimen Geometry

3.2 Explosive Charge

The explosive used in the near field blast experiments is an RP-503 charge manufactured by Teledyne RISI, Figure 5. The charge is comprised of 454 mg RDX and 167 mg PETN contained within an outer plastic sleeve.

Figure 5 - RP-503 Explosive Charge (units of mm)

3.3 Measurement Equipment

3.3.1 Pressure Transducers / Data Recorder

The free field pressure transducers employed in this study to measure the pressure field resulting from the detonation of the RP-503 charge are Series 138 ICP Tourmaline Underwater Blast Sensors produced by PCB Piezotronics, Inc. (item number 138A05). The sensors have a pressure range of 34.475 MPa, rise time is less than 1.5 μ s, and a resonant frequency greater than 1MHz.

A Tektronix DPO 3034 Digital Phosphor Oscilloscope has been used to record the pressure histories during the near field blast experiments. The oscilloscope has 4 analog channels, each with a 2.5 GS/s sample rate, 300 MHz bandwidth, and 5 mega-point record length.

3.3.2 Digital Image Correlation

High speed photography, coupled with three dimensional Digital Image Correlation (DIC) was used to capture the full-field deformation of the back-face (side opposite of the explosive) of the plates during the UNDEX loading. During the experiments two cameras are arranged in a stereo configuration such that they view the back face of the test specimen. To record the transient response with this system, the cameras must be calibrated and have synchronized image recording throughout the event. The calibration of the cameras is performed by placing a grid containing a known pattern of points (dots) in the test space where the composite sample is located during the experiment. This grid is then translated and rotated in and out of plane while manually recording a series of images this grid pattern is predetermined, the coordinates of the center of each point (dot) is extracted from every image thus allowing for a correlation of the coordinate system of each camera. Prior to the conduct of the experiments, the face

of the composite plate facing the cameras (back-face) is painted with a random speckle pattern (white background with small densely spaced black dots). The software employed to synchronize the high speed cameras and record the images during the experiments is Photron Fastcam Viewer (PFV). PFV is a user interface that enables the editing and storage of captured images and video. The post processing is performed with the VIC-3D software package which matches common pixel subsets of the random speckle pattern between the deformed and un-deformed images. The matching of pixel subsets is used to calculate the three-dimensional location of distinct points on the face of the plate. This provides a full-field displacement history of the transient event throughout time.

The cameras used during experimentation were Photron FastCam SA1. Each camera is capable of frame rates from 1,000 to 675,000 fps with image resolution ranging from 1,024 x 1,024 to 64 x 16 pixels depending on the frame rate. In the current effort, a frame rate of 27,000 fps was utilized for an inter-frame time of 37 μ s. The camera resolution at this frame rate is 448 x 480 pixels.

4. Experimental Methodology

Experiments were performed to understand the behavior of E-Glass/Epoxy plates subjected to near field underwater explosions. Three plate configurations have been studied: (1) 0.762 mm thick uncoated plate, (2) 1.524 mm thick uncoated plate, and (3) 0.762 mm thick plate with 0.762 mm polyurea coating on the back-face. Two high speed cameras were positioned 330 mm behind the tank walls perpendicular to the viewing windows to avoid any distortion effects from the windows themselves. A third high speed camera was positioned at the side of the tank to view the detonation of the

explosive, resulting bubble growth, and interaction of the bubble with the composite plate. Two free field tourmaline pressure sensors are located within the tank to record the pressure field at two distinct standoff distances. Let it be noted that the gages are located at a larger standoff from the charge than the distance between the charge and the composite plate to avoid damage to the sensors. Figure 6 is a combination, isometric view and aerial schematic of the tank providing an overview of the camera, explosive and pressure sensor positioning. An overview of the experimental process is presented in the following discussion.

To begin the experiment, the high speed cameras comprising the DIC system are calibrated to establish a correspondence of the respective camera coordinate systems. Calibration is conducted according to the previously described method in which images of a calibration grid are captured while rotating and translating the grid. Once acceptable calibration and time syncing of the cameras is established the plate is bolted into the fixture with the DIC speckle pattern facing the cameras (air backed side). When mounting the polyurea coated plates, the coating is located on the back side of the composite plates with respect to the charge location. Once the plate is bolted into the fixture the RP-503 charge is placed within the tank. The charge is suspended by its detonation wire into the tank and placed 50.8 mm from the center of the composite plate. To ensure consistent charge standoff distances for each experiment a 3.18 mm diameter foam spacer is placed between the charge and plate. The foam is secured to both the charge and plate by a fast setting epoxy. The use of this foam spacer was critical to the conduct of the experiments for two reasons: (1) it ensures there is no disturbance of the charge location during the filling of the tank, and (2) it accounted for panel flexure

(induced the hydrostatic load of the water after filling) by ensuring that the charge moved with the plate, thus maintaining a consistent standoff distance. Two tourmaline pressure sensors are positioned in the tank at horizontal standoff distances from the charge center of approximately 100 and 175 mm. The sensors are suspended in the tank from their water resistant cables and are also secured to a weight on the bottom of the tank by means of a thin line to maintain relative positioning. Each sensor is then fixed in position by a wire which is secured to a weight resting on the bottom of the tank. After the plate is secured in the fixture and the pressure sensors are in place, the tank is filled with water to a depth of 1.06 m. The center of the plate is located 0.55 m below the surface of the water. As mentioned previously, an aerial schematic of the total setup is shown in Figure 6.

Once the tank is filled, and the operation of all measurement equipment is verified, the RP-503 charge is detonated through the use of a detonation box. The box simultaneously sends a high voltage to the RP-503 charge to initiate detonation and a simultaneous 9 volt pulse to the oscilloscope which captures the pressure data. The oscilloscope also relays a negative TTL voltage to all the cameras to capture the high speed photos. The use of this single initiation system to both initiate the charge detonation and trigger the oscilloscope/cameras ensures complete time synchronization between all experimental equipment and a common time zero datum for all measurements. Upon completion of the experiment all images from the DIC cameras are processed through VIC 3D to extract full-field plate deformation.

Figure 6 - Experimental Setup Measurement Equipment

5. Results and Discussion

The response of the composite plates in this study is characterized by the transient center-point displacement of the back-face of the plate, deformation evolution mechanisms during the displacement, and full-field DIC observations. All plate deflection data presented for the plates is extracted from the post processed images through DIC.

The pressure profiles resulting from the detonation of the RP-503 charge, as measured by the two free field pressure sensors at 100 and 175 mm standoff distances from the charge, are shown in Figure 7. The pressure profiles display the characteristic components of an UNDEX, namely: a rapid pressure increase associated with the shock front, followed by an exponential decay and a reduction in peak pressure with increasing radial standoff from the charge center. It is noted that for the 100 mm standoff pressure gage there is a sudden drop in pressure occurring at 0.12 ms. This corresponds to the arrival of the reflected pressure wave from the surface of the plate. The peak pressure of the shock front experienced by the plate surface (50.8 mm standoff) is on the order of 40 MPa determined from the computational simulations.

The behavior of the bubble resulting from the detonation and its associated interaction with the composite plate is shown in Figure 8. The sequence of images shows the clear formation of the bubble at 80 μ s and its subsequent growth in size due to the combustion of the explosive products. Due to the high pressure of these gaseous products the bubble expands, reaching a diameter of ~50 mm at 320 μ s at which point it reaches and interacts with the surface of the composite plate. As a result of this interaction with the plate it is prevented from further expansion in the direction of the plate but continues a spherical expansion in the remaining directions. The uncoated plates experience edge tearing (see later discussion) between 1200 μ s and 1400 μ s during which time the bubble is still expanding and has not yet reached its maximum diameter. Once tearing of the plate occurs, the plate can no longer be considered a standing plate and any resulting bubble behavior would be heavily influenced by the resulting motion of the plate. Figure 8 displays only the first 560 μ s of the bubble behavior to show initial plate contact and radial expansion. Times between 560 and 1200 μ s consist mainly of further bubble expansion, thus images after 560 μ s are not shown.

Figure 7 - UNDEX Pressure Profiles (Time zero corresponds to charge detonation)

Figure 8- UNDEX gas bubble behavior

While filling tank with water during the setup of the experiment it was observed that the plates sustain a measurable level of flexure due to the hydrostatic pressure and the thin nature of the plates. The peak center-point deflection of the plates after filling the tank is provided in Table 3. These deflections are determined by taking photographs of the plate surface before and after filling the tank and processing the images through the DIC software. The baseline for all subsequent plate deflection measurements is taken to be the deformed shape after tank filling. As previously described, a constant charge

standoff for all plate configurations is achieved through the use of a foam spacer which connects the charge and plate. Although beyond the scope of the current study, it is noted that it is likely that as the initial depth pressure is increased (i.e. a deep diving submersible) the effects of the corresponding pre-stress prior to UNDEX loading should be considered. As the material has a finite strength capability, the additive effect of depth pressure and UNDEX pressure loading will reduce the ability of a structure to resist an explosion event that may have been survivable at shallower depths.

Table 3: Specimen deflections under hydrostatic preload

Plate	Deflection (mm)
0.762 mm	5.6
0.762 mm (Coated)	5.3
1.524 mm	4.6

The center-point displacement for each respective plate configuration is shown in Figure 9. From this figure it is observed that there are several distinct differences in the overall plate response as influenced by the plate construction. The first difference is the overall center-point deflection of the plates. It is evident that, as compared to the baseline 0.762 mm plate, increasing the plate thickness or including a polyurea coating reduces the peak overall deflection for a given level of loading. The peak displacement for the uncoated 0.762 mm plate is 28 mm, whereas for the 1.524 mm uncoated plate and the 0.762 mm polyurea coated plate the peak deflections are 20.5 mm and 24.8 mm, reductions of 27% and 12% respectively. It is noted that the center-point velocity during the initial deflection is nearly constant for each configuration. The main difference is the

time that it takes for the plate to arrest its outward motion and begin to recover, with the 1.524 mm uncoated and the 0.762 mm polyurea coated plates arresting their outward motion ~0.25 ms sooner than the baseline 0.762 mm plate. The peak center-point deflection and time to reach the peak displacement are provided in Table 4. The center-point deflection comparison between the 1.524 mm uncoated plate and the 0.762 mm plate with a 0.762 mm coating of polyurea indicate that for a plate thickness it is more advantageous to utilize additional structural plies rather than an elastomeric coating. However, when a structure has previously been designed and further thickening of the structural shape is not possible, the application of a polyurea coating can improve the transient response to shock loading.

The second primary difference in the response of the plate configurations is the onset of material damage. Both the uncoated 0.762 mm and 1.524 mm specimens experienced significant through-thickness tearing at the plate boundaries at approximately 1.1 and 1.4 msec respectively. Upon rupture of the plate edges water entered the cameras' field of view and caused decorrelation in the DIC images. Their plots, Figure 9, are accordingly abbreviated at the onset of tearing prior to DIC decorrelation due to water intrusion. However, it is further observed that although the 0.762 mm plate with the polyurea coating did experience larger deflections than the 1.524 mm uncoated plate, there was no edge tearing of the plate itself. Thus in terms of reducing material damage itself, the polyurea coatings offer an advantage over a thicker uncoated plate.

Table 4: Plate center-point deflection results

Plate	Maximum Deflection	Time to Peak
0.762 mm	28.2 mm	1.07 msec
0.762 mm (Coated)	24.8 mm	0.78 msec
1.524 mm	20.5 mm	0.74 msec

Figure 9 - Plate center-point deflections

The deformation history of the baseline 0.762 mm uncoated composite plate as measured along a horizontal cut through the center of the plate is shown in Figure 10. The deformed profile plotted throughout time is illustrative of the deformation mechanics of the composite plate. From this figure it is seen that for a plate subjected to a centralized near field UNDEX loading, the deformation is initially dominated by localized

deflections at the center with minimal deflection near the boundaries. As the plate responds to the pressure loading, it gradually transitions to an overall plate flexure mode as shown by the cross sectional shape at 0.63 and 1.11 ms. At 1.11 ms the plate experiences significant edge tearing and further observations of the plate deformation mechanics would be invalid due to partial rigid body motion of the plate. The significant observation is that the initial plate deformation is governed by the highly localized pressure loading and then subsequently shifts to a mode I flexure deformation profile later in time.

The full-field displacement profiles for the back-face of each plate configuration are provided in Figure 11. The localized center-point deflection can be visualized in the 0.37 ms time frame and is consistent with the cross sectional shape plot, Figure 10. Furthermore, the overall flexural deformation mode at 1.11 ms is clearly visible in the contour plots. Each of the three panel configurations exhibit similar deformation along their centerline with the primary difference being the magnitude of the displacement itself.

Figure 10 - Plate Deformation - Horizontal Centerline

Figure 11 – Full-Field Deflection Contours

The transient displacement results discussed thus far indicate a performance advantage when the thickness of the baseline plate is increased, or alternatively a polyurea coating is applied to the surface of the plate. However, when the plate thickness is increased or a coating is applied there is an associated penalty in that the plate weight is correspondingly increased. One means of quantifying the added mass penalty in terms

of transient deflection of the respective plates is to establish an Areal Weight Ratio (AWR) between the plate configurations [13]. The AWR is calculated by Equation 1

$$AWR = \frac{W_{2,3}}{W_1} \quad (1)$$

where W_1 is the areal weight of the uncoated 0.762 mm composite baseline plate and $W_{2,3}$ is the areal weight of the polyurea coated 0.762 mm plate and the and 1.524 mm specimens, respectively. The AWRs for the 1.524 mm plate and the polyurea coated plate are 2 and 1.57 (Table 2). The AWR is subsequently employed as a multiplier applied to the transient center-point deflection data. The displacement data that has been adjusted (raw data multiplied by AWR) to account for the areal mass increase is shown in Figure 12. This plot shows that when the displacements are adjusted to account for the increased areal weight, the baseline plate outperforms both the thicker and polyurea coated plates. The normalized deflection of the polyurea coated specimen was 37.9% greater than the uncoated 0.762 mm specimen, and that the normalized deflection of 1.524 mm specimen was similarly 45.4% greater. This suggests that the additional laminate plies and the employed polyurea regime serve to degrade the deflection performance of the plate specimen with respect to AWR. This observation is consistent with previous findings for curved polyurea composite plates subjected to far field UNDEX loading in which polyurea coatings have been seen to result in larger AWR adjusted deflections [13]. It is noted that in the previous study, multiple coating thicknesses were considered and it was found that there are coating thicknesses for which the coated plate outperforms the baseline plate, even when accounting for the AWR penalty. Thus, the findings of the single coating thickness considered in the current study do not preclude the existence of

a polyurea coating thickness for composite plates subjected to near field UNDEX loading that both outweighs the weight penalty while also improving the deflection performance. Further work is needed to identify such a regime in the future. Finally, the 1.524 mm plate and 0.762 mm polyurea coated plates have approximately the same relative performance in terms of adjusted peak displacement when the added mass penalty is taken into consideration.

Figure 12 – Areal Weight Adjusted Deflections

Table 5: Normalized plate center-point deflection results

Plate	Maximum AWR Deflection
0.762 mm	28.2 mm
0.762 mm (Coated)	38.9 mm
1.524 mm	41.0 mm

6. Finite Element Modeling

Finite element modeling of the experiments has been performed with the LS-DYNA code available from the Livermore Software Technology Corporation (LSTC). The models utilize the coupled Lagrange-Eulerian formulation of the code which allows for accurate representation of the detonation of the explosive charge as well as the fluid structure interaction between the fluid and the composite plate. All simulations are generated with Version 971, Release 4.2.1 and are run in double precision mode. All models are constructed in the CGS unit system.

The finite element model of the UNDEX test setup is shown in Figure 13. The model consists of the test plate (Coated / Uncoated Composite Plate), tank water, air, and the RP-503 charge. The model represents a subdomain of the full experimental test tank for computational efficiency. Included in the model is the unsupported portion of the composite plate (Plate edge corresponds to the clamped boundary), 120 mm of air extending behind the plate, and 200 mm of water extending from the plate surface towards the charge. The charge is located 50.8 mm from the center of the plate surface. The use of such a sub-domain for the modeling of the corresponding experiments is deemed appropriate as the loading of the plate and subsequent response occurs sufficiently fast that reflections from the tank walls do not affect the overall transient response of the plate. In the model the outer surface of the fluid sub-domain is prescribed a non-reflecting boundary

condition (*BOUNDARY_NON_REFLECTING) which allows the associated pressure waves to leave the domain, as they would in a free field detonation, rather than reflect off of the free surface.

The water, air, and explosive charge are modeled with solid elements utilizing the LS-Dyna ALE multi-material element formulation (Type 11 solid element). Each of the Eulerian components in the model utilizes a material definition in combination with an equation of state (EOS) to fully define the appropriate behavior. The water and air utilize the *Mat_Null material definition with the density of the water and air given as 1 g/cm³ and 0.0013 g/cm³ respectively. The Gruneisen EOS is used for the definition of the water with the speed of sound taken to be 149,000 cm/s. A Linear Polynomial EOS defines the air domain in the model with the parameters defined in Table 6. By defining C₀, C₁, C₂, C₃, and C₆ equal to zero, and C₄, and C₅ equal to $\gamma-1$, a gamma law EOS is achieved. Finally, the explosive charge is modeled with the *Mat_High_Explosive_Burn material model combined with the JWL EOS. Although the RP-503 charge contains both RDX (454 mg) and PETN (167 mg), the model assumes a charge comprised of only RDX, with the overall charge weight being maintained. This is deemed suitable for the model since the RDX is the larger component and RDX and PETN have similar JWL coefficients. Furthermore, the pressure generated from the detonation in the model is suitably correlated to the corresponding experimental profile. The Material and EOS parameters for the RDX are provided in

Table 7 and Table 8.

Table 6 - Air EOS Parameters

C0	0
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C1	0
C2	0
C3	0
C4	0.4
C5	0.4
C6	0

Table 7 - RDX Material Parameters

ρ (g/cm ³)	1.77
D (cm/s)	850e3
Chapman-Jouget Pressure (dyn/cm ²)	3.41e13

Table 8 - RDX EOS (JWL) Parameters [19]

A	7.78e12 (dyn/cm ²)
B	7.07e10 (dyn/cm ²)
R1	4.485
R2	1.068
ω	0.3
E _o	5.93e10
V _o	1.0

The structural aspect of the coupled model consists of the composite plate and polyurea coating. In all models, only the unsupported section of the plates is included. The outer edge of the plate is fully clamped with appropriate boundary conditions, thus negating the need to explicitly model the fixturing in the test setup. It is noted that after the completion of each test there was no slippage observed at the plate boundary. The composite plate in the simulations is modeled using a single layer of shell elements, Figure 14, with an edge length of 2.5 mm. The *Section_Shell property for the shell element allows for the laminate schedule to be defined within the section card, including the angle of each respective ply. By defining the ICOMP parameter to be equal to 1 on the section

card, the orthotropic layered composite option is activated. Through the use of this option an arbitrary number of equally distributed integration points may be defined through the thickness of the shell, with each integration point being assigned a material angle. In the current models, each ply is represented as having two integration points so as to capture the correct bending behavior on a per ply level. The polyurea material is represented in the model by solid elements, Figure 14, with a constant stress formulation. Furthermore, the polyurea coatings are assumed to be perfectly bonded to the composite plate and are thus meshed directly to the composite. This assumption is valid as there was no visual debonding between the composite and polyurea observed during testing.

The LS-DYNA material model utilized for the composite plate is Mat_Composite_Damage (Mat_022) [20]. This is an orthotropic material definition capable of modeling the progressive failure of the material due to any of several failure criteria including in-plane shear, tension in the longitudinal/transverse directions, and compression transverse direction. The material model for the polyurea coating is Mat_Simplified_Rubber. This model is a visco-elastic material definition which captures both the strain and strain-rate effects through the use of a family of load curves. The model reproduces the uniaxial tension and compression behavior as obtained through material testing at discrete strain rates. The stress-strain curves for each strain rate are shown in Figure 1. The model determines the appropriate strain rate curve from the family of curves through an internal calculation.

The loading of the composite plates in the models occurs in a two-step process. During the first step a uniform pressure is quasi-statically applied over the entire front face of the plate. This pressure corresponds to the depth pressure (at the mid point of the plate)

acting on the submerged plate. During the experiments it was observed that due to the relatively thin nature of the plates as compared to the unsupported dimensions of the plate there was a sufficient level of center-point defection (~5 mm for all plates) such that it should be accounted for during the simulations. Thus this pressure is applied to the plates and any resulting motion is allowed to damp out resulting in a static stress state. At this point the detonation of the explosive charge is initiated and the plate responds transiently. In all subsequent discussions of plate displacements, the reported values are measured from the preloaded state by subtracting out the displacement resulting from the preload.

Figure 13 - Finite Element Model of UNDEX Experiment (3 Quadrants of Fluid Domain Hidden)

Figure 14 - Structure Mesh Details

7. Finite Element Model Correlation to Test Data

7.1 Center-point Displacement – Simulation Correlation to Test

The center-point displacement data captured during the experiments with the DIC method is used as a basis to correlate and validate the finite element model results. The quality of the correlation between the test data and numerical results in this study is quantified using the Russell Comprehensive Error measurement. The Russell error technique is one method which evaluates the differences in two transient data sets by quantifying the variation in magnitude and phase. The magnitude and phase error are then combined into a single error measure, the comprehensive error factor. The full derivation of the error measure is provided by Russell [21] with the phase, magnitude, and comprehensive error measures respectively given as:

$$\begin{aligned}
RP &= \frac{1}{\pi} \cos^{-1} \left(\frac{\sum c_i m_i}{\sqrt{\sum c_i^2 \sum m_i^2}} \right) \\
RM &= \text{sign}(m) \log_{10}(1 + |m|) \\
RC &= \sqrt{\frac{\pi}{4} (RM^2 + RP^2)}
\end{aligned}$$

In the above equations c_i and m_i represent the calculated (simulated) and measured responses respectively. Excellent, acceptable, and poor correlation using the Russell error measure is given as: Excellent - $RC \leq 0.15$, Acceptable – $0.15 < RC \leq 0.28$, and Poor $RC > 0.28$. The definition of these criteria levels are the result of a study that was undertaken to determine the correlation opinions of a team in support of a ship shock trial. A summary of the process used to determine the criteria is presented by Russell [22].

The center-point time history correlation between the experimental data and the corresponding computational simulation for each respective plate configuration is shown in Figure 15. A summary of the Russell error for each of these comparisons is provided in Table 9. The correlations presented in the figure show that there is a high level of correlation between the experiment and simulations, both temporally and in terms of displacement magnitudes. The simulation and experiment results exhibit consistent results in the early time frame of the event (0 – 0.4 ms) in terms of displacement and velocity, with some deviation beyond this point, although the deviation is somewhat minor. Additionally for both of the uncoated plates (0.762 mm and 1.524 mm) it is seen that the onset of edge tearing occurs slightly later (0.1 ms) in time as compared to the experimental results. The timing differences in the onset of damage is expected as the model assumes a uniform plate in terms of material properties and does not account for

manufacturing variability or minor internal defects which can contribute to the onset of damage or slightly weaker/stronger areas of the plates as compared to the gross material strengths. That the model is able to predict the onset of damage in a consistent manner as observed during the testing, namely edge tearing, is encouraging. Overall, it is shown that the Russell error values for the center-point comparisons show excellent correlation ($RC < 0.15$).

Figure 15 - Center-point Displacement Model Correlation

Table 9 - Russell Error Measure Summary

	Magnitude Error (RM)	Phase Error (RP)	Comprehensive Error (RC)
0.762 mm (Uncoated)	0.08	0.02	0.07
0.762 mm (Coated)	0.10	0.02	0.09
1.524 mm (Uncoated)	0.03	0.02	0.03
RC < 0.15 – Excellent 0.15 < RC < 0.28 – Acceptable RC > 0.28 - Poor			

8. Summary and Conclusions

The response of submerged, air backed E-Glass / Epoxy composite plates, including polyurea coatings, when subjected to near field underwater explosive loading has been studied through the use of experiments and computational modeling. The focus of the work is on determining how the response of a composite plate subjected to UNDEX is influenced by increased plate thickness or through the application of an elastomeric coating to the baseline plate. A water filled blast tank has been used to impart UNDEX loading to the composite plates in a controlled manner. The Digital Image Correlation system is used to capture the full-field, transient response of the back (dry) surface of the plates. Computational models of the experiments have been developed utilizing the commercially available LS-DYNA explicit finite element code.

In the study the response of three unique plate configurations is studied: (1) 0.762 mm baseline plate, (2) 1.524 mm plate, and (3) 0.762 mm plate with a 0.762 mm polyurea coating applied to the back-face. Performance of the plate configurations is evaluated using the center-point and full-field time histories of the deflection of the back-face of the plates, as well as level of material damage. The experimental results show several effects on the transient response of the plates based on configuration. The use of a plate two times as thick as the baseline plate reduces the center-point deflection by 27% while the application of a polyurea coating equal in thickness to the baseline plate results in a 12% deflection decrease. Additionally, the polyurea coating is effective in reducing material damage as compared to both the baseline and thicker uncoated plates. Thus, when considering a plate design, the desired performance metric of the plate response should be considered. A thicker plate of structural material (composite) is preferable to reduce center-point deflection, while the use of polyurea coating are effective in reducing overall damage. However, in the case of an existing design the use of polyurea coatings can be an effective retrofitting application to improve the blast resistance of a structure while reducing overall material damage. Furthermore, it has been shown that through the use of an Areal Weight Ratio, there is a tradeoff between increased panel weight and mechanical performance. Although, both the thicker composite plate and the coated plate outperform the baseline plate, this performance increase comes at a penalty of increased weight. Thus if weight is a strong consideration in a specific application then maximum blast resistance may not be achievable and a relative tradeoff between weight and performance must be considered. The computational models developed in the study to correspond to the experimental testing, simulate the testing accurately, and using the Russell Error measure, demonstrate

model correlation that can be described as excellent. The models are able to accurately simulate the detonation of the explosive charge and the resulting pressure fields and plate deflections.

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CHAPTER 3

EXPERIMENTAL AND COMPUTATIONAL INVESTIGATION OF BLAST RESPONSE OF CARBON EPOXY WEATHERED COMPOSITE MATERIALS

by

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Abstract

An experimental study, with corresponding numerical simulations, was conducted to investigate the blast response of weathered Carbon-Epoxy composite plates. The dynamic behavior of the composite plates with and without prior exposure to an aggressive marine environment was explored using a shock tube apparatus coupled with a high speed photography system. In order to simulate prolonged exposure in an aggressive marine environment, specimens were submerged in an elevated temperature, 3.5% salt solution for 0, 30 and 60 days. The saline solution temperature was maintained at 65°C to accelerate the aging process. Finite element modeling (FEM) for the blast loading experiments was performed using the Ls-Dyna code. Models have been developed for both the simply supported and fixed boundary condition cases.

Tensile and four-point bend tests were performed to characterize the quasi-static mechanical behavior of the composite material before and after prolonged exposure to aggressive marine environments. After 30 and 60 days of submergence, the tensile modulus decreased by 11% and 13%, the ultimate tensile strength decreased by 12% and 13%, and the ultimate flexural strength decreased by 22% and 22%, respectively.

Dynamic blast loading experiments were performed on simply supported and fully clamped specimens, to determine the effects of the boundary conditions on the Carbon-Epoxy specimen response. The Weathered (30 and 60 days) and Non-Weathered (0 day) specimens displayed dramatically different behavior after being subjected to a blast load. For the simply supported case, Non-Weathered specimens displayed an average maximum out of plane displacement of 20 mm and recovered elastically. Weathered specimens, both 30 and 60 days exhibited similar initial transient behavior but failed catastrophically due to through thickness cracking at the point of maximum deflection. For the fixed boundary condition, the Non-Weathered specimens displayed an average maximum out of plane displacement of 5.57 mm, whereas the 30 day and 60 day weathered specimens displayed a maximum out of plane displacement of 6.89mm and 6.96mm, respectively. The corresponding numerical simulations matched well with the experimental data. However, for the fixed boundary case, the beam vibration of the simulation was off phase with the experimental results due to imperfect boundary conditions in the experiments.

1. Introduction

A series of experiments were conducted to study the blast response of weathered Carbon-Epoxy composite plates subjected to simply supported and fully clamped boundary conditions. The blast loading was created using a shock tube and the structural response of the composite plate was recorded using high speed photography in conjunction with a 3D digital image correlation technique used to obtain full-field data.

In the marine community there is an increasing interest to use composite materials for the construction of structures due to their high strength to weight ratio, reduced radar

signatures, and noise dampening properties [1]. Composite materials have been used to create small parts within ships such as fins and rudders, thus reducing the weight of vessels [1]. However, composites have lower impact resistance than steels and can degrade due to the undersea environment. Thus, studying the effects of the degradation of mechanical properties of composite materials, in particular on shock response, is of high priority.

To date, there have been numerous studies on the mechanical response of composite materials subjected to a dynamic loading. Abrate has written a detailed review of literature on the impact of laminated composites [2]. The review covers work from the late 1970s to early 1990s with a focus in discussing the experimental and theoretical approaches of early composites work. In the 2000s work by Zaretsky et al [3] and Yuan et al [4] focused on the damage of composite materials when subjected impact loading, specifically low velocity impacts. Current studies by Avachat and Zhou [5] have experimentally and computationally modeled the dynamic failure of sandwich composites subjected to underwater impulsive loads. However, these studies did not focus on the effect of aggressive marine environments on the dynamic response.

Accelerated life testing (ALT) methods simulate long term exposure to marine environments are used to study the effects of exposure on the mechanical properties of materials. In these investigations, composite materials are subjected to marine aging through submersion in seawater baths at elevated temperatures [6-14]. Nakada and Miyano [15] have developed prediction methods for the long term fatigue life of fiber reinforced plastic (FRP) laminates under elevated temperature and absorption conditions. The long term effect of submersion on composite sandwich structures was studied by

Siriruk et al [16] with a focus on the interface between the face sheets and the core. Park et al [17] presented the effects of aging after an impact event on polymer composites. Submersion studies focus on the degradation of material properties due to the diffusion of water into the composite. Elevated temperatures are used to increase this rate of diffusion, therefore requiring additional data to determine the relationship between the exposure time in the accelerated life test and an equivalent time in a typical operating environment.

Diffusion studies to find an acceleration factor relating ALT submersion times to an equivalent time at operating temperatures have been conducted, including studies involving weight gain monitoring of samples to find diffusion coefficients [18]. Rice and Ramotowski [19] used the Arrhenius equation to derive a method for finding this acceleration factor using the matrix material of the composite. The acceleration factor was found to be dependent on the experimentally determined activation energy of the matrix.

Computational investigations of the mechanical response of composite materials have become more prevalent in recent years. LeBlanc et al. [20] were able to correlate experimental results of the dynamic shock response of composite plates with finite element simulations using LS-DYNA. Arbaoui et al. [21] investigated modeling the response of composites in a split Hopkinson pressure bar (SHPB) experiment and were able to successfully correlate experimental data to their simulations.

In the current study, the blast responses of weathered and Non-Weathered Carbon-Epoxy plates are compared. Additionally, computational models for the 0 and 30 day

submergence conditions are developed to simulate the dynamic experiments. The simulations are shown to have good correlation to the experimental results.

2. Material and Specimen Geometry

The composite material used in this investigation is a Carbon-Epoxy (CE) plate produced by Rock West Composites. The carbon fiber is a 2x2 twill weave cured in an epoxy resin. The plate is 2.92 mm thick and composed of a 670 GSM 12k carbon fiber fabric (Aksaca 12K A-42) and PT2712 low viscosity epoxy produced by PTW&W Industries, Inc. Fiber volume fraction of the material is ~60%. The total thickness of the Carbon-Epoxy plate is made up of four twill woven plies, and the density of the composite is 1.45 g/cm³. An image of the composite material and the fiber construction is shown in Figure 1.

Figure 1 - Zoomed representation of the 2x2 twill weave

Table 1 displays the specimen dimensions for each experiment type. Dimensions were chosen to meet experimental specifications including ASTM standards D3039/D

3039M- 000 (tensile) [23], D7264/D7264M-07 [24]: Procedure B (four point bend) and prior shock tube studies [28].

Table 10: Composite specimen dimensions by experiment type

Experiment Type	Tensile	Four Point Bending	Shock tube
Dimensions (L cm x W cm)	25.4 x 2.54	15.24 x 1.27	20.32 x 5.08

Prior to mechanical testing, all specimens were desiccated for 48 hours to remove accumulated atmospheric moisture.

3. Experimental Setups and Methods

To obtain the blast response of the Carbon-Epoxy composite, the specimens were subjected to blast loading using a shock tube apparatus. Prior to blast loading, the specimens underwent a procedure to artificially accelerate the underwater aging of the material. The experimental details of the blast generating apparatus, high speed photography data acquisition, weathering process and the quantification of accumulated weathering are described in detail below.

3.1 Shock Tube Apparatus

A shock tube apparatus is used to generate a pre-determined amplitude of blast loading that is imparted to the composite plates. High speed cameras, coupled with 3D DIC were used to record the side and back face transient response during loading. A schematic of the shock tube setup along with high speed cameras is shown in Figure 2.

Figure 2 - (a) Shock Tube apparatus (b) Shock tube and high speed camera setup

The 8 meter long shock tube is composed of four separate sections: driver section, driven section, converging conical section, and reduced diameter muzzle. A 0.127 mm thick Mylar diaphragm separates the driver and driven sections, while the driver section is pressurized using helium gas. Under a critical pressure of ~ 0.25 MPa, the diaphragm bursts, releasing a high pressure wave. The high pressure travels down the length of the driven section and develops into a shock wave front. The shockwave then reaches the muzzle section, and the pressure of the event is captured by two piezoelectric pressure transducers that are mounted flush to the interior of the muzzle. The shockwave then impacts the specimen and the pressure from the impact is reflected back into the muzzle. The reflected pressure is the loading that the specimen experiences. Figure 3 is a plot of

the pressure created by the shock tube apparatus as a function of time, as recorded by a pressure sensor 20 mm from the end of the muzzle exit.

Figure 3: Shock tube pressure profile

3.2 Digital Image Correlation

High speed photography coupled with 3D DIC was utilized to capture full field displacements and velocities on the back surface of the specimens during blast loading. Two Photron FastCam SA1 cameras, coupled with 3D +DIC, were used to track the 3D displacements of the composite plates during blast loading, while a side view camera is positioned to record the out of plane displacements. The specimen was positioned vertically with the muzzle normal to the specimen, with a gap of (~0.1 mm) between the muzzle face and specimen. The processing of the high speed images from the experiments was performed using the VIC-3D software package, which matches common pixel subsets of the random speckle pattern between the deformed and un-deformed images. The matching of pixel subsets was used to calculate the three-dimensional location of distinct points on the face of the plate. This provided a full field displacement

history of the transient event through time. For the blast experiments, a frame rate of 50,000 fps was utilized for an inter-frame time of 20 μ s.

3.3 Accelerated Weathering Facility

A submergence tank was created to subject the composite plates to an elevated temperature saline solution. The tank is composed of two high temperature polypropylene reservoirs. A double wall was created by placing the volumetrically smaller tank inside of the larger tank, creating a fluid boundary to separate the immersion heaters from the internal salt solution, and prevent any unwanted salt water corrosion. A 3.5% salt solution fills the internal tank where specimens were submerged for 30 and 60 days. The immersion heaters in the external tank were used to heat the external boundary of deionized water and through convection and conduction, heat the internal saline solution. The outer tank is insulated and maintained at 65°C which was chosen to increase the rate of water diffusion in the composite material, while remaining below the published 71.7 - 95.6 °C glass transition temperature range of the composite's epoxy matrix. The immersion heaters chosen to heat the submergence tank were Cole Parmer PolyScience LX Immersion Circulators. The maximum capacity of each heater is 20 liters of fluid with a temperature range of ambient to 98°C. Temperature stability is $\pm 0.07^\circ\text{C}$. Figure 4 shows a schematic of the weathering facility.

Figure 4: Accelerated Weathering Facility

3.4 Determining the Acceleration Factor

The dominant factor contributing to material degradation during prolonged submersion is fluid absorption in the matrix. In order to mathematically relate the experimental submergence of the composite plate to actual service submergence time, a water diffusion study of the matrix material was conducted. The study assumes that the only factor in the degradation of the composite is the accumulated water in the matrix material via diffusion. The relationship between the accelerated life test and service time immersion is governed by the Arrhenius equation (1). This equation describes the temperature dependence of the rate of reaction for a given process.

$$k = Ae^{\frac{-E_a}{RT}} \quad (1)$$

Where k is a rate constant, A is a prefactor, E_a is the activation energy, R is the universal gas constant, and T is the absolute temperature. A series of three salt water solutions were prepared, and maintained at different temperatures, T_a , T_b and T_c , to determine the diffusion coefficients and water saturation limits for the epoxy matrix. The spread of diffusion coefficients at various temperatures produced Arrhenius activation energy values for the matrix material, mathematically related to an Acceleration Factor (AF).

Disks of the matrix material were submerged in 3.5% saline solution and their weight recorded periodically until the saturation limit was reached. To calculate the diffusion coefficient, the following expression can be used. [27]

$$\frac{m_t}{m_s} = \frac{4}{h} \sqrt{\frac{Dt}{\pi}} \quad (2)$$

Where m_t is the mass of water absorbed at the time t , m_s is saturated water mass, D is the diffusion coefficient, and h is the disk thickness. The diffusion coefficient can be determined through a simplification of Equation (2) when specimens reach 50% of the total saturation. A solution of the diffusion coefficient is approximated as:

$$D = 0.049 \frac{h^2}{t_{50}} \quad (3)$$

Where t_{50} is the time it takes to reach 50% of total saturation. Recasting equation (1) to reflect the diffusion coefficient gives the following expression:

$$D = D_o e^{-\frac{E_a}{RT}} \quad (4)$$

Where D_o is an arbitrary constant. Equation (4) can be rewritten as

$$\ln(D_T) = \ln(D_o) - \frac{E_a}{RT} \quad (5)$$

The acceleration factor, AF, is the ratio between the normal working condition reaction rate and a higher test reaction rate. Using equation (5) the acceleration factor is obtained [22]

$$AF = e^{\left(\frac{E_a}{R}\right)\left(\frac{T_2 - T_1}{T_1 T_2}\right)} \quad (6)$$

Where T_1 is the theoretical service temperature and T_2 is the accelerated weathering temperature. To determine the AF, a diffusion study was performed. Three beakers of 3.5% salt solution were prepared and maintained for 60 days at different temperatures: 22, 45, and 65 °C. Three epoxy disks of PT2712 were submerged in each of the three beakers to ensure repeatability at each temperature. All 9 epoxy disks were desiccated for 48 hours to remove moisture and the mass recorded before submergence. Throughout the duration of the experiment, epoxy disks were periodically removed from their beaker, dried, weighed, and placed back in their respective beaker in accordance to ASTM D5229 / D5229M - 14 [29]. The percent mass increase was recorded for all epoxy disks for 60 days, and plotted versus time.

4. Experimental Results and Discussion

The Carbon-Epoxy composite material was subjected to prolonged submergence in a saline solution at high temperature to increase the rate of weathering. A diffusion study was performed to relate the experimental weathering time to service time and quasi-static

tests were conducted to determine the effects of weathering on the mechanical behavior of the composite material. In order to evaluate the dynamic behavior of the Carbon-Epoxy composite, shock tube experiments were performed to characterize the effects of weathering on the dynamic response of Carbon-Epoxy composite. The dynamic response of the Carbon-Epoxy composite was also compared with numerical results.

4.1 Acceleration Factor Results

The average percent increase in the mass of the epoxy disks for each temperature as a function of time is shown in Figure 5. Total saturation was obtained by the 65 °C samples at 60 days and the remaining temperature trials reached at least 50% saturation during the trial t_{50} .

Figure 5: Average epoxy mass increase over time

Knowing the t_{50} value and the thickness of the material, the diffusion coefficient D was calculated. Figure 6 shows the natural log of D plotted vs. $1/T$. The slope of the linear trend gives the activation energy of the epoxy matrix.

Figure 6: Activation energy calculation

The AF is then solved for, with the experimental submergence temperature (65 °C), and is displayed in Figure 7 (a) and Figure 7 (b). Figure 7 shows that with a decrease in service temperature, the AF will increase. Similarly, with increase in service temperature, the AF will decrease.

From the diffusion study, the calculated acceleration factors range from 48 to 18, corresponding to service temperatures of 10 °C to 22 °C respectively. With 30 days of accelerated ageing the real weathering time will be between 1.5 to 4 years of aging due to diffusion, depending on service temperature. For 60 days, the range is between 3 and 8 years. Since the diffusion of water into the epoxy matrix is a dominant factor contributing to composite material degradation, the calculated AF is a strong estimate used to correlate experimental versus service weathering times. However, it should be noted that after prolonged accelerated ageing, there is possible debonding between the fibers and the matrix, leading to further material degradation.

Figure 7: Acceleration factor (a) exponential variation of AF versus an increase in service temperature (b) visual representation of AF at various service depths

4.2 Quasi-Static Test

Material testing was performed to establish the quasi-static mechanical properties of the composite material before and after accelerated environmental exposure.

The average quasi-static behavior of the material before and after submergence is listed in Table 2 with the percent changes provided Table 3. From the quasi-static results it is shown that prolonged submergence reduced the modulus and strength of the Carbon-Epoxy significantly from the Non-Weathered material. The change in quasi-static results between 30 and 60 days is minor which corresponds to the fact that the matrix reached total saturation at 30 days. Since the majority of the degradation effects are accumulated during diffusion, this supports the assumption that water diffusion into the epoxy material is the primary factor in degradation of the mechanical properties of the composite to the ageing time frames considered in this study.

Table 11: Quasi-static properties after various submergence times

Exposure Time (Days)	Tensile Modulus (GPa)	Ultimate Tensile Strength (MPa)	Ultimate Flexural Strength (MPa)
0	53	563	545
30	47	496	426
60	46	492	424

Table 12: Variation (% change) in quasi-static behavior after various submergence times

Exposure Time (Days)	Tensile Modulus	Ultimate Tensile Strength	Ultimate Flexural Strength
30	-11%	-12%	- 22%
60	-13%	-13%	- 22%

4.3 Shock Tube Experimental

The following experimental results of the shock tube study compare the weathered (30 days) and Non-Weathered (0 days) cases. Weathered experiments for 30 and 60 days show similar quasi-static and dynamic behavior, so the comparison between 0 and 30 day blast scenarios will suffice. Two different support conditions for the plate under dynamic loading were considered; a simply supported case and a fixed boundary support. Figure 8 shows a schematic of the simple support boundary and of the fixed boundary case.

Figure 8: (a) Simply supported boundary case (b) Fixed-Fixed boundary case

A series of side view images for the simply supported boundary case, captured throughout the dynamic blast loading event is shown in Figure 9. From 0 to 0.8 ms the Non-Weathered and Weathered composite specimens display similar out of plane displacements, however the change in behavior can be noted at 1.2 ms. While the Non-Weathered composite maintains curvature and beyond the scope of the presented time frame, recovers elastically, the Weathered composite buckles at 1.6 ms at the moment of maximum deflection. At 1.6 ms, a crack develops through the thickness of the composite plate leading to a complete through thickness fracture of the plate.

Figure 9: Side view images of the simply supported plate during the shock loading event

From the 3D digital correlation images the full field behavior of the Non-Weathered and Weathered cases are quantified. The full field out of plane displacement on the same scale from 0 to 40 mm is plotted in Figure 10. As seen in the side view images, the displacement behavior shows a similar trend as the back face out of plane displacement provided by the DIC.

Figure 10: Full field out of plane displacement evolution for simply supported boundary

Figure 11: Simply supported center point out of plane displacement from 3D DIC

Using the full field 3D DIC data, the out of plane displacement is plotted for the 0 and 30 day weathering cases in Figure 11. Observed cracking of the 30 day weathered composite is determined to be at 1.6 ms from images captured by the side view camera. From the center point displacement versus time the rate of deformation remains consistent between all experiments, but the load bearing capacity significantly decreases, as an effect of weathering, leading to brittle catastrophic failure.

After 30 and 60 days of weathering the damage mechanisms leading to catastrophic failure showed distinct similarities. Due to similarities between the 30 day and 60 day weathering data, the 60 day weathering is not shown. From the side view images, delamination is seen developing from the compression side of the specimen, further propagating through the composite thickness. Commonly, continuous fiber composites do not behave well in compression due to the localized fiber buckling under compression. This behavior typically leads to localized buckling of the material while in compression. After significant time in submergence, the matrix material's mechanical properties inevitably deteriorated leading to failure in compression. This failure, therefore, compromised the structure leading to crack propagation in the composite laminate. The fibers in the post mortem images are short in nature meaning brittle, catastrophic failure occurred through fiber fracture rather than fiber pullout. Additional quantitative evidence of matrix initiated and matrix dominant failure is apparent in the DIC, in-plane strain data. The maximum strain achieved prior to through fracture in all weathered experiments is between 1.6-1.7%. The failure strain of the carbon fiber is 2.1% and for Carbon-Epoxy composite it is 1.7% [25]. This implies that while the damage occurs during the dynamic blast loading, the carbon fibers have not reached their failure limit.

Thus, during blast loading matrix cracking and delamination happens first leading eventually to fiber failure.

The case for the fixed boundary investigates the effect of zero displacements and rotations at the boundaries. In order to fix the boundaries of the composite, two vice clamps with knurled grips were utilized. Figure 12 shows a series of side view images for both the 0 and 30 day weathering cases for the fixed boundary case.

Figure 12: Side view images of the Fixed-Fixed composite plate during blast loading

Throughout the duration of the shock, the behavior of the composite material displays similar out of plane displacement for both weathering cases. Unlike the simply supported case, where the 30 day weathering case fractured, with fixed boundaries, the 30 day weathering case returns to its original undeformed state. For the simply supported case, the maximum moment is at the center of the plate, thus it fails at that region. However, for the fixed boundary case, the maximum moments are at the boundaries,

which increase the rigidity of the plate, and lower the center point deflection. It should be noted that the maximum moment for the fixed-fixed boundary case is lower than the maximum moment for the simply supported case. Figure 13 shows the full field out of plane displacement evolution for the 0 day and 30 day weathering for the case of fixed boundary conditions. The maximum out of plane displacement occurs at ~ 0.6 ms. The center point displacement history for this case is given in Figure 14.

Figure 13: Full field of the out of plane displacement evolution for Fixed-Fixed boundary

Figure 14: Fixed-Fixed center point out of plane displacement from 3D DIC data

Both weathering cases reached the maximum out of plane displacement at similar times. However, the 30 day weathering case reached a larger magnitude of center point displacement indicating a reduction in stiffness of the material due to ageing. The magnitude in out of plane displacement increased by a factor of 23.75 % between the Non-Weathered and the 30 day weathered case. At ~5ms, the 30 day weathered case reaches a region where the specimen no longer goes through beam oscillations, and the displacement linearly decays to zero. This behavior implies that the strain energy stored in the 30 day weathered case dissipated at a faster rate than Non-Weathered case. It should be noted that the fixed boundary condition case does sustain minimal grip slippage of less than 0.5 mm. Due to the slight slipping at the boundaries, the ability of the specimen to recover is hindered, since the material that slipped out of the clamps

during the outward deflection will encounter a resistive frictional force while it slips back into the clamps during specimen recovery.

5. Finite Element Modeling

Finite element modeling (FEM) for the blast loading experiments was performed using the Ls-Dyna code available from the Livermore Software Technology Corporation. Models have been developed for both the simply supported and fixed boundary condition cases. Based on the mechanical testing which indicated minimal additional material degradation beyond 30 day ageing, the 60 day weather scenario is not considered in the FEM study. All simulations were performed with Ls-Dyna release 6.1.1 in double precision mode.

The models for the simply supported and fully fixed boundary condition configurations are shown in Figure 15. In each of the models the composite specimens were modeled with fully integrated solid elements and consist of 4 through thickness elements. Each layer represents one of the 4, 2x2 twill woven plies in the plate. In the simply supported model, the full length of the 19.7 cm specimen is modeled with the pin supports located at 15.2 cm center-center spacing. Automatic Surface to Surface contact is defined between the pins and the back face of the plate. The full length of the specimen is accounted for to allow the plate to slide against the pins as the out of plane deflection occurs. In the fixed boundary condition configuration, only 15.2 cm of the specimen is modeled with all degrees of freedom along the top and bottom edges fixed. This approach is taken for computational efficiency as any additional elements beyond the fixed edge would also be fully fixed and thus sustain no strain or deformation. The material model used in the numerical simulation is the *Mat_Composite_Failure_Option_Model*

(Mat_059, Option = Solid). This is an orthotropic material definition capable of modeling the progressive failure of the material due to any of several failure criterion including tension / compression in the longitudinal and transverse directions, compression in the through thickness direction, and through thickness shear. Mechanical properties determined from the quasi-static experimentation were used to create the model. Although mechanical properties are known to change with respect to the rate of loading, the quasi-static properties for each weathering scenario were sufficient to conduct the numerical study. The support pins were modeled as linear elastic steel with appropriate material properties.

The blast loading was applied to the surface of the composite plate by applying the pressure profile measured during the respective experiments to the composite specimen in the models. This ensures that there is consistent loading between the model and experiments. Due to the width of the specimens being larger than the internal diameter of the shock tube, there is a need to numerically capture the spreading of the pressure over the specimen surface that occurs as the plate deforms outwards. During the onset of loading there is a uniform pressure loading over the central circular area corresponding to the inner shock tube diameter which is equal to the pressure recorded during the experiment. However, as the specimen deforms outwards there is a “venting” of the gas as a gap forms between the shock tube face and the specimen. This results in an expanded loading area over the face of the plate which is assumed to vary linearly from the measured pressure profile at the inner surface of the shock tube to zero at the outer edge of the plate. The linear variation in pressure is accounted for in a stepwise fashion as

shown in Figure 16. A similar computational approach is documented by Yazici et al [26].

Figure 15: Finite element model details

Figure 16: Finite element model pressure loading schematic

5.1 Finite Element Model Correlation to Test Data

The correlation of the finite element models to the corresponding experimental results consists of comparisons between pointwise (centerpoint) time histories as well as the full field deformation profiles. A comparison between the experimental and finite element simulation full field out of plane displacement for the simply supported case is shown in Figure17.

Figure 17: Full field simulation and experimental visualization

The center point displacement data captured during the experiments with the DIC method is used as a pointwise time history basis to correlate and validate the finite element model results. The center-point time history correlation between the experimental data and the corresponding computational simulation for the simply supported and fully fixed boundary condition configurations are provided in Figure 18 and Figure 19, respectively. The correlations presented in the figure show that there is a high level of agreement between the experiment and simulations, both temporally and in terms of displacement magnitudes. For both the simply supported and fully fixed conditions the simulation and experiment results exhibit nearly consistent results during the initial deformation up to the point where maximum value is obtained. For the 0 Day Simply supported case the model results display a slightly faster recovery after maximum out of plane displacement although the rise time and peak displacement are in excellent agreement. For the fixed support condition models, both 0 and 30 day weathering, it is seen that although the rise time and maximum displacement values are in very good agreement, the numerical models recover significantly more rapidly than the corresponding experiments. The authors believe that this difference in the recovery process between the model and experiments is due to the difficulty in matching a “Fully Fixed” numerical boundary condition to a corresponding experimental condition. During the experiments the composite specimens were clamped between two grips on the top and bottom to provide maximum gripping force. However, due to the forces involved during the blast loading, a minimal amount of material slippage was observed during the out of plane deformation process as a result of the material being “drawn” in from the grips. During the recovery phase this material must be forced back into the grips, which tends to

slow down the process as compared to the numerical model. Even a very small amount of material slippage within the grips has the effect of “shortening” the length of a buckled beam and preventing a return to its un-deformed shape. Finally, it is seen from Figure 18 that the onset of damage for the 30 Day Simply Supported case occurs slightly later than the corresponding experimental fracture. Although the computational models utilize experimentally based values of compressive and tensile strengths, these values are a nominal value representing a statistical basis of multiple tests and the true value for any given specimen is +/- from that average. Furthermore, the models assume a uniform specimen in terms of material properties and does not account for manufacturing variability or minor internal defects which can contribute to the onset of damage or slightly weaker/stronger areas of the plates as compared to the gross material strengths. That the model is able to predict the onset of damage in a consistent manner as observed during the testing is significant. Overall, it is shown that the models are able to accurately predict the transient response of the composite specimens, particularly the initial rise and peak displacement.

Figure 18: Simply supported condition – Center point time history correlation

Figure 19: Fixed support condition – Center point time history correlation

6.0 Conclusions

A series of experiments were conducted to investigate the dynamic behavior of Carbon-Epoxy composite materials after the specimens were subjected to aggressive marine environments. A 3D DIC technique was utilized with high-speed photography to record the dynamic behavior due to blast loading of said materials. Based upon experiments performed to study effects of prolonged submersion, it was concluded after significant salt water exposure, the mechanical properties of a carbon/epoxy composite material were degraded. The summary of the results are as follows:

- The diffusion study showed that the AF for the composite material ranged from 18 to 48. This corresponds to a range of service temperatures from 22 °C to 10 °C. The actual aging time of the composite ranges from 1.5 to 4 years for 30 days of submergence and 3 to 8 years for 60 days of submergence.
- After 30 days of exposure, the quasi-static mechanical properties decreased significantly. The tensile modulus, tensile strength, and ultimate flexural strength decreased by 11%, 12%, and 22% respectively.
- After 60 days of submergence the quasi-static mechanical properties of the composite essentially did not change from the properties at 30 days submergence.
- Shock loading experiments displayed vastly different behavior with respect to 0 day versus 30 and 60 day exposures.
 - For the simply supported case, the Non-Weathered (0 day) composite plates displayed maximum out of plane displacement on

average of ~20 mm. After multiple oscillations the plates elastically returned to their original form. Internal damage was not measured/quantified post mortem in these experiments.

- For the simply supported case, the 30 and 60 day plates showed similar out of plane displacements versus time as the 0 day specimens, but catastrophically failed at roughly 20 mm. The plates displayed delamination on the compression face of the plates.
- It can be concluded that brittle-like failure occurred throughout the dynamic event since the region of failure there was visible evidence of short fibers, thus implying fiber fracture rather than fiber pullout.
- For the fixed-fixed support case, the Non-Weathered (0 day) composite displayed a maximum out of plane displacement of 5.57 mm. The fixed boundary restrained the out of plane displacement of the plates and thus sustained lower deflection magnitudes than the simply supported case.
- For the fixed support case, the 30 day and 60 day plates showed similar out of plane displacements. However, the frequencies of beam oscillations in these cases were lower than the 0 day case. This is due to the reduction in the stiffness of the material with ageing.

- The numerical results for both boundary conditions are in good agreement with the experimental data.

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CHAPTER 4

NEAR FIELD UNDERWATER EXPLOSION RESPONSE OF POLYUREA COATED COMPOSITE CYLINDERS

by

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Abstract

The response of composite cylinders to near field underwater explosive (UNDEX) loading, including the effects of polyurea coatings, have been studied through experiments with corresponding computational simulations. Experiments were conducted on woven E-glass/epoxy roll wrapped cylinders in three unique configurations: (1) base composite, (2) base composite with a thin (100% composite thickness) coating, and (3) base composite with a thick (200% composite thickness) coating. Each cylinder configuration was subjected to near field underwater explosive loading in a large diameter test tank at charge standoff distances of 2.54 cm and 5.08 cm. The response of the cylinders on the non-loaded side was evaluated through high speed photography coupled with three-dimensional Digital Image Correlation (DIC). Transient deformation and Post-mortem damage comparisons were made to evaluate the effects of the applied coatings. The LS-Dyna finite element code has been utilized to conduct corresponding computational simulations of the experiments to allow for additional evaluations of the cylinder response. The simulations are shown to provide high correlation to the experiments in terms of pressure loading and final damage mechanisms. Results for the internal / kinetic energy levels and the material strains as determined through the simulations are presented. The experimental and numerical results show that the application of a polyurea coating is effective for significantly reducing damage in the cylinders. It is also shown that there is an increase in both material internal energies as well as overall strains with increasing coating thickness.

1. Introduction

Composite materials have several characteristics which make them particularly appealing for utilization in marine environments such as high strength to weight ratios, superior resistance to corrosion, and overall reductions in required maintenance. When structures composed of these advanced materials are fielded in a marine environment, in addition to operational loading, they may be subjected to harsh transient conditions such as underwater explosions. Maximizing the benefit of these materials, particularly for minimum weight and increased survivability, requires a full understanding of the response to such loadings and the effects of any potential mitigators, such as blast resistant polymeric coatings, in order to avoid overly conservative designs.

Studies on the response of composites subjected to UNDEX have generally focused on far field loading in which the encroaching shock front is nearly planar and there is no interaction between the UNDEX bubble and the structure. LeBlanc and Shukla [1,2] have studied the response of both flat and curved E-glass/epoxy composite plates to far field loading through both experimental and computational means. Avachat and Zhou [3] investigated the response of monolithic as well as sandwich structure composite cylinders to underwater impulsive loading imparted via a novel Underwater Shock Loading Simulator. The key findings were that the inclusion of a foam core reduced damage to the cylinder as compared with a monolithic composite wall of similar mass. Further, decreasing foam core density resulted in a decrease in observed damage. Mouritz, et al., [4], conducted a study of the development of damage in a glass reinforced composite subjected to underwater explosive loading at increasing pressures. Both air backed and water backed conditions were evaluated. In the case of the water backed laminates no damage or degradation in strength was noted. In the air backed laminates

delamination and matrix cracking led to a degradation of the residual strength of the composite.

Near-field loading is generally characterized by a spherical shock front impinging upon the structure as well as interaction of the UNDEX bubble and the target structure. This can lead to a highly localized damage response in the structure rather than the more global deformation characteristic of the far field loading. In LeBlanc, et al., [5], coated and non-coated flat E-glass/epoxy plates were subjected to near field UNDEX loading. Deflections and damage extents were compared across the plate configurations. It was found that the application of a polyurea coating reduced the overall response of the plate and significantly reduced damage to the composite. Brett, et al., [6,7], presented a study of steel cylinders subjected to near field UNDEX. They observed that at standoff distances less than the UNDEX bubble radius the bubble was attracted to the cylinder and collapsed upon it resulting in a significant structural response.

Recently polyurea has found interest as a potential blast mitigating coating. It is an easy to apply polymer that exhibits stiffening with increasing strain rate of loading and is finding use as a post-design phase enhancement. Several studies have been conducted to determine polyurea's ability to reduce structural response to blast loading as well as reduce damage in materials. LeBlanc, et al., [8,9] studied the response of composite plates coated with polyurea to UNDEX loading. It was determined that both location and thickness of the coating were important considerations in efforts to reduce damage and deflection. When considering a weight penalty there is a coating thickness at which the polyurea becomes more advantageous in mitigating the out of plane response of the structure than simply increasing the base composite thickness. Tekalur, et al., [10] and

Gardner, et al., [11] studied monolithic and sandwich composites, respectively, subjected to air blast loading. It was found that polyurea was able to mitigate damage and deflection in the monolithic plates. For the sandwich composites blast resistance was improved by placing the polyurea between the back face sheet and the foam core; performance was degraded when the polyurea was applied between the front face sheet and the foam core.

2. Materials

This investigation tested composite cylinders in a base configuration comprised solely of the composite material as well as the base composite with applied polymeric coatings. Material details are outlined in the following two sections.

2.1 Composite

The composite cylinders were manufactured by ACP Composites, Inc. of Livermore, CA. The material is a cured, roll-wrapped E-glass/epoxy with a woven 0°/90° structure produced by Axiom Materials, Inc of Santa Ana, CA as AX-3112T. The cylinders have a wall thickness of 1.14 mm with 4 plies through the thickness and a laminate schedule of [0/45/45/0]. Resin content is 38% by weight and the areal weight is 0.49 kg/m² per ply. The material properties, as provided by the manufacturer, are listed in Table 1.

Table 1 - Composite Material Properties

	Strength (MPa)	Modulus (GPa)	Test Method
Tensile	531	29	ASTM D638
Compressive	510	26	ASTM D695
Interlaminar Shear	60		ASTM D1002

2.2 Polyurea

A polyurea coating, Dragonsshield-BC, was manufactured and applied via spray-cast by Specialty Products, Inc., of Lakewood, WA. This is a 2-part polymer which may be applied to a variety of surfaces. The coating was applied in two thicknesses, 100% and 200% of the composite wall thickness, to the outer surface of the cylinders and was cured at 160°F for 48hrs. As in the previous study by the authors [5] this configuration is intended to represent the post-design and manufacture application of the coating as reinforcement rather than an integral design aspect.

A characterization of the polyurea material was conducted at strain rates of 0.01s^{-1} to 100s^{-1} for both tensile and compressive loading in a previous study, [8]. Additionally, during the same study, strain rates of 2000 s^{-1} in compression were achieved via a Split Hopkinson Pressure Bar (SHPB). It is assumed that the behavior of the polyurea is similar in tension for the 2000 s^{-1} strain rate. Figure 1 illustrates the stress-strain behavior of the Dragonsshield-BC polyurea monolithic material over the range of tested strain rates. It is clear from Figure 1 that with increasing strain rate the response of the material becomes stiffer in both tension and compression, exhibiting a distinct plateau in tension.

Figure 1 - Dragonshield-BC Polyurea Stress-Strain Behavior [8]

3. Experimental Set-up

The following sections detail the experimental set-up for this investigation. A full account is given regarding the specimen geometry, test vessel, and data acquisition system and methods.

3.1 Specimen Geometry

The outside diameter of the base composite cylinder is 7.44 cm with a thickness of 1.14 mm. The total length of the cylinder is 40.64 cm with an unsupported length of 38.1 cm. Each end of the cylinder is fitted with an aluminum endcap protruding 12.7 mm into the length of the cylinder which seals against the inner diameter of the cylinder via a rubber o-ring to prevent water infiltration during experiments. The endcaps are held in place and the cylinder further sealed by the application of epoxy to the joints between the endcaps and cylinder. In addition to the base cylinder, cylinders were prepared with

either a thick ($2.26 \text{ mm} \pm 0.5 \text{ mm}$) or thin ($1.19 \text{ mm} \pm 0.3 \text{ mm}$) outer coating of polyurea.

Figure 2 provides a schematic of the cylinder construction.

Figure 2 - Cylinder Construction

The areal weights and wall thicknesses of each cylinder configuration is given in Table 2, below.

Table 2 - Cylinder Configuration Wall Thicknesses and Areal Weights

	Thickness (mm)	Areal Weight (kg/m²)
Composite	1.14	1.96
Thin Coating	2.34	3.15
Thick Coating	3.04	3.90

3.2 Explosive Charge

The explosive used in this study is an RP-503 charge manufactured by Teledyne RSI, Inc. of Tracy, CA. It contains 454mg of RDX and 167mg of PETN. A

characterization of the explosive was conducted in equivalent test conditions. Figure 3 provides a plot of bubble diameter over time from detonation until the initial collapse of the bubble and it is shown that the maximum bubble diameter was measured to be 21.7 cm. As will be discussed later in the paper, this is significant because the maximum bubble diameter is larger than the charge standoff itself and thus there is an interaction between the bubble and the cylinders. Figure 4 provides the pressure profile in the water at three different radial distances from the charge center. For the purposes of pressure characterization, these pressure profiles are obtained from a free field experiment with no cylinder present. The characteristic $1/R$ decay of peak pressure with standoff distance is observed. The characterization of the RP-503, as well as all subsequent experiments, was performed at ambient tank pressure with no additional pressure supplied to the tank.

Figure 3 - RP-503 Bubble Diameter – Growth and Collapse

Figure 4 - RP-503 Characterization Pressure Profile

3.3 Test Tank

All experiments were conducted in a large diameter (2.1 m) water filled cylindrical pressure vessel located in the University of Rhode Island Dynamic Photomechanics Laboratory (DPML). An array of windows along the horizontal axis of the test tank allow for full viewing and recording of experiments as well as illumination of the test article. The cylinder is mounted and held in the center of the tank via cables suspended from pad eyes located along the tank walls above and below the specimen. The cables include a ratcheting mechanism for adjusting the position of the specimen within the tank as well as tensioning of the cables to minimize rigid body motion of the test article during transient loading. Pressures in the vicinity of the cylinder were recorded using PCB 138A05 tourmaline dynamic pressure sensors with data recorded at a sampling rate of 2 MHz.

3.4 High Speed Video and Digital Image Correlation

Three high speed video cameras, FastCam SA1, were used to capture video during experiments. One camera was mounted to align with the longitudinal axis of the cylinder, providing a side view of the UNDEX event and two cameras were arranged to provide a stereoscopic view of the cylinder on the opposite side of the explosive. High intensity lights were used to provide the necessary illumination for the high speed video capture. Frame rates of 36,000 fps were used for both the side view and front view cameras.

Each cylinder was prepared for Digital Image Correlation (DIC) data extraction in order to obtain full-field in- and out- of plane displacements of the cylinders during the test event. A coating of white paint was applied to each cylinder and a random pattern of

black speckles was applied using flat black paint. Calibration of the DIC system, which includes the two stereoscopic front view cameras, for use in the large diameter test tank was accomplished by Gupta, et al., in [12]. Post processing of the front view high speed video to obtain full field displacements was accomplished using the VIC-3D software package. Displacements are obtained by comparison of pixel subsets of the random speckles between images as the cylinder deforms and the reference un-deformed state.

4. Experimental Methodology

For each experiment the cylinder under test was fixed within a wire support cage used to secure the pressure sensors and the explosive at set distances from the cylinder surface. Figure 5 illustrates the arrangement of the pressure sensors around the cylinders. Collars were affixed to the cylinder endcaps to which the wire cage and the support cabling were attached. The cylinder was then firmly secured in the center of the tank using the support cables and the alignment with the high speed video cameras was confirmed. Figure 6 provides a schematic of the test set-up.

Figure 5 - Pressure Sensor Arrangement (not to scale)

Figure 6 - Test Configuration, (a) Tank Schematic, (b) Cylinder in Support Cage

Each cylinder configuration (base composite, thick coating, and thin coating) was tested at two charge stand-offs, 2.54cm and 5.08cm. Two experiments of each cylinder configuration/charge standoff combination were conducted to ensure repeatable results. The charge distance to the cylinder surface was maintained by fixing the charge within the support cage with monofilament line, see Figure 6b. All experiments were conducted at ambient pressure within the flooded tank.

5. Results and Discussion

5.1 Bubble-Cylinder Interaction and Local Pressures

The near field nature of experiments resulted in a complex interaction between the UNDEX bubble and the cylinders. In all experiments the interactions were characterized by a splitting of the bubble with one bubble forming in front (non-charge side) of the cylinder and the bulk of the UNDEX bubble remaining behind (charge side) the cylinder. Initially, as the shock from the explosive detonation passes the cylinder small cavitation

bubbles form on the surface of the cylinder. This happens at 0.36 ± 0.08 msec for the 5.08 cm charge standoff and at 0.23 ± 0.05 msec for the 2.54 cm standoff. This is the result of the UNDEX shock wave interacting and passing by the cylinder and is consistent with the observations of Brett and Yiannakopolous [6]. As time progresses, the cavitation bubbles begin to coalesce. Following coalescence the cavitation bubbles collapse in front of the central region of the cylinder after about 1 msec. Figure 7 provides images of key developments observed during the bubble-structure interaction during an experiment conducted at a charge standoff of 2.54 cm on a cylinder with a thick coating applied. Similar features are observed in the experiments with a 5.08 cm charge standoff with difference in timing in accordance with the increased distance between structure and bubble center. No significant differences were noted in the bubble interaction between uncoated and coated cylinders.

(a)

(b)

Figure 7 - Bubble Growth and Interaction (a) Front View, (b) Side View

At around 5.0 msec for the 2.54 cm standoff a large bubble can be seen to form in the front of the cylinder. For the case of the 5.08 cm standoff the bubble forms around 5.5 msec from detonation. The formation of the front bubble coincides with bubble diameters of 18.76 cm and 19.20 cm for the case of the 2.54 cm and 5.08 cm standoffs respectively. Stack-up of the standoff and cylinder diameter show that the UNDEX bubble radius is approximately 3 cm (2.54 cm standoff) and 1 cm (5.08 cm standoff) shorter than the length of the standoff and cylinder diameter. This does not account for cylinder deflection which cannot be determined due to the bubble obscuring the cylinder in the high speed video. This result suggests bubble migration, whereby the center of the bubble is attracted

toward the structure. Analysis of side view images shows a horizontal elongation of the bubble as it interacts with the structure and attachment of the bubble to the surface of the cylinder, Figure 8.

Figure 8 - Bubble Attachment to Cylinder, side view

The large bubble which forms on the non-charge side of the cylinder collapses upon itself at approximately 12.7 msec. This provides a secondary loading of the cylinder. A third loading occurs with the collapse of the main UNDEX bubble approximately 4 msec following the collapse of the front bubble.

The pressure recorded on the non-charge side of the cylinder is shown in Figure 9. This pressure profile was recorded during the experiment from which the images presented above were taken. At 0.28 msec a second pressure peak (4.23 MPa) is recorded. This is the reflection of the incident shock from the surface of the cylinder. At

1.4 msec and 2.8 msec small pressure peaks can be seen which are the result of successive reflections of the shock wave from the walls of the test tank. From approximately 6.40 to 10.50 msec the front bubble encapsulates the pressure sensor. At 10.50 msec the passage of the bubble edge past the sensor results in a small pressure increase. At 12.70 msec the pressure sensor records the peak resulting from the collapse of the front bubble which is quickly followed by the reflection from the surface of the cylinder. The magnitude of this pressure peak is 0.95 MPa, 18% of the initial shock recorded at the same location, and represents a significant secondary loading of the cylinder from the bubble collapse. Following the initial collapse the bubble expands and collapses for a second time at 15.12 msec. At 16.63 msec an additional increase in pressure is observed due to the collapse of the main UNDEX bubble behind the cylinder. Due to the low magnitudes of the reflected pressure from the tank walls it is assumed that the primary cylinder damage, when present, occurs due to the initial charge detonation pressure.

Figure 9 - Pressure Profile, Non-Charge Side

5.2 Transient Cylinder Response

The physical response of the cylinders to the near field UNDEX loading will be described primarily by the radial displacement of the center point on the non-charge side of each cylinder, determined via image analysis through DIC. Due to the bubble interaction with the cylinder described in the previous section the displacements of the cylinders could not be determined for the entirety of the loading events. Large scale cavitation on the surface of the cylinder and the formation of a bubble between the cylinder and the cameras prevent DIC analysis by obfuscation of the speckle pattern. Comparisons will be limited to the time period for which DIC results are available and may not include the peak displacements experienced by the cylinder during test.

5.2.1 Charge Standoff – 5.08 cm

The radial displacement of the cylinders exposed to an UNDEX at a 5.1 cm charge standoff is characterized by an initial global deformation in the positive radial direction (away from the charge and toward the cameras) followed by an inflection and dimpling in the center of the cylinder away from the camera view and toward the charge location as the cylinder rebounds. Figure 10, below, depicts the radial displacement of line segments along the cylinder centers for all three cylinder configurations (uncoated, thin coated and thick coated) over time. At 0.5 msec the center point displacement for the coated cylinders is 2.5 mm in the positive direction. At this period in time the uncoated cylinder lags with a center point displacement of 1.9 mm in the positive direction. At 1.0 msec the uncoated cylinder has overtaken both the coated cylinders with a positive central displacement of 4.8 mm. The cylinder with the thin polyurea coating has a central displacement of 4.2 mm and the cylinder with the thick coating a 3.7 mm center point deflection. At 2.5msec all cylinders display a negative center point deflection of

approximately 2 mm. Scattered cavitation on the surface of the cylinders then obscures portions of the speckle pattern on each cylinder and precludes a high confidence in directly comparing further displacement values.

Figure 10 - Centerline Displacements for 5.08 cm Standoff

Full field displacement contours over the initial 2.75 msec of the experiments can be seen in Figure 11. The full field contours confirm the general shape suggested by the center line displacements presented in Figure 10 above. Comparisons with the uncoated cylinder are difficult due to obscuration of the speckle pattern in that image set after 1.25 msec.

Figure 11 - Full Field Radial Displacement Contours

In [9] LeBlanc, et al., introduced the areal weight ratio (AWR) as a means to account for the weight penalty associated with adding material, such as a coating, to an existing design. The AWR acts as a multiplier to quantify the added mass penalty associated with any additional material in terms of transient deflection. The AWR is given by Equation 1 as:

$$AWR = \frac{W_2}{W_1} \quad (1)$$

W_1 is the areal weight of the base material. In this case it is the areal weight of the composite from which the cylinder is constructed. W_2 is the areal weight of the base material plus any added material or coating. The AWR for each cylinder in this study is given in Table 3.

Table 3 - Areal Weight Ratio

	AWR
Base Cylinder	1
Thin Coating	1.61
Thick Coating	1.99

Figure 12(a) provides a comparison of center point deflection of the three cylinder configurations at 1.0 msec as determined through DIC analysis. The selection of center point deflections at 1.0 msec as a basis of comparison is driven by the low confidence in the precision of the data past that point in time due to large areas of cavitation and bubble activity following that point in time. Figure 12(b) illustrates the center point deflections with the AWR penalty applied. When added mass is accounted for the thick coating results in an increase in normalized deflection of 54%, from 4.8 mm to 7.4 mm. The thin coating results in an increase in normalized displacement of 42%, from 4.8 mm to 6.8 mm. This degradation in performance was also observed in previous studies by LeBlanc, et al., [5,8,9] on both flat and curved plates subjected to far field loading as well as near field UNDEX loading of flat composite plates. In [9], LeBlanc, et al., studied an array of polyurea coating thicknesses on the response of E-glass/epoxy cross-ply panels and found that there is a coating thickness which does provide an improvement in transient response characteristics even when weight penalty is considered. A similar result for near field

UNDEX loading of composite cylinders with polyurea coatings cannot be ruled out by the findings of this study.

Figure 12 - Center Point Displacements at 1msec – 5.08 cm Charge Standoff, (a) Absolute, (b) Weight Penalty Applied

5.2.2 Charge Standoff - 2.54 cm

With a charge standoff of 2.54 cm the deflection of all three cylinder configurations is characterized by global deformation in the positive radial direction during the time domain for which DIC analysis is possible. It is not clear whether or not the cylinders develop the negative radial dimpling observed for the cylinders tested at a standoff of 5.08 cm. Figure 13 provides an illustration of the radial displacement of the center line of each cylinder configuration over time. Again it can be observed that the uncoated cylinder lags in central displacement initially and then overtakes the coated cylinders over time. At 3.0 msec the center point displacement of the uncoated cylinder is 23.9 mm

in the positive radial direction (toward the cameras). The thin coating results in a displacement of 21.9 mm and the thick coating 21.3 mm.

Figure 13 - Centerline Displacements for 2.54 cm Standoff

Full field radial displacement contours are shown in Figure 14, below. The bowed shape indicated by the line segment plots in Figure 13 can be discerned in the contour plots.

Figure 14 - Radial Displacement Contours for 2.54 cm Charge Standoff

The center point deflection at 3.0 msec is used to compare the performance of the uncoated and coated cylinders in accordance with the method outlined in the previous section. Figure 15(a) shows the absolute displacement while Figure 15(b) shows the normalized (by AWR) displacement. Again, it can be seen that the application of the polyurea coatings degrades performance of the cylinders when the additional weight is accounted for. The normalized peak displacement is increased from 23.9 mm to 35.3 mm for the case of the cylinder with the thin coating, 48%. For the thickly coated cylinder normalized peak displacement increases 77%, from 23.9 mm to 42.4 mm. This result shows that at the closer charge standoff (2.54 cm) the application of the polyurea coatings has a much more deleterious effect on the transient response of the cylinder (as adjusted for weight) than at the larger (5.08 cm) standoff, where the change in normalized displacement were 42% and 54% for the thin and thick coating, respectively.

Figure 15 - Center Point Displacements at 3 msec – 2.54 cm Charge Standoff, (a) Absolute, (b) Weight Penalty Applied

5.3 Damage

While the application of the polyurea had minimal effect on the transient response of the cylinders, and a detrimental effect when accounting for weight penalty, there was a significant effect of the polyurea coatings on the damage observed in the cylinders post-test. For both charge standoffs the damage was significantly reduced with increasing thickness. For this study damage assessments are limited to post-mortem evaluation as evolution could not be ascertained by inspection of the high speed video as most of the damage occurred on the charge side of the cylinders and was not visible to the cameras.

Figure 16 and Figure 17 provide interior and exterior views, respectively, of the damage in the cylinders tested at a charge standoff of 2.54 cm. Damage in the uncoated

cylinders was dominated by large cracks and missing sections of material. At the center point of the cylinder, nearest the charge location, sections of delamination can be seen along the edges of the missing portions of the cylinder, Figure 17(a). The damaged section extends 23 cm along the 40.64 cm length of the cylinder. Additionally, curving cracks, suggestive of an ellipsoid indenting of the cylinder, at approximately $\pm 90^\circ$ from the cylinder centroid can be seen, Figure 17 (b).

**Figure 16 - Interior View of Cylinder Damage – 2.54 cm Charge Standoff, (a)
Uncoated, (b) Thin Coating, (c) Thick Coating**

Figure 17 - Exterior View of Cylinder Damage – 2.54 cm Charge Standoff, (a) Uncoated, (b) Thin Coating, (c) Thick Coating

Figure 18 - Delamination Along Crack Edge (a), Curved Crack at $\pm 70^\circ$ (b)

For the thinly coated cylinders tested at 2.54 cm the curving cracks are also observed. They occur at a similar angle although extend only 3.8 cm, **Error! Reference source not found.**(a). The damage to these cylinders is dominated by large

circumferential and longitudinal cracks emanating from the point closest to the charge location. At the nexus of the longitudinal and circumferential cracks the damage extends through the thickness of both the composite and coating, Figure 19(b). The circumferential crack continues to extend through the coating to its termination at $\pm 90^\circ$. The longitudinal crack extends through the coating for only 4.1 cm on either side of the center point and then continues an additional 5.6 cm through the thickness of the base composite only. As with the uncoated cylinder, delamination can be observed near the area closest to the charge on the interior and exterior surfaces, Figure 20 (b) and Figure 21 (b).

(a)

(b)

Figure 19 - Damage in Thin Coated Cylinder – 2.54 cm Charge Standoff, (a) Curving Crack, (b) Nexus

In the cylinder with a thick coating of polyurea the damage was similar in character to that observed in the thinly coated cylinder but lesser in extent. Again, longitudinal and circumferential cracks extend from the center point, nearest the charge location. Delaminations can be observed on the interior of the cylinder, Figure 20 (c). The circumferential crack, which ranges $\pm 90^\circ$ from the centroid extends through the thickness of the base composite as well as the coating. Fiber pull-out along the interior

edge of the crack can be seen in Figure 21 (c). The curving cracks at the termination of the circumferential cracks in the uncoated and thinly coated cylinders are not present in the thickly coated cylinders. The longitudinal crack, visible in Figure 21 (c), runs 7.6 cm along either side of the center point but extends only through the thickness of the base composite.

As would be expected, the damage to the cylinders tested with a charge standoff of 5.08 cm was less severe for all configurations. Figure 20 and Figure 21 provide interior and exterior views, respectively, of the damage in these cylinders. For the uncoated cylinders the damage is primarily described by a “punched-in” ellipsoid area circumscribed by a fairly clean crack through the thickness of the composite. An additional crack, running 19.7 cm along the length of the cylinder, is visible below the main ellipsoid crack. It can be seen clearly on the interior of the cylinder, Figure 20 (a). Emanating from this secondary longitudinal crack is a circumferential crack along the interior of the cylinder. This crack does not extend through the thickness of the cylinder.

**Figure 20 - Interior View of Cylinder Damage – 5.08 cm Charge Standoff (a)
Uncoated, (b) Thin Coating, (c) Thick Coating**

Figure 21 - Exterior View of Cylinder Damage – 5.08 cm Charge Standoff (a) Uncoated, (b) Thin Coating, (c) Thick Coating

In the thinly coated cylinder exposed to a charge standoff of 5.08 cm longitudinal and circumferential cracks can be seen on the interior of the cylinder, Figure 20 (b). The circumferential cracks extend $\pm 70^\circ$ about the centroid, however, they do not extend through the coating, only the base composite. The longitudinal crack extends 8.9 cm on either side of the point closest to the charge location and penetrates through only the base composite, not the polyurea coating.

The cylinders with the thick polyurea coating (5.08 cm standoff) showed significant reduction in damage even as compared to the thinly coated cylinders. In these cases the damage was confined to two small sections of damage at $\pm 60^\circ$ from the centroid. These damage areas consisted of circumferential cracks of 2.5 cm length and

longitudinal cracks of about 1.3 cm centered against the circumferential cracks. These cracks, which extend only through the base composite, can be seen in Figure 20 (c).

6. Finite Element Modeling

The experiments which have been previously discussed, have been simulated utilizing the the LS-DYNA finite element code. The purpose of the modeling effort is twofold: (1) Implement a methodology for the simulation of near field UNDEX loading on composite cylinders, and (2) utilize the correlated model to extract additional information pertaining to the response of the cylinders which is not readily captured through experimental measurement techniques. In the current study, a fully coupled Lagrange-Eulerian formulation is utilized due to the nature of the problem, namely highly curved wave fronts and dependence of the decay of the pressure wave during propagation through the fluid domain. This approach allows for accurate representation of the detonation of the explosive charge, resulting pressure wave propagation into the fluid, and the transient fluid structure interaction between the pressure wave and the cylinder. All models are constructed in the CGS unit system.

6.1 Model Overview

The finite element model representation of the cylinder explosive experiments is provided in Figure 22, and consists of the cylinder body / endcap, polyurea coating, surrounding tank water, internal air, and the RP-503 charge. The model represents a selected subdomain of the full experimental test tank for computational efficiency. Furthermore, due to the nature of the setup, 2 planes of symmetry are utilized in the model as indicated in Figure 23 which results in an overall $\frac{1}{4}$ model of the experiments.

Included in the model is the $\frac{1}{4}$ representation of the cylinder/fluid with the fluid domain extending out to a distance of 12.93 cm from the outer surface of the cylinder. The maximum charge standoff considered in the experiments was 5.08 cm, and thus the inclusion of the domain to a distance larger than twice the standoff value ensures boundary effects do not influence the fluid structure interaction. Furthermore, the use of such a sub-domain for the modeling of the corresponding experiments is deemed appropriate as the loading of the cylinder and subsequent response occurs sufficiently fast that reflections from the tank walls do not affect the overall transient response of the cylinder. In the model the outer surface of the fluid sub-domain is prescribed a non-reflecting boundary condition (*BOUNDARY_NON_REFLECTING) which allows the associated pressure waves to leave the domain, as they would in a free field detonation, rather than reflect off of the free surface.

The Eulerian components of the coupled model consist of the water, air, and explosive charge, and are modeled with solid elements utilizing ALE multi-material element formulation (Type 11 solid element). The fluid/explosive components of the model are fully defined through the combined use of a material model and an equation of state (EOS). The details of the EOS definitions and parameters are found in [14]. The water is represented with the *Mat_Null material definition coupled with the Gruneisen EOS. The density and sound speed are 1 g/cm^3 and $149,000 \text{ cm/s}$, respectively. Similarly, the air is defined with the *Mat_Null material but is combined with the Linear Polynomial EOS. The density of the air is 0.0013 g/cm^3 and by defining C_0 , C_1 , C_2 , C_3 , and C_6 equal to zero, and C_4 , and C_5 equal to $\gamma-1$, a gamma law EOS is achieved with the parameters provided in Table 4. A *Mat_High_Explosive_Burn material model combined with the

JWL EOS is used to fully define the explosive charge. It is noted that the RP-503 charge contains both RDX (454 mg) and PETN (167 mg), however the model assumes a charge comprised of only RDX, with the overall charge weight being maintained. This is deemed suitable for the model since the RDX is the larger component and RDX and PETN have similar JWL coefficients. The Material and EOS parameters for the RDX are provided in Table 5 and Table 6.

Table 4 - Air EOS Parameters

C0	0
C1	0
C2	0
C3	0
C4	0.4
C5	0.4
C6	0

Table 5 - RDX Material Parameters

ρ (g/cm ³)	1.77
D (cm/s)	850e3
Chapman-Jouget Pressure (dyn/cm ²)	3.41e13

Table 6 - RDX EOS (JWL) Parameters [13]

A	7.78e12 (dyn/cm ²)
B	7.07e10 (dyn/cm ²)
R1	4.485
R2	1.068
Ω	0.3
E _o	5.93e10
V _o	1.0

The structural aspect of the coupled model consists of the composite cylinder and the polyurea coating. The edges of the cylinder corresponding to the horizontal and longitudinal planes are represented through appropriate symmetry boundary conditions.

The cylinder itself however is free to move within the fluid domain as there were no rigid constraints applied during the experiments. The composite cylinder in the simulations is modeled using a single layer of shell elements, Figure 23, with a nominal element sizing of 2.5 mm. The laminate schedule of the cylinder is accounted for through the use of the *Section_Shell property, with the ICOMP parameter activated. This means of modeling the composite laminate allows for the layup, including ply angle, to be defined within the section card. Each ply is represented as having two through thickness integration points so as to capture the correct bending behavior on a per ply basis. The polyurea material is represented in the model by solid elements, Figure 23, with a constant stress formulation and the coatings are assumed to be perfectly bonded to the cylinders. The only debonding observed during the testing was directly in way of the composite damage zones and thus the assumption of a perfectly bonded coating is deemed appropriate. The LS-DYNA material model utilized for the composite cylinder is Mat_Composite_Damage (Mat_022) with the input material properties given previously. The material model for the polyurea coating is Mat_Simplified_Rubber with the stress-strain curves for each strain rate shown in Figure 1. The model determines the appropriate strain rate curve from the family of curves through an internal calculation.

Figure 22 - Finite Element Model of UNDEX Experiment (3 Quadrants of Fluid Domain Hidden)

Figure 23 - Structure Mesh Details

6.2 Model Correlation

The demonstration that the computational model is accurately representing the corresponding experiments prior to its use for further data analysis is comprised of two key correlation parameters, namely the agreement between the: (1) pressure profiles of the UNDEX detonation, and (2) final damage states in the cylinders. Due to the loss of

correlation in the DIC data early on in the experiments, meaningful comparisons of the transient response of the cylinders were not possible.

The pressure profiles, for the 5.08 cm charge standoff case, as predicted by the computational model, along with the corresponding experimental profile are shown in Figure 24. Correlations are provided for the sensor located directly above the cylinder (Sensor 6) and the 2 sensors located in line with the charge itself (1/7). The linear standoff for sensor 6 and sensor 1/7 are 11.0 cm and 12.7 cm respectively. From the correlations it is seen that the peak pressure predicted by the computational models are nearly identical to the magnitudes recorded during the experiment, although the simulation exhibits a longer rise time and slightly shorter decay time. The overall impulse between the two signals is comparable. Based on this correlation the representation of the detonation of the RP-503 charge and resulting pressure wave is appropriate and the loading of the cylinders is consistent with the experiments. It should be noted that the peak pressure recorded at sensor 6 is slightly lower than that recorded at sensors 1/7, although it has a smaller standoff. There is a partial shielding effect by the cylinder itself which influences the peak pressure recorded above the cylinder, whereas the sensors 1/7 are in an unimpeded line to the charge.

Comparisons between the computational and experimental results of the final damage state in the cylinder for the 5.08 cm charge standoff are shown in Figure 25. The comparison of damage type (cracking/material failure) and extent for each of the coating thickness considered in the study highlights that the finite element modeling approach is able to accurately capture the damage evolution characteristics as observed in the experimental testing. From the images of the uncoated cylinder (top), the elliptical

damage pattern and complete rupture of the cylinder wall is present in both results.

Furthermore, the model predicts the radial crack formation in the cylinder with the thin coating with no longitudinal cracks present. Finally, as was shown in the experimental results, there is no damage present in the model of the thick polyurea coating.

Based on the high correlation between both the detonation/resulting pressure wave and the final damage states in the composite cylinders presented above, the computational models are deemed suitable for the evaluation of parameters which were not easily captured during the experimental testing. Specifically, the models will be used to evaluate the effects of the coatings on energy levels and material strains during the transient shock loading events.

Figure 24 - UNDEX Pressure Correlation, 5.08 cm Charge Standoff

Figure 25 - Damage Correlation, 5.08 cm Charge Standoff

6. 3 Energy Comparisons

The internal and kinetic energy of the cylinders during the explosive loading event are provided in Figure 26 for the 5.08 cm charge standoff cases. The time history trends are similar in nature for the 2.54 cm charge standoff scenarios. Additionally, the internal energies are separated by the cylinder and coating individually whereas the kinetic energies are presented as the net sum of the system. The results are presented in this manner so as to differentiate the internal energy distribution between the individual components during deformation, whereas the kinetic energy is a measure of the net motion of the system as a whole. There are several key aspects related to the energy characteristics that are illustrated by the results. Through the internal energy comparison, it is evident that in terms of the energy experienced by the cylinder itself, there is an increase as a function of coating thickness. The uncoated cylinder has a peak energy of ~51 J, whereas the cylinder with the thick coating experiences a peak value of 56 J, an increase of ~10%. The cylinder with a thin coating has a peak value just lower than that of the thick coating value. Furthermore, for a given coating thickness it is evident that the cylinders themselves comprise ~90% of the total internal energy (cylinder plus coating) sustained with the coatings comprising 10% of the net peak energies occurring at 0.1 ms. This result is anticipated as the composite material is significantly stiffer than the coating and thus for a given deformation would represent the primary load carrying mechanism. Finally, it is noted that as the coating thickness is increased there is a corresponding increase in the amount of internal energy that can be absorbed by the system (cylinder plus coating) as a whole. For the case of an uncoated cylinder, the sole

mechanisms for energy absorption/dissipation are strain energy in the composite and fracture energy corresponding to the evolution of damage through fiber and matrix failure. In the presence of the coatings, there is the additional energy absorption/dissipation reservoir of the coating itself. Thus, whereas the uncoated cylinders sustain damage, the coated cylinders can dissipate that energy into the coating itself and reduce the overall composite material loading. Hence, the coated cylinders experience higher levels of loading, but also a corresponding decrease in material damage. In terms of the internal energy observations, though comparison of the energies of the coatings themselves, the thicker coating does experience a higher level of internal energy as compared to the thin coating.

The comparison of the relative kinetic energies is provided in the lower plot of Figure 26. Consistent with the internal energy measures it is seen that the kinetic energy of the respective cylinder configurations increases with increasing coating thickness. As indicated previously, the kinetic energy is presented for the combined composite/coating system as it is a measure of the velocity characteristic of the system. It should be further noted that based on the relative mass values presented, the coated cylinders have respective areal weight ratios of 1.66 and 1.99 as compared to the uncoated cylinder. In order to remove the mass dependence of the kinetic energy results, the respective curves have been normalized by the AWR and are presented in Figure 27. From the normalized time histories it is seen that the coated cylinders have nearly the same kinetic energy values though time and that both are lower than those of the uncoated cylinder.

Figure 26 - Energy Time Histories, 5.08 cm Standoff

Figure 27 - Normalized Kinetic Energy, 5.08 Charge Standoff

6.4 Strain Comparison

The strain time histories, radial and longitudinal, for the back and top surfaces of the cylinder are presented in Figure 28 and Figure 29 respectively. The strain values which are presented are measured on the surface of the composite cylinder itself rather than the coating surface to allow direct comparison between cylinder configurations. The overall trends in the strain histories are consistent with those observed in the internal energy comparisons. Specifically, there is an increase in overall strain level with increasing coating thickness in both the radial and longitudinal directions. Comparisons of the back face peak strains show that as compared to the uncoated cylinder, there is an increase in both radial and longitudinal strains of ~36% for the thick coating and 25% for the thin coatings. However, it is further noted that in observing the temporal evolution of the strains, the time to reach the peak strains is longer as the coating thickness is increased by approximately 0.02 ms for all cases. The increase in strain as a function of increasing thickness can be attributed to the additional mass that the coatings contribute to the

overall structure, while providing limited additional stiffness to the system. A similar effect as was seen in the overall internal energy measures previously discussed. For the case of the cylinders with a thick coating, there is an overall doubling of the structural mass of the composite/coating system. As the cylinders are accelerated and undergo deformation due to the UNDEX pressure loading, the composite cylinder is the primary load carrying mechanism due to its overall higher stiffness as compared to the coating. During this initial response the coating is adding additional mass to the system which must be arrested primarily by the composite through additional deformation which leads to resulting increases in strain. Additionally, in a similar manner as was observed with the internal energies of the system, the increase in the rear face surface strains with increasing thickness can be partially attributed to the coatings reducing or preventing the onset of material damage. The uncoated cylinders sustain significant material damage on the charge side surface which has the effect of dissipating a certain level of energy. By reducing the damage levels, the coating have the effect of allowing the cylinders to undergo larger overall deformations, and corresponding strains, as the energy is distributed through the system as a whole. The presence of damage only of the charge side of the cylinders indicates that the surface strains would be larger for the uncoated cylinders than for the coated ones, an inverse trend as exhibited on the non-charge side.

Figure 28 - Back Surface Strain Time History, 5.08 cm Charge Standoff

Figure 29 - Top Surface Strain Time History, 5.08 cm Charge Standoff

7. Summary and Conclusions

The effects of polyurea coatings on the response and damage of submerged, air-backed, composite cylinders subjected to near field UNDEX loading has been investigated through a series of detailed experiments with corresponding computational simulations. The investigation consists of three unique cylinder configurations: (1) base composite (1.14 mm thick), (2) base composite with thin polyurea coating (2.34 mm thick), and (3) base composite with thick polyurea coating (3.04 mm thick). Each cylinder configuration was tested at charge standoff distances of 2.54 cm and 5.08 cm. The UNDEX experiments have been performed in a large diameter water filled blast tank and utilize high speed video combined with DIC to capture the transient response of the cylinders. The computational modeling of the experiments has been conducted with the

LS-Dyna finite element code and specifically utilizes the ALE methodology so as to develop fully coupled fluid structure interaction models. The primary parameters of interest in the study are deformations, damage extents, energy levels, and material strains.

The significant results of the study are:

1. For both of the charge standoffs investigated there is a splitting of the UNDEX bubble upon interaction with the cylinders. The bubble on the non-charge side of the cylinder collapsed in close proximity to the surface of the cylinder and produced localized pressure loading on the non-charge side of the cylinder.
2. During the early time phase of the UNDEX loading, the cylinders with polyurea coatings undergo larger radial deflections than the uncoated cylinders as measured on the non-charge side. As the deformation evolves later in time the uncoated cylinder does experience a larger overall deflection magnitude.
3. When a weight penalty is applied to the overall displacement response of the cylinder to account for the weight penalty of the coatings, there is a net degradation of the relative performance on a per unit weight basis.
4. The polyurea coatings had a more beneficial mitigating effect on the center point displacement at the larger charge standoff; however, when accounting for weight penalty the response was degraded on a per unit weight basis.
5. Damage to the coated composites was dramatically reduced as a function of increasing coating thickness as compared with the baseline cylinders.
6. The modeling approach utilized in the study is able to accurately simulate the detonation of the explosive charge as well as predict the overall damage extents in the composite cylinders.

7. During the transient loading of the cylinders, both the internal material energy and the overall system kinetic energy increase with increasing coating thickness. Furthermore, the composite material experiences ~90% of the overall internal energy with the coatings carrying the remaining 10%
8. The radial and longitudinal surface strains during the early time response of the cylinders increases with increasing coating thickness.
9. Polyurea coatings can affect structures subject to shock loading in both beneficial and adverse means. There is an observed increase in deformation and strains with increasing coating thickness during the early time deformation, whereas there is an overall reduction in material damage and failure due to the presence of the coating over the entire time duration.

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CHAPTER 5

UNDERWATER NEARFIELD BLAST PERFORMANCE OF HYDROTHERMALLY DEGRADED CARBON-EPOXY COMPOSITE STRUCTURES

by

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Abstract

An experimental and computational study was conducted to evaluate the dynamic response of weathered biaxial composite plates subjected to near-field explosive/blast loadings. Naval structures are subjected to aggressive marine environments during their service life that can significantly degrade their performance over time. The composite materials in this study are carbon-epoxy composite plates with [0, 90]_s and [45, -45]_s layups. The composites were aged rapidly through submersion in 65°C seawater for 35 and 70 days; which through Arrhenius' methodology, simulates approximately 10 and 20 years of operating conditions, respectively. Experiments were performed by clamping the composite plates to an air-backed enclosure inside an underwater blast facility. During the experiments, an RP-503 explosive was submerged, behind the composite specimen, and detonated. Meanwhile, transducers measured the pressure emitted by the explosive, and three high-speed cameras captured the event. Two of the cameras were placed facing the specimen to measure full field displacement, velocities, and strains through a 3D Digital Image Correlation analysis. The third high-speed camera was used to record the explosive's behavior and bubble-to-specimen interaction. Additional experiments were performed to obtain the non-weathered and weathered material properties as well as the residual strength post the blast experiments. Additionally, a Coupled Eulerian-Lagrangian finite element simulation was conducted to complement the experimental findings. Results show that the diffusion of water into the composite material leads to a more prominent blast response as well as the degradation of mechanical properties, especially shear properties which are dominated by the epoxy matrix. Residual strength experiments also show a substantial

decrease in the structural integrity post blast loading for the weathered composites. Lastly, the numerical simulations showed substantial increase in maximum strains with relatively small decreases in mechanical stiffness. Hence, even past the saturation point, incremental changes in material properties can have a significant impact on mechanical performance.

1. Introduction

An experimental and computational investigation was conducted to evaluate the dynamic response of weathered biaxial composite plates subjected to near-field explosive/blast loadings. This research arises from the concern of damage to naval and marine composite structures such as ships, submarines, and underwater vehicles [1, 2]. During the service life of these structures, their mechanical properties degrade from continuous exposure to an aggressive sea environment [3]. In undesirable circumstances, marine structures can be further subjected to shock and blast loadings. If the degradation of mechanical properties is not accounted for under these highly dynamic conditions, the damages and losses could be catastrophic.

A significant cause of mechanical degradation in composites in a marine environment is the diffusion of water into the matrix material [3]. The diffusion process is relatively well established and can be described by a diffusion coefficient that is a function of parameters such as temperature, type of resin and curing agent, surrounding medium composition, fillers, void content, and so on. The value for diffusion coefficient and the theoretical models used to describe the diffusion varies in previous studies of diffusion in composites [4-18]. A standard and well-accepted model for epoxy resins, in terms of mass diffusions, is a Fickian model [14] which uses Fick's second law to predict how the concentration of a diffusive substance changes over time within a material [19-20].

Previous works used the Fickian model to study the properties changes during low strain rate loading of diffused composites. These studies agreed that the mechanical property degrades over time from an increase in mass, internal stresses from swelling, and loss of interlaminar strength [15-18]. Current research on the high strain rate response of weathered composites is limited. Recently, there has been a study that analyzes the shock response of weathered composites plates within an air medium [21]. Moreover, many experimental and numerical studies analyze the dynamic response of non-weathered composite plates subjected to underwater explosives [22-26].

The aim of this study is to understand how a composite's blast performance is affected by prolonged exposure to seawater. This work experimentally and computationally analyses the dynamic response of weathered composite plates subjected to nearfield underwater blasts. In the experimental portion, a 3D Digital Image Correlation (DIC) technique is implemented to capture real-time high-speed deformation to characterize the fluid-structure interaction. In the computational portion, a Coupled Eulerian-Lagrangian (CEL) simulation was used to simulate the experimental conditions to predict the composite's performance in scenarios beyond the experiments performed.

2. Experimental Procedures

2.1 Composite Material

The composite materials used consist of four unidirectional carbon fiber layers with [0, 90]s and [45, -45]s layups. These materials were manufactured by the University of Rhode Island students at TPI Composites Inc. in Warren, RI. The composites were manufactured from two layers of +/- 45° biaxial carbon fabric and an epoxy resin/hardener mixture. The fabric material is Tenax HTS40 F13 24K 1600tex carbon fibers (1% polyurethane-based

sizing finish) from Toho Tenax Inc. in Rockwood, TN. Also, the resin/hardener is a 100/30 weight mixture of the RIMR135/RIMH137 epoxy from Momentive Performance Materials Inc. in Waterford, NY.

The epoxy mixture was drawn into the fabric by vacuum infusion at a constant pressure of 730 mmHg. After hardening, curing was performed by placing the composite plate in an oven at 70 °C for 10 hours. All specimens for both layups were cut from a single sizeable composite sheet to minimize variations in the epoxy mixture and fiber content. The final product was a 1.26 mm (0.050 in) thick composite plate with 1% void content (measured in accordance with ASTM Standard D2734 [27]) and 60% fiber volume content. Table 1 lists the product information and properties of interest for the fiber, fabric, epoxy, and composite plate.

Table 1 - Carbon and epoxy product information and properties

	Carbon Fiber	Fabric	Epoxy	Composite Plate
Manufacturer	Toho Tenax Inc.	Saertex LLC.	Momentive Performance Materials Inc.	University of Rhode Island
Product Number	HTS40	XC611	RIMR135/RIMH137	---
Density	1600 tex (Linear)	602 g/m ² (Areal)	1150/955 kg/m ³	1420 kg/m ³
Wet/Dry Glass Transition Temperature	---	---	72/86 °C	72/86 °C

2.2 Mechanical Testing

Quasi-static tensile and shear properties were obtained by using an Instron 5585 and following ASTM Standards D3039 [28] (with [0, 90]s specimens) and D3518 [29] (with [45, -45]s specimens) respectively. The strain data was measured with 2-D DIC from

images captured by a Prosilica camera (model GC2450 from Allied Vision Technologies GmbH in Stadtroda, Germany). The tensile and shear tests were used to calculate the effective material properties used in the computational models. The strain rate sensitivity of carbon/epoxy composites, though not negligible, is minimal (especially for normal stresses) [30]; therefore, numerical results are reasonably comparable to the actual (experimental) results. Lastly, quasi-static compressive tests were performed on post-experiment specimens using ASTM Standard 7137 [31] to measure their residual strength.

2.3 Weathering Facility

The composites were submerged in a 3.5% NaCl solution (prepared in accordance with ASTM Standard D1141 [32]) as shown in Figure 1; this salinity matches the concentration of most ocean bodies. Before submersion, all specimens were placed in a desiccator to dry for a minimum of 72 hours. In the submersion tank, four water heaters (Model LXC from PolyScience in Niles, IL) are used to maintain a constant temperature of 65°C. It is crucial for the solution temperature to be below the wet glass transition temperature of the composite material. Beyond glass transition, there will be changes in the mechanical properties unrelated to the aging aspect of this study [5]. However, a high temperature is still desired to attain a fast acceleration factor. Therefore, a temperature well under the wet glass transition was chosen to weather the experimental specimens.

Figure 1 - Weathering facility setup

Float switches and water pumps are used to maintain a constant water level. As water evaporates, one float switch in the deionized water and one in the saltwater tank will independently activate water pumps to replenish the lost water. For this reason, the salinity remains constant, and water passively circulates as room temperature water is introduced. Also, the composite materials were exposed to salt water for 35 and 70 days. The blast experiments were performed immediately after the specimens left the salt water bath (to avoid moisture loss) as advised by ASTM Standard D5229 [33].

2.4 Blast Facility

To perform the experiments, the underwater blast facility shown in Figure 2 is used. This facility holds 1800 L (475 gallons) of water (where the charge is placed) and 45 L (12 gallons) of air in a chamber separated by the composite specimen. Also, the facility is made of a steel cubic shell that is dimensioned $1.2 \times 1.2 \times 1.2 \text{ m}^3$ ($4 \times 4 \times 4 \text{ ft}^3$) with a shell thickness of 12.7 mm (0.5 in). The composite specimen is clamped between the water and air chambers with a 25.4 mm (1 in) all-around clamping width; leaving a $254 \times 254 \text{ mm}^2$ ($10 \times 10 \text{ in}^2$) exposed area (see Figure 2).

Figure 2 - Underwater blast facility and experimental setup

An RP-503 explosive (from Teledyne RISI, San Joaquin County, CA) was used to load the composite structure. The explosive charge is composed of 454 mg RDX and 167 mg PETN contained within an outer plastic sleeve. For reference, it is energy equivalent to 1.5 grams of TNT. Moreover, the charge is submerged underwater, centered to the specimen, and placed at a 152 mm (6 in) standoff distance (additional standoff distances were also explored; see Table 2 for details). Two dynamic pressure transducers (PCB 138A05, PCB Piezotronics Inc. in Depew, NY) are located next to the specimen and explosive (as illustrated in Figure 2) at 152 mm (6 in) and 203 mm (8 in) distances from the explosive. During the experiments, a Dash 8HF data acquisition system (from AstroNova Inc. in Warwick, RI) captured the pressure data at two mega samples per second.

Furthermore, two Photron SA1 high-speed cameras (from Photron USA Inc. in San Diego, CA) are placed 14° apart outside the blast facility and used to record high-speed images of the specimen at 10,000 frames per second. Each image has an 832x748 spatial pixel resolution; which is approximately equivalent to 259x287 cm (10.2x11.3 in) view

from the specimen's center. The photographs from the high-speed cameras are captured through the facility's optical windows. These images are later used for the DIC analysis. Also, a third Photron SA1 camera is used (as shown in Figure 2) to record the explosive and bubble-to-structure interactions at 10,000 frames per second (with a 576x992 spatial pixel resolution; approximately equivalent to 186x320 cm). High-intensity light sources (Super Sun-Gun SSG-400 from Frezzi Energy Systems Inc. in Hawthorne, NJ; not shown in Figure 2) are used to illuminate the object for recording images. The details of the experimental cases are summarized in Table 2. Each experimental case has been repeated two times to validate the results (three for the E45-0WD case in Table 2).

Table 2 - Experimental details

Cases	Layup	Standoff Distance, mm (in)	Weathering Time, days (simulated years)
E45-0wd	[45,-45]s	152 (6)	0
E45-0wd-2	[45,-45]s	114 (4.5)	0
E45-0wd-3	[45,-45]s	76 (3)	0
E45-35wd	[45,-45]s	152 (6)	35 (10)
E45-70wd	[45,-45]s	152 (6)	70 (20)
E90-0wd	[0,90]s	152 (6)	0
E90-70wd	[0,90]s	152 (6)	70 (20)

The composite specimen's 254x254 mm² (10x10 in²) exposed area that is facing the high-speed cameras is coated with high-contrast speckle patterns. The speckle patterns are created by randomly placing flat-white paint dots (sized 9 to 12 pixels per dot) on a flat-black painted background until approximately 50% of the surface area of the specimens is covered by the white dots. When clamping the composite plate, a skin layer of silicone adhesive is applied to the clamping surface to avoid water penetration into the air chamber

from the clamping boundaries; therefore during the experiments, the specimen has water and air-fluid boundaries similar to a ship hull.

2.5 Digital Image Correlation Reliability

The high-speed images are analyzed using the commercially available DIC software VIC3D 7 from Correlated Solutions, Inc., Columbia, SC. During the DIC analysis, measurements of the full-field displacements across the specimen's viewable surface are calculated by triangulating the position of each unique feature in the speckle pattern. Previous work [34] outlines the calibration procedures that validate the accuracy of the DIC results when capturing images through an optical window (where changes in refractive index are present). It was found that the camera's viewing axis needs to be perpendicular to the optical windows in order to minimize DIC displacement errors. This technique can yield displacement errors in the order of 1.2% and 2.5% for in-plane and out-of-plane measurements, respectively.

3. Numerical Model

A computational Finite Element Analysis (FEA) model similar to previous work [26] was created with the LS-DYNA code from the Livermore Software Technology Corp. The model uses a CEL formulation that is capable of capturing the fluid-structure interaction between the fluid and composite plate as well as an accurate representation of the explosive's detonation. All models were constructed using the CGS unit system, and simulations run in the double precision mode of LS-DYNA's Version 971, Release 9.1.0.

The FEA model consists of the air, composite specimen, water, and RP-503 charge as shown in Figure 3. This model is representative of a subdomain from the full experimental

test facility for computational efficiency. The composite specimen, 120 mm of air, and 200 mm of water is included in the modeled subdomain. The explosive is centered with respect to the composite plate with a standoff distance of 152 mm. During the experiments, the reflections from the tank walls are relatively small in magnitude and have minor effects on the composite's response. Therefore, the experiments behave as they would in a free-field condition (where no reflections are present), and the model's external fluid faces are set as non-reflecting boundaries.

Figure 3 - Finite element model configuration

All Eulerian components in the model use a combination of a material model definition and equation of state (EOS). For water, density is defined as 1 g/cm^3 , and a Gruneisen EOS is used with a sound speed of $149,000 \text{ cm/s}$. For air, density is defined as 0.0013 g/cm^3 and a Linear Polynomial EOS is used as a gamma law EOS (where $C_0 = C_1 = C_2 = C_3 = C_6 = 0$ and $C_4 = C_5 = \gamma - 1 = 0.4$). The RP-503 explosive is created with a JWL EOS by assuming it is composed of 621 mg of RDX instead of the actual 454 mg of RDX and 167 mg of PETN. This assumption is acceptable since the explosive is mostly RDX and the JWL coefficient of the PETN is similar to the RDX's. The explosive's physical and JWL EOS

parameters are provided in Table 3. More details about EOS models and assumptions can be found in previous work [26].

Table 3 - RDX and JWL EOS material parameters [35]

ρ (Explosive Density)	1.77 g/cm ³
D (Detonation velocity)	850e3 cm/s
E_o (Detonation energy per unit volume)	5.93e10 dyn/cm ²
P (Chapman-Jouguet pressure)	3.41e13 dyn/cm ²
A (JWL linear coefficient)	7.78e12 dyn/cm ²
B (JWL linear coefficient)	7.07e11 dyn/cm ²
R1(JWL nonlinear coefficient)	4.485
R2 (JWL nonlinear coefficient)	1.068
ω (JWL nonlinear coefficient)	0.3

The composite plate is modeled using 3D continuum solid elements through the thickness of the plate. Solid elements were used instead of shell elements to estimate interlaminar stresses/strains. Each ply of the composite laminate is represented by a single layer of solids, with 4 in total through the thickness. For the boundary conditions, the plate's out-of-plane displacements and rotations were fully constrained at its edges. Also, during the experiments, slippage on the clamped edges in the order of 1 mm was observed. Hence, no in-plane restrictions were applied. The amount of edge slippage from the numerical simulations is consistent with DIC measurements. This slippage was caused by the overwhelming loading magnitudes which overcame the clamping friction. Moreover,

the density of the plate was set to 1.42 g/cm³, and the effective stiffness of the plate is defined in Sec. 4.2. Lastly, the composite damage was modeled by LS-Dyna's Mat_Composite_Damage (Mat_022). This material definition encompasses failure criterions of tensile, compression, and in-plane shear.

The loading on the composite plates occurs in a two-step process. First, a quasi-static pressure (from the water column weight) is uniformly applied over the face of the plate. Subsequently, the explosive detonation is initiated which leads to a transient response of the composite plate. In the computational part of this study, six different numerical cases are analyzed as shown Table 4. The cases consist of the 2 layup configurations, [0,90]s and [45/-45]s with three levels of weathering (0, 35, and 70 Days). All cases are evaluated with the 152 mm charge standoff scenario.

Table 4 - Numerical cases details

Cases	Layup	Weathering Exposure, days
C45-0wd	[45,-45]s	0
C45-35wd	[45,-45]s	35
C45-70wd	[45,-45]s	70
C90-0wd	[0,90]s	0
C90-35wd	[0,90]s	35
C90-70wd	[0,90]s	70

4. Results and Discussions

4.1 Weathering

From Arrhenius' methodology, the water diffusion activation energy (E_a) for an epoxy is assumed to be constant [36]. Therefore, a mass diffusion study was performed at various temperatures (different diffusion rates) to obtain a diffusion acceleration factor (AF) with respect to a specific temperature. The moisture absorption was measured for composites

submerged in 3.5% NaCl solutions at 5, 25, 45, 65, and 85 °C in accordance with ASTM Standard D5229 [33]. Note that the wet glass transition temperature (72°C) is based on the composite's storage modulus and not its diffusion activation energy. The composite's diffusivity still follows Arrhenius' methodology at 85°C even though its stiffness is lower at this temperature. Therefore, this high temperature is only used for the mass diffusion study and not for weathering the experimental specimen.

The water diffusivity into the composite plate obeys Fick's second law of diffusion [19]. Fick's second law was simplified into one dimension to calculate the diffusion coefficient (D) using Eq. (1) [20]. The diffusion coefficient was calculated from a point that is within the initial linear portion of the mass diffusion curve ($\leq 50\%$ mass saturation). The diffusion coefficient was related to E_a by using Arrhenius' equation. To solve for E_a , Eq. (2) was written in logarithmic form as shown in Eq. (3), then $-E_a/R$ was found as the slope of the linear trend for the various diffusion temperatures [20]. Figure 4 (a) and (b) show the mass diffusion for different temperatures and the logarithmic relationship between D and E_a respectively; the markers in Figure 4 represents measured experimental data while the line trends are the estimated exponential functions used to extrapolate values needed.

$$D = \frac{\pi}{t} \left(\frac{h}{4} \frac{M_t}{M_s} \right)^2 \quad (1)$$

$$D = Ce^{-\frac{E_a}{RT}} \quad (2)$$

$$\ln(D) = \ln(C) - \frac{E_a}{RT} \quad (3)$$

Where t is time; M_t is the composite's mass at time t; M_s is the composite's saturated mass; h is the composite plate's thickness; C is the diffusion constant; R is the universal gas constant; and T is the temperature in the absolute scale.

Figure 4 - (a) Mass diffusion for five temperatures and (b) logarithmic relationship between diffusivity and temperature

After obtaining the activation energy for the composite material, AF can be found as the ratio of diffusions at different temperatures as shown in Eq. (4) [36]. The submersion specimens were kept at a constant temperature ($T_1 = 338\text{K}$), but the service temperature (T_2) can vary depending on the application. Hence, AF is application dependent. For reference, if the average ocean temperature (17°C) is assumed to be the operational temperature, then 35 and 70 days of submersion approximates to 10 and 20 years of service respectively.

$$AF = \frac{C e^{-\frac{E_a}{RT_2}}}{C e^{-\frac{E_a}{RT_1}}} = e^{\left(\frac{E_a}{R}\right)\left(\frac{T_2 - T_1}{T_1 T_2}\right)} \quad (4)$$

4.2 Mechanical Properties

In the material model, a plane stress assumption is used for the composite plate. The materials tested were from the same batch of materials used to for the experimental specimen. Therefore, effective properties (homogenized laminate properties) [37] were measured instead of ply properties. Table 5 shows the effective elastic modulus (E_x and E_y), Poisson's ratio (ν_{xy}), shear modulus (G_{xy}), and failure strains which were calculated with the standards outlined in Section 2. The effective elastic modulus was the same in

both principal directions ($E_x = E_y$) since the layup is symmetric and evenly balanced. The normal stress has a linear behavior until failure, but the shear stress has a bilinear behavior; the shear yield and failure stresses are also listed in Table 5. Each result for the effective material properties in Table 5 is calculated from six tests.

Table 5 - Composite's Effective Mechanical Properties

Weathering time (Days)	0	35	70
E_x, E_y (GPa)	78.4 +/- 1.8	78.0 +/- 2.1	74.9 +/- 2.6
V_{xy}	0.039 +/- 0.014	0.040 +/- 0.010	0.042 +/- 0.009
Failure Normal Strain (%)	1.46 +/- 0.09	1.38 +/- 0.09	1.36 +/- 0.07
G_{xy} (GPa)	7.38 +/- 0.19	5.32 +/- 0.24	4.92 +/- 0.22
Yield Shear Stress (kPa)	36.1 +/- 1.1	25.3 +/- 1.0	21.7 +/- 0.6
Failure Shear Stress (kPa)	45.3 +/- 1.2	41.3 +/- 1.9	38.7 +/- 2.6
Failure Shear Strain (%)	4.92 +/- 0.79	7.25 +/- 0.25	7.28 +/- 0.89

4.3 Blast Response

During the experiments, the RP-503 underwater explosive (UNDEX) combusts at $t = 0$ as shown in Figure 5 (a). The high pressures from the explosive loads the composite specimen and forms a cavitation bubble at the charge location at $t = 3$ ms. The cavitation bubble expands spherically until it begins to interact with the composite plate. As a result, its growth is skewed away from the composite. The bubble's expansion peaks at $t = 9$ ms, which is when the bubble begins to collapse from its low internal cavitation pressure and high external pressure. During this collapse, the surrounding fluid accelerates towards the bubble, which leads to a new surface cavitation on the composite due to its close proximity as seen in Figure 5 (a) at $t = 15$ ms. When the bubble finally collapses at $t = 22$ ms, the composite's surface is fully engulfed by this new surface cavitation. Therefore, the composite specimen does not react to the bubble collapse. However, the pressures from the bubble collapse initiates the surface cavitation collapse; which does so at $t = 24$ ms. The

bubble pulsation cycle is interrupted by the surface cavitation collapse; hence the loading cycles of interest are completed by this time.

Moreover, the high pressures from the explosive are shown in Figure 5 (b) for different distances (each measured during a different experiment). The shock from the explosive is distinguished by an immediate rise in pressure followed by exponential decay. The amplitude of the explosive pressure decreases spherically by $1/R$ from the explosive location. When the explosive pressures are normalized in time for charge distance and in magnitude by $1/R$, the pressure trends are nearly identical; hence the loading condition is highly repeatable between experiments. Also, the reflections from the tank's boundaries are small relative to the initial explosive pressures. Furthermore, the pressure from the bubble pulse and surface cavitation collapse are shown in Figure 5 (c) for the 152mm standoff case. The bubble pulse has a comparable impulse to the initial explosive pulse due to its long duration. The surface cavitation collapse has low recorded pressure signatures. However, pressure signatures at this point in time are partially blocked by the bubble. Even so, an acoustic spike is seen when the surface cavitation's water boundary slaps against the composite plate; which leads to a substantial amount of momentum transfer to the composite. Additionally, the bubble pulse was nearly identical in magnitude as well as duration between experiments and the surface cavitation spike is only consistent in time (not shown in Figure 5 (c)).

Figure 5 - (a) Fluid-structure interaction images, (b) pressure history from the explosive, and (c) bubble as well as surface cavitation pressure pulses

4. 4 Deformation and Image Analysis

The out of plane deformation from the 3D DIC is illustrated in Figure 6; which shows center point displacements. Each of the displacement curve shown is from one representative experiment. The center point displacements for the non-weathered [45,-45]_s composite plate at different standoff distances is shown in Figure 6 (a). Decreasing the standoff distance leads to higher loading pressure and higher deformation rates. The

displacement curves for the 76 mm and 114 mm standoff ended when failure (in the form of through-thickness cracking) is observed during the experiment (in the high-speed images). For the 152 mm standoff distance, failure is not observed during the experiments but is seen during the post-mortem analysis.

As loading initiated on the composite's surface, it flexes towards the air-side (forward) to a maximum displacement. When the composite begins to rebound, the surface cavitation (at vacuum pressure) begins at 8 ms, and the specimen rapidly abruptly flexes towards its water-side (backward) to a magnitude beyond its initial displacement (seen 8 and 24 ms). At $t = 24$ ms, the surface cavitation collapses, and an abrupt increase in displacement forward occurs once again as shown by the full displacement cycle in Figure 6 (b). Figure 6 (b) also illustrates the repeatability of the three experiments for the E45-0wd case.

Weathering the composite plates led to an increase in maximum displacements for the same loading condition. The center point displacement curves for the [45,-45]_s composite plates at 152 mm (6 in) standoff is shown in Figure 6 (c) for the non-weathered, 35 weathering days (WD), and 70 WD cases. After weathering the [45,-45]_s composite for 35 days, the maximum center point displacements increase by an average of 20%. An additional 5% increase in displacement is seen for the 70 WD cases; which is a further decrease in performance post-saturation. The response in 70 WD from 35 WD could be halted by the increase in damage as it will be shown in the next section. Also, the stiffness of a fully clamped plate increases with deformation; hence, further changes from a highly deformed plate are countered by immense resistance. The [0,90]_s composite plates behaved similarly to the [45,-45]_s plates. The 70 WD case for the [0,90]_s layup had a center point displacement 15% higher than the non-weathered case as shown in Figure 6 (d).

Figure 6 - Center point displacements for (a) [45,-45]s non-weathered composites at different standoff distances, (b) [45,-45]s non-weathered composite at 152 mm (6 in) standoff, (c) [45,-45]s weathered composites, and (d) [0,90]s weathered composites

4.5 Composite Post-Mortem

The post-mortem analysis revealed that damage increased with weathering time. The main type of damage for the [45,-45]s cases was interfibrillar, and through-thickness damage near the plate's corners are illustrated in Figure 7 (a). In terms of the 35 WD and 70 WD, there is a notable increase in average crack length. This increase in crack length suggests further material degradation from fiber/matrix debonding after saturation. For the [0,90]s cases, the damage was predominately seen in the form of delamination near its corners (not shown in Figure 7). Weathering the [0,90]s specimen showed an increase in damage in terms of increased delamination area. The difference in damage between [45,-

45]s and [0,90]s arises from how the boundary interacts with the fiber orientation. The deformation mode for a plate (mode 1) has diagonal lobes. Hence high strain levels diagonally and matrix cracking (through-thickness) in the [45,-45]s plates. Also, when the fiber direction is not perfectly aligned with the deformation lobes, interfibrillar cracking occurs during the through thickness crack propagation. For the [0,90]s, the diagonal lobes lead to bend and twisting of the fibers, which forces debonding/delamination. Figure 7 (b) shows the relative increase in average crack length for the [45,-45]s cases, and the relative increase in delamination area for the [0,90]s cases. Based on the [45,-45]s crack lengths, the damage levels seem to increase with weathering time consistently.

Figure 7 - (a) Interfibrillar and through-thickness cracking for the [45,-45]s cases and (b) relative change in damage

4.6 Residual Strength

Quasi-static compressive tests were performed on specimens after the explosive/blast experiments using ASTM Standard 7137 [31] to measure and compare compressive residual strength properties between non-weathered and weathered samples. To perform this residual strength tests, the composite specimen was simply supported at the 254x254 mm² (10x10 in²) central area (same boundary locations as the blast experiments) as shown

in Figure 8 (a). A schematic of the boundary and loading condition is shown in Figure 8 (b) as well as a model for the loading fixture in Figure 8 (c). Figures 8 (d) and (e) show the load applied in units of MPa versus the relative change in length for the [45,-45]s and [0,90]s cases respectively. For the [45,-45]s cases, the average ultimate strength decrease by 29.6% after 35 WD, and 45.7% after 70 WD. For the [0,90]s cases, the average residual strength decrease by 46.5% after 70 WD.

During blast experiments, the difference in performance between the 35 WD and 70 WD cases is not very distinguishable. However, a substantial decrease in residual strength is observed between the 35 WD and 70 WD cases. This is consistent with what was observed during the post-mortem. For this reason, it is shown that material degradation for carbon/epoxy composites occurs even after saturation from additional chemical processes.

Figure 8 - Residual strength for the (a) [45,-45]s weathered composites, and (d) [0,90]s weathered composites

5. Numerical Results

5.1 Numerical Model Correlation

The correlation between the computational model and the corresponding experiment in terms of the UNDEX pressure profile is shown in Figure 9 (a) as measured by the 152 mm standoff. The experimental trends seen in Figure 9 (a) and (b) were selected from a representative experiment; experimental variation is shown in Figure 6 (b). In Figure 9 (a), the peak pressure predicted by the simulation is nearly identical to the value observed during the experiment. The simulation shows a longer rise time and similar decay time. The overall impulse between the two signals is comparable; hence, the UNDEX EOS definition and parameters are deemed to be appropriate for this model. Furthermore, the tank reflections were relatively small compared to the initial load. Therefore, the non-reflective boundary condition is also appropriate for this model.

Figure 9 - Numerical and experimental comparison in terms of (a) pressure history at 152 mm from the explosive, and (b) center point displacements for the [45,-45]s non-weathered case

The transient displacement time history of the center point displacement for the E45-0wd and C45-0wd cases are shown in Figure 9 (b). The model captured the peak center point displacements relatively well; with the simulations over predicting the peak by ~10-15%. However, the simulations show notable discrepancies during the flexural motion of the composite. The first discrepancy is the prolonged response in deformation seen between 0.5 and 1.25 ms in the experiments. This same prolonged response behavior in the experimental data can be seen after the plate reaches its maximum displacement and starts to recoil between 3.5 and 5 ms. These discrepancies are believed to be the result of an underdefined material model. The numerical model was from the effective stiffness (homogenized laminate properties) of the composite. Hence, the full stiffness matrix or any rate dependency was not specified in the model. With the current material model, things such as delamination and other out-of-plane failure mechanisms cannot be accounted for. However, the maximum displacements and, in turn, maximum strains, can still be predicted by the current material model. Moreover, the displacement velocities leading up to the maximum displacement, and velocities that soon follow, are well matched during the simulations. Lastly, the surface cavitation to composite interaction was not predicted by the numerical model. Therefore, nothing after the maximum displacements/strains will be considered in the following discussions.

5.2 Maximum Strains

The maximum in-plane ϵ_{xx} strain field for the [45,-45]_s non-weathered numerical model is shown in Figure 10 (a). For all simulations, ϵ_{xx} and ϵ_{yy} are nearly the same; hence they will just be referred to as normal strains. The maximum normal strains are located in

the lobes of the buckling mode, at 57.2 mm (2.25 inches) away from the corners. Moreover, the maximum strains (normal and shear) for all numerical cases are listed in Table 6. The values in Table 6 are greater than the failure strains listed in Table 5. These higher values are expected since the transverse composite properties are not incorporated into the failure model. However, the maximum simulation strains are still valuable information because they illustrate how the weathering affects strain levels.

Figure 10 - (a) ϵ_{xx} strain distribution for the numerical model at $t = 1.1$ ms, (b) relative change in failure probability vs weathering time, and (c) relative change in failure probability vs through-thickness crack length for the [45,-45]s cases

Table 6 - Maximum strains for composite simulations with a 152 mm (6 in) standoff

Cases:	Maximum strains, ϵ_{max} (%)	
	$\epsilon_{xx}/\epsilon_{yy}$	ϵ_{xy}
C45-0wd	2.51	5.83
C45-35wd	2.52	8.63
C45-70wd	2.68	9.02

C90-0wd	2.41	2.74
C90-35wd	2.43	3.46
C90-70wd	2.57	3.66

The maximum strain values from Table 6 are used to calculate the relative failure probability as a function of weathering time with Eq. (5); where the maximum strains (normal and shear) are subtracted from the non-weathered case strains then divided by its respective failure strain (listed in Table 5). The results from this calculation are illustrated in Figure 10 (b). Based on the normal strain data, failures from normal stresses are strongly proportional to weathering time regardless of saturation level. From the shear strain data, it is unclear if there is a relationship between failure from shear stresses and weathering time. All maximum strain values increase with weathering time. However, the epoxy matrix itself becomes more compliant (as shown by the decrease in stiffness and higher failure strains in Table 5); which offsets the failure probability as defined by Eq. (5). Furthermore, there is also a proportional relationship between the failure probability from normal stresses and damage accumulation in terms of through-thickness cracking for the [45,-45]_s cases as illustrated in Figure 10 (c). In turn, damage accumulation is also proportional to weathering time regardless of saturation levels as it was inferred by Figure 7 (b).

$$\left(\frac{\varepsilon_{max} - \varepsilon_{max|WD=0}}{\varepsilon_{failure}} \right) 100 \quad (5)$$

5.3 Stress Evolution

A comparison of the stress field in the [45,-45] and [0,90]_s laminates are shown in Figure 11 for the no weathering cases. The evolution and propagation of the stress field out to the plate boundaries through time illustrate several trends regarding the plate load distribution. Foremost the areas of highest stresses are located in different areas of the

plates for the respective configurations and also occur at different points in time during the loading. The [45,-45]_s case sustains the highest stress state in the way of the corners with the peak stress occurring at ~0.2 ms after the onset of pressure loading. Conversely, the [0,90]_s case sustain the highest magnitude of stress along the vertical and horizontal plate edges and are highly localized along a thin line. The highest stresses in the [0,90]_s laminates occurs at 0.40 ms, later in time than the peak stresses in the [45,-45]_s cases. This illustrates how the laminate orientation could be used as "stress guides" to direct the high stresses to areas in a structure (or boundary) that are stronger or has higher dissipation properties (in the case of hybrid composites).

Figure 11 - Stress field evolution after charge combustion

6. Conclusions

This work experimentally and numerically analyzed the dynamic response of weathered composite plates subjected to nearfield underwater blasts from explosives. The aim of this study was to understand better how a composite plate's blast performance is affected by prolonged exposure to seawater. The main findings of this study are as follows:

- The mechanical properties of the carbon-epoxy composites degraded even after its saturation point (after 35 days of weathering) during hydrothermal degradation.

Most notably the shear properties had the highest degradation, which is governed by the matrix material.

- The maximum center point displacements during the blast experiments for the [45,-45]_s composite increased (+20%) between the non-weathered and 35 WD specimen. Only a small increase in displacements (+5%) was attained by doubling the exposure to 70 WD. Similarly, for the [0,90]_s composites, a 70 WD exposure yielded a 15% higher than the non-weathered case.
- The damage accumulation increased with weathering time during the post-mortem analysis. The predominant damage type is also different for the two layup configurations. For the [45,-45]_s cases, interfibrillar and through-thickness cracking was the primary type of damage. For the [0,90]_s cases, delamination was the main type of damage. Also, the damage locations are consistent with lobe locations for a mode 1 plate deformation as it would be expected.
- Residual strength experiments showed a significant decrease in performance between the 35 WD and 70 WD cases in comparison to the blast experiments. This decrease in performance is consistent with the increase in damage levels measured during the post-mortem. This illustrates how material degradation occurs after saturation. For the [45,-45]_s composite plates, the average residual strength decreased by 30% for the 35 WD case, and 46% for the 70 WD when compared to the non-weathered case.
- The effective material properties used in the numerical model led to discrepancies such as simulation rise time and rebound behavior. However, the properties and EOS used in the model was able to predict center point peak displacements,

deformation shape, and explosive loading profile. In the future, properties should be obtained from parallel laminates at different angles as well as strain rates and use CLT to build rate-dependent stiffness matrices; this would likely require a user subroutine to define the material in numerical codes. Also, high rate failure properties should be obtained for future studies.

- Based on the normal strain data from Tables 5 and 6, failures from normal stresses are strongly proportional to weathering time regardless of saturation level. From the shear strain data, it is unclear if there is a relationship between failures from shear stresses and weathering time.
- Failure probability from normal stresses is proportional to damage accumulation in terms of through-thickness cracking for the [45,-45]_s cases as illustrated in Figure 10 (c). In turn, damage accumulation is also proportional to weathering time regardless of saturation levels as it was inferred by Figure 7 (b) and previous conclusion.
- The laminate orientation could be used as "stress guides" to direct the high stresses to areas in a structure (or boundary) that are stronger or has higher dissipation properties for dynamic applications.

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CHAPTER 6

ARCTIC AND LOW TEMPERATURE EFFECTS ON THE MECHANICAL AND FRACTURE BEHAVIOR OF THERMOSET FRP COMPOSITE LAMINATES

by

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Abstract

The effects of low temperatures on the mechanical and fracture behaviors of fiber-reinforced polymer (FRP) composites is presented. The objective is towards developing a fundamental knowledgebase of the temperature effects to guide the optimizing of fiber and matrix material selections. The mechanical properties at typical operating temperatures ($\sim 15 - 20\text{ }^{\circ}\text{C}$) have been extensively studied, however there is limited data pertaining to the response of these materials at the lower temperatures associated with arctic seawater as well as those found at extreme depth ($\sim -2 - 4\text{ }^{\circ}\text{C}$). The specific focus and goal is to support the design of structures and components operating in cold temperature environments. The environments of interest are those found in the Arctic regions of the world as well as at the deep depths of the ocean. In the study, both carbon/epoxy and E-glass/epoxy laminates are considered. The mechanical characterization of the carbon and E-Glass / Epoxy woven laminates consists of controlled Tension, Compression, Short Beam Shear, and Mode-I fracture evaluations. The range of temperatures considered for the mechanical characterization experiments was $-2\text{ }^{\circ}\text{C}$ ($28\text{ }^{\circ}\text{F}$) to $20\text{ }^{\circ}\text{C}$ ($68\text{ }^{\circ}\text{C}$).

1. Introduction

A key advantage to the use of fibrous composites in structural design is the ability to customize material performance to prescribed design requirements and operating environments. Fiber-reinforced polymer (FRP) composites provide many readily recognized performance benefits; lightweight, high strength- and stiffness-to-weight ratios, corrosion resistance, net shape manufacturing, and more. Because significant

mechanical performance advantages can be achieved beyond traditional homogeneous and isotropic materials, many marine industries utilize FRP composites in various surface and sub-surface systems. Composite material selections are often made based upon empirical data derived from material level tests, prototype experiments, and/or analytical/computational mechanics models for the required operating conditions.

Compared to traditional materials such as metals, there is a limited understanding of the mechanical and failure/fracture mechanisms of complex composite structures operating in cold environments. Understanding these environmental influences is critical towards the: (1) improvement of fiber and matrix material performance, (2) development of damage and fracture resistant laminate designs, (3) progress in new manufacturing processes necessary to meet the challenges of operations in cold environments.

A key advantage of FRP composites is the ability to tailor material performance to design requirements and operating environments simultaneously. FRP composites provide attractive performance benefits such as corrosion resistance and high strength-to-weight ratios. Proper selections of the fabric architectures, fiber and matrix materials, ply stacking arrangements, etc. can yield significant mechanical advantages over traditional structural materials. However, selections are often based upon experiments and analyses performed without consideration of extreme operating temperatures and water ingress. Many polymers exhibit increased stiffness and decreased toughness with colder temperatures. Therefore, the potential for unanticipated failure mechanisms to occur with polymer based composites at low temperatures. Therefore, material considerations in the design of composite structures subject to cold operating environments require an in-depth

understanding of the relationships between laminate stress distributions, ply interface mechanics, hygrothermal behaviors, damage mechanisms, and fracture mechanics.

Improving fracture toughness of FRP composites is of greater importance when severe loading events including blast, ballistic impact, underwater explosion (UNDEX) shock, wave slap, and implosion may occur in cold environments. Characterizing the effects of cold temperatures and moisture absorption on static and dynamic fracture toughness's using analytical work and laboratory measurements, sensible decisions can be made when considering material selection in future composite components. Comprehensive knowledge of the effects of temperature and moisture (including expansion/contraction of absorbed water) on the failure response of FRP composites beyond what is currently known will greatly advance the ability to design and implement future systems.

Studies of temperature effects on FRP composites have been limited. Kichhannagari [4] observed from experiments that micro-cracking was more pronounced at cold versus ambient temperatures when specimens were tested under nominal uniaxial and biaxial loads. Thermal contraction causes matrix shrinkage and forms residual inter-laminar shear stresses. These stresses degrade the inter-laminar shear strength, fatigue life, and laminate stiffness. Microcracks can lead to increased permeability, creating paths for hygrothermal effects [5,6,7] such as moisture and fluid absorption and swelling at cold temperatures. Swelling is a major source of environmentally-induced stress when laminates are subjected to a freeze-thaw cycling in the presence of water. The generation of microcracks can lead to coalescence which can cause larger meso-scale and macro-scale cracks leading to reductions in fracture toughness and damage tolerance. Hybrid

laminates with different fiber materials and orientations, show sensitivity to low temperature-induced microcracking. The effects of freeze-thaw cycles when combined with loading cycles are of significant importance and must be addressed in the design process. In order to study these influences, comprehensive testing and analysis must be performed. In general, fracture can occur in one or more of three modes: Mode-I (separation), Mode-II (in-plane shearing), or Mode-III (out-of-plane tearing). The fracture toughness of a given material is defined by the mixed mode critical strain energy release rate, G_C . Fracture propagation occurs when the total strain energy release rate acting on the material, G_T , exceeds the critical strain energy release rate G_C .

Improving fracture toughness of FRP composites is of greater importance when severe loading events including blast, ballistic impact, UNDEX shock, wave slap, and implosion may occur in cold environments. Controlled laboratory experiments can be performed to simulate these events by which the effects of cold temperatures on static and dynamic fracture toughnesses can be characterized for optimizing material selections in design. Increased knowledge of cold temperature effects on the fracture response of FRP composites will advance the structural integrity and reliability of future Navy systems.

The vast majority of loading conditions upon a material consist of a combination of Mode-I, Mode-II, and Mode-III loadings. Investigating fracture toughness when subjected to mixed mode loading at varying temperatures and moisture levels provides a more complete scope of the failure mechanisms associated with a given composite construction and its operating environment. For applications where blast, ballistic impact, or UNDEX shock are a concern, fracture toughness as it relates to mixed mode failure is vital in preventing structural failure of critical composite components.

This investigation provides an overview of the effects of decreasing temperature on the material and fracture properties of carbon and E-glass composites

2. Materials

The materials studied in the current investigation consisted of carbon/epoxy and E-glass epoxy laminates. In each laminate the base fabric was a “plain weave” style in which the yarns are woven in a one-over one-under pattern as shown in Figure 1. This resulted in a balanced fabric in the warp and weft directions. The E-glass fabric was designated by the manufacturer (JPS Composite Materials) as Style 7533 which had a weight of 5.61 oz/yd², yarn count of 18 in the warp and fill directions, and was in a Greige (untreated) condition. The carbon fabric was designated as S 611 which had a weight of 5.88 oz/yd², yarn counts of 12.5 in each direction, and was also untreated. To support the various testing requirements, panels of thicknesses of 0.1 inch and 0.2 inch were procured from Core Composites.

Figure 1- Plain Weave Fabric Architecture (Courtesy of JPS Composites Technical Reference Handbook)

3. Mechanical Testing

3.1 Tensile Testing

Tensile testing was conducted on the carbon and E-glass laminates to determine the effect of decreasing temperature on these properties. The testing was conducted in accordance with ASTM D638 (Standard Test Method for Tensile Properties of Plastics). Each material was characterized for both elastic modulus and tensile strength at temperatures of 20 °C, 5 °C, and -2 °C. All tests were performed on an Instron® machine operated in displacement-controlled loading, with the low-temperature conditions performed in an environmental chamber (figure 2) with liquid nitrogen as the cooling source. An image of the test specimen geometry and test configuration are provided in figures 3 and 4. For each material and temperature, a total of six specimens were tested.

Figure 2 - Environmental Thermal Chamber

Figure 3 - ASTM D638 Tensile Specimen Geometry (Inches)

Figure 4 - Tensile Test Setup

The results for the E-glass tensile tests conducted at 5 °C are shown in figure 5 which highlights the repeatability of the results across the six test specimens. Similar repeatability was seen across both sets of materials and across the temperatures considered. From the figure it was shown that material response is nearly linear up to

failure and the failure was classified as brittle in nature such that there was minimal plastic response prior to failure.

Figure 5 - Tension Test Results, E-Glass at 5 °C

The tensile characterization results for each material are summarized in table 1 and detailed in figures 6 and 7 for the E-glass and carbon respectively. From these results several trends were identified:

- The carbon based laminates were about five times stiffer than the E-glass laminates across all temperatures (8500 kilopounds per square inch (ksi) vs. 1645 ksi)
- The carbon laminates had slightly more than double the tensile strength of the E-glass laminate

- The specific E-glass/epoxy material that was evaluated did not demonstrate a significant dependence on decreasing temperature across the range considered. Both the modulus and tensile strength were statistically equal at each temperature ranging from 20 to -2 °C. Graphically, this trend is highlighted by the nearly flat trend line in both the modulus and strength plots for the E-glass material.

- The carbon/epoxy laminate did exhibit a dependence on the test temperature in that the material became both stiffer and stronger as the temperature decreased from 20 to -2 °C. The modulus displayed a 11% increase and the strength displayed a 7% increase over the temperature range, hence an inverse relationship between tensile performance and temperature.

Table 1 - Tensile Testing Result Summary

Temperature (°C)	Modulus (ksi)	Strength (ksi)
E-Glass/Epoxy		
20	1652	41.8
5	1645	41.7
-2	1670	42.5
Carbon/Epoxy		
20	7954	86.4
5	8520	88.1
-2	8891	92.4

Figure 6- E-Glass Tensile Modulus and Strength Characterization

Figure 7 - Carbon Tensile Modulus and Strength Characterization

3.2 Compression Testing

Consistent with the tensile testing previously discussed, compression mechanical testing was performed on each of the material laminates over the identified temperature range of interest. ASTM D3410 (Standard Test Method for Compressive Properties of Polymer Matrix Composite Materials with Unsupported Gauge Section by Shear Loading) was used as the standard method for the conduct of all tests. The

characterization consisted of quantification of elastic compressive modulus and strength at temperatures of 20 °C, 5 °C, and -2 °C in the environmental chamber. A specimen geometry and test configuration are provided in figures 8 and 9. For each material and temperature, a total of six specimens were tested.

Figure 8 - ASTM D3410 Compression Specimen Geometry (Inches)

Figure 9 - Compression Test Setup

Compressive test data for the carbon/epoxy laminate at a temperature of 5 °C is provided in figure 10. As previously shown for the tensile testing, the results were repeatable across each specimen evaluated. The compressive behavior of the E-glass was consistent with the carbon in that each material was nearly linear up to failure and the failure was characterized by a sudden drop in load-carrying capacity with very little reduction in stiffness occurring prior to failure.

Figure 10 - Compression Test Results, Carbon at 5 °C

Table 2 summarizes the compression characterization results for each material with the details provided in figures 11 and 12 for the E-glass and carbon respectively. From these results there are several trends that were identified:

- Both the carbon and E-glass laminates are significantly less stiff in compression than in tension. The E-glass laminate was ~4.5 times stiffer

in tension and the carbon laminate was on the order of 10 times stiffer in tension.

- Specific to the compressive behavior, the carbon-based laminates were about two times stiffer than the E-glass laminates across all temperatures.
- The carbon laminates had approximately 2.5 times the compressive strength of the E-glass laminates across the temperature range.
- The specific E-glass/epoxy material utilized in this study exhibited differing trends in moduli and strength with decreasing temperature. As evidenced from figure 11, it was seen that with decreasing temperature there was a decrease in modulus but a corresponding increase in strength. In other words, as the temperature is reduced, the material got softer but stronger.
- The carbon/epoxy laminate exhibited a clear trend in decreasing with decreasing temperature, a decrease of 30% over the range from 20 °C to -2 °C. Similarly, the material strength exhibited a decrease from 20 °C to 5 °C, but then remained statistically constant from 5 °C to -2 °C.

Table 2 - Compression Testing Result Summary

Temperature (°C)	Modulus (ksi)	Strength (ksi)
E-Glass/Epoxy		
20	358	22.3

5	266	23.2
-2	267	24
Carbon/Epoxy		
20	818	65.1
5	631	58.2
-2	564	61.0

Figure 11 - E-Glass Compressive Modulus and Strength Characterization

Figure 12 - Carbon Compressive Modulus and Strength Characterization

3.3 Short Beam Testing

The short beam strength testing was conducted in accordance with ASTM D2344 (Standard Test Method for Short-Beam Strength of Polymer Matrix Composite Materials and Their Laminates) for each of the material laminates over the 20 °C to -2 °C temperature range. For each temperature the stress vs. displacement as well as overall short beam strength as a function of temperature was determined. The specimen

geometry and test configuration are provided in figures 13 and 14. For each material and temperature, a total of six specimens were tested.

Figure 13 - ASTM D2344 Short Beam Specimen Geometry (Inches)

Figure 14 - Short Beam Test Configuration

Short beam shear test data in the form of stress vs. displacement for tests conducted at 20 °C, 5 °C, and -2 °C is provided in figures 15 and 16 for the carbon/epoxy and E-glass epoxy laminates respectively. At a given temperature the results were shown to be

highly repeatable for each temperature. The E-glass specimens were characterized by a linear ramping of stress followed by a flat plateau in the stress response. Similarly, the carbon specimens exhibited the same linear ramp up of stress during the initial loading, but displayed a decrease in stress capacity after reaching the maximum value rather than a flat plateau.

Figure 15 - E-Glass Short Beam Shear Stress Results

Figure 16 - Carbon Short Beam Shear Stress Results

Table 3 summarizes the short beam shear strength for each material and the trends were graphically highlighted in figure 17. From these results there were several trends that are identified:

- In terms of overall short beam shear strength, the carbon laminates exhibited approximately twice the strength as compared to the E-glass laminates at a given temperature.
- The E-glass/epoxy laminates were characterized by a flat plateau in stress with increasing displacement after reaching maximum load whereas the carbon laminates exhibited decreasing stress capacity after maximum load.

- The carbon and E-glass laminates exhibited a 20% increase in short beam shear strength over the range from 20 °C to -2 °C.

Table 3 - Short Beam Testing Result Summary

Temperature (°C)	Short Beam Shear Strength (ksi)
E-Glass/Epoxy	
20	4.02
5	4.82
-2	4.89
Carbon/Epoxy	
20	7.66
5	9.08
-2	9.51

Figure 17 - Short Beam Shear Strengths of E-Glass and Carbon Laminates

3.4 Fracture Testing

Mode-I fracture tests were conducted to measure the effects of decreasing temperature on the Mode-I (see figure 18) critical strain energy release rates G_{IC} for both the carbon and E-glass laminates. The Mode-I fracture tests were performed at 20 °C, 5 °C, and -2°C to ensure consistency across the mechanical characterization study. The specimens were double cantilever beam (DCB) type and fracture gauges were bonded to the specimen edges to measure the crack lengths, crack growth stability and G_{IC} .

Figure 18 - Fracture Modes: (A) Mode-I, (B) Mode-II, and (C) Mode-III

Fracture toughness was measured in the form of Mode-I critical strain energy release rates using the DCB test method in accordance with ASTM-D5528. The laminates were machined into 9.00-inch long by 1.00-inch wide DCB fracture specimens as shown in figure 19. Each specimen included a Teflon insert positioned at the mid-plane spanning across the width but extending partially inward from one end along the length by a distance of 3 inches. The specimens were gripped using hinges bonded to the top and bottom surfaces of the specimens at the ends containing the Teflon inserts (figure 20). The free end of each specimen was not restrained. Loading was applied in displacement control mode at a crosshead rate of 0.20 inch/min. Crack gauges from Vishay Precision Group, Inc. (Part No. TK-09-CPC03-003/DP) were used to monitor crack growth. Single crack gauges were bonded to one side of each specimen and were connected to a data acquisition system. Each gauge consisted of 20 strands oriented along the specimen crack direction with a strand spacing of 0.08-inch. As the crack front propagated across each strand, the strands broke consecutively and the change in gauge resistance was recorded. The instantaneous crack length was tracked by monitoring the time-history changes in gauge resistance. Additionally, the load and deflection time histories were

recorded by the test machine. Using the time history data, crack growth and G_{IC} values were characterized.

Figure 19 - ASTM D5528 Fracture Specimen Geometry

Figure 20 - Fracture Test Configuration

A description of the fracture behavior in the laminated DCB specimens follows. Upon loading, strain energy is produced in the specimen and a critical load P_c is reached. At this load, the corresponding strain energy causes crack initiation to occur at the Teflon insert. The DCB loading arrangement generates a Mode-I crack extending from the Teflon insert along the specimen mid-plane. As the deflection of each beam increases further, increases in crack length occur, strain energy is released (lost) and compliance is increased. (Note that the crosshead extension δ for symmetric laminates is assumed to equal 2x the deflection of an individual beam or arm.) The load versus deflection curve for the -2 °C E-glass fracture test is shown in figure 21 and was representative of the DCB Mode-I fracture test specimen behavior of all samples tested.

Figure 21 - Typical Load vs. Extension for Fracture Test

Strain energy released through Mode-I fracture was calculated by continually monitoring the loads and deflections for each crack length prior to the next increment of crack

growth. The overall crack lengths were measured by monitoring the resistance changes across the crack gauges. The data acquisition system recorded the crack gauge voltages over the time duration of the test. Discrete jumps in the crack gauge voltage were due to failures of the individual strands within the gauges as shown in figure 22. Each jump in voltage corresponded to an incremental increase of 0.08-inch in crack length.

Figure 22 - Time vs Crack Gauge Voltage

3.5 Strain Energy Release Rate Calculation

The critical strain energy release rate G_{IC} is defined as the strain energy released per unit area of new crack surface generated. The method of calculating G_{IC} in the current study is the modified beam theory (MBT) approach.

The modified beam theory governing equation is shown in equation 1. Load and deflection data are determined experimentally, therefore the sum of the laminate bending and transverse shearing components of deflection is treated as the total bending deflection of the beam. If the laminate thickness is small and the transverse shearing deformations can be neglected, MBT can be used. G_{IC} can be plotted vs. crack length a by using each crack gauge strand location. With the exception of the first few gauge strand locations, which are close to the initial crack front, G_{IC} is consistent along the crack path.

$$G_{IC} = \frac{3P\delta}{2wa} \quad (1)$$

The values of G_{IC} were calculated for each material and temperature considered using equation (10) with the E-glass and carbon laminate results provided in figures 23 and 24 respectively. From these figure it is seen that each laminate material exhibits a direct dependence of G_{IC} on temperature. It is evident that with decreasing temperature there is a corresponding decrease in G_{IC} . For the E-glass laminate the G_{IC} value (8 lb-in/in²) at -2°C is ~65% of the corresponding value at 20 °C (12 lb-in/in²). The Carbon laminate exhibits less of a dependence on temperature as seen in the E-Glass material. There is a very small decrease in G_{IC} from 20 to 5°C, and a statistically constant value from 5 to -2°C. The differing trends in fracture behavior between the materials indicates that the strain energy release rate, G_{IC} , is primarily a function of fiber type and not matrix material for the current laminates. Each laminate in this study utilized the same resin composition.

Figure 23 - E-Glass Strain Energy Release Rate G_{IC} vs. Crack Length

Figure 24 - Carbon Strain Energy Release Rate G_{IC} vs. Crack Length

4. Conclusions

A detailed experimental and analytical investigation of the effects of low temperature on the mechanical, water ingress/diffusion, and acoustic properties of E-

Glass/Epoxy and Carbon/Epoxy laminates has been conducted. The investigation was primarily aimed at establishing a foundational understanding of the temperature effects on composite laminates at temperatures of interest to the undersea warfare community. Historically, mechanical and acoustic properties at operating temperatures in the range of $\sim 15 - 20$ °C have been evaluated, with minimal data pertaining to the temperatures associated with arctic seawater in the $\sim 2 - 4$ °C regime. The key findings of the study were as follows:

- Mechanical Performance
 - Tension
 - The specific E-glass/epoxy material investigated exhibited a minimal dependence on decreasing temperature in terms of elastic modulus and tensile strength.
 - There is a measurable dependence on decreasing temperature for the carbon/epoxy laminate evaluated with the material both stiffening and strengthening over the range 20 to -2 °C. Specifically, an 11% increase in modulus and a 7% increase in strength with decreasing temperature. Hence an inverse relationship between tensile performance and temperature.
 - Compression
 - The specific E-glass/epoxy material utilized in this study exhibited opposite trends in stiffness and strength with decreasing temperature. As the temperature is decreased the material become softer but stronger.

- Carbon/epoxy laminates exhibited decreasing stiffness with decreasing temperature, a decrease of 30% over the range considered, and a material strength decrease from 20 °C to 5 °C followed by near constant strength from 5 °C to -2 °C.
- Short Beam Shear
 - The E-glass/epoxy laminates were characterized by a flat plateau in stress with increasing displacement after reaching maximum load whereas the carbon laminates exhibit decreasing stress capacity after maximum load.
 - The carbon and E-glass laminates exhibited a 20% increase in short beam shear strength over the range from 20 °C to -2 °C.
- Fracture
 - Both the E-glass and carbon laminates considered, exhibited a direct dependence of G_{IC} on temperature with a observable trend that with decreasing temperature there was a corresponding decrease in G_{IC} .
 - The E-glass laminate had a G_{IC} value of 8 lb-in/in² at -2 °C which was ~65% of the corresponding value at 20 °C, 12 lb-in/in².

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CHAPTER 7

CONCLUSIONS AND FUTURE WORK

1. CONCLUSIONS

This research has studied the response and structural performance of composite materials when subjected to extreme loading conditions, namely in the form of shock and low temperatures. The studies consisted of experimental work with corresponding numerical simulations, with the primary contributions of this author being the numerical modeling. The fundamental objective of the study as a whole was to develop a better understanding of the response of composite materials leading to more efficiently designed structures while understanding the effects of elastomeric coatings, long term seawater immersion/degradation, and low temperature exposure. The relevant findings resulting from the present study are presented below.

- (1) Through an experimental and numerical study, the response of flat composite plates subjected to near field underwater explosive loading was investigated, including the influence of polyurea coatings. The relative performance of the plate configurations was evaluated in terms of center-point and full-field time histories of the deflection of the back-face of the plates, as well as level of material damage. It was shown that the use of the coatings reduced both the transient deflections as well as the material damage. The computational models developed in the study were shown to accurately simulate the testing with good correlation between the transient responses. Additionally, the models are able to accurately simulate the detonation of the explosive charge and the resulting pressure fields and plate deflections.

- (2) The effects of material degradation due to long term seawater immersion on the air blast response of a Carbon-Epoxy material was investigated through experimental and numerical approaches. It was shown that when ageing effects were included, both the transient deflections under load increased and the material suffered material failure. The corresponding numerical simulations matched well with the experimental data. However, for the fixed boundary case, the beam vibration of the simulation was off phase with the experimental results due to imperfect boundary conditions in the experiments.
- (3) The response of composite cylinders, including elastomeric coatings, subjected to near field UNDEX loading was studied through a combined experimental and computational approach. The primary parameters of interest in the study were transient response, damage extents, energy levels, and material strains. Damage to the coated composites was dramatically reduced as a function of increasing coating thickness as compared with the baseline cylinders. The modeling approach utilized in the study is able to accurately simulate the detonation of the explosive charge as well as predict the overall damage extents in the composite cylinders. During the transient loading of the cylinders, both the internal material energy and the overall system kinetic energy increase with increasing coating thickness.
- (4) A detailed experimental study was conducted to investigate the influence of low temperatures associated with arctic and deep ocean seawater on the mechanical performance of Carbon and E-Glass / Epoxy laminates. The study showed that both the moduli (stiffness) and the strength of the materials considered were

effected in the temperature range considered. Furthermore, the fracture toughness of the materials decreased with decreasing temperature.

2. FUTURE WORK

The current investigation has provided a basis for the development of numerical modeling approaches for the simulation of composite materials when subjected to dynamic loading conditions, namely air and underwater shock loading. It has also presented the effects of reduced temperatures on the mechanical characteristics of similar laminates. As with all research, there remains a significant body of work to be completed in this area before the dynamic response of these materials matures to an equivalent level of understanding as that for metallic materials. This work includes further experimental and computational studies as well as work which correlates the two. This will effectively lead to validated modeling practices that can be applied during the design phase of composite structures. The proposed potential future projects are summarized as follows:

1. Perform additional experimental dynamic loading studies involving additional material combinations and/or laminate architectures to further populate the available data to support model validation. Composite materials are inherently unique and dependent upon material construction, further data will help to quantify the variability under dynamic loading of these materials to allow for design considerations. The performance of other materials such as S-Glass and Kevlar should be examined as well as the possibility of hybrid materials such as Glass / Carbon constructions. The performance of these materials needs to be understood as they inherently have different characteristics. Furthermore, there now exist three

dimensional (3D) fabrics which include through thickness fibers which are interwoven through the cloth. These through thickness fibers may improve the performance of the laminates in terms of reducing the delamination damage.

2. Conduct shock experiments in which the complexity of the geometry of the test articles is further increased. More complex geometries could include doubly curved surfaces, oblong spheroids, and plates with abrupt angle changes. The goal should be to incorporate real world design shapes into the test article geometry. The current finite element modeling methodology should also be expanded to simulate these experiments to ensure it is able to accurately simulate the geometrical effects.
3. Conduct experimental work in which the influence of low temperatures under dynamic loading conditions are evaluated. The current study performed mechanical characterization at quasi-static loading, although of far more interest is those loading rates associated with impact, ballistic, and shock conditions.

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