Failure evaluation of filament wound composite risers with isotropic liner

Boon, Yi Di

2018

Boon, Y. D. (2018). Failure evaluation of filament wound composite risers with isotropic liner. Doctoral thesis, Nanyang Technological University, Singapore.

<http://hdl.handle.net/10356/73627>

<https://doi.org/10.32657/10356/73627>

**NANYANG
TECHNOLOGICAL
UNIVERSITY**

SINGAPORE

**FAILURE EVALUATION OF FILAMENT WOUND COMPOSITE
RISERS WITH ISOTROPIC LINER**

BOON YI DI

SCHOOL OF MECHANICAL AND AEROSPACE ENGINEERING

2018

**FAILURE EVALUATION OF FILAMENT WOUND COMPOSITE
RISERS WITH ISOTROPIC LINER**

BOON YI DI

School of Mechanical and Aerospace Engineering

A thesis submitted to Nanyang Technological University
in partial fulfilment of the requirement for the degree of

Doctor of Philosophy

2018

Acknowledgements

Firstly, I would like to express my deepest gratitude to my supervisor Assoc. Professor Sunil Chandrakant Joshi. His invaluable insights and continued support have guided me throughout. Many thanks also to my co-supervisor Assoc. Professor Ong Lin Seng for his advice and teaching offered to me.

Secondly, I would like to thank my Thesis Advisory Committee members Assoc. Professor Sridhar Idapalapati, Asst. Professor Aravind Dasari and Assoc. Professor Yang Jinglei (former committee member) for their help and feedback on my research.

I would also like to thank my seniors and friends in my research group, Dr. Vishwesh Dikshit, Dr. Zhong Yucheng and Mr. Bhudolia Somen Kumar for their kind assistance in all aspects of my research. Special thanks go to all MAE laboratory staff for their help in my experiments.

Finally, I would like to thank my family and friends for their encouragements and support.

Contents

Acknowledgements	i
Contents	ii
Abstract	vi
List of Publications	vii
Journal	vii
Conference	vii
List of Tables	viii
List of Figures	xi
Nomenclature	xvi
1. Introduction	1
1.1. Background and Motivation	1
1.2. Objectives and Scope	5
1.3. Thesis Layout	5
2. Literature Review	6
2.1. Design Requirements for Composite Risers	6
2.2. Failure of Composite Tubes under Pressure and Functional Loads	8
2.3. Environmental Loads and Long Term Considerations	13
2.4. Finite Element Analysis	16
2.4.1. Pre-Failure Constitutive Model	17
2.4.2. Failure Criteria to Predict Onset of Failure	20
2.4.3. Intra-Laminar Post-Failure Progressive Damage Analysis (PDA)	29
2.4.4. Cohesive Zone Model for Interlaminar Failure Analysis	37
2.5. Numerical Studies of the Failure of Composite Tubes	40
2.6. Conclusions and Remarks	48

3. Materials and Methods	51
3.1. Composite Pipe Test Specimens.....	51
3.1.1. Selection of Materials	51
3.1.2. Filament Winding Process	52
3.2. Mechanical Tests	54
3.3. Finite Element Software and Simulations.....	59
3.3.1. Material Properties for Simulations.....	61
3.4. Scaling Philosophy	64
4. Composite Material Model	66
4.1. Nonlinear Behavior of Composite Materials Pre-Failure	66
4.2. Development of Bimodulus-Plastic Model	67
4.2.1. Plastic Deformation.....	68
4.2.2. Matrix Ductile Damage.....	69
4.2.3. Model Implementation	73
4.3. Determination of Material Properties for Bimodulus-Plastic Model	75
4.4. Model Validation	79
4.5. Conclusions	84
5. Predictive Model for Composite Material Properties with Absorbed Moisture	85
5.1. Effective Additive Group Contributions for Polymers	85
5.2. Localized Reduced Modulus of Elasticity	87
5.3. Tensile Properties of Bulk Polymer.....	89
5.4. Transverse Tensile Strength of CFRP Laminae	91
5.5. Validation Results and Discussion	93
5.6. Conclusions	97
6. Experiments and Simulations of Effects of Moisture on CFRP	99
6.1. Moisture Absorption.....	99

6.2.	Tensile Tests and Simulations	101
6.3.	Compression Tests and Simulations	104
6.4.	Flexural Tests and Simulations	107
6.5.	Conclusions	108
7.	Liner-Composite Interface in Risers (Experiments)	110
7.1.	Mechanical Surface Treatment for Liner	110
7.1.1.	Grit Blasting	110
7.1.2.	Groove	111
7.2.	Lateral Compression Tests	112
7.3.	Axial Compression Tests	114
7.4.	Conclusions	118
8.	Liner-Composite Interface in Risers (Simulations)	120
8.1.	Surface-Based Cohesive Behavior	120
8.2.	Lateral Compression Simulations	121
8.3.	Axial Compression Simulations	125
8.4.	Discussion on the Grooving Surface Treatment	131
8.5.	Conclusions	132
9.	Progressive Damage Simulations of Composite Risers	134
9.1.	Composite Riser Simulation Setup	134
9.2.	Failure under Burst Pressure	136
9.3.	Failure under Collapse Pressure	140
9.4.	Effects of Accidental Loads	142
9.5.	Conclusions	144
10.	Conclusions and Recommendations for Future Work	146
10.1.	Conclusions	146
10.2.	Major Contributions	148

10.3. Recommendations for Future Work	149
References	151
Appendices	160
Appendix A. Composite Material Model in the Fiber Direction	160
Appendix B. IM2A Carbon Fiber Technical Data Sheet	162
Appendix C. Epolam 5015/5015 Epoxy System Technical Data Sheet	164

Abstract

The use of composite materials such as carbon/epoxy for the construction of deep water risers in place of metals can lead to significant weight reduction and cost savings. In order to realize this potential, a thorough understanding of the mechanical behavior and failure modes of composite risers is required.

In this study, the progressive damage analysis technique and finite element simulations are used extensively to investigate the failure of composite risers. The nonlinear behavior of composites in the pre-failure stage is taken into consideration in the bimodulus-plastic model developed in this study. The inclusion of the nonlinear behavior can result in better predictions in the finite element simulations.

A predictive model is also developed to determine the tensile properties of composite materials affected by moisture. The model is based on the use of additive group contributions for polymers. The tensile properties calculated using the model agreed well with experimental data. The effect of moisture on the compressive properties of composite materials is also investigated in experiments and simulations.

Failure at the liner-composite interface is also considered in this study. The failure and damage mechanisms at the interface for the grooving and grit blasting mechanical surface treatment methods are investigated and discussed. The behavior and failure of the liner-composite interface are modeled successfully in finite element simulations.

The local analysis of a composite riser segment using finite element simulations is demonstrated. The progressive damage analysis method is effective in predicting failure, especially for cases where damage due to accidental load has occurred.

List of Publications

Journal

1. Y. D. Boon, S. C. Joshi and L. S. Ong, "Interfacial Bonding between CFRP and Mechanically-Treated Aluminum Liner Surfaces for Risers." *Composite Structures*, vol. 188, pp. 374-386, 2018.
2. Y. D. Boon and S. C. Joshi, "Predictive Model for Tensile Properties of Carbon Fiber-Reinforced Polymers at Various Moisture Contents," *Plastic and Polymer Technology*, vol. 4, pp. 53-62, 2016.
3. Y. D. Boon, S. C. Joshi and L. S. Ong, "Bimodulus-Plastic Model for Pre-Failure Analysis of Fiber Reinforced Polymer Composites." (Submitted to *Journal of Composite Materials*)

Conference

1. Y. D. Boon, S. C. Joshi and L. S. Ong, "Effects of Mechanical Surface Treatment on Bonding between Aluminum and Carbon/Epoxy Composites," in *Advances in Materials & Processing Technologies Conference*, November 2016, Kuala Lumpur, Malaysia, *Procedia Engineering*, vol. 184, pp. 552-559.

List of Tables

Table 2-1. Inclination parameters for the Puck failure theory	26
Table 2-2. Composite layup configuration studied by Ribeiro et al.....	45
Table 3-1. Mechanical properties of IM2A carbon fiber	51
Table 3-2. Mechanical properties of Epolam 5015/5015 epoxy system.....	52
Table 3-3. Summary of composite test specimens without liner	58
Table 3-4. Summary of composite test specimens with aluminum liner.....	59
Table 3-5. Mechanical properties of aluminum	63
Table 3-6. Elastic properties of the undamaged carbon fiber-reinforced composite ...	63
Table 3-7. Strength properties of the carbon fiber-reinforced composite	64
Table 4-1. Comparison of simulation and experimental results for the tensile and axial compression tests for composite test specimens without liner	79
Table 4-2. Material properties for the bimodulus-plastic model for tension and compression failure modes	79
Table 4-3. Comparison of simulation and experimental results for the three-point flexure test for composite test specimens without liner	81
Table 5-1. Elastic modulus (E) and ultimate tensile strength (X) of two polymers at various moisture content	94
Table 5-2. Properties of G30-500/5208 and T300/Fiberite 1034 CFRP laminae	95
Table 5-3. Transverse tensile strength (X_T) of CFRP laminae at various moisture contents	96
Table 6-1. Moisture content at saturation	100

Table 6-2. Tensile test results for dry and wet specimens	102
Table 6-3. Tensile properties for the carbon/epoxy composite in dry and wet conditions.....	104
Table 6-4. Axial compressive test results for dry and wet specimens	105
Table 6-5. Compressive properties for the carbon/epoxy composite in dry and wet conditions.....	107
Table 6-6. Simulation and experimental results for flexural tests of dry and wet specimens.....	107
Table 7-1. Summary of lateral compression test results for composite test specimens with liner	114
Table 7-2. Summary of axial compression test results for composite test specimens with liner	117
Table 8-1. Comparison of simulation and experimental results for the lateral compression test on composite pipe specimens with liner treated with grit blasting and grooving.....	122
Table 8-2. Interface properties used in simulations	123
Table 8-3. Comparison of simulation and experimental results for the axial compression test on composite pipe specimens with liner treated with grit blasting and grooving.....	130
Table 9-1. Composite riser dimensions.....	134
Table 9-2. Results for burst simulations performed using different models	137
Table 9-3. Burst simulation results for composite riser segment in dry and wet conditions.....	140

Table 9-4. Collapse simulation results for composite riser segment in dry and wet conditions.....	140
Table 9-5. Collapse simulation results for composite riser segments with and without accidental damage.....	143

List of Figures

Figure 1-1. Tension leg platform.....	2
Figure 1-2. Schematic of the filament winding process	3
Figure 1-3. IFP-Aerospatiale composite riser design	3
Figure 1-4. The Heidrun TLP	4
Figure 2-1. Loads acting on a typical riser in operation	7
Figure 2-2. Final failure of composite pipe under burst pressure in Mode 2 conditions.	9
Figure 2-3. Structural strength of multi-angle composite tubes	12
Figure 2-4. Shear stress-strain response from cyclic tensile test	19
Figure 2-5. Schematic of sudden and gradual degradation models for progressive damage analysis.....	30
Figure 2-6. Linear softening model for intra-laminar damage	34
Figure 2-7. Example to illustrate the limitation of current progressive damage analysis techniques	36
Figure 2-8. Mixed-mode response in the cohesive zone model by Camanho et al.....	40
Figure 2-9. Burst pressure simulation by Martins et al.....	43
Figure 2-10. Moment-curvature response using different knockdown factor values for stiffness degradation	47
Figure 3-1. Fabrication of composite pipe using a filament winding machine.....	53
Figure 3-2. Binder FP 720 oven used for post-cure	54

Figure 3-3. Universal Testing Machines	55
Figure 3-4. Tensile test specimens.....	56
Figure 3-5. Compression tests	57
Figure 3-6. (a) Water bath and (b) weighing balance	57
Figure 3-7. Lateral compression test of aluminum pipe	61
Figure 3-8. Aluminum pipe lateral compression load-displacement curves from experiment and simulation	62
Figure 4-1. Comparison of simulation (with linear-elastic behavior in the pre-failure stage) and experimental results for tensile test carried out on $[\pm 55^\circ]_3$ carbon/epoxy composite.....	66
Figure 4-2. Optical micrograph showing cracks in the matrix in the pre-failure stage	67
Figure 4-3. Schematic diagram of the bimodulus model with linear softening after final failure.....	71
Figure 4-4. Flow chart of the bimodulus-plastic model for an increment in FE simulation.....	74
Figure 4-5. Composite test specimens after (a) tensile test and (b) axial compression test	75
Figure 4-6. Finite element mesh for (a) tensile test and (b) axial compression test simulations for composite test specimens without liner	76
Figure 4-7. Tensile test experimental and simulation results for composite test specimens without liner.....	77
Figure 4-8. Compression test experimental and simulation results for composite pipe specimens without liner.....	78

Figure 4-9. Finite element mesh for three-point flexure test simulation	80
Figure 4-10. Width and height used in calculation of stresses and strains for flexural tests.....	80
Figure 4-11. Three-point flexure test experimental and simulation results for composite test specimens without liner	82
Figure 4-12. Permanent mid-span deflection after unloading in the three-point flexure test	82
Figure 4-13. Contour plots of the shear stress calculated from FE simulation of the flexure test	83
Figure 5-1. SEM image of the cross section of a CFRP laminate after hygrothermal conditioning.....	88
Figure 5-2. Schematic diagram of the distribution of region with reduced E by water in polymers.....	90
Figure 5-3. Moisture weakens the matrix around the fibers.	92
Figure 5-4. A comparison of the calculated transverse tensile strengths of CFRP laminae with experimental values.....	97
Figure 6-1. Saturation moisture content determined when equilibrium is reached.	100
Figure 6-2. Stress-strain response of wet specimens from tensile tests and simulation	104
Figure 6-3. Axial compression test results for wet specimens	105
Figure 6-4. Schematic of CFRP composite under (a) compressive and (b) tensile loads	106
Figure 6-5. Finite element mesh for axial compression test (short specimen)	107

Figure 6-6. Stress-strain response of wet specimens from flexural tests and simulation	108
Figure 7-1. Aluminum liner before and after grit blasting	111
Figure 7-2. Groove surface treatment	111
Figure 7-3. Microscope images showing cross sectional areas around two different grooves.....	112
Figure 7-4. Lateral compression test results for composite test specimens with liner	113
Figure 7-5. Composite pipe specimens with liner after lateral compression test	114
Figure 7-6. Axial compression test results for aluminum liner.....	115
Figure 7-7. Compressive stress vs axial strain from axial compression tests on composite pipe specimens with liner	116
Figure 7-8. Bulging at one end of a composite pipe specimen after axial compression test indicating liner-composite debonding	118
Figure 8-1. Finite element mesh for lateral compression simulations	121
Figure 8-2. Lateral compression simulation and experimental results.....	122
Figure 8-3. Liner-composite debonding (shown in red) from lateral compression simulations	124
Figure 8-4. Comparison of lateral compression simulations with different interaction properties at the liner-composite interface	125
Figure 8-5. Slanted contact model for axial compression simulation.....	126
Figure 8-6. Sandwich model for axial compression simulation	127

Figure 8-7. Axial compression simulation and experimental results for composite pipe with liner treated with grit blasting	128
Figure 8-8. Axial compression simulation and experimental results for composite pipe with liner treated with grooving.....	129
Figure 8-9. Schematic diagram of plastic deformation in aluminum liner with grooves	131
Figure 8-10. Additional lateral compression simulation	132
Figure 9-1. Loads acting on a composite riser segment.....	135
Figure 9-2. Finite element mesh for composite riser simulations	136
Figure 9-3. Plots of hoop stress vs hoop and axial strains from burst simulation using the bimodulus-plastic model (for dry condition).....	139
Figure 9-4. Hoop stress vs axial and hoop strains from collapse simulation (for dry condition)	141
Figure 9-5. Finite element mesh for the study on accidental load	142
Figure 9-6. Contour plot of matrix damage at (a) external pressure = 32.8 MPa and (b) external pressure = 33.4 MPa.....	144

Nomenclature

PDA	Progressive damage analysis
FRP	Fiber reinforced polymer
CFRP	Carbon fiber reinforced polymer
GFRP	Glass fiber reinforced polymer
σ_{ij}	Stress components, $i, j = 1$ denotes the fiber direction and 2 denotes the transverse direction
ϵ_{ij}	Strain components
u_i	Displacements
γ_{ij}	Engineering shear strain
E_i	Modulus of elasticity
G_{ij}	Shear modulus
ν_{ij}	Poisson's ratio
X_T	Tensile strength in the fiber direction
X_C	Compressive strength in the fiber direction
Y_T	Tensile strength in the transverse direction
Y_C	Compressive strength in the transverse direction
S_L	Longitudinal shear strength
S_T	Transverse shear strength
G_C	Critical energy release rate

FI	Failure index
FI^d	Failure index at onset of matrix ductile damage
d_f, d_m	Damage variables corresponding to fiber and matrix failure respectively
k	Modulus reduction factor for matrix ductile damage
γ_{12}^{el}	Elastic component of in-plane engineering shear strain
γ_{12}^{pl}	Plastic component of in-plane engineering shear strain
γ_{12}^y	In-plane engineering shear strain at yield
R	Ratio of plastic to elastic shear strain
SCF	Stress concentration factor
MC	Moisture content
E_{red}	Localized reduced modulus of the polymer matrix due to moisture
F	Fiber-matrix interface weakening factor
τ_i	Traction components for liner-composite interface, $i = 1, 2$ denotes shear directions, 3 denotes normal direction
δ_i	Separation components for liner-composite interface
τ_i^o	Traction at the onset of liner-composite interface failure
K_{ij}	Liner-composite interface stiffness components

1. Introduction

1.1. Background and Motivation

Composite materials have been identified as improved alternative materials for use in offshore structures [1, 2]. This is because composite materials have high specific strength, good corrosion resistance, good fatigue resistance and can be tailored to meet specific requirements. In particular, the high specific strengths of composite materials results in weight reduction that can allow for lower tensioner requirement and smaller platform size, thus leading to significant cost reductions. The cost reductions are more substantial for deep water risers because the required platform size increases at a higher rate as tensioner requirement increases. For small tensioner capacity, 500 kips (2224 kN) or less, an increase of 1 lb (0.454 kg) leads to an increase of 1.33 times to the platform size. In contrast, for high tensioner capacity of about 700 kips (3114 kN), the same weight increase results in 2.11 times larger platform size [1]. For deep water applications, the tension leg platform (TLP) is used. Figure 1-1 shows a schematic of a TLP. Salama [2] identified three areas of a TLP system where composite materials can be applied: tendons, production risers, and drilling risers. The composite risers will be the main focus of this work.

Figure 1-1. Tension leg platform [3]

Composite risers or tubes can be fabricated using the filament winding technique. A schematic of the two axis winding process is shown in Figure 1-2. The process consists of the rotation of the mandrel and the movement of the carriage along the longitudinal direction of the tube. Glass or carbon fibers are impregnated with resin as they pass through the resin bath. The winding angle can be adjusted by adjusting the velocity of the carriage relative to the rotation of the mandrel. The fiber tension, fiber tow size, winding speed and resin viscosity need to be set properly to obtain the desired fiber volume fraction and to minimize void formation in the composite tube.

Figure 1-2. Schematic of the filament winding process [4]

The first composite riser that was produced and analyzed was a joint effort by Institute Francais du Petrole and Aerospatiale of France in the '80s [1, 2]. The riser was made with buna internal and external liners, longitudinal helical wound carbon layers at $\pm 20^\circ$ and circumferential S-glass fiber layers at 90° (Figure 1-3). The riser was subjected to burst tests, axial tension tests, fatigue and creep tests. The riser was found to have very good fatigue resistance.

Figure 1-3. IFP-Aerospatiale composite riser design [2]

The first offshore composite riser field application was a project by Norske Conoco AS and Aker Kvaerner [5]. The 15 m long composite drilling riser joint was made of

carbon/epoxy. It has a multi-layered internal liner consisted of rubber and titanium. The composite body was also protected by an external rubber layer. The composite riser joint was able to meet all the requirements for the project including internal pressure, external impact, and bending fatigue. The riser joint was successfully implemented in field operations on the Heidrun TLP from 2001 to 2002 (Figure 1-4). During the operation period, inspections and pressure tests were carried out on the composite riser joint between drilling operations to check for cracks. It was reported that no degradation of material properties was observed for the composite riser joint after undergoing 16 drilling operations.

Figure 1-4. The Heidrun TLP [6]

Composite materials show great potential of being able to replace metal for building deep sea risers. In order to make use of this potential, the behavior of composite risers when subjected to various loadings need to be studied and understood.

1.2.Objectives and Scope

The aims of this research are:

- I. Investigate the failure and failure modes of composite risers with isotropic liners under various load conditions, including burst pressure, collapse pressure, axial tensile forces and combinations of loads.
- II. Simulate the damage growth in the composite layers beyond the onset of localized constituent failure using progressive damage analysis techniques.
- III. Model the interface between the composite layers and the isotropic liner using cohesive zone models and study the debonding.
- IV. Study the effects of moisture absorption on the behavior of the composite tubes.
- V. Provide a comprehensive approach for the local analysis of composite risers to evaluate and predict damage growth and failure.

1.3.Thesis Layout

The thesis is divided into 10 chapters. Chapter 1 covers the background and the objectives of this study. Chapter 2 is the literature review. Previous studies related to the research topic as well as areas where improvements can be made are discussed in this chapter. The methodology for the current study is explained in Chapter 3. Discussion on the material model for composite materials is given in Chapter 4. The effects of moisture content on the mechanical properties of composites are discussed in Chapters 5 and 6. This is followed by the experimental and numerical findings on the failure at the liner-composite interface discussed in Chapters 7 and 8. In Chapter 9, progressive damage simulations of the failure of composite risers are demonstrated and discussed. The conclusions and major contributions are presented in Chapter 10.

2. Literature Review

2.1.Design Requirements for Composite Risers

There are many factors that need to be taken into consideration when designing a composite riser system. Ochoa gave a detailed guideline for the design of filament wound composite riser for offshore applications [1]. The composite riser usually consists of an internal liner and the composite body. The main function of the internal liner is to prevent the leakage of fluid that is carried by the riser. On the other hand, the composite body's function is to carry load. An external liner could be required in some cases to protect the composite body from various environmental effects and corrosion. The materials that are used for each component of the composite riser should, therefore, be chosen to serve their respective functions. For the internal liner, polymers (such as rubber), metals (such as steel and titanium) or a combination of the two types of materials can be used. For the composite body, glass/epoxy and carbon/epoxy are commonly used. Carbon fiber is stronger but more expensive compared to glass fiber.

In the design of the composite body, the load that the riser needs to carry should be considered. The loads are divided into four categories by Det Norske Veritas (DNV) [7, 8]: (i) pressure loads including internal and external pressure, (ii) functional loads such as top tension and the weight of the riser, (iii) environmental loads due to wave, current or wind, and (iv) accidental loads, such as impact and collisions. The requirement for internal pressure is higher than external pressure. For example, the required internal pressure for the riser joint at the Heidrun TLP was 86 MPa [5]. On the other hand, for external pressure, a water depth of 3000 m leads to external pressure at the sea bed of about 30 MPa. Some of the loads acting on deep water

risers are shown in Figure 2-1. Besides this, the composite body should also be designed for long term use. The degradation of the composite material properties by factors such as sea water corrosion, temperature changes and fatigue needs to be minimized to ensure the composite riser can operate long term [1, 7].

Figure 2-1. Loads acting on a typical riser in operation [9]. BOP refers to a blowout preventer.

The analysis of the composite riser system can be performed using a global-local procedure [1, 7]. The global analysis includes the whole riser system and can be done similar to the analysis of conventional metal riser systems. The resulting load effects from the global analysis can then be used as the boundary conditions for the local analysis of the critical components in the system.

Other considerations including manufacturing, monitoring and maintenance should be covered in the overall design of the system as well. Ochoa concluded that one of the major barriers to the use of composite risers in the industry is the lack of data from field trials that can be used to ensure the integrity of the system [1].

2.2.Failure of Composite Tubes under Pressure and Functional Loads

As discussed briefly in Section 2.1, composite risers in operation are subject to internal and external pressures, together with functional loads such as top tension and weight. Some of the past studies of the failure of composite tubes under these loads will be discussed in this section.

Hull, Legg and Spencer carried out an experimental study on the failure of composite pipes under burst pressure with two different loading methods named mode two and mode three [10]. Mode two testing was carried out with closed-end condition such that the hoop stress of the pipe σ_H was two times the axial stress, σ_A . Mode three testing was carried out where the ends of the pipe were free to slide in the axial direction, resulting in the axial stress, $\sigma_A = 0$. The composite pipes that were tested in this study were made with E-glass/polyester. The pipes had four composite layers with winding angle of $\pm 55^\circ$. The composite pipes were found to fail in different modes for different loading methods. For mode two pressure test, whitening was observed parallel to the fiber direction when $\sigma_H = 50$ MPa indicating matrix failure. The whitening grows as pressure was increased and at $\sigma_H = 95$ to 110 MPa weepage was observed. Weepage is fluid leakage occurring in small amounts. At pressures above $\sigma_H = 100$ MPa, axial strain and hoop strain increased at the same rate as pressure was increased. This indicates a decoupling between the matrix and the fiber in the composite layers. The final failure occurred at $\sigma_H = 460$ to 500 MPa when the pipe

burst and fiber breakage was observed (Figure 2-2). For mode three pressure test, whitening was observed at a higher stress range of $\sigma_H = 75$ to 130 MPa. Weepage occurred much more suddenly compared to mode 2 testing at around $\sigma_H = 240$ MPa. Final failure occurred shortly after weepage at around $\sigma_H = 250$ MPa. Delamination of the composite layers due to bending of the pipe was observed before fiber fracture took place.

Figure 2-2. Final failure of composite pipe under burst pressure in Mode 2 conditions. Matrix cracking and fiber breakage can be observed.

Spencer and Hull then moved on to study the effect of winding angles on the failure of the composite pipes under burst pressure [11]. Similar to their previous study [10], mode two (where hoop stress, σ_H is twice of axial stress, σ_A) and mode three (where $\sigma_A = 0$) pressure tests were performed. Glass/polyester composite pipes with winding angles $\pm 35^\circ$, $\pm 35^\circ$, $\pm 65^\circ$ and $\pm 75^\circ$ were tested. For mode two pressure tests, the pipes with winding angles below 55° showed a contraction in the axial direction before burst. This shortening of the pipe resulted in a rotation of the fiber direction to an angle of approximately 50° . The opposite occurred for the pipe with winding angle of 65° , where axial extension and hoop contraction was observed. For the pipe with winding angle of 75° , brittle failure was observed where fracture took place parallel to

the fiber direction. Compared to [10], for mode two pressure testing, the pressures at the onset of weepage and at final failure were both highest at winding angle of 55° . On the other hand, for mode three pressure tests, as the winding angles increased from 35° to 75° , the measured hoop stresses at burst increased while strains decreased. This means that the composite pipes with larger winding angles have higher resistance to deformation. Also, the composite pipes were found to have a tendency to bend and buckle, especially at high values of winding angle. The behavior and failure mode of the pipes with winding angles 35° and 45° were similar to the results of the pipe with winding angle 55° reported in [10]. For the pipes with winding angles 65° and 75° , the whitening before final failure was less obvious. Delamination was observed for all the test cases.

Soden, Kitching and Tse determined the experimental failure stresses of composite tubes under biaxial loads [12]. The composite tubes being tested had four layers of E-glass/epoxy composite with fiber volume fraction of about 0.6 wound at $\pm 55^\circ$. The ratios of applied hoop stress to axial stress, S , was varied to study the different failure modes of the composite tubes. Both tensile and compressive axial stresses were considered. The strength of the tubes was reported to change significantly when the ratio S is changed. The maximum hoop strength of the $\pm 55^\circ$ composite tubes was determined to be 939 MPa and occurred at the applied stress ratio, $S = 3.3:1$. When the applied hoop stresses are small, the strength of the tubes is reduced significantly. The strengths of the tubes are also reduced when axial compression is applied.

The study of [12] was extended by Soden et al. to include the effect of winding angles [13]. E-glass/epoxy composite tubes were tested and the experimental procedure was similar to that of [12]. In this study, three different winding angles were considered: $\pm 45^\circ$, $\pm 55^\circ$ and $\pm 75^\circ$. A general trend that was observed was that the hoop strength of

the tubes increased as the winding angle increased whereas for axial strength the opposite occurs. The compressive axial strength was not affected much by the winding angles. For both winding angles $\pm 45^\circ$ and $\pm 55^\circ$, the highest tensile axial strengths were observed in biaxial loading condition instead of in uniaxial loading where only axial stress was applied. The winding angle $\pm 75^\circ$ gave the highest hoop strength. The hoop stress at failure under open-ended burst test conditions for winding angle $\pm 75^\circ$ was recorded at about 1200 MPa. This is much higher than that of the tube with winding angle $\pm 55^\circ$ which is about 600 MPa. The experimental data and failure envelopes obtained in the study can be compared to theoretical predictions to help in understanding the mechanics of the composite tubes. However, the authors commented that the failure envelopes are only applicable to the composite tested that is E-glass/epoxy with fiber volume fraction of around 0.6.

A study on the failure of multi-angle composite tubes has been carried out by Mertiny, Ellyn and Hothan [14]. E-glass/epoxy composite tubes at winding configurations of $[\pm 60^\circ_3]_T$, $[\pm 45^\circ, \pm 60^\circ_2]_T$ and $[\pm 30^\circ, \pm 60^\circ_2]_T$ were tested under tensile biaxial loads. The results for the $[\pm 60^\circ_3]_T$ configuration were taken as the baseline. The composite tubes were tested under five different hoop stress to axial stress ratios, $S = 1H: 15A, 1H: 3A, 1H: 1A, 2H: 1A$ (closed end burst test), $3H: 1A$ and $1H: 0A$. The stresses at functional failure and structural failure were determined. Functional failure was determined when weepage occurred and structural failure determined when the tube was no longer capable of carrying load. The failure modes at structural failure were different for different load conditions. At $S = 2H: 1A$, localized failure near the ends of the composite tubes were observed. At $S = 3H: 1A$, burst failure occurred with fracture surfaces parallel to the fiber direction. At pure hoop loading, burst failure resulted in the destruction of the test specimens. The study found that the two multi angle configurations $[\pm 45^\circ, \pm 60^\circ_2]_T$ and

$[\pm 30^\circ, \pm 60^\circ_2]_T$ both performed better than the baseline configuration of $[\pm 60^\circ_3]_T$ under tensile biaxial loading conditions (Figure 2-3). In particular, at $S = 1H:15A$, the functional strength of the $[\pm 30^\circ, \pm 60^\circ_2]_T$ tube was found to be about 4.6 times higher than the baseline case. The increase in strength in the multi angle composite tubes was due to the $\pm 30^\circ$ and $\pm 45^\circ$ layers carrying the axial load and thus reducing the axial strain due to axial loading. This study shows that a multi-angle design can lead to better performances for composite tubes subjected to variable loading conditions.

Figure 2-3. Structural strength of multi-angle composite tubes $[\pm 45^\circ, \pm 60^\circ_2]_T$ and $[\pm 30^\circ, \pm 60^\circ_2]_T$ relative to the baseline $[\pm 60^\circ_3]_T$ [14].

Another experimental study on composite tubes was carried out by Ramirez and Engelhardt [15]. They studied the failure of a full-scale composite tube under external pressure. The specimen that was tested was a carbon-epoxy composite tube. For the tube, the length was 4.6 m, outer diameter was 56.4 cm and wall thickness was 3.05 cm. The composite tube consisted of 48 layers with a combination of winding angles 15° , 45° , -45° and 80° . The tube had a delamination at around the mid-thickness of

the tube wall. During the pressure test, the ends of the tube were allowed to slide along the axial direction so that the tube can be tested under pure external pressure. The hoop strain was found to vary linearly with applied pressure. The internal hoop strain was initially slightly larger than the external hoop strain of the tube. However, at high pressure, the two hoop strains converged. This behavior was attributed to the pre-existing delamination of the tube. Ramirez and Engelhardt also performed finite element analysis (FEA) to compare their numerical results to the experimental results. From the FEA results, it was found that the delamination caused a reduction in the collapse pressure of the tube. The collapse pressure of the tube with delamination was measured experimentally at 20.2 MPa while the predicted collapse pressure for a tube without delamination was 38.6 MPa.

Besides pressure and functional loads, composite risers also need to withstand environmental loads. Studies on the effect of the marine environment on the performance of composite materials will be discussed next.

2.3.Environmental Loads and Long Term Considerations

Deep sea composite risers need to be able to operate under harsh environmental conditions. The environmental loads acting on deep sea composite risers include hydrodynamic loads due to sea current and high thermal gradient due to the difference in temperatures between the fluid carried and the surrounding sea water [16]. Seawater ingress can also cause the degradation of composite material properties. Also, deep sea composite risers need to be able to withstand the loads for 20 years or more in operation with little maintenance [17].

Chamis has given a simplified approach to incorporate hygrothermal effects in the stress analysis of composite laminates [18]. In the report, Chamis presented a unified set of micromechanics equations to predict the mechanical, thermal and hygral

properties of unidirectional lamina using the constituent material properties. Calculations of the elastic moduli and moisture expansion coefficient for sample fiber reinforced polymer (FRP) composite laminae were illustrated. However, the strength degradation in composite materials due to moisture was not discussed. The equations in [18] were incorporated into computational analysis code by Minnetyan et al. to simulate the fracture of carbon fiber reinforced composites (CFRPs) in various hygrothermal environments [19].

Many experimental studies have been carried out to measure the mechanical properties of FRPs with various water content levels. The tensile strength of glass fibers has been shown to decrease when they are immersed in humid environments [20]. On the other hand, it is found that the fiber-dominated properties of CFRPs do not change significantly with water content because carbon fibers do not absorb water [21, 22, 23]. A compilation of experimental results on the ultimate strengths and elastic moduli of 0° , 45° and 90° CFRP laminates with various water content levels was presented by Shen and Springer [22, 24]. The ultimate tensile strengths of the 90° laminates were found to be significantly reduced (from 40 to 60% reduction) with increasing water content. For the tensile moduli of the 90° laminates, there is a large scatter in the results where some authors found a negligible change in the modulus while others found a reduction of up to 50%. Selzer and Friedrich reported similar results for CFRPs with epoxy matrix [23]. However, for CFRP laminates with polyetheretherketone (PEEK) as matrix, the authors found that the effect of water on the transverse tensile strength was insignificant [23].

Besides the polymer matrix, the fiber-matrix interface in CFRPs is also affected by water. The damage mechanism at the interface can include debonding, as well as changes in the surface energy and residual stresses [25]. Ramirez, Carlsson and Acha

studied the fiber-matrix interface using the single fiber fragmentation test [26]. They found that moisture caused a significant reduction in the interface shear strength as well as debonding. This translated to a large decrease in the transverse tensile strength of the CFRP laminates. Totten studied the tensile strength at the fiber-matrix interface by performing tests on transverse tensile single-fiber specimens [27]. The fiber sizing was reported to have a large effect on the interface strength and strong adhesion at the interface can reduce the degradation caused by water.

Composite risers are also subjected to hydrodynamic loads due to sea current. The calculation of the hydrodynamic load is usually done using the Morison's equations. This has been presented by Guesnon et al. [28]. In the load calculations, the riser body is assumed to be small such that it does not interfere with the sea current flow. The hydrodynamic load is made up of two components: the drag force and the inertia force. The drag force, F_d and the inertia force, F_i are given by:

$$F_d = \frac{1}{2} D \rho C_d v |v| \quad (2.1)$$

$$F_i = \frac{\pi D^2}{4} \rho [C_m (a - \ddot{x}) + \ddot{x}] \quad (2.2)$$

where D is the riser diameter, ρ is the density of seawater, v is the fluid velocity, $\ddot{x}$ is the riser acceleration, a is the wave acceleration, C_d is the drag coefficient and C_m is the added mass coefficient. The two coefficients introduced in the Morison's equations, C_d and C_m are usually determined from experimental data. The value of C_d depends on the flow regime, which is characterized by the Reynolds number and the Keulegan-Carpenter number.

Le Gac et al. discussed the use of accelerated test methods to investigate the long term performance of polymer and composite materials in seawater [17]. One method of performing accelerated test is to use temperature as the accelerating agent.

Natural seawater should be used as the aging medium if possible. Other options include artificial seawater or just tap water. Elevated temperatures can increase the water absorption of polymers thus allowing the long term exposure to seawater to be simulated and the mechanical properties to be studied. Another method discussed by Le Gac et al. involves the application of mechanical loading to accelerate aging. The authors investigated the water absorption of specimens under high pressure. A glass/epoxy composite and an epoxy plate were tested. It was found that the applied pressure increased the water absorption of the composite plate but not for the epoxy plate. This was attributed to the presence of voids in the composite plates. Therefore, the accelerating agent used in accelerated test methods need to be considered carefully to ensure that the tests can give meaningful results.

The environmental loads can have a significant effect on the performance of composite risers, especially in long term operation. However, research on the behavior of composite risers under deep sea operating conditions is currently lacking. Also, experimental studies on the long term performance of composite risers can be expensive and difficult to perform. Numerical simulations such as finite element analysis (FEA) have been identified as a means to complement experimental studies. Some of the finite element simulation models and techniques will be discussed in the next section.

2.4.Finite Element Analysis

Analysis of the mechanical behavior of a fiber-reinforced polymer (FRP) composite lamina includes three stages: pre-failure, the onset of failure and post failure analysis. For the analysis of FRP composite laminates, interlaminar failure or delamination needs to be considered as well.

2.4.1. Pre-Failure Constitutive Model

For linear elastic materials, the stress-strain relations are given by the Hooke's Law. Unidirectional FRP lamina can be taken as a transversely isotropic material. For the plane stress case, the stress-strain relations are given as [29]:

$$\begin{Bmatrix} \sigma_{11} \\ \sigma_{22} \\ \sigma_{12} \end{Bmatrix} = \frac{1}{1 - \nu_{12}\nu_{21}} \begin{bmatrix} E_1 & \nu_{12}E_2 & 0 \\ \nu_{21}E_1 & E_2 & 0 \\ 0 & 0 & (1 - \nu_{12}\nu_{21})G_{12} \end{bmatrix} \begin{Bmatrix} \epsilon_{11} \\ \epsilon_{22} \\ \gamma_{12} \end{Bmatrix} \quad (2.3)$$

Experimental studies have shown that FRP composites exhibit linear elastic behavior in the fiber direction and nonlinear behavior in the transverse and shear directions [30, 31, 32]. For cases where the fibers are the main load bearing component, the effects of the transverse and shear nonlinearity are small and thus the stress-strain relations given in equation (2.3) are sufficient. However, for cases where shear and transverse loads are significant, the nonlinear behavior in the pre-failure stage needs to be considered.

Hahn and Tsai used a complementary strain energy function to derive a set of constitutive equations which include nonlinear in-plane shear for FRP laminae [30].

The derived stress-strain relations are given as:

$$\begin{Bmatrix} \epsilon_{11} \\ \epsilon_{22} \\ \gamma_{12} \end{Bmatrix} = \begin{bmatrix} S_{11} & S_{12} & 0 \\ S_{12} & S_{22} & 0 \\ 0 & 0 & S_{66} \end{bmatrix} \begin{Bmatrix} \sigma_{11} \\ \sigma_{22} \\ \sigma_{12} \end{Bmatrix} + S_{6666}\sigma_{12}^2 \begin{Bmatrix} 0 \\ 0 \\ \sigma_{12} \end{Bmatrix} \quad (2.4)$$

where S_{ij} are the compliance matrix components and S_{6666} is a fourth order constant to describe the nonlinear shear behavior. In the derivation, Hahn and Tsai assumed the stress-strain relation in the transverse direction to be linear. The mathematical model also does not consider permanent deformations.

Lin and Hu built on Hahn and Tsai's findings and proposed a model which includes plastic deformations in the fiber and transverse directions [33]. The pre-failure stress-

strain relations in the principal directions were described using bilinear models. For the in-plane shear, Lin and Hu considered two models for the shear parameter, S_{6666} : a constant model and a variable model. For the variable model, the following expression for S_{6666} was used:

$$S_{6666} = A - B e^{-\gamma_{12}/C} \quad (2.5)$$

where the constants A , B and C were determined from curve fitting using experimental data. The authors performed FE simulations using the model and compared the results to experiments. The simulations performed using the variable shear parameter, S_{6666} were found to give better predictions compared to the constant S_{6666} model.

Another nonlinear model for FRP composites was proposed by Van Paepegem et al. [34, 35]. In the first part of their study, they investigated the stress-strain response of glass fiber reinforced composites under cyclic tests. They found that the shear modulus decreased when the specimen is unloaded in the nonlinear stress-strain region. Permanent shear strain was also observed in the test specimens (Figure 2-4).

Figure 2-4. Shear stress-strain response from cyclic tensile test on $[+45^\circ/-45^\circ]_{2s}$ glass/epoxy laminate. [34]

Based on the experimental findings, Van Paepegem et al. proposed the following model [35]:

$$\frac{dD_{12}}{d\gamma_{12}^e} = c_1 \exp(c_2 D_{12}) \quad (2.6)$$

$$\frac{d\gamma_{12}^p}{d\gamma_{12}} = c_3 \gamma_{12} \exp(c_4 \gamma_{12}^p) \quad (2.7)$$

where γ_{12}^p is the permanent shear strain, γ_{12}^e is the elastic shear strain, γ_{12} is the total shear strain, D_{12} is a damage parameter for shear, and $c_1, \dots, c_4$ are material constants. The shear stress-strain response is given by:

$$\sigma_{12} = G_{12}(1 - D_{12})\gamma_{12}^e \quad (2.8)$$

where G_{12} is the shear modulus of the undamaged material. Van Paepegem used the experimental results from pure shear tensile tests to determine the values of $c_1, \dots, c_4$ for the glass/epoxy laminates studied. The model was then validated against experimental data from flexural tests. The simulation where the proposed model was

only applied to the tensile stress-strain relations overestimated the load, whereas the simulation with the proposed model applied to the whole model underestimated the load. The authors noted that for load cases where the transverse stress is significant, the nonlinear behavior in the transverse direction needs to be taken into consideration in the model.

Many researchers have proposed various models to simulate the pre-failure nonlinear behavior of FRP composites. The models have been reported to be able to provide results that agree well with experimental data. However, the damage mechanisms involved in the nonlinear behavior are still not properly reflected in many of the proposed models. Further studies are required to improve on the models to simulate the pre-failure nonlinear behavior of FRP composites.

2.4.2. Failure Criteria to Predict Onset of Failure

Several macro mechanical failure theories have been developed for anisotropic materials by extending on the failure theories used for isotropic materials. Three such theories will be discussed below, namely the maximum stress theory, the maximum strain theory and the Tsai-Wu theory.

The maximum stress theory states that failure occurs when one of the stresses in the principal material direction reaches its corresponding ultimate value. The maximum stress theory does not take the stress interactions into consideration. It is generally used for predicting brittle failure modes.

In the maximum strain theory, the strains in the principal material directions are compared to their corresponding ultimate values. Failure is predicted when one of the strains reaches its ultimate value. The maximum strain theory includes some interactions between the stresses because of the Poisson's effect.

Tsai and Wu [36] proposed a general theory for the failure of composite materials that accounts for interactions between stresses in different directions. In the Tsai-Wu theory, the failure surface is given by the following expression:

$$F_{ij}\sigma_i\sigma_j + F_i\sigma_i = 1 \quad (2.9)$$

where the contracted notations have been used, that is $i, j = 1, 2, \dots, 6$ and $1 \equiv 11$, $2 \equiv 22$, $3 \equiv 33$, $4 \equiv 31$, $5 \equiv 23$ and $6 \equiv 12$.

For a unidirectional fiber composite with the fiber in the 1 direction, the strength coefficients, F_i, F_{ij} are given by

$$\begin{aligned} F_{11} &= \frac{1}{X_T X_C} & F_{22} &= \frac{1}{Y_T Y_C} = F_{33} \\ F_1 &= \frac{1}{X_T} - \frac{1}{X_C} & F_2 &= \frac{1}{Y_T} - \frac{1}{Y_C} = F_3 \\ F_{66} &= \frac{1}{S_L^2} = F_{44} & F_{55} &= \frac{1}{S_T^2} \end{aligned} \quad (2.10)$$

where

X_T = tensile strength in the fiber direction,

X_C = compressive strength in the fiber direction,

Y_T = tensile strength in the transverse direction,

Y_C = compressive strength in the transverse direction,

S_L = longitudinal shear strength, and

S_T = transverse shear strength.

The failure of the composite laminate is independent of the sign of shear stresses.

This follows that all the terms containing shear stresses (indices 4, 5, 6) to the first

power must be zero. Therefore, the strength coefficients that remain unknown are F_{12} , F_{23} and F_{13} . These three coefficients can be determined experimentally by performing biaxial mechanical tests.

The expression used to describe the failure surface in the Tsai-Wu theory is a scalar equation and it is invariant. The strength coefficients are tensors in the second and fourth order. This enables the use of well-established tensor transformation to be applied to the strength coefficients. As a result, the Tsai-Wu theory is flexible and can be applied to various stress conditions. However, the failure mode of the composite materials cannot be easily distinguished from the simple scalar equation of the Tsai-Wu theory.

As the use of composite materials on critical structures increases, the understanding of the failure mechanisms of composite materials becomes increasingly important. There is, therefore, a need to develop failure theories that take into account the failure mechanisms of composite materials. These failure theories will be discussed next.

Hashin's failure theory [37] uses a piecewise smooth form in its failure criteria, in contrast to the single smooth function in the Tsai-Wu theory. Each smooth branch describes a failure mode. Four failure modes are taken into account, namely tensile fiber, compressive fiber, tensile matrix and compressive matrix failure modes. Hashin proposed the use of quadratic stresses to obtain a balance between accuracy and simplicity. Hashin used the quadratic stress invariants in the transverse plane of a unidirectional fiber lamina in the formulation of the failure criteria. The Hashin failure criteria are given below:

- i. Tensile fiber mode: for $\sigma_{11} > 0$,

$$FI = \left(\frac{\sigma_{11}}{X_T} \right)^2 + \frac{\sigma_{12}^2 + \sigma_{13}^2}{S_L^2} \quad (2.11)$$

- ii. Compressive fiber mode: for $\sigma_{11} \leq 0$,

$$FI = \left(\frac{\sigma_{11}}{X_C} \right)^2 \quad (2.12)$$

- iii. Tensile matrix mode: for $\sigma_{22} + \sigma_{33} > 0$,

$$FI = \left(\frac{\sigma_{22} + \sigma_{33}}{Y_T} \right)^2 + \frac{\sigma_{23}^2 + \sigma_{22}\sigma_{33}}{S_T^2} + \frac{\sigma_{12}^2 + \sigma_{13}^2}{S_L^2} \quad (2.13)$$

- iv. Compressive matrix mode: for $\sigma_{22} + \sigma_{33} \leq 0$,

$$FI = \frac{1}{Y_C} \left(\left(\frac{Y_C}{2S_T} \right)^2 - 1 \right) (\sigma_{22} + \sigma_{33}) + \left(\frac{\sigma_{22} + \sigma_{33}}{2S_T} \right)^2 + \frac{\sigma_{23}^2 - \sigma_{22}\sigma_{33}}{S_T^2} + \frac{\sigma_{12}^2 - \sigma_{13}^2}{S_L^2} \quad (2.14)$$

where

FI is the failure index where failure occurs if $FI \geq 1$,

index 1 denotes the fiber direction,

indices 2 and 3 denotes the transverse direction,

X_T, X_C, Y_T, Y_C, S_L and S_T carry the same meaning as previously mentioned in the discussion of the Tsai-Wu theory.

The criteria for the fiber failure modes in the Hashin's failure theory are similar to the maximum stress theory, with the exception of an additional shear term for fiber tensile failure. The matrix failure modes are more complicated as the plane of failure is not known. Hashin proposed the use of principal stresses in a plane stress system in the failure criteria for the matrix failure modes. As a result of this simplification and the use of quadratic stresses, the Hashin's failure theory predicts that the matrix

failure occurs on the maximum transverse shear plane, which may not always be the case.

Puck and Shürmann proposed the Puck failure theory [38] which is based on phenomenological models for the failure of composite materials. Similar to the Hashin's failure theory, the failure criteria for fiber failure modes and matrix failure modes were derived separately. Matrix failure or inter-fiber fracture (IFF) was examined and discussed extensively. Puck and Shürmann extended upon an idea that was first suggested by Hashin [37] in their derivation of the failure criteria for IFF. The derivation involves formulating a fracture condition expressed in the stresses acting on the fracture plane. This is consistent with the Mohr's strength theory which states that fracture failure is only dependent on the stresses acting on the fracture plane. The stresses are then rotated about the fiber direction using well- established tensor transformation to form a general expression for the fracture condition. The general expression will contain the transformation angle, θ . The angle of the plane of failure, θ_{fp} can then be determined from the global maximum of the general expression.

For a plane stress state, Puck was able to obtain analytical solutions for the IFF criteria. Three failure modes were identified. The Puck failure criteria for IFF are given below [38, 39]:

- i. Mode A: for $\sigma_{22} \geq 0$,

$$FI = \sqrt{\left(\frac{\sigma_{21}}{S_L}\right)^2 + \left(1 - P_{\perp}^{(+)} \frac{Y_T}{S_L}\right)^2 \left(\frac{\sigma_{22}}{Y_T}\right)^2} + P_{\perp}^{(+)} \frac{\sigma_{22}}{S_L}, \theta_{fp} = 0^\circ \quad (2.15)$$

- iii. Mode B: for $\sigma_{22} < 0$ and $0 \leq \left|\frac{\sigma_{22}}{\sigma_{11}}\right| \leq \frac{R_{\perp}^A}{|\sigma_{21c}|}$,

$$FI = \frac{1}{S_L} \left(\sqrt{\sigma_{21}^2 + \left(P_{\perp}^{(-)} \sigma_{22}\right)^2} + P_{\perp}^{(-)} \sigma_{22} \right), \theta_{fp} = 0^\circ \quad (2.16)$$

iv. Mode C: for $\sigma_{22} < 0$ and $0 \leq \left| \frac{\sigma_{22}}{\sigma_{11}} \right| \leq \frac{|\sigma_{21c}|}{R_{\perp\perp}^A}$,

$$FI = \left[\left(\frac{\sigma_{21}}{2(1 + P_{\perp\perp}^{(-)})S_L} \right)^2 + \left(\frac{\sigma_{22}}{Y_C} \right)^2 \right] \frac{Y_T}{(-\sigma_{22})},$$

$$\theta_{fp} = \arccos \left(\sqrt{\frac{f_w R_{\perp\perp}^A}{(-\sigma_{22})}} \right) \quad (2.17)$$

where

$P_{\perp\parallel}^{(-)}$ and $P_{\perp\parallel}^{(+)}$ are the slopes of the $(\sigma_{11}, \sigma_{21})$ fracture envelope,

$R_{\perp\parallel}^A$ is the fracture resistance of the action plane when subjected to transverse/parallel shear stressing,

$R_{\perp\perp}^A$ is the fracture resistance of the action plane when subjected to transverse/transverse shear stressing,

σ_{21c} is defined as $\sigma_{21c} = S_L \sqrt{1 + 2P_{\perp\parallel}^{(-)}}$, and

f_w is a weakening factor.

Puck employed the following simplification to arrive at the analytical solution

$$\frac{P_{\perp\perp}^{(-)}}{R_{\perp\perp}^A} = \frac{P_{\perp\parallel}^{(+)}}{R_{\perp\parallel}^A} = \left(\frac{P}{R} \right) = \text{const.} \quad (2.18)$$

This follows that $P_{\perp\perp}^{(-)} = \left(\frac{P_{\perp\parallel}^{(+)}}{R_{\perp\parallel}^A} \right) R_{\perp\perp}^A$. $R_{\perp\parallel}^A$ can be assumed to be the same as S_L , that is

$R_{\perp\parallel}^A = S_L$. This allows $R_{\perp\perp}^A$ to be determined from

$$R_{\perp\perp}^A = \frac{S_L}{2P_{\perp\parallel}^{(-)}} \left(\sqrt{1 + P_{\perp\parallel}^{(-)} \frac{Y_C}{S_L}} - 1 \right) \quad (2.19)$$

The fracture angle, θ_{fp} for mode C was found to be around 50° for both glass fiber reinforce plastic (GFRP) and carbon fiber reinforce plastic (CFRP) [39]. The

parameters, $P_{\perp\parallel}^{(-)}$ and $P_{\perp\parallel}^{(+)}$, are also known as the inclination parameters. They need to be determined by fitting the failure criteria to experimental results. Based on a large number of experiments, Puck and Mannigel [40] recommended a set of values for the inclination parameters for GFRP and CFRP in Table 2-1.

Table 2-1. Inclination parameters for the Puck failure theory [40]

Composite	$P_{\perp\parallel}^{(-)}$	$P_{\perp\parallel}^{(+)}$
GFRP	0.25	0.3
CFRP	0.3	0.35

The Puck failure theory was part of the World-Wide Failure Exercise (WWFE) where failure theories for composite laminates were evaluated by comparing the theoretical predictions with experimental results [41, 42, 43]. The Puck failure theory was found to give very good predictions for most of the load cases being examined for both unidirectional and multi-directional laminae. One shortcoming of the Puck failure theory is that if large nonlinear deformations were present, the predicted strains at failure would be much smaller than the experimental observations.

Dávila and Camanho proposed the LaRC03 failure criteria for composite laminates under plane stress [44]. Dávila and Camanho built upon the findings of the WWFE and sought to address the shortcomings in the failure theories tested in the WWFE. The LaRC03 failure criteria are based on physical models and do not contain parameters that require experimental curve-fitting. Similar to the Puck failure theory, the matrix compressive failure is also based on the Mohr criterion. For matrix tensile failure, an approach based on fracture mechanics was used. For fiber compressive failure, the formation of a kink band was considered. The stresses in the misaligned kink band are applied to the matrix compressive and tensile failure criteria to arrive at two separate

criteria for fiber compressive failure. An additional matrix compressive failure mode was identified. This failure mode occurs under biaxial compressive load. This leads to a total of six failure modes that are predicted by LaRC03. The LaRC03 failure criteria are given below:

- i. Fiber tensile failure: $\sigma_{11} \geq 0$

$$FI = \frac{\epsilon_{11}}{\epsilon_{1T}} \quad (2.20)$$

- ii. Fiber compressive failure: $\sigma_{11} < 0$ and $\sigma_{22}^m < 0$ (matrix compression)

$$FI = \frac{|\sigma_{22}^m| + \eta^L \sigma_{22}^m}{S_{L,is}} \quad (2.21)$$

- iii. Fiber compressive failure: $\sigma_{11} < 0$ and $\sigma_{22}^m \geq 0$ (matrix tension)

$$FI = (1 - g) \left(\frac{\sigma_{22}^m}{Y_{T,is}} \right) + g \left(\frac{\sigma_{22}^m}{Y_{T,is}} \right)^2 + \left(\frac{\sigma_{12}^m}{S_{L,is}} \right)^2 \quad (2.22)$$

- iv. Matrix tensile failure: $\sigma_{22} \geq 0$

$$FI = (1 - g) \left(\frac{\sigma_{22}}{Y_{T,is}} \right) + g \left(\frac{\sigma_{22}}{Y_{T,is}} \right)^2 + \left(\frac{\sigma_{12}}{S_{L,is}} \right)^2 \quad (2.23)$$

- v. Matrix compressive failure: $\sigma_{22} < 0$ and $\sigma_{11} \geq |Y_C|$

$$FI = \left(\frac{\sigma_{eff}^T}{S_T} \right)^2 + \left(\frac{\sigma_{eff}^L}{S_{L,is}} \right)^2 \quad (2.24)$$

- vi. Matrix compressive failure: $\sigma_{22} < 0$ and $\sigma_{11} \leq |Y_C|$

$$FI = \left(\frac{\sigma_{eff}^{mT}}{S_T} \right)^2 + \left(\frac{\sigma_{eff}^{mL}}{S_{L,is}} \right)^2 \quad (2.25)$$

where

ϵ_{1T} = tensile failure strain in the fiber direction,

σ_{ij}^m = 2-D stresses in the fiber kinking plane,

η^L and η^T = longitudinal and transverse friction coefficients,

$S_{L,ls}$ = is-situ longitudinal shear strength = $\sqrt{2} S_L$ (for unidirectional laminate),

S_T = transverse shear strength = $Y_C \cos \alpha_0 \left(\sin \alpha_0 + \frac{\cos \alpha_0}{\tan 2\alpha_0} \right)$,

α_0 = initial fracture angle under pure transverse compression,

$Y_{T,ls}$ = in-situ tensile transverse strength = $1.12\sqrt{2} Y_T$ (for unidirectional laminate),

g = fracture toughness ratio between mode I and mode II,

σ_{eff}^T and σ_{eff}^L = effective transverse and longitudinal stresses on an action plane parallel to the fiber direction,

σ_{eff}^{mT} and σ_{eff}^{mL} = effective transverse and longitudinal stresses on an action plane parallel to the misaligned fibers.

The effective stresses, σ_{eff}^T and σ_{eff}^L , are functions of stresses acting on the matrix fracture plane and the two friction coefficients, η^L and η^T . For the friction coefficients, η^T is given by $\eta^T = -\frac{1}{\tan 2\alpha_0}$ while η^L can be approximated from the relation proposed by Puck [38]: $\frac{\eta^T}{S_T} = \frac{\eta^L}{S_L}$. Therefore, we can arrive at

$$\eta^L = -\frac{S_L \cos 2\alpha_0}{Y_C \cos^2 2\alpha_0} \quad (2.26)$$

The angle α_0 is the same as the fracture plane angle for the Puck failure theory.

Dávila and Camanho tested the LaRC03 failure criteria using a typical value of $\alpha_0 = 50^\circ$ and compared their predicted results to the results from the WWFE. The predicted failure envelope was found to be in good agreement with experimental results but slightly less accurate compared to the predictions from the Puck failure theory. This was attributed to the fact that the Puck failure theory contains parameters that are obtained from experimental curve fitting. The LaRC03 failure

criteria were later expanded upon by Pinho et al in their proposed LaRC04 failure criteria which can account for general three-dimensional loading [45].

Among the failure criteria discussed, the Puck failure theory, LaRC03 and LaRC04 failure criteria are the criteria that can accurately predict failure and failure mode for a large range of loading conditions. However, they are also more difficult to apply and require more parameters in the determination of failure. Therefore, for a particular loading condition, a balance should be maintained between accuracy and simplicity in choosing the failure criteria to use.

2.4.3. Intra-Laminar Post-Failure Progressive Damage Analysis (PDA)

In order to have a thorough understanding of the damage of composite materials, the failure mechanisms and damage growth post-failure need to be investigated. This can be achieved by performing progressive damage analysis (PDA) in finite element (FE) based simulations.

For intra-laminar failure, damage growth can be simulated using the material property degradation method in FE simulations. The method has been described by Liu and Zheng [46] and is summarized in the steps below:

- i. Stress analysis.
- ii. Failure analysis:
 - a. If no damage is found, increase the load and return to step i.
 - b. Continue to step iii if damage is found.
- iii. Stiffness degradation corresponding to the failure mode on the damaged parts.
- iv. Check for final failure:
 - a. If final failure has not occurred, return to step i.

b. End simulation if final failure has occurred.

The failure criteria discussed in section 2.4.2 can be applied in step ii to determine the onset of failure. For step iii, many degradation models have been proposed. Garnich and Akula [47] divided the degradation models into two groups: sudden degradation models and gradual degradation models. In sudden degradation models, the stiffness degradation is assumed to take place immediately. The material stiffness is set to a fraction of its original value after the onset of failure. This is achieved by using a set of knockdown factors/internal damage variables d_I , where subscript I denotes the failure mode, $d_I = 1$ indicates no damage and $d_I = 0$ indicates the complete loss of stiffness. The ply-discount model is an example of a sudden degradation model where all the knockdown factors are set to zero, resulting in a simple but conservative prediction for the strength of composite laminates. On the other hand, in gradual degradation models, the material stiffness is reduced based on softening laws. Figure 2-5 shows the stiffness reduction in the two approaches. Damage is given by the ratio of the degraded elastic modulus over the initial modulus (E^d/E). The path $OBCD$ represents the sudden degradation model where the stiffness is reduced by a factor d_f while the path OBD represents the gradual degradation model.

Figure 2-5. Schematic of sudden and gradual degradation models for progressive damage analysis [47]

The sudden degradation model was investigated by Tan et al. [48, 49, 50, 51]. The damage variables for fiber failure, d_f and for matrix failure, d_m were considered separately. It was also assumed that the damage variables corresponding to failure under tensile loading (d_f^+, d_m^+) were different from the damage variables for failure due to compressive loading (d_f^-, d_m^-). For matrix cracking failure, Tan et al. used a theoretical approach and were able to relate the damage variable to the crack density using an energy-based criterion. The damage variables corresponding to fiber failure and failures under compressive loading were determined using a parametric approach where numerical results were fit to experimental results. The damage variables were assumed to be properties intrinsic to the materials. This enables the same damage variables to be used for different composite layup configurations. For graphite/epoxy composite plate, Tan et al. determined the values for the damage variables: $d_f^+ = 0.07$, $d_f^- = 0.14$, $d_m^+ = 0.2$, $d_m^- = 0.4$. It was found that the damage variables associated with compressive loading were twice the values for tensile loading. This result was consistent with expectation as composite structures that have failed under compressive loading can still carry some load. These values were also used by Camanho and Matthews in their three-dimensional study of the failure of mechanically fastened joints in composite laminates [52].

Chang and Chang proposed a degradation model that is a mix between the sudden degradation and the gradual degradation models [53, 54]. For matrix tensile and compressive failure modes, the transverse modulus, E_2 and Poisson's ratio, ν_{21} were both set to zero after the onset of failure while the longitudinal modulus, E_1 remained unchanged. For fiber failure modes, Chang and Chang used a micromechanics approach for fiber bundle failure. E_2 and ν_{21} were again reduced to

zero but E_1 and shear modulus G_{12} were degraded following a Weibull distribution given below:

$$\frac{E_1^d}{E_1} = \exp \left[- \left(\frac{A}{A_0} \right)^\beta \right] \quad (2.27)$$

$$\frac{G_{12}^d}{G_{12}} = \exp \left[- \left(\frac{A}{A_0} \right)^\beta \right] \quad (2.28)$$

where

E_1^d and G_{12}^d are the reduced longitudinal and shear moduli respectively,

A is the damage zone predicted from the fiber failure criterion,

A_0 is the fiber failure interaction zone related to the composite ply longitudinal tensile strength, and

β is the Weibull shape parameter.

Chang and Chang used the proposed model to determine the ultimate strength of a graphite/epoxy notched laminate. The value for A_0 was calculated from $A_0 = \delta^2$ where δ was determined as 0.035 inches for T300/1034-C graphite/epoxy laminate. The value of 7.6 was used for β . The authors reported that changing the value of β by more than 30% had little effect on the predicted ultimate strength.

Lapczyk and Hurtado proposed a gradual degradation model based on fracture energy dissipated [55]. The model is a generalization of the cohesive model proposed by Camanho et al. [56]. Failure initiations were determined using the Hashin failure criteria. A linear softening law was used for the stiffness degradation. The Bazant-and Oh crack band model [57] was used when implementing the damage model for finite element analysis. This reduced the mesh dependency for the analysis results. Using the crack band model, a characteristic length, L_c was introduced such that each failure mode can be modeled as a 1-D stress displacement problem shown in Figure

2-6. For each failure mode, the equivalent stresses, σ_{eq} and equivalent displacements, δ_{eq} were expressed as functions of the stresses and strains in the corresponding failure direction. For example, for matrix tensile failure (denoted by subscript mt):

$$\delta_{mt,eq} = L_c \sqrt{\langle \epsilon_{22} \rangle^2 + \gamma_{12}^2} \quad (2.29)$$

$$\sigma_{mt,eq} = \frac{\langle \sigma_{22} \rangle \langle \epsilon_{22} \rangle + \sigma_{12} \gamma_{12}}{\delta_{mt,eq} / L_c} \quad (2.30)$$

where $\langle x \rangle$ is the Macaulay operator defined as $\langle x \rangle = (x + |x|)/2$. From Figure 2-6, the damage variable d for the different failure modes can be expressed as:

$$d = \frac{\delta_{eq}^c (\delta_{eq} - \delta_{eq}^0)}{\delta_{eq} (\delta_{eq}^c - \delta_{eq}^0)} \quad (2.31)$$

The area under the graph in Figure 2-6 is related to the critical energy release rate, G_C . Lapczyk and Hurtado implemented the model in the Abaqus finite element software.

Barbero et al. introduced a method to determine the material parameters for the PDA model proposed by Lapczyk and Hurtado [58]. Barbero et al. highlighted that G_C used for the model correspond to the intra-laminar failure modes. In their study, experimental results for $[0_2/90_4]_S$ laminates were used to determine the values of the in situ strengths and G_C for the matrix tensile failure mode. The values were then used to simulate the stiffness degradation for laminates with various layup configurations. Barbero et al. found that the model was able to give good predictions for failure resulting from mode I matrix cracking. However, for failure involving mixed mode cracking, the model underestimates the stiffness reduction. The authors also reported that the progressive damage analysis was not sensitive to changes in the G_C value.

Figure 2-6. Linear softening model for intra-laminar damage [58]

Maimí et al. proposed a gradual degradation model based on irreversible thermodynamics [59, 60]. The LaRC04 failure criteria were used to determine the failure initiation. After the onset of failure, the damage variables d_M were expressed in an exponential form given below:

$$d_M = 1 - \frac{1}{f_N(r_N)} \exp[A_M(1 - f_N(r_N))] \quad (2.32)$$

where subscripts M, N give the failure mode ($M = 1\pm, 2\pm, 6$ and $N = 1\pm, 2\pm$), A_M is an adjusting parameter, r_N is the elastic domain threshold and the function $f_N(r_N)$ is chosen to force the softening of the constitutive relation. The shear damage parameter, d_6 is dependent on r_{2+} . The adjusting parameter, A_M for each failure mode is related to the corresponding fracture toughness, G_M . Maimí et al. discussed the experimental methods to obtain the values for G_M . For matrix tensile failure, G_{2+} can be obtained from double cantilever beam tests. G_6 which is related to shear failure can be obtained from four-point end notched flexure tests. The proposed method to measure G_{1+} is the compact tension test. For compressive failure modes, the energy dissipation measurement is not as straightforward. Therefore, the authors

suggested two approximations which enable the values of G_{1-} and G_{2-} to be calculated from G_6 and the fracture angle.

Liu and Zheng discussed the challenges in implementing progressive damage analysis in FE simulations [46]. In FE analysis, the finite element equation is given by $\mathbf{K}u = P$ where $\mathbf{K}$ is the stiffness matrix, u is the displacement vector and P is the load vector. In PDA, when elements fail, the stiffness reduction can sometimes result in "ill-conditioned" finite element equations, especially when some of the elastic constants approach zero. Also, the nonlinear nature of PDA can sometimes lead to the load-displacement response showing negative stiffness and negative eigenvalues. These factors make the convergence of the FE analysis difficult. Liu and Zheng presented three methods to overcome the problem: the dynamic algorithm, the viscous regularization algorithm and the arc-length algorithm. In the dynamic algorithm, the mass matrix $\mathbf{M}$ is introduced to give the FE equation $\mathbf{K}u + \mathbf{M}\ddot{u} = P$. The introduction of $\mathbf{M}$ can prevent singularity in the calculations. The inertia effect as a result of $\mathbf{M}$ is usually small enough to not affect the accuracy of the simulation. In the viscous regularization algorithm, a damping tensor $\mathbf{C}$ is included in the FE equation $\mathbf{K}u + \mathbf{C}\dot{u} = P$. The elements in $\mathbf{C}$ increase as $\mathbf{K}$ becomes singular. This results in viscous forces that can prevent the collapse of the system. In the arc-length algorithm, a load factor λ within $[-1,1]$ is included in the FE equation $\mathbf{K}u = \lambda P$. The value of λ depends on the displacement increment and the arc-length radius. Liu and Zheng reported that among the three algorithms discussed, the arc-length algorithm gives the best solution but also requires the most computing power.

Rose et al. [61] discussed some of the limitations of progressive damage analysis. The limitations are largely due to two aspects of the method, which are the homogenization of the composite ply and the localization of damage. In FE stress

analysis at the mesoscale, a composite ply is modeled as a homogenized anisotropic material. The fiber and matrix are not modeled explicitly. For failure analysis, damage is evaluated at individual integration points. This means that failure at each integration point is only dependent on the stress field at that point. The stresses and damage states at the surrounding elements are not taken into consideration. As a result of homogenization and damage localization, PDA models cannot properly predict the crack path. This can be illustrated using the example shown in Figure 2-7. The two notched lamina shown have different configurations. Damage propagation normal to the fibers in the right lamina requires more work compared to the damage propagation parallel to the fiber direction in the left lamina. However, due to the stress fields being the same, PDA models would predict the same damage propagation. Another deficiency of the PDA models is the sensitivity of the models to the finite element mesh orientation. It is reported that PDA models have a tendency to predict damage propagation along the element edges or the element diagonals. Rose et al. reported that the problem with the PDA crack propagation predictions is particularly significant when the failure involves matrix splitting and pullouts or when there is a strong coupling between transverse matrix cracking and delamination.

Figure 2-7. Example to illustrate the limitation of current progressive damage analysis techniques - two notched laminae with different configurations subjected to shear [61]

Many researchers have investigated progressive damage modeling methods and proposed various damage evolution models. Simple models such as the sudden degradation models can be implemented in FE analysis relatively easily but might not give the most accurate results, whereas for more complex models, the predictions achieved are usually more accurate but the models are also more difficult to implement. Despite some challenges and limitations, progressive damage analysis has been shown to be very useful in the study of the post-failure behavior of FRP composite laminae. For the simulation of composite laminates, interlaminar failure also needs to be accounted for. This will be discussed in the next section.

2.4.4. Cohesive Zone Model for Interlaminar Failure Analysis

For delamination studies, cohesive zone models have been developed. Cohesive behavior can be implemented using interface elements or surface elements in FE simulations [62]. The traction-separation relations of the surfaces of interest are modeled as linear elastic before failure. Damage initiation can be determined using criteria based on stress or strain. After damage initiation, the damage evolution is modeled using softening laws based on final separation or energy dissipated, G_C . The use of cohesive zone models has the advantage of being able to simulate delamination without having an initial crack and a predetermined delamination propagation direction.

The application of cohesive zone models for delamination analysis was discussed by Alfano and Crisfield [63]. For the simulation of delamination that involves both mode I and mode II fractures (mixed mode delamination), researchers have proposed models that define a single critical energy release rate, G_C to model the damage propagation. Alfano and Crisfield discussed the use of the generalized ellipse criterion given by:

$$\left(\frac{G_I}{G_{IC}}\right)^{\alpha/2} + \left(\frac{G_{II}}{G_{IIC}}\right)^{\alpha/2} = 1 \quad (2.33)$$

where G_I , G_{II} are the energy release rates for mode I and mode II, G_{IC} , G_{IIC} are the mode I and mode II critical energy release rates, and α is a material parameter. If the mode ratio G_I/G_{II} is known, the critical energy release rate for the damage propagation can then be obtained as $G_C = G_I + G_{II}$. It was noted that this approach requires the mode ratio to be calculated from linear elastic fracture mechanics (LEFM) in advance. Therefore, the authors proposed a method where the pre-calculation of the mode ratio is not needed. The method is based on the inclusion of a parameter $\bar{\gamma}$ in the calculation for damage propagation [63]:

$$\bar{\gamma}(t) = \max_{0 \leq t' \leq t} \gamma(t'),$$

$$\text{where } \gamma(t') = \left[\left(\frac{\langle \delta_3(t') \rangle}{\delta_3^o} \right)^\alpha + \left(\frac{|\delta_2(t')|}{\delta_2^o} \right)^\alpha \right]^{1/\alpha} - 1 \quad (2.34)$$

Here,

t is a pseudo-time parameter,

δ_i is the separation of the interface,

δ_i^o is the separation at the onset of damage,

index $i = 1, 2$ denotes the shear directions, 3 denotes the normal direction,

$\langle \cdot \rangle$ is the Macaulay operator, and

α is the material parameter from equation (2.33).

In order to allow for the two different delamination modes to occur simultaneously, the following relationship is required:

$$\left(\frac{\delta_1^o}{\delta_1^f} \right) = \left(\frac{\delta_2^o}{\delta_2^f} \right) \quad (2.35)$$

where δ_i^f is the separation at complete delamination. The total dissipated energy for this approach is dependent on the history of the assigned relative separations.

Another cohesive model was proposed by Camanho et al. [56]. For the determination of the onset of delamination, a quadratic stress criterion is used:

$$\left(\frac{\langle \tau_3 \rangle}{\tau_3^o}\right)^2 + \left(\frac{\tau_2}{\tau_2^o}\right)^2 + \left(\frac{\tau_1}{\tau_1^o}\right)^2 = 1 \quad (2.36)$$

where τ_i is the traction for direction i , τ_i^o is the traction at the onset of damage, and $\langle \cdot \rangle$ is the Macaulay operator. For mixed-mode delamination, a single relative displacement, δ_m was introduced:

$$\delta_m = \sqrt{\delta_1^2 + \delta_2^2 + \langle \delta_3 \rangle^2} = \sqrt{\delta_{shear}^2 + \langle \delta_3 \rangle^2} \quad (2.37)$$

The relative displacement, δ_m can then be used to compute the damage parameter for the delamination. The ellipse criterion used in Alfano and Crisfield's model was unable to accurately predict the delamination propagation for composites with epoxy resin. In order to overcome this, Camanho et al. used the B-K criterion, where the mixed mode critical energy release rate, G_C is given as follows [64]:

$$G_{IC} + (G_{IIC} - G_{IC}) \left(\frac{G_{II}}{G_T}\right)^\eta = G_C, \text{ with } G_T = G_I + G_{II} \quad (2.38)$$

If mode III loading happens, G_C is determined from:

$$G_{IC} + (G_{IIC} - G_{IC}) \left(\frac{G_{shear}}{G_T}\right)^\eta = G_C, \text{ with } G_T = G_I + G_{shear} \quad (2.39)$$

where $G_{shear} = G_{II} + G_{III}$. The additional parameter η can be determined from mixed-mode bending tests. Camanho et al. reported that the B-K criterion gave a good prediction for composites with epoxy over a large range of mode ratios. Figure 2-8 shows the mixed mode response in the model by Camanho et al. where damage propagation is determined from the B-K criterion.

Figure 2-8. Mixed-mode response in the cohesive zone model by Camanho et al. [56]. The image has been modified to show symbols consistent with the text.

Cohesive zone models are useful when simulating delamination or debonding without requiring an initial crack or a predetermined crack propagation direction. For mixed-mode delamination, the B-K criterion has been found to give good predictions for the damage propagation, especially for carbon/epoxy composites. The cohesive zone models coupled with material property degradation models discussed in section 2.4.3 can simulate interlaminar and intra-laminar failure and damage growth simultaneously.

2.5. Numerical Studies of the Failure of Composite Tubes

An early study by Eckold et al. used the ply-discount model to predict the failure envelopes of composite tubes under biaxial loading [65]. Failure analysis was carried out for each lamina using the maximum stress criteria. For each lamina, if a failure criterion was met, the corresponding elastic properties were set to zero. Using this simple model, Eckold et al. plotted the initial and final failure envelopes of laminates

with winding angles $\pm 35^\circ$, $\pm 55^\circ$ and $\pm 72.5^\circ$ under various biaxial loading conditions. The failure envelopes obtained were good approximates to the experimental results.

Rafiee [4] used progressive damage analysis to determine the hoop tensile strength (HTS) and longitudinal tensile strength (LTS) of composite pipes. The Hashin failure criteria were used. Failure analysis and material property degradation were carried out at the lamina level. Fiber tensile and compressive failure modes were considered to be catastrophic failures, therefore the mechanical properties of a lamina that has failed under any of these two modes were reduced to zero completely. For matrix tensile and compressive failure modes, the transverse elastic modulus E_2 , in-plane shear modulus G_{12} and major Poisson's ratio ν_{12} of the lamina were reduced to 10% of their original values. For fiber/matrix shearing failure mode, only G_{12} and ν_{12} of the lamina were reduced to 10% of their original values. This means that the composite layer that has failed in matrix tensile mode, matrix compressive mode or fiber/matrix shearing mode can still carry load in the fiber direction. Rafiee compared the predicted HTS and LTS to experimental results and found that the predicted values were within 10% of the experimental strengths. However, in this study, the author did not provide any explanation to why the value of 10% was chosen for the material property degradation.

Martins et al. studied the structural and functional failures of filament wound composite tubes under burst pressure using PDA [66]. E-glass/epoxy composite tubes with layup configurations $[\pm 45^\circ]_4$, $[\pm 55^\circ]_4$, $[\pm 60^\circ]_4$ and $[\pm 75^\circ]_4$ were investigated in this study. For the numerical analysis, the failure analysis was carried out using a set of criteria based on the failure strains. For the stiffness reduction after the onset of failure, a gradual degradation model was used. The damage variables for matrix and fiber failure, d_m and d_f were expressed using exponential functions of the

fracture energy for the matrix G_m and fracture energy for the fiber G_f respectively. For the FE calculation, viscous regularization of the damage variables was implemented to ensure the convergence of the solution. The simulation results were compared to experimental results. Functional failure is determined when leakage occurs and structural failure is the complete burst of the tube. For the winding angles, Martins et al measured the actual angles of the filament wound tube and found that the actual angles differed from the nominal angles by up to 2°. The authors showed that this difference can lead to an increase in the error for the simulation results. Due to the large variation on the failure pressure and the presence of other uncertainties in the fabrication process, Martins et al. suggested the use of probabilistic methods coupled with PDA in the design of composite tubes. For the simulation of the $[\pm 55^\circ]_4$ burst test, matrix degradation was observed to start from the outermost ply (ply 8). The pressure at functional failure was determined to be 4.74 MPa where all the plies show some degradation. Fiber failure was observed starting at 12.99 MPa leading to structural failure shortly after that. The burst pressure simulation of the $[\pm 55^\circ]_4$ tube is shown in Figure 2-9. The model used in this study was able to predict the failure mechanisms for the $[\pm 55^\circ]_4$ and $[\pm 60^\circ]_4$ tubes. However, the failure criteria were not able to predict the fiber failure for the $[\pm 40^\circ]_4$ tube.

Figure 2-9. Burst pressure simulation by Martins et al. for the $[\pm 55^\circ]_4$ composite tube [66]

In another study, Martins et al. investigated the minimum length required in a model to represent an infinite tube, the optimal winding angle for composite tubes subjected to burst pressure under different end conditions and the effects of diameter and tube thickness on the failure pressure [67]. The PDA model from their previous study [66] was also used here. For the minimum length required, the authors calculated the burst pressure of pipes with various length to diameter ratios, L/D . It was found that at $L/D = 12$ the failure pressure is approximately constant. For the burst test simulations, three end conditions were considered, namely open-end, restrained end and closed-end conditions. The optimal winding angles calculated were 88° , 74.5° and 53.25° respectively. For the third part of the study, Martins et al. studied the change of the burst pressure to the ratio of thickness to radius, h/R . For the range studied ($0.03 \leq h/R \leq 0.1$), it was found that the pressure increased linearly with h/R . The authors also suggested that the split disk test (or hoop tensile test) should be used to characterize the mechanical properties of composite tubes due to the low cost and simplicity of the test.

Ribeiro et al. studied the behavior of composite cylinders under impact loads [68]. The PDA model used here was described in their previous study [69]. The degradation model is a mix between the sudden and gradual degradation models. For fiber tensile failure, the stiffness in the fiber direction, E_{11} is reduced to zero. For matrix tensile failure and shear failure, the damage variables d_2 and d_6 were expressed in linear functions of the ply orientation angle, θ and the thermodynamic forces, Y_2 and Y_6 . This relationship was derived from a concept that related the damage variables to the strain energy density and thermodynamic forces. For compressive failures in the fiber and matrix, the secant moduli for E_{11} and E_{22} were used to model the nonlinear stress-strain behavior after failure. A series of mechanical tests were carried out in [69] to determine the parameters required for the PDA model.

In [68], Ribeiro et al. studied composite cylinders with three different layup configurations shown in Table 2-2. 10 J and 30 J impact energy tests were performed. C-scan images of the specimens subjected to the 10 J impact energy tests did not reveal any damage. Therefore, only the 30 J impact energy tests were simulated. One important aspect of the simulation identified by the authors was the energy dissipated by damping. In this study, damping was introduced using the Rayleigh's model. The simulation results with and without damping were compared to the experimental results. For the simulations without damping, the initial force-displacement response in the impact tests was in good agreement with experiments. However, the model without damping was not able to accurately simulate the dissipation of the kinetic energy of the impactor, resulting in a sudden drop of the impactor force at the end of the simulated impact test. On the other hand, for the model with damping, the force-displacement response near the end of the impact test was improved but the peaks and valleys in the beginning of the test cannot be reproduced. Overall, the authors concluded that the damping effect improved the

simulation results. For the energy dissipated, ΔE_c , the calculated value for the type A cylinders was lower than the experimental value, whereas for types B and C, the calculated values were higher. The study was able to provide some insight into the damage on composite cylinders under impact loads. However, delamination was observed in the test specimens but was not simulated by the PDA model. Delamination would result in higher energy dissipated. Despite the exclusion of delamination in the model, the calculated ΔE_c values for types B and C were higher than experimental values. This could be a result of the energy dissipated through damping being overestimated by the authors.

Table 2-2. Composite layup configuration studied by Ribeiro et al. [68]

Identification	Layup
Type A	$[90/60/-60/90/60/-60/90]_s$
Type B	$[90/30/-30/90/30/-30/90]_s$
Type C	$[90/30/-30/60/-60/30/-30]_s$

Rodriguez and Ochoa studied the failure of composite tubes under bending [70]. The objective was to identify the minimum spool radius that is required for storing composite tubes without damaging them. Carbon/epoxy and glass/epoxy composite tubes with layups of $[\pm 45]_4$ and $[\pm 55]_4$ were investigated. For the experimental part of the study, four-point flexural tests were carried out. The carbon specimens were found to be about 33% stiffer than the glass specimens. For the numerical simulations of the study, three sets of failure criteria were considered: maximum stress criteria, Hashin criteria and Hashin-Rotem criteria. For the damage evolution, a sudden degradation model was used. For failure in the fiber and matrix, the stiffness is reduced to a small value of 70 kPa, whereas for shear damage, a few knockdown factors were considered: 0, 0.01, 0.1, 0.2 and 1. One important aspect of the

simulations in this study was the inclusion of thermal residual stresses in the initial state of the model. The residual stresses were a result of the manufacturing process. It was shown that the residual stresses can have a large impact on the simulation results and thus need to be included. From their simulations, Rodriguez and Ochoa found that the maximum stress criteria overestimate the stiffness of the tubes. The Hashin and Hashin-Rotem criteria gave similar results that were closer to experiments. The predominant failure mode was found to be matrix tensile failure. Damage initiation occurred at the bottom of the tube. For the shear damage variable, the choice of the knockdown factor had a significant effect on the moment-curvature response of the simulated tubes as shown in Figure 2-10. The authors concluded that the knockdown factor of 0.2 gave the closest result compared to experiments. However, it can be seen from Figure 2-10 that at curvatures larger than 0.7 m^{-1} , the simulation results using the knockdown factor of 0.2 deviates from the experimental results. This shows that the simple sudden degradation model used was inadequate for simulating the bending of composite tubes over a large curvature range. A more complex degradation model is needed to obtain better results.

Figure 2-10. Moment-curvature response using different knockdown factor values for stiffness degradation (simulation results for $[\pm 45]_4$ composite tubes) [70]

Vedvik and Gustafson presented a model to simulate the behavior of thick walled composite pipes with a metallic liner [71]. The simulation of transverse cracking in the composite laminates was one of the main objectives of the study. A representative volume element (RVE) consisting of two connected half-plyies with $+\theta$ and $-\theta$ orientations was used to model the distributed transverse cracks in the angle-ply laminate. Here, the assumption that the cracks in the $+\theta$ and $-\theta$ plyies are equally spaced was required. The principle of minimum potential energy was applied to obtain the displacement field and the crack shape function for the RVE. The effective elastic moduli of the model degrade with the increase of crack density. For the damage growth modeling, the maximum stress and maximum strain criteria were considered for the determination of failure initiation. A failure index, $f = (\text{failure load}/\text{applied load})$ was used in the model. When $f < 1$, damage growth occurs and is modeled by using the expression

$$(d_c)_{i+1} = (d_c)_i \cdot f \quad (2.40)$$

where d_c is the crack distance and i is the iteration number. For the metallic liner, the von Mises yield criterion and isotropic hardening were used to model the plastic flow of the liner. Vedvik and Gustafson investigated the behavior of composite pipes with metallic liner under closed end internal pressure using the proposed model. Average stresses across the surface of the RVE where crack is likely to develop were used in the failure criteria to determine the damage initiation. This was compared to the damage initiation determined using point stresses. The failure evaluated using point stresses gave lower crack density while the average stresses provided a more conservative prediction. However, the failure at the interface between the composite layers and the metallic liner was not discussed in this study.

Numerical simulations can be very helpful in the study of the failure and damage modes of composite pipes. Many researchers have used FE analysis and PDA to study the behaviors of composite pipes of various configurations under different loading conditions and have achieved varying levels of accuracy in the failure prediction. However, not many studies have been carried out to investigate the effects of environmental loads on the failure of composite pipes. The failure at the interface between the liner and the composite body in lined composite tubes also need to be studied further.

2.6.Conclusions and Remarks

In this chapter, some of the past studies on the failure of composite pipes under pressure loads, functional loads, as well as environmental loads were discussed. This is followed by a discussion on the simulation models and techniques for the pre-failure, onset of failure and post-failure stages that can be used in FE analysis of composite materials. For the onset of failure, various failure criteria for composite materials including the Hashin criteria, Puck criteria and LarC criteria have been

presented. For the selection of failure criteria, the accuracy of the failure prediction and the ease of implementation of the criteria need to be considered. The Puck criteria and LarC criteria are found to give more accurate failure predictions but require more parameters. On the other hand, the Hashin criteria are available in many FE software packages and have been more commonly used. For post-failure analysis, various damage models for intralaminar and interlaminar damage can be used to study the damage growth leading to the ultimate failure of composite structures. Intralaminar damage can be modeled using material property degradation models while cohesive zone models can be used to study interlaminar damage. However, using cohesive zone models also increases the computational resources needed for the FE simulation. Therefore, it is advisable to include cohesive zone models only for areas where failure is likely to occur. A review of some of the numerical studies that have been carried out on the failure of composite pipes is also included.

Many studies have focused on the behavior of composite pipes under pressure and functional loads but not many have considered the effects of environmental loads such as hygrothermal effects. For the numerical studies on composite pipes and risers, many researchers assumed linear elastic behavior for the composite material before failure occurs. The nonlinear behavior in the transverse and shear directions of the composites needs to be considered to improve the accuracy of the numerical simulations. Also, the mechanical behavior of lined composite pipes where the liner can carry some load is not fully understood. In particular, not many researchers have investigated the debonding between the liner and the composite layers in lined composite pipes. Therefore, the objective of this work will be to include environmental loads and the failure at the liner-composite interface in the analysis of failure and damage growth in composite pipes. The nonlinear behavior in the pre-

failure stage will also be included in the analysis. This can provide a comprehensive approach for the local analysis of composite risers under deep sea operating conditions.

3. Materials and Methods

3.1.Composite Pipe Test Specimens

3.1.1. Selection of Materials

For the fiber reinforcement of the composite, carbon fiber is used in this study. This is because carbon fibers have higher modulus and strength compared to glass fibers. Also, compared to glass fibers, the strength of carbon fibers is not affected as seriously by moisture content [20, 21, 22]. This makes carbon fibers more suitable for marine and offshore applications than glass fibers. The higher cost of the carbon fibers can be compensated by the savings due to the weight reduction leading to a smaller TLP size. The carbon fiber used in this study is the HexTow® IM2A carbon fiber supplied by Hexcel Corp. The mechanical properties of the IM2A carbon fiber are shown in Table 3-1. The IM2A carbon fiber has intermediate modulus (276 GPa) which is higher than other grades such as the AS4 carbon fiber (modulus of about 230 GPa). The higher stiffness allows the resulting composite riser to better meet the design pressure requirements. The IM2A carbon fiber is also suitable for filament winding.

Table 3-1. Mechanical properties of IM2A carbon fiber

Tensile modulus	276 GPa
Tensile strength	5310 MPa
Ultimate elongation at failure	1.7%
Density	1.78 g/cm ³

For the matrix component, epoxy resin is used due to its high strength and good corrosion resistance compared to other polymers [1]. The Epolam 5015/5015 epoxy system supplied by Axson Technologies is used in this study because of its low

viscosity making it suitable for use for filament winding. The epoxy system is also claimed to be suitable for marine applications. The mechanical properties of the Epolam 5015/5015 epoxy system are shown in Table 3-2.

Table 3-2. Mechanical properties of Epolam 5015/5015 epoxy system	
Density	1.10 g/cm ³
Tensile Strength	80 MPa
Elongation at break	6%
Flexural Strength	105 MPa
Flexural Modulus	3000 MPa

For the inner liner of the composite pipe, aluminum is used. Aluminum pipes are readily available and can be processed relatively easily. In this study, only mechanical surface treatment of the liner is considered. Therefore, the analysis approach for the composite pipes with aluminum inner liner can also be applied to the analysis of composite pipes with other metallic inner liners such as steel and titanium. The aluminum liners used in this study have an outer diameter of 76.2 mm and a wall thickness of 1.7 mm.

3.1.2. Filament Winding Process

Composite pipes were fabricated using a two axis CNC filament winding machine shown in Figure 3-1. In order to minimize the void in the composite pipes, the epoxy resin was degassed using a vacuum oven before the filament winding process. For the fabrication of composite pipes with aluminum inner liner, the liners were treated with two different surface treatment methods, namely grit blasting and grooving. The surface treatment methods and their effects will be discussed further in chapters 7 and 8. After surface treatment, the outer surface of the aluminum liners was cleaned

with soap to remove any grease and dust. After the liner is dried, the composite layers were then wound onto the liner directly. Three helical layers with the winding angle of $\pm 55^\circ$ to the axial direction make up the composite layers. The resulting thickness of the composite layers is about 1 mm. For the fabrication of composite pipes without liner, a mandrel was used during winding and later removed after curing to produce the unlined composite pipes.

Figure 3-1. Fabrication of composite pipe using a filament winding machine

The composite pipes were cured for 24 hours at room temperature followed by post-cure at 80°C for 16 hours. During curing and post-cure, the pipes were rotated continuously so that the resulting pipe wall thickness is close to uniform. Figure 3-2 shows the Binder FP 720 oven used for post-cure.

Figure 3-2. Binder FP 720 oven used for post-cure

3.2.Mechanical Tests

Mechanical tests were carried out on composite pipe specimens to study their behavior. The mechanical test results are also used for the validation of the simulation results. The mechanical test carried out in this study include tensile, compression and flexural. All mechanical tests were carried out using Universal Testing Machines (Figure 3-3). Tests with loading below 50 kN were carried out using the Instron 5569 machine. For loads between 50 to 100 kN, the Shimadzu AGX 100 kN machine was used. For loads above 100 kN, tests were carried out using the Instron 8506 machine.

Figure 3-3. Universal Testing Machines: (a) Instron 5569, (b) Shimadzu 100kN, and (c) Instron 8506

Tensile and flexural test specimens were cut from the composite pipes without liner and have an approximately rectangular shape. The tensile test specimens have a curved surface parallel to the loading direction. Glass/epoxy flat tabs with lengths of about 60 mm were attached to the ends of the specimens to prevent damage to the specimens from the clamps (Figure 3-4). The gauge length of the tensile test specimens is 100 mm. The speed of testing was set to 2 mm/min. The tensile tests were carried out following the ASTM D3039/D3039M standard [72]. For flexural tests, the specimens were tested with the convex side facing upwards. Three point flexural tests were carried out following the ASTM D7264/D7264M standard [73]. The support span for the tests was 60 mm and speed of test was 1 mm/min. The flexural tests were performed until the mid-span deflection reached about 18.5 mm.

For the tensile and flexural test specimens, the measured strength for fiber failure modes might be different from the composite pipe. This is because for the composite pipe, the carbon fibers are continuous across the pipe length, but this is not the case for the test specimens. However, the tests performed on the test specimens resulted in matrix failure being the primary failure mode (Sections 4.3 and 4.4). Therefore, the

test results obtained using the cut specimens are representative of the composite pipe, albeit being more conservative.

Figure 3-4. Tensile test specimens

For compression tests, cylindrical specimens cut from composite pipes with and without liners were tested. Axial and lateral compression tests were carried out (Figure 3-5). Axial compression tests were carried out on two types of specimens: long specimens with lengths of about 150 mm and short specimens with lengths of about 40 mm. For the long specimens, strain gauges were attached to the middle of the specimens to measure the axial and hoop strains. The axial compression tests were carried out at a speed of 1.3 mm/min. The ASTM D695 standard was followed closely [74]. For lateral compression tests, strain gauges were also attached to the test specimens (Figure 3-5) for strain measurements. The same testing speed of 1.3 mm/min was used. The tests were carried out following the ASTM D2412 standard [75]. For the compression test specimens, there was no sign that the failure modes were affected by cutting them from the composite pipe. This indicates that the lengths of the specimens were sufficiently large. Separate axial and lateral compression tests were also carried out on aluminum specimens to determine the material properties of the aluminum liner.

Figure 3-5. Compression tests: (a) lateral compression test, and (b) axial compression test

Besides mechanical tests on dry or unconditioned specimens, wet specimens were also tested. The wet specimens were conditioned by submerging in water containing 35 ppm salt at room temperature. The salt concentration was set to the average seawater salt content. The specimens were weighed periodically until saturation is reached. The water bath and weighing balance used in this study are shown in Figure 3-6.

Figure 3-6. (a) Water bath and (b) weighing balance

The dimensions for the mechanical test specimens are shown in Table 3-3 and Table 3-4. For the composite pipe specimens with aluminum liner, the specimens with different liner surface treatment were labeled separately.

Table 3-3. Summary of composite test specimens without liner

Tensile test specimen	Label for dry specimens	U\$-AT-D#
	Label for wet specimens	U\$-AT-W#
	Width (mm)	14
	Thickness (mm)	1
	Overall length (mm)	250
	Gauge length (mm)	100
Axial compression test specimen (Long)	Label for dry specimens	U\$-ACL-D#
	Inner diameter (mm)	76.2
	Wall thickness (mm)	1
	Length (mm)	150
Axial compression test specimen (Short)	Label for dry specimens	U\$-ACS-D#
	Label for wet specimens	U\$-ACS-W#
	Inner diameter (mm)	76.2
	Wall thickness (mm)	1
	Length (mm)	40
Three-point flexure test specimen	Label for dry specimens	U\$-F-D#
	Label for wet specimens	U\$-F-W#
	Width (mm)	13.5
	Thickness (mm)	1
	Overall length (mm)	150

Note: \$ represents the pipe number and # represents the specimen number

Table 3-4. Summary of composite test specimens with aluminum liner

Axial compression test specimen (Long)	Label for dry specimens with grit blasting	L\$-ACL-D#
	Label for dry specimens with groove	G\$-ACL-D#
	Inner diameter (mm)	72.8
	Liner thickness (mm)	1.7
	Composite layers thickness (mm)	1
	Length (mm)	150
Lateral compression test specimen	Label for dry specimens with grit blasting	L\$-LC-D#
	Label for dry specimens with groove	G\$-LC-D#
	Inner diameter (mm)	72.8
	Liner thickness (mm)	1.7
	Composite layers thickness (mm)	1
	Length (mm)	120

Note: \$ represents the pipe number and # represents the specimen number

3.3.Finite Element Software and Simulations

The finite element analysis in this study is carried out using Abaqus version 6.14 [62].

Some of the software's capabilities and features are given below:

- Offers various 1-D, 2-D and 3-D elements that can be used to study a wide range of loading conditions.
- Composite laminates can be modeled easily with layup definitions for shell or solid elements.
- Both implicit and explicit finite element analysis can be performed.

- Various user subroutines can be used with the program for user specific requirements. This includes the UMAT and VUMAT material model subroutines.
- Delamination and debonding can be modeled using the virtual crack closure technique (VCCT) or cohesive zone modeling. For cohesive zone modeling, the mixed mode behavior can be defined using a power law or the Benzeggagh-Kenane criterion.
- Plasticity models for ductile materials available include isotropic hardening and the Johnson-Cook model.
- Progressive damage analysis can be performed for FRP composites and ductile materials. For FRP composites, failure initiation is determined by the Hashin failure criteria and damage evolution is modeled as material property degradation by linear softening.
- Various post processing options are available for data analysis.

The composite pipe is modeled using 8-node hexahedron continuum shell elements SC8R [62]. The continuum shell elements can be used to model thin to moderately thick shell structures. The shell elements were used with layered section definitions with each layer having three integration points. The 3-D geometry of the continuum shell elements allows for better contact modeling compared to the 2-D conventional shell elements.

For the liner-composite interface and the interface between composite layers, contact surfaces with cohesive behavior were used where necessary. The surface-based cohesive behavior uses a linear elastic traction-separation law before failure [62]. In this study, failure at the interface is determined using a quadratic stress

criterion and the damage evolution is simulated using the linear softening model. The interface simulation is discussed in more detail in Chapter 8.

3.3.1. Material Properties for Simulations

The aluminum liners are characterized using the lateral compression test data. For lateral compression test, the pipe specimen is subjected to bending. This is relevant to the analysis of risers as they are subjected to transverse loads such as those due to cross currents resulting in bending of the risers. As aluminum is an isotropic material, the material properties determined using data from the lateral compression test would also be applicable to other load cases such as tensile or compressive loadings. The characterization was performed using a similar method to the method described by Rathnaweera et al. [76]. The aluminum pipe was tested past yield point until it has undergone significant plastic deformation (Figure 3-7). This provides ample data for the characterization of the plasticity of aluminum.

Figure 3-7. Lateral compression test of aluminum pipe

For the FE simulation of the lateral compression test, yielding is determined by the von Mises yield criterion. Plasticity is simulated using a power law nonlinear isotropic hardening model given by [77]:

$$\frac{\sigma_r}{\sigma_Y} = \left(\frac{\sigma_r}{\sigma_Y} + \frac{3G}{\sigma_Y} \bar{\epsilon}^p \right)^N \quad (3.1)$$

where σ_r is the current yield stress, σ_Y is the initial yield stress, G is the material shear modulus, $\bar{\epsilon}^p$ is the accumulated equivalent plastic strain, and N is the exponent where $0 < N < 1$. The elastic modulus and yield stress of aluminum was obtained from available material data and verified using FE simulations. The value of the exponent N is varied in the simulations in increments of 0.01 to fit the simulation results to experimental data. The N value of 0.08 was found to give a good fit. This can be seen from the load-displacement curves from experiment and simulation shown in Figure 3-8. This value is the same as that obtained by Rathnaweera et al. although the power law hardening model used in their study was slightly different [76]. The mechanical properties for aluminum are then determined and shown in Table 3-5.

Figure 3-8. Aluminum pipe lateral compression load-displacement curves from experiment and simulation

Table 3-5. Mechanical properties of aluminum

	E (GPa)	ν	Initial yield stress, σ_Y (MPa)	Exponent N
Aluminum	69	0.33	201	0.08

For the carbon/epoxy composite lamina, the mechanical properties were calculated from the properties of the constituent materials. For the lamina properties in the fiber direction, the rule of mixture can be applied [29]:

$$P_c = P_f V_f + P_m V_m \quad (3.2)$$

where P_c is the composite property, P_f is the fiber property, P_m is the matrix property, V_f is the fiber volume fraction and V_m is the matrix volume fraction.

For the calculation of the lamina elastic moduli in the transverse and shear directions, the Halpin-Tsai equations were used [29]:

$$\frac{P_c}{P_m} = \frac{1 + \xi \eta V_f}{1 - \eta V_f}, \text{ where } \eta = \frac{(P_f/P_m) - 1}{(P_f/P_m) + \xi} \quad (3.3)$$

For the transverse modulus, the recommended value for ξ is 2 whereas for the shear modulus, the recommended value for ξ is 1 [29]. The undamaged composite elastic properties used in FE simulations are shown in Table 3-6. The transverse and shear moduli have different values for tensile and compressive loading denoted by subscripts t and c .

Table 3-6. Elastic properties of the undamaged carbon fiber-reinforced composite

E_1 (GPa)	E_{2t} (GPa)	E_{2c} (GPa)	ν_{12}	G_{12t} (GPa)	G_{12c} (GPa)
128.5	10.8	8.1	0.3	2.7	2.09

For the strength properties in the transverse direction, the composite properties can be calculated using a micromechanics approach. The stress concentration factor (*SCF*) due to the fibers can be calculated as [29]:

$$SCF = \frac{1 - V_f(1 - E_m/E_f)}{1 - \sqrt{4V_f/\pi}(1 - E_m/E_f)} \quad (3.4)$$

where E_m is the matrix elastic modulus and E_f is the fiber elastic modulus. Here, square packing was assumed for the fibers, thus maximum V_f is $\pi/4$. Poisson's effect was also assumed to be negligible [29]. The transverse strength properties can then be calculated using

$$\sigma_{cu} = \frac{\sigma_{mu}}{SCF} \quad (3.5)$$

where σ_{cu} is the composite strength property and σ_{mu} is the matrix strength property. The calculated strength properties of the FRP lamina are shown in Table 3-7. The calculated elastic and strength values were verified against experimental data (discussed in chapter 4).

Table 3-7. Strength properties of the carbon fiber-reinforced composite

X_T (MPa)	X_C (MPa)	Y_T (MPa)	Y_C (MPa)	S_L (MPa)	S_T (MPa)
2443	1221	60.4	78	77	60

3.4. Scaling Philosophy

In this study, representative composite pipes were fabricated and tested. The representative composite pipes are small compared to full-scale composite risers. Therefore, for the mechanical testing of the representative composite pipes, the ASTM testing standards were followed closely in order to obtain accurate material properties from the test results. The material properties can then be used in FE

simulations to simulate the behavior of full-scale composite risers. For the FE simulations of the composite riser, the boundary conditions were considered carefully so that they represent the actual conditions in a full-scale composite riser system.

4. Composite Material Model

4.1. Nonlinear Behavior of Composite Materials Pre-Failure

As discussed in Section 2.4.1, in the pre-failure stage, FRP composites exhibit nonlinear behavior in the transverse and shear directions. For a composite riser system, the riser is subjected to a combination of loads which can lead to significant transverse and shear loads. In such cases, the pre-failure behavior cannot be adequately described using the linear elastic model (Figure 4-1). Therefore, in this study, a model for the pre-failure nonlinear behavior of FRP composites is proposed for the analysis of composite risers.

Figure 4-1. Comparison of simulation (with linear-elastic behavior in the pre-failure stage) and experimental results for tensile test carried out on $[\pm 55^\circ]_3$ carbon/epoxy composite

From experimental studies, two damage mechanisms related to the nonlinear behavior have been determined. Damage in the matrix in the form of cracks has been observed in FRP laminates when the material is loaded in the shear direction to the nonlinear stress-strain region [78] (Figure 4-2). At this stage, the FRP laminates were

still capable of carrying load indicating final failure has not been reached. Here, the damage is termed matrix ductile damage. Permanent deformation in the FRP laminates is also reported when the laminates are unloaded from the nonlinear region [34, 35]. Therefore, yielding and plastic deformation should be included in the model for the polymer matrix. The incorporation of matrix ductile damage and plastic deformation into the model will be discussed in the next section.

Figure 4-2. Optical micrograph showing cracks in the matrix in the pre-failure stage

4.2. Development of Bimodulus-Plastic Model

The model used in this study is an extension of the progressive damage model presented by Lapczyk and Hurtado [55]. The damage compliance matrix, $\mathbf{H}$ and stiffness matrix, $\mathbf{Q}$ are given by:

$$\mathbf{H} = \begin{bmatrix} \frac{1}{(1-d_f)E_1} & -\frac{\nu_{21}}{E_2} & 0 \\ -\frac{\nu_{12}}{E_1} & \frac{1}{(1-d_m)E_2} & 0 \\ 0 & 0 & \frac{1}{(1-d_s)G_{12}} \end{bmatrix} \quad (4.1)$$

$$\mathbf{Q} = \frac{1}{D} \begin{bmatrix} (1-d_f)E_1 & (1-d_f)(1-d_m)\nu_{21}E_1 & 0 \\ (1-d_f)(1-d_m)\nu_{12}E_2 & (1-d_m)E_2 & 0 \\ 0 & 0 & D(1-d_s)G_{12} \end{bmatrix} \quad (4.2)$$

where $D = 1 - (1 - d_f)(1 - d_m)v_{12}v_{21}$, E_1 , E_2 , G_{12} are the undamaged moduli, v_{12} , v_{21} are the undamaged Poisson's ratios, and d_m , d_f are the damage parameters for matrix and fiber respectively. The damage parameters are different for tension and compression failure modes which are denoted by the subscripts t and c respectively. The damage parameter for shear, d_s is dependent on the damage parameters for matrix and fiber [55]:

$$d_s = 1 - (1 - d_{ft})(1 - d_{fc})(1 - d_{mt})(1 - d_{mc}) \quad (4.3)$$

The onset of failure is determined using the Hashin criteria [37]. After failure, a linear softening model is used where the stiffness of the material is reduced by changing the damage parameters d_m and d_f [55].

For the current model, the material model for the fiber direction remains the same as the model described by Lapczyk and Hurtado (linear elastic followed by linear softening after failure). As such, only the transverse and shear parts of the model will be discussed. The details for the material model for the fiber direction are given in Appendix A.

4.2.1. Plastic Deformation

Experiments have shown that the nonlinear behavior of CFRP is more significant in the shear direction compared to the transverse direction [30, 32]. Therefore, in the current model, the plastic deformation of the matrix is assumed to be in the shear direction only. The shear strain is given by

$$\gamma_{12}^{tot} = \gamma_{12}^{el} + \gamma_{12}^{pl} \quad (4.4)$$

where γ_{12}^{tot} is the total shear strain, γ_{12}^{el} is the elastic shear strain and γ_{12}^{pl} is the plastic shear strain. The engineering shear strains are used here.

For the current model, a linear expression is used for the plastic shear strain:

$$\gamma_{12}^{pl} = R (\gamma_{12}^{tot} - \gamma_{12}^y) \quad (4.5)$$

where γ_{12}^y is the shear strain at yield and R is a factor describing the ratio of plastic to elastic shear strain after yielding. γ_{12}^{pl} has the same direction as γ_{12}^{tot} and its value increases monotonically until final failure. After the point of final failure is reached, the value of γ_{12}^{pl} is kept constant. γ_{12}^y and R can have different values for tension and compression failure modes.

4.2.2. Matrix Ductile Damage

For the matrix ductile damage, a bimodulus model is adopted. The bimodulus model is implemented using the matrix damage parameter, d_m . The material stiffness in both the transverse and shear directions is thus affected by matrix ductile damage. The different stages of the model are determined using the failure indices calculated from the Hashin criteria for matrix failure FI_{mt} and FI_{mc} [37]. The criteria for matrix damage are given as:

Matrix tension ($\sigma_{22} \geq 0$):

$$FI_{mt} = \left(\frac{\sigma_{22}}{Y_T} \right)^2 + \left(\frac{\sigma_{12}}{S_L} \right)^2 \quad (4.6)$$

Matrix compression ($\sigma_{22} < 0$):

$$FI_{mc} = \left(\frac{\sigma_{22}}{2S_T} \right)^2 + \left[\left(\frac{Y_C}{2S_T} \right)^2 - 1 \right] \frac{\sigma_{22}}{Y_C} + \left(\frac{\sigma_{12}}{S_L} \right)^2 \quad (4.7)$$

where σ_{ij} are the stress tensor components, Y_T and Y_C are the tensile and compressive strengths in the transverse direction, S_L and S_T are the longitudinal and transverse shear strengths.

The equivalent displacement and stress definitions introduced by Lapczyk and Hurtado are also used in here [55]. For the matrix failure modes, the equivalent displacements and stresses are given by:

Matrix tension ($\sigma_{22} \geq 0$):

$$\delta_{mt,eq} = L_c \sqrt{\langle \varepsilon_{22} \rangle^2 + \gamma_{12}^2} \quad (4.8)$$

$$\sigma_{mt,eq} = \frac{L_c (\langle \sigma_{22} \rangle \langle \varepsilon_{22} \rangle + \sigma_{12} \gamma_{12})}{\delta_{mt,eq}} \quad (4.9)$$

Matrix compression ($\sigma_{22} < 0$):

$$\delta_{mc,eq} = L_c \sqrt{\langle -\varepsilon_{22} \rangle^2 + \gamma_{12}^2} \quad (4.10)$$

$$\sigma_{mc,eq} = \frac{L_c (\langle -\sigma_{22} \rangle \langle -\varepsilon_{22} \rangle + \sigma_{12} \gamma_{12})}{\delta_{mc,eq}} \quad (4.11)$$

where L_c is the characteristic length of the element in FE simulation, δ_{eq} and σ_{eq} are the equivalent displacement and stress respectively, and the subscripts mt and mc denote the matrix tension and compression failure modes.

Figure 4-3 shows a schematic of the stress-strain response of the proposed bimodulus model in terms of the equivalent displacement and stress for failure mode I where $I \in \{mt, mc\}$.

Figure 4-3. Schematic diagram of the bimodulus model with linear softening after final failure

At the initial stage, the material is undamaged and is linearly elastic with effective modulus E^* . Ductile damage occurs in the matrix when the failure index FI_I reaches FI_I^d where $0 < FI_I^d < 1$. At this stage, the effective modulus of the matrix is reduced by a factor k due to the ductile damage. The arrows in Figure 2 show the unloading and reloading paths after ductile damage takes place. At $FI_I = 1$, final failure occurs and the material modulus is degraded through linear softening. The parameters for the bimodulus model can have different values for tension and compression failure modes. The matrix damage parameter, d_m at the different stages is calculated using the following equations:

For $FI_I^d \leq FI_I < 1$ (ductile damage),

$$d_I = 1 - \frac{(1 - k)\delta_{I,eq}^d + k_I \delta_{I,eq}}{\delta_{I,eq}} \quad (4.12)$$

For $FI_I \geq 1$ (linear softening after final failure),

$$d_I = d_I^0 + (1 - d_I^0) \frac{\delta_{I,eq}^f (\delta_{I,eq} - \delta_{I,eq}^0)}{\delta_{I,eq} (\delta_{I,eq}^f - \delta_{I,eq}^0)} \quad (4.13)$$

The equivalent displacements and stresses at the onset of ductile damage $(\delta_{I,eq}^d, \sigma_{I,eq}^d)$ and final failure $(\delta_{I,eq}^0, \sigma_{I,eq}^0)$ can be determined using a scaling function f_I^{sc} [55]. The scaling functions for the different failure modes are:

Matrix tension ($\sigma_{22} \geq 0$):

$$f_{mt}^{sc} = \frac{\lambda}{\sqrt{FI_{mc}}} \quad (4.14)$$

Matrix compression ($\sigma_{22} < 0$):

$$f_{mc}^{sc} = \frac{-\gamma + \sqrt{\gamma^2 + 4\lambda\beta}}{2\beta}$$

$$\text{where } \gamma = \left[\left(\frac{Y_C}{2S_T} \right)^2 - 1 \right] \frac{\sigma_{22}}{Y_C} \text{ and } \beta = \left(\frac{\sigma_{22}}{2S_T} \right)^2 + \left(\frac{\sigma_{12}}{S_L} \right)^2 \quad (4.15)$$

For the onset of ductile damage, $\lambda = FI_I^d$ and for the onset of final failure, $\lambda = 1$. The equivalent displacements and stresses can then be calculated as follows:

$$\sigma_{I,eq}^d = \sigma_{I,eq} f_I^{sc} \text{ where } \lambda = FI_I^d \quad (4.16)$$

$$\delta_{I,eq}^d = \delta_{I,eq} f_I^{sc} \text{ where } \lambda = FI_I^d \quad (4.17)$$

$$\sigma_{I,eq}^0 = \sigma_{I,eq} f_I^{sc} \text{ where } \lambda = 1 \quad (4.18)$$

$$\delta_{I,eq}^0 = \delta_{I,eq}^d + \frac{1}{k_I} (\delta_{I,eq} f_I^{sc} - \delta_{I,eq}^d) \text{ where } \lambda = 1 \quad (4.19)$$

The remaining variables required for the determination of d_I are the damage parameter at the onset of final failure, d_I^0 and the equivalent displacement at the end of linear softening, $\delta_{I,eq}^f$. They can be computed as follows:

$$d_I^0 = 1 - \left(\frac{\delta_{I,eq}^d}{\sigma_{I,eq}^d} \right) \left(\frac{\sigma_{I,eq}^0}{\delta_{I,eq}^0} \right) \quad (4.20)$$

$$\delta_{I,eq}^f = \frac{2G_{I,C} + \delta_{I,eq}^d \sigma_{I,eq}^0 - \delta_{I,eq}^0 \sigma_{I,eq}^d}{\sigma_{I,eq}^0} \quad (4.21)$$

where $G_{I,C}$ is the fracture energy for failure in mode I .

Viscous regularization for the linear softening of the material after final failure is used to overcome convergence difficulties in implicit FE simulations. The viscous damage variable is given by

$$(\dot{d}_I^v) = \frac{1}{\eta_I} (d_I - d_I^v) \quad (4.22)$$

where η_I is the viscosity coefficient and d_I^v is the regularized damage variable for the failure mode I [55]. This regularization model is also used for the material in the ductile damage stage but a different viscosity coefficient value is used.

In addition to the properties required for the progressive damage model proposed by Lapczyk and Hurtado [55], the material properties required for the current model are FI_I^d , k_I , γ_{12}^y and R for matrix tension and compression failure modes. These material properties can be determined using experimental results from unidirectional mechanical tests.

4.2.3. Model Implementation

The proposed bimodulus-plastic material model for FRP composites is implemented in a user-subroutine UMAT for the Abaqus/Standard finite element analysis program [62]. The implementation of the proposed model is summarized in Figure 4-4.

Figure 4-4. Flow chart of the bimodulus-plastic model for an increment in FE simulation

4.3.Determination of Material Properties for Bimodulus-Plastic Model

Tensile and compression tests data for composite test specimens without liner were used to determine the material properties required. Figure 4-5 shows the composite test specimens after tensile and axial compression tests. In both tests, the composite specimens failed due to damage in the epoxy matrix.

Figure 4-5. Composite test specimens after (a) tensile test and (b) axial compression test

FE simulations of the tensile and axial compression test were carried out. Figure 4-6 shows the FE models for the tensile and axial compression simulations. For both simulations, the inter-laminar interaction did not affect the simulation results. Therefore, the composite layers were assumed to be perfectly bonded. For the axial compression simulation, the results obtained when using a full cylinder model were found to be similar to that of a quarter cylinder model. Hence, the quarter cylinder model (Figure 4-6(b)) was used to reduce computation time.

Figure 4-6. Finite element mesh for (a) tensile test and (b) axial compression test simulations for composite test specimens without liner

The FE simulations for the tensile and axial compression tests were fitted to the experimental results to determine the composite material properties required for the bimodulus-plastic model. Figure 4-7 and Figure 4-8 show the stress-strain plots for the tensile and axial compression simulations and experiments. The different stages of damage for the tensile and axial compression test simulations are also indicated in the plots. The yielding of the matrix is assumed to occur before ductile damage comes into effect. This assumption is consistent with the findings of Totry et al. [78]. For the axial compression simulations, matrix compressive failure occurred before buckling. This is consistent with observations from the axial compression experiments.

The area under the stress-strain curves is calculated and used to compare the simulations to experiments. The difference between the experimental and simulation results is calculated using

$$\% \text{ Error} = \frac{P_{Exp} - P_{Sim}}{P_{Exp}} \times 100\% \quad (4.23)$$

where P_{Exp} is the experimental value and P_{Sim} is the simulation value. This difference or error is minimized to obtain a good fit for the determination of the material properties required for the bimodulus-plastic model.

Figure 4-7. Tensile test experimental and simulation results for composite test specimens without liner

Figure 4-8. Compression test experimental and simulation results for composite pipe specimens without liner: Compressive stress vs axial and hoop strains.

Table 4-1 gives a summary of the simulation and experimental results for the tensile and axial compression tests. The properties required for the bimodulus-plastic model for the tension and compression failure modes for matrix were determined through experimental data fitting and are shown in Table 4-2.

Table 4-1. Comparison of simulation and experimental results for the tensile and axial compression tests for composite test specimens without liner

		Experiment	Simulation	% Error
Tensile test	Maximum tensile stress (MPa)	56.6 ±1.5	55.4	-2.1
	Strain at maximum stress (μϵ)	13838 ±1090	14023	+1.3
	Area under graph (x10 ⁵ Pa)	5.32 ±0.66	5.36	+0.8
Axial compression tests	Maximum compressive stress (MPa)	77.8 ±7.5	81.4	+4.6
	Axial strain at maximum stress (μϵ)	18196 ±916	17882	-1.7
	Area under axial graph (x10 ⁵ Pa)	9.01 ±0.76	8.92	-1.0

Table 4-2. Material properties for the bimodulus-plastic model for tension and compression failure modes

	Tension	Compression
FI_I^d	0.50	0.75
k	0.28	0.25
R	0.50	0.25
γ_{12}^y	0.0035	0.0090

4.4. Model Validation

The three-point flexure test data for composite test specimens without liner were used for model validation. The material properties determined using the tensile and axial compression tests were used in the simulation of the three-point flexure test. Figure 4-9 shows the finite element model for the flexure test. The three helical layers

of the test specimen are modeled separately and then bonded with cohesive contact interaction. The pins are modeled as rigid surfaces. At the area close to the pins, a small mesh size was used to achieve mesh convergence. The three-point flexure test simulation is compared to experiments for validation of the proposed model.

Figure 4-9. Finite element mesh for three-point flexure test simulation

For the three point flexural tests, the stresses and strains were calculated using equations for flat specimens and as such are only approximates. The stresses and strains were calculated using [73]:

$$\sigma = \frac{3PL}{4bh^2} \quad (4.24)$$

$$\epsilon = \frac{6\delta h}{L^2} \quad (4.25)$$

where P is the applied load, δ is the mid-span deflection, b is the specimen width, h is the specimen height and L is the support span. The width and height used in the calculations are shown in Figure 4-10.

Figure 4-10. Width and height used in calculation of stresses and strains for flexural tests

The simulation and experimental results for the three-point flexure test are summarized in Table 4-3. The maximum stress calculated from the FE simulation is slightly lower than the average maximum stress obtained from experiments. Plastic deformation was observed in the flexure test specimens after unloading. The permanent mid-span deflection after unloading is measured to compare the plastic deformations in the experiments and simulation. The permanent deflection calculated from the simulation was also slightly smaller than that of the experiments.

Table 4-3. Comparison of simulation and experimental results for the three-point flexure test for composite test specimens without liner

	Experiment	Simulation	% Error
Maximum stress (MPa)	51.7 $\pm$ 2.0	47.6	-7.9
Permanent mid-span deflection (mm)	2.14 $\pm$ 0.11	2.00	-6.5

The stress-strain response of the FE simulation compared to experiments for the flexure test is shown in Figure 4-11. The stress-strain response calculated from the FE simulation using the bimodulus-plastic model was in good agreement with the experimental results. Figure 4-12 shows the deformed shape of the flexure test specimen after unloading. The shape of the specimen in the FE simulation agrees well with the experiment.

Figure 4-11. Three-point flexure test experimental and simulation results for composite test specimens without liner

Figure 4-12. Permanent mid-span deflection after unloading in the three-point flexure test: (a) experiment; (b) FE simulation

Bending or flexural loads are relevant to the analysis of composite risers as the risers are subjected to loads due to waves and water currents. Due to the $\pm 55^\circ$ angle of the FRP composite, the shear stress is a significant component in the three-point flexure test. Therefore, the shear stress calculated from the FE simulation using the current model is examined further. Figure 4-13 shows the shear stress at the top and bottom surfaces of the flexure test specimen from the simulation at mid-span deflection of 12.5 mm. This is when the maximum stress of 120.5 MPa was reached. At the bottom surface, the shear stress is concentrated at the two ends across the width. For the top surface, the shear stress is concentrated near the middle. This is due to the curved shape of the flexure test specimen. The convex side was placed upwards (Figure 4-9) causing the two ends across the width to experience larger deformation. The stress distribution is also not completely symmetrical along the specimen length due to the angle $\pm 55^\circ$ of the FRP laminate.

Figure 4-13. Contour plots of the shear stress calculated from FE simulation of the flexure test at (a) the bottom surface and (b) the top surface of the specimen at mid-span deflection of 12.5 mm

4.5.Conclusions

A bimodulus-plastic model to simulate the pre-failure nonlinear behavior in FRP composites is proposed. The model includes two damage mechanisms that were observed in experimental studies: (i) ductile damage and (ii) plastic deformation in the polymer matrix. The material parameters and properties required for the proposed model were determined by fitting the model to experimental data.

For the validation of the current model, the model was used in a finite element simulation of a three-point flexure test. The simulation result was found to agree well with experimental results. The model was able to determine the matrix ductile damage in the transverse and shear directions of the FRP composite. The plastic deformation in the flexure test specimen was also reproduced in the simulation using the proposed model.

The model is applicable for load cases where the shear and transverse stresses in the composite structure are significant. For load cases with small shear and transverse stresses, the linear-elastic model is sufficient. Also, for highly nonlinear composite materials, the simple linear equation used to describe plastic deformation in the current model could be inadequate and a more complex model might be needed.

The bimodulus-plastic model will be used in the simulations of the composite risers. This allows the pre-failure nonlinear behavior of the composite materials to be taken into consideration in the failure evaluation.

5. Predictive Model for Composite Material Properties with Absorbed Moisture

Composite risers are subjected to environmental loads such as the seawater ingress. Therefore, the degradation of the mechanical properties of the composite material used needs to be determined. In this chapter, a model to estimate the tensile properties of carbon fiber reinforced polymers (CFRP) based on their moisture contents and molecular structures is formulated and studied.

5.1. Effective Additive Group Contributions for Polymers

Water absorbed into polymers can be categorized into two groups: free water and bound water [79, 80, 81]. For the current model, bound water is considered to be the main cause of the reduction in mechanical properties of the polymer. This is because the bound water molecules form hydrogen bonds with the polymer molecules resulting in a decrease in the hydrogen bonding between the polymer molecules themselves. The decrease in bonding between the polymer molecules leads to a reduction in the mechanical properties of the polymer. The effect of free water on the mechanical properties of the polymer is considered small compared to that of bound water.

Various physical properties of polymers can be estimated from the sum of contributions made by the functional groups in the polymer [82]. In this study, the sum of contributions is modified to reflect the effect of moisture. This modified sum is termed the effective additive group contribution, A_{eff} . For polymers with some moisture content MC , the effective additive group contribution, A_{eff} can be calculated using equation (5.1):

$$A_{eff} = \sum A(hydrophobic) + g(MC) \sum A(hydrophilic) \quad (5.1)$$

where A represents the additive properties and $g(C)$ is a function related to the moisture content, MC . Here, the functional groups in the polymer are divided into hydrophobic and hydrophilic groups. The contribution from the hydrophilic groups in the polymer is modified by the function $g(MC)$ to account for the effect of moisture. The hydrophobic groups are not affected by water so their contributions do not change with moisture content.

In order to calculate the elastic moduli and ultimate tensile strengths, the additive properties required are the Rao-function (U_R), the Hartmann-function (U_H), the molar parachor (P_S) and the amorphous molar volume (V_a). The effective additive properties are given in equations (5.2) to (5.5). The values of the additive properties used in this study are obtained from Van Krevelen and Te Nijenhuis [82].

$$U_{R,eff} = \sum U_R(hydrophobic) + g(MC) \sum U_R(hydrophilic) \quad (5.2)$$

$$U_{H,eff} = \sum U_H(hydrophobic) + g(MC) \sum U_H(hydrophilic) \quad (5.3)$$

$$P_{S,eff} = \sum P_S(hydrophobic) + g(MC) \sum P_S(hydrophilic) \quad (5.4)$$

$$V_{a,eff} = \sum V_a(hydrophobic) + g(MC) \sum V_a(hydrophilic) \quad (5.5)$$

In this study, a linear relationship between the moisture content and the effective additive group contribution is assumed. When the polymer reaches saturation moisture content, the hydrogen bonding sites of the polymer molecules are assumed to be completely taken up by water molecules. The function $g(MC)$ is then given by equation (5.6):

$$g(MC = MC_i) = \frac{MC_S - MC_i}{MC_S} \quad (5.6)$$

where MC is the moisture content, MC_i is current moisture content and MC_s is the moisture content at saturation. The function $g(MC)$ decreases from 1 to 0 as moisture content increases from 0% to saturation.

The material properties calculated from additive group contributions are only estimates of the actual properties. This is because many factors such as temperature and pressure during the curing process can affect the material properties. Therefore, the properties of the dry polymer or CFRP lamina are used as the reference in the calculation.

5.2. Localized Reduced Modulus of Elasticity

The Rao- and Hartmann-functions can be used to estimate the elastic properties of polymers [82]. The bulk modulus K and shear modulus G are given by equations (5.7) and (5.8):

$$K = \rho \left(\frac{U_R}{V} \right)^6 \quad (5.7)$$

$$G = \rho \left(\frac{U_H}{V} \right)^6 \quad (5.8)$$

where V is the molar volume defined as $V = M/\rho$, M is the molar mass, ρ is the density, U_R is the Rao-function and U_H is the Hartmann-function. Using the relationship between the bulk, shear and elastic modulus, the modulus of elasticity, E can be calculated following equation (5.9):

$$E = 9\rho^7 \left(\frac{U_R}{M} \right)^6 \left[\frac{(U_H/U_R)^6}{3 + (U_H/U_R)^6} \right] \quad (5.9)$$

Water molecules tend to cluster in the micro- or sub-micro-cavities in the polymer structure [80]. The size of the cavities is in the order of 10^{-7} to 10^{-6} m [83]. Figure 5-1 shows a scanning electron microscope (SEM) image of the cross- section of a CFRP

laminate after hygrothermal conditioning and cavities with the size of microns can be observed (figure taken from [84]). The clustering of water molecules results in the polymer molecules near the sub-micro-cavities to have a high chance of interacting with the water molecules. Some of the free water molecules in the sub-micro-cavities interact and form hydrogen bonds with the polymer molecules and become bound water molecules. This results in the formation of a region with a reduced modulus of elasticity near the sub-micro-cavities due to bound water molecules (magnified view in Figure 5-2). The localized reduced modulus of elasticity E_{red} can be calculated from equation (5.10):

$$E_{red} = 9\rho^7 \left(\frac{U_{R,eff}}{M} \right)^6 \left[\frac{(U_{H,eff}/U_{R,eff})^6}{3 + (U_{H,eff}/U_{R,eff})^6} \right] \quad (5.10)$$

where the effective contributions for the Rao- and Hartmann-functions ($U_{R,eff}$ and $U_{H,eff}$) are obtained from equations (5.2) and (5.3) respectively.

Figure 5-1. SEM image of the cross section of a CFRP laminate after hygrothermal conditioning [84].
Micro-cavities can be observed in the laminate.

5.3. Tensile Properties of Bulk Polymer

A schematic diagram of the water distribution in the bulk polymer is shown in Figure 5-2. Sub-micro-cavities can be found randomly distributed in the polymer structure. When exposed to high humidity, free water molecules will occupy some of the sub-micro-cavities. The free water molecules can interact with the polymer molecules around the sub-micro-cavities to become bound water molecules. The bound water molecules weaken the polymer structure in the nearby surrounding (region with reduced E in Figure 5-2).

For the ultimate tensile strength, the region around a sub-micro-cavity in the polymer is considered (magnified view of Figure 5-2). The cavity can be considered as a sharp crack. The ultimate tensile strength X is taken to be the stress required to cause fracture at the crack. This can be calculated using Griffith's equation shown in equation (5.11) [83]:

$$X = \sqrt{\frac{2\gamma_{red}E_{red}}{\pi a}} \quad (5.11)$$

where γ_{red} is the reduced surface energy, E_{red} is the reduced elastic modulus given by equation (5.10) and a is the cavity size. Two additive properties are required to estimate the surface energy: the molar parachor, P_S and the amorphous molar volume, V_a . The effective molar parachor, $P_{S,eff}$ and effective amorphous molar volume, $V_{a,eff}$ can be calculated from equations (5.4) and (5.5) respectively. The reduced surface energy, γ_{red} is then given by equation (5.12) [82]:

$$\gamma_{red} = \left(\frac{P_{S,eff}}{V_{a,eff}} \right)^4 \quad (5.12)$$

Figure 5-2. Schematic diagram of the distribution of region with reduced E by water in polymers.

From experimental results, under the effect of moisture, the degradation of ultimate tensile strength, X is more significant compared to the degradation of the modulus of elasticity, E [81, 85]. This can be explained from the different regions in the model shown in Figure 5-2. In the regions far away from the sub-micro-cavities occupied by free water, both free and bound water molecules are still present but they are less concentrated and their effect on the mechanical properties of the polymer is less significant. The region can also include sub-micro-cavities unoccupied by water. Therefore, the regions far away from the sub-micro-cavities occupied by water are considered largely unaffected by water and are assumed to have the same properties as dry polymer. The combination of the different regions results in the degradation of E being less severe than the degradation of X . For the calculation of E for the bulk

polymer, the simple rule of mixture is assumed. Using the moisture content calculated from weights to estimate the fraction of region with reduced E in the polymer, the elastic modulus, E at moisture content MC_i can be calculated from equation (5.13):

$$E_{MC=MC_i} = E_{dry} \frac{100 - MC_i}{100} + E_{red} \frac{MC_i}{100} \quad (5.13)$$

where E_{dry} is the elastic modulus of the dry polymer and E_{red} is the reduced elastic modulus calculated from equation (5.10).

5.4. Transverse Tensile Strength of CFRP Laminae

As discussed in Section 3.3.1, the transverse tensile strength X_T of a CFRP lamina can be calculated from the mechanical properties of its constituents. The equations are repeated here for convenience:

$$X_T = \frac{X_m}{SCF} \quad (5.14)$$

where SCF is the stress concentration factor given by equation (5.15):

$$SCF = \frac{1 - V_f(1 - E_m/E_{ft})}{1 - \sqrt{4V_f/\pi}(1 - E_m/E_{ft})} \quad (5.15)$$

X_m is the tensile strength of the matrix, V_f is the fiber volume fraction and E_m is the matrix elastic modulus. For calculation of transverse tensile strength, the fiber transverse elastic modulus E_{ft} is used in the computation of the stress concentration factor [29].

From equation (5.14), X_T for CFRP lamina decreases under the effects of moisture since the tensile strength of the polymer matrix X_m reduces. However, the decrease in X_T is larger than the reduction in X_m . This is due to the change in the stress concentration factor. The matrix around the fibers is affected by water and has its

elastic modulus reduced (Figure 5-3). This results in a higher stress concentration. By substituting E_m in equation (5.15) with E_{red} , the stress concentration factor can be calculated. The matrix far away from the fibers is modeled to be the same as the bulk polymer under the effects of moisture as discussed in the previous section (Figure 5-2).

Figure 5-3. Moisture weakens the matrix around the fibers.

Besides this, the fiber-matrix interface bonding is also weakened due to moisture [23, 26, 27]. For carbon fiber sizing that is compatible with the matrix, the interface at the matrix side will have properties similar to the matrix. Therefore, for a load applied in the transverse direction, the weakening of the interface bonding can be estimated by equation (5.16):

$$F = \begin{cases} 1 & \text{if } MC \leq 1/2 MC_S \\ E_{red}/E_m & \text{if } MC > 1/2 MC_S \end{cases} \quad (5.16)$$

where F is the interface weakening factor, E_m is the dry matrix elastic modulus and E_{red} is the reduced elastic modulus for the matrix given by equation (5.10). Mechanical tests have shown that for low moisture content, the reduction in the transverse tensile strength of CFRP laminae is small [24, 23]. This can be due to the moisture not being evenly distributed in the laminae. The weakening of the fiber-matrix interface by water occurs at moisture levels above $1/2 MC_S$ where MC_S is the

moisture content at saturation. Therefore, the factor F in equation (5.16) is taken as 1 at moisture content below $1/2 MC_S$.

The transverse tensile strength of CFRP laminae under the effects of water can then be calculated from equation (5.17):

$$X_T(MC = MC_i) = F \left[\frac{X_m(MC = MC_i)}{SCF^*} \right] \quad (5.17)$$

where SCF^* is given by:

$$SCF^* = \frac{1 - V_f(1 - E_{red}/E_{ft})}{1 - \sqrt{4V_f/\pi} (1 - E_{red}/E_{ft})} \quad (5.18)$$

5.5. Validation Results and Discussion

The proposed method and the model developed based on that are tested by comparing the calculated properties to the experimental properties. The experimental properties of the dry polymer matrix of CFRP laminae are used as the references for the calculations. The relative error (% Error) for the calculated property is computed from equation (5.19):

$$\% Error = \frac{P_{calc} - P_{exp}}{P_{exp}} \times 100\% \quad (5.19)$$

where P_{calc} is the calculated value and P_{exp} is the experimental value.

For the mechanical properties of polymers, two systems were investigated. The first system is an epoxy fabricated from diglycidyl ether bisphenol A (DGEBA) resin and diethylenetriamine (DETA) hardener [85]. The second system was cured from a combination of tetraglycidyl-4,4'-diaminodiphenylmethane (TGDDM) and multifunctional novolac glycidyl ether (EPN) epoxies using a 4,4'-diaminodiphenylsulphone (DDS) hardener [81, 86]. A comparison of the calculated

and experimental mechanical properties is given in Table 5-1. The saturated moisture contents, MC_S are also included.

Table 5-1. Elastic modulus (E) and ultimate tensile strength (X) of two polymers at various moisture content

Polymer	Moisture (%)	Experimental		Calculated				
		E (GPa)	X (MPa)	E_{red} (GPa)	E (GPa)	% Error	X (MPa)	% Error
DGEBA/DETA	0	3.165	80.17					
Experimental data from Zhong [85]	*3.154	2.609	40.135	0.967	3.096	+18.7	36.81	-8.30
TGDDM/EPN/DDS	0	1.5	64					
Experimental data from Nogueira et al. and Barral et al. [81, 86]	1.9	1.55	54	1.212	1.495	-3.58	57.84	+7.11
	3.2	1.53	49	1.029	1.482	-3.14	53.48	+9.15
	4.3	1.49	49	0.883	1.459	-2.08	49.71	+1.45
	4.7	1.43	44	0.818	1.432	+0.13	47.87	+8.81
	*5.22							

*saturated moisture content MC_S

For the DGEBA/DETA epoxy, the calculated E is 18.7% larger than the experimental value whereas the calculated X agrees well with the experimental value. The model underestimated the effect of moisture on the elastic modulus of the bulk material. On the other hand, for the TGDDM/EPN/DDS epoxy, both the calculated elastic moduli and ultimate tensile strengths were in good agreement with experimental values with relative errors of below 10%. From moisture content from 0 to 1.9%, there was a small increase in the elastic modulus. Nogueira et al. attributed this increase to a reactivation of the curing process due to high temperature [81]. The effect of

temperature is not taken into account in the current model so this effect cannot be predicted.

For the transverse tensile strengths of CFRP laminae, G30-500/5208 and T300/Fiberite 1034 laminae were investigated. The matrix for the G30-500/5208 laminae is based on TGDDM and DGEBA epoxies cured with DDS hardener [23]. For T300/Fiberite 1034, the matrix consists of aromatic diglycidyl esters and aniline derivatives as hardener [24]. The volume fraction (V_f), matrix elastic modulus (E_m) and fiber transverse elastic modulus (E_{ft}) are given in Table 5-2. The values for E_{ft} shown in Table 5-2 were calculated using the properties of dry CFRP as reference as the experimental values were not readily available. The experimental and calculated properties of the CFRP laminae are given in Table 5-3.

Table 5-2. Properties of G30-500/5208 and T300/Fiberite 1034 CFRP laminae

CFRP	V_f	E_m (GPa)	E_{ft} (GPa)
G30-500/5208	0.6	4.24	8.67
(from Selzer and Friedrich [23])			
T300/Fiberite 1034	0.68	4.1	10.74
(from Shen and Springer [24])			

Note: E_{ft} values calculated using properties of dry CFRP as reference

Table 5-3. Transverse tensile strength (X_T) of CFRP laminae at various moisture contents

CFRP	Moisture (%)	Experiment	Calculated				
		X_T (MPa)	E_{red} (GPa)	X_m (MPa)	F	X_T (MPa)	% Error
G30-500/5208 (from Selzer and Friedrich [23, 87])	0	50					
	0.8	44	3.219	57.11	1	41.31	-6.11
	*1.6	23	2.421	50.03	0.571	18.63	-19.00
T300/Fiberite 1034 (from Shen and Springer [24])	0	59					
	0.5	58	3.713	76.62	1	54.49	-6.05
	0.75	55	3.532	73.68	1	51.36	-6.62
	1	43	3.359	70.80	0.819	39.62	-7.86
	*1.5	33	3.035	65.22	0.740	31.64	-4.13

*saturated moisture content MC_S

For both composites, the calculated transverse tensile strengths agree well with the experimental values. The highest relative error of 19.00% is for the calculated X_T of G30-500/5208 composite at the saturation moisture content of 1.6%. However, the calculated value only differs from the experimental value by 4.37 MPa and is within the scatter. A comparison of the experimental and calculated transverse tensile strengths of CFRP laminae is shown in Figure 5-4.

Selzer and Friedrich also studied the mechanical properties of composites with AS4 carbon fibers (AS4) and polyether ether ketone (PEEK) matrix [23]. They found that moisture has little effect on the mechanical properties of the AS4/PEEK composite (Figure 5-4). This can be explained by examining the molecular structure of PEEK. PEEK contains hydrogen acceptors but no hydrogen donors. Therefore, hydrogen bonds cannot be formed between the PEEK molecules and are not important in

determining the strength of the polymer. This result is consistent with the proposed model.

Figure 5-4. A comparison of the calculated transverse tensile strengths of CFRP laminae with experimental values. Experimental data were obtained from Selzer and Friedrich [23], and Shen and Springer [24].

5.6. Conclusions

A method to calculate some of the mechanical properties of polymer and carbon fiber-reinforced polymer composites under the effects of moisture is proposed and a model is established. Water molecules tend to congregate at the sub-micro-cavities in the polymer. Based on this finding, the bulk polymer can be modeled as having regions with a reduced tensile modulus. The tensile modulus and ultimate tensile strength can then be calculated from the reduced tensile modulus. For CFRP laminae, the transverse tensile strength is affected by the reduced elastic modulus and strength of the matrix. At higher moisture levels, the fiber-matrix interface bonding is also weakened due to water and causes further reduction in the transverse tensile strength of the CFRP lamina.

The model has been demonstrated to be able to provide good predictions for carbon fiber-reinforced composites with some of the commonly used polymer matrices. The

proposed model enables the tensile strengths of polymer and CFRP laminae at various moisture contents to be predicted based on the molecular structure of the matrix. This will reduce the time and materials required to study the mechanical performance of CFRP structures exposed to a humid environment.

The proposed model will be used to predict the tensile properties of the carbon/epoxy composite used in this study. Further validation will be carried out and discussed in the next chapter.

6. Experiments and Simulations of Effects of Moisture on CFRP

6.1. Moisture Absorption

For epoxy matrix composites, the moisture absorption behavior can be described by Fickian diffusion [81, 88, 89]. For diffusion in one-dimension, the Fick's law is given as:

$$\frac{\partial(MC)}{\partial t} = D_z \frac{\partial^2(MC)}{\partial z^2} \quad (6.1)$$

where MC is the moisture content, D_z is the diffusivity through the laminate thickness z and t is time. The diffusion of water is affected by void volume fraction, hygrothermal temperature history [90, 91] and cure temperature [80].

Moisture absorption and the conditioning of FRP composites are described in the ASTM D5229/D5229M standard [92]. The moisture content of the test specimens can be calculated from their weights using:

$$MC_i = \frac{W_i - W_0}{W_0} \times 100\% \quad (6.2)$$

where MC_i is the current moisture content, W_i is the current weight and W_0 is the initial or dry weight. The initial or dry weights were measured after drying the specimens in an oven at 60°C. For the weight measurement during the conditioning period, the specimens were wiped to remove surface moisture before weighing.

The equilibrium moisture content is taken as the moisture content at saturation. From the ASTM D5229/D5229M standard, equilibrium moisture content is defined as the moisture content when the change in weight of the specimens is lower than 0.020 % over two consecutive reference time periods. The requirement for determining equilibrium moisture content can be expressed as [92]:

$$|MC_i - MC_{i-1}| < 0.020\% \text{ and } |MC_{i-1} - MC_{i-2}| < 0.020\% \quad (6.3)$$

where MC_i is the current moisture content, MC_{i-1} and MC_{i-2} are the moisture contents measured at previous times. For this study, the reference time period used was seven days.

Besides the requirement given by equation (6.3), the moisture absorption graphs (plot of moisture content versus $\sqrt{\text{time}}$) of the specimens also need to be examined to check that equilibrium has been reached. Figure 6-1 shows the moisture absorption curves of the axial compression test specimens. The saturation moisture contents for the test specimens determined at the end of conditioning are shown in Table 6-1.

Figure 6-1. Saturation moisture content determined when equilibrium is reached

Table 6-1. Moisture content at saturation

Specimen type	Specimen label	Moisture content at saturation, MC_s (%)
Tensile test specimen	U\$-AT-W#	1.21 $\pm$ 0.15
Axial compression test specimen (short)	U\$-ACS-W#	1.08 $\pm$ 0.17
Flexural test specimen	U\$-F-W#	1.82 $\pm$ 0.32

Note: \$ represents the pipe number and # represents the specimen number

The moisture absorption behavior of the test specimens is different from a composite riser due to the additional surfaces of the specimens that were exposed to moisture. However, in principle, the moisture content at saturation for both cases would be the same, with the composite riser taking longer to reach saturation compared to the test specimens. The mechanical properties of the test specimens at saturation moisture content would thus be representative of the composite riser. Therefore, mechanical tests were carried out on the specimens after reaching saturation moisture content. Tensile tests, axial compression tests and three-point flexural tests were performed. The specimens with saturation moisture content (wet condition) are referred to as “wet specimens.” The test results for the wet specimens are compared to the results for specimens without conditioning (dry condition) to investigate the effects of moisture on the carbon/epoxy composite’s mechanical properties. The specimens without conditioning are referred to as “dry specimens.”

6.2. Tensile Tests and Simulations

For tensile tests, flat tabs made of glass/epoxy needed to be attached to the ends of the specimens. Therefore, there was a gap of about 30 minutes between the time when the specimens were taken out from the water bath and the time for the tensile tests. However, the 30 minutes gap is small compared to the time for the specimens to reach saturation moisture content and thus should not affect the test results significantly.

Table 6-2 shows a comparison of the tensile test results for the dry and wet specimens. The percentage difference is calculated using:

$$\% \text{ Difference} = \left(\frac{P_{wet}}{P_{dry}} - 1 \right) \times 100\% \quad (6.4)$$

where P_{wet} is the property for the wet specimens and P_{dry} is the property for the dry specimens. The average linear elastic modulus for the wet specimens was determined as 8.10 GPa. This is very similar to the average modulus for the dry specimens, which is 8.07 GPa. On the other hand, the average maximum tensile stress for the wet specimens (52.9 MPa) was 6.5% lower than that of the dry specimens (56.6 MPa). Therefore, for the tensile properties, only the strength of the carbon/epoxy composite was affected by moisture.

Table 6-2. Tensile test results for dry and wet specimens

	Dry specimens	Wet Specimens	% Difference
Specimen label	U\$-AT-D#	U\$-AT-W#	
Linear elastic modulus (GPa)	8.07 ±0.15	8.10 ±0.34	+0.4
Average maximum tensile stress (MPa)	56.6 ±1.5	52.9 ±1.9	-6.5

Note: \$ represents the pipe number and # represents the specimen number

Simulations of the tensile test for the wet specimens were also carried out. The finite element mesh shown in Figure 4-6(a) was also used here. From past experiments, it was found that the shear properties of carbon/epoxy composites are largely unaffected by moisture [22, 24]. Hence, the shear properties, G_{12} and S_L for the simulations of dry specimens were also used for the wet specimens. For the transverse tensile properties, the model presented in Chapter 5 was used for the calculation. The epoxy matrix used in this study consists of bisphenol A epoxy (DGEBA), bisphenol F epoxy (DGEBF) and 1,4-butanedioldiglycidyl ether for the resin component, as well as isophoronediamine and polyoxypropylenediamine for the hardener. From the molecular composition of the epoxy matrix, the localized reduced modulus of elasticity of the epoxy matrix E_{red} was determined as 2.64 GPa. The calculated value of E_{22} for the wet carbon/epoxy composite (10.8 GPa) remained

largely unchanged from the dry composite. This is consistent with the experimental results. For the calculation of composite transverse tensile strength Y_T , equation (5.16) was used. The computed value for Y_T was 52.7 MPa using $F = 1$ and 43.9 MPa using $F = E_{red}/E_m$, where E_m is the dry epoxy elastic modulus. The tensile test simulation carried out using the value of $Y_T = 52.7$ MPa gave a better agreement with the experimental results. This implies that the fiber-matrix interface was not degraded due to moisture even though saturation moisture content has been reached. The degradation of the fiber-matrix interface could be affected by factors other than the moisture content of the composite.

Figure 6-2 shows the experimental and simulation results for the stress-strain response of the wet specimens under tensile testing. The maximum stress from the simulation carried out using $Y_T = 52.7$ MPa was 50.0 MPa. This agrees well with the average maximum stress from experiments (53.0 MPa) albeit being slightly lower. The modulus reduction factor, k_{mt} (for the bimodulus-plastic model as discussed in Chapter 4) was increased slightly to 0.31 to obtain a better fit to experimental data. The increase in k_{mt} can be due to the fiber-matrix interface being unaffected by moisture for the specimens tested. A summary of the tensile properties for the wet specimens determined from the simulation is given in Table 6-3.

Figure 6-2. Stress-strain response of wet specimens from tensile tests and simulation

Table 6-3. Tensile properties for the carbon/epoxy composite in dry and wet conditions

	Dry	Wet
Transverse tensile strength, Y_T (MPa)	60.4	52.7
k_{mt}	0.28	0.31

6.3.Compression Tests and Simulations

Axial compression tests were carried out to study the effects of moisture on the compressive properties of the carbon/epoxy composite. The compression tests were performed on short cylindrical specimens with length to diameter ratio, L/D of about 0.5. The compression tests on short specimens are suitable for the determination of the compressive strength of the material [74]. Figure 6-3 shows the axial compression test results for the wet specimens. Table 6-4 gives a comparison of the compressive properties of the dry and wet specimens. The maximum compressive stress of the composite is affected more significantly by moisture (20.2% reduction) compared to

the maximum tensile stress (6.5% reduction). This can be explained by the schematic diagram shown in Figure 6-4. Under compressive loading (Figure 6-4(a)), the matrix is pulled away from the fiber. The strength degradation of the matrix can cause fiber kinking to occur more easily. In comparison, under tensile loading (Figure 6-4(b)), the matrix is compressed against the fiber. Therefore, the effect of the strength degradation of the matrix on the load carrying capacity of the fiber is smaller.

Figure 6-3. Axial compression test results for wet specimens

Table 6-4. Axial compressive test results for dry and wet specimens

	Dry specimens	Wet specimens	% Difference
Specimen label	U\$-ACS-D#	U\$-ACS-D#	
Average maximum compressive stress (MPa)	88.9 ±1.6	70.9 ±6.4	-20.2

Note: \$ represents the pipe number and # represents the specimen number

Figure 6-4. Schematic of CFRP composite under (a) compressive and (b) tensile loads

Figure 6-5 shows the finite element mesh for the simulation of the axial compression tests carried out on short specimens. For the simulation of the axial compression test, the properties for the wet specimens were initially estimated using past experimental studies. Experimental studies on carbon/epoxy composites have shown that the transverse compressive modulus (E_{22}) and transverse compressive strength (Y_C) are reduced by 36~42% and 30~37% respectively when saturation moisture content is reached [23]. Using a reduction of 30% for both E_{22} and Y_C , the maximum compressive stress of 69.5 MPa was obtained from axial compression simulation. This value is very close to the average maximum compressive stress from experiments. The reduction factor, k_{mc} was unchanged for the wet specimens. The compressive properties of the wet composite determined from simulation are given in Table 6-5.

Figure 6-5. Finite element mesh for axial compression test (short specimen)

Table 6-5. Compressive properties for the carbon/epoxy composite in dry and wet conditions

	Dry	Wet
Transverse compressive strength, Y_C (MPa)	78	55
E_{22} (GPa)	8.1	5.7
k_{mc}	0.25	0.25

6.4.Flexural Tests and Simulations

Table 6-6 shows a summary of the three point flexural test experimental and simulation results. From experiments, the average maximum stress for wet specimens (47.0 MPa) was 9.1% lower than that of dry specimens (51.7 MPa). This result is in line with the tensile and compression test results as specimens in flexural tests experience both tensile and compressive deformations.

Table 6-6. Simulation and experimental results for flexural tests of dry and wet specimens

	Dry	Wet		
	Experiment	Experiment	Simulation	% Error
Specimen label	U\$-F-D#	U\$-F-W#		
Average maximum stress (MPa)	51.7	47.0	43.4	-7.7

For the simulation of the flexural test on wet specimens, the material properties determined from tensile and compressive test simulations were used. A comparison of the simulation result with experimental data is shown in Figure 6-6. The maximum stress obtained in the simulation agreed well with experiment (error within 10%).

Figure 6-6. Stress-strain response of wet specimens from flexural tests and simulation

6.5. Conclusions

The carbon/epoxy composite test specimens with saturation moisture content have been tested under tensile and compressive loading conditions. From tensile tests, the maximum stress recorded for the wet specimens were 6.5% lower than dry specimens. In comparison, the material degradation for the compressive strength of the composite due to moisture was found to be more significant. The maximum stress under axial compression for the wet specimens was 20.2% lower than dry specimens.

Simulations of the wet carbon/epoxy composites have also been carried out. The tensile properties of the composite with saturation moisture content were estimated using the mathematical model discussed in Chapter 5. It was found that the carbon fiber-epoxy interface was unaffected by moisture. The effect of moisture on the carbon fiber-epoxy interface needs to be studied further. When the degradation at the fiber-matrix interface is omitted, the tensile strength estimated using the mathematical model produced results that agreed well with experiments.

The compressive properties of the composite with saturation moisture content were determined from past experimental studies and by fitting the simulation results to experimental data. Simulation of the three-point flexural test of the wet specimens was also carried out for validation.

7. Liner-Composite Interface in Risers (Experiments)

7.1. Mechanical Surface Treatment for Liner

For composite risers, especially in risers where the metal liner contributes in bearing some load, the bonding between the metal liner and composite layers is important to ensure the overall mechanical performance of the riser. Failure at the interface between the liner and composite layers can lead to buckling and fluid pressure build-up at the interface [7]. Therefore, a strong bonding between the liner and composite layers is required. In order to increase the bonding between metal and carbon/epoxy composites, surface treatment on the metal needs to be carried out. The surface treatment aims to adjust the surface tension, surface roughness and surface chemistry of the metal to improve its bonding with polymers [93]. Many surface treatment methods have been studied by researchers, including mechanical surface treatment such as grit blasting [94], and chemical surface treatment such as anodizing [95, 96]. In this study, two mechanical surface treatment methods were investigated: grit blasting and grooving.

7.1.1. Grit Blasting

Grit blasting can increase the surface roughness of the metal liner and thus improve the composite-metal bonding by mechanical interlocking [93, 94]. Grit blasting was carried out using the Tech-Flo suction blast cabinet SC2424. The Ballotini® glass impact beads supplied by Potters Industries Inc. were used. Figure 7-1 shows the aluminum liner surface before and after the grit blasting treatment.

Figure 7-1. Aluminum liner before and after grit blasting

7.1.2. Groove

Round bottomed grooves were cut into the outer surface of the aluminum liner at the same angle as the filament winding angle that is $\pm 55^\circ$ to the axial direction. This allows the carbon fibers to be placed in the grooves. The grooves are 0.2 mm in depth and the groove spacing is 15mm. Figure 7-2 shows the aluminum liner with grooves.

Figure 7-2. Groove surface treatment: (a) Aluminum liner with grooves; (b) Grooves were cut at the same angle as the filament winding angle

The cross sectional area of the composite pipe specimens with its aluminum liner treated with grooving was inspected using a microscope to study the fiber distribution around and into the grooves. Figure 7-3 shows the cross sectional area of the composite pipe specimen near two different grooves. Fibers were found into the groove shown in Figure 7-3(a) whereas for the groove in Figure 7-3(b), it is only filled with the epoxy matrix. For cases where the fibers were found into the groove, a larger matrix rich area between the first and second composite layers was also observed.

Figure 7-3. Microscope images showing cross sectional areas around two different grooves

Axial and lateral compression tests were carried out to study the effects of the two surface treatment methods. In order to compare the two treatment methods, the mechanical properties of the composite pipe specimens with inner liner treated with grit blasting (label starting with L) were taken as base values. The difference compared to composite pipe specimens with inner liner treated with grooving (label starting with G) can then be calculated as:

$$\% \text{ Difference} = \left(\frac{P_G}{P_L} - 1 \right) \times 100\% \quad (7.1)$$

where P_G and P_L are the mechanical properties of composite pipe specimens with liners treated with grooving and grit blasting respectively. The test results are presented in the next sections.

7.2.Lateral Compression Tests

Figure 7-4 shows the load-displacement curves from the lateral compression tests on composite pipe specimens with liner. The load per unit length is plotted to account for the small differences in length between the pipe specimens. From the load displacement data, the pipe stiffness (PS) can be calculated from the slope of the

graph at small displacements (below 2.5 mm) [75]. The pipe stiffness is related to the bending behavior of the pipe and thus affected by the liner-composite interface.

Figure 7-4. Lateral compression test results for composite test specimens with liner treated with grooving (label starting with G) and grit blasting (label starting with L)

From the load-displacement curve, the average pipe stiffness for the composite pipes with aluminum liner treated with grooving (specimens labeled G $\text{\textcircled{L}}$ -LC) was obtained as 10.71 MPa. This is higher than the average pipe stiffness of composite pipes with aluminum liner treated with grit blasting (specimens labeled L $\text{\textcircled{L}}$ -LC), which is 7.76 MPa. For the lateral compression tests, failure at the liner-composite interface results in a sharp drop in the load per unit length. The interface failure for the G $\text{\textcircled{L}}$ -LC specimens occurred at a lower load and displacement compared to the L $\text{\textcircled{L}}$ -LC specimens. After the interface failure (at displacements greater than 8 mm), both the L $\text{\textcircled{L}}$ -LC and G $\text{\textcircled{L}}$ -LC specimens showed a similar load-displacement response. The lateral compression test results are summarized in Table 7-1.

Table 7-1. Summary of lateral compression test results for composite test specimens with liner

	Composite pipe with aluminum liner treated with grit blasting	Composite pipe with aluminum liner treated with grooving	% Difference
Pipe specimen label	L\$-LC-D#	G\$-LC-D#	
Pipe stiffness (MPa)	7.76 $\pm$ 0.06	10.71 $\pm$ 0.23	+38.0
Load at first failure (N/mm)	30.7 $\pm$ 2.1	26.2 $\pm$ 4.0	-14.7
Displacement at first failure (mm)	5.81 $\pm$ 1.03	2.75 $\pm$ 0.19	-52.7

Note: \$ represents the pipe number and # represents the specimen number, % Difference calculated using equation (7.1)

Figure 7-5 shows the damage on the specimens after the lateral compression tests. Failure at the liner-composite interface was observed for both G\$-LC and L\$-LC specimens. For the L\$-LC specimens, matrix cracking can also be seen at the top part of the specimen. This was not the case for G\$-LC specimens.

Figure 7-5. Composite pipe specimens with liner after lateral compression test: (a) specimen with liner treated with grit blasting; (b) specimen with liner treated with grooving

7.3.Axial Compression Tests

A separate axial compression test was carried out on an aluminum pipe specimen. The pipe specimen had an outer diameter of 76.2 mm, a wall thickness of 1.7 mm and

length of 150 mm. The axial and hoop strains of the aluminum pipe were measured using strain gauges attached to the specimen. The axial compression test results for aluminum liner are shown in Figure 7-6.

Figure 7-6. Axial compression test results for aluminum liner

The apparent elastic modulus of aluminum measured from the axial compression test is 43.4 GPa. This value is much lower than the modulus obtained from the lateral compression test (Section 3.3.1). This difference can be explained by the findings of Liu et al. [97]. The lower modulus value from the axial compression test is caused by stress concentration in the specimen resulting in the early yielding of some parts of the specimen. Stress concentration in the specimen can be caused by factors such as friction or a slanted contact between the loading plates and the specimen. This stress concentration is related to the aspect ratio of the specimen [97]. As the composite pipe specimens have a similar aspect ratio as the aluminum pipe specimen, the stress concentration effect in the aluminum liner is expected to be present for the axial compression tests on the composite pipe specimens as well.

For the composite pipe specimens tested in this study, the aluminum liner bears a large part of the load because the liner is thicker than the composite lay-up. This

configuration allows the effect of failure at the liner on the liner-composite interface to be investigated. Under axial compressive load, the specimens initially showed linear elastic behavior. The axial linear elastic modulus for the L $\text{\$}$ -ACL specimens (35.5 GPa) was slightly larger than that of the G $\text{\$}$ -ACL specimens (31.6 GPa). However, the G $\text{\$}$ -ACL specimens failed at a larger average maximum compressive stress of 145.6 MPa compared to the L $\text{\$}$ -ACL specimens which failed at 126.6 MPa. Figure 7-7 shows a comparison of the stress-strain response for the specimens with liner treated with grit blasting and grooving. A summary of the axial compression test results is given in Table 7-2.

Figure 7-7. Compressive stress vs axial strain from axial compression tests on composite pipe specimens with liner treated with grooving (label starting with G) and grit blasting (label starting with L)

Table 7-2. Summary of axial compression test results for composite test specimens with liner

	Composite pipe with aluminum liner treated with grit blasting	Composite pipe with aluminum liner treated with grooving	% Difference
Pipe specimen label	L\$-ACL-D#	G\$-ACL-D#	
Axial linear elastic modulus (GPa)	35.3 ±3.1	31.6 ±2.8	-10.5
Maximum compressive stress (MPa)	126.6 ±4.2	145.6 ±3.8	+15.0
Axial strain at final failure ($\mu\epsilon$)	-10198 ±1729	-16945 ±6066	-66.0

Note: \$ represents the pipe number and # represents the specimen number, % Difference calculated using equation (7.1)

Comparing the axial compression test results for aluminum and composite pipes with liner, the damage mechanisms in the axial compression tests can be deduced. For the composite pipes with liner, the stress-strain response deviated from linear elastic behavior when yielding of the aluminum liner occurred. Yielding of the liner occurred at the compressive stress of around 100 MPa for L\$-ACL specimens and around 120 MPa for G\$-ACL specimens. For both specimen types (L\$-ACL and G\$-ACL), the specimens bulged around the circumference right before final failure indicating debonding at the liner-composite interface (Figure 7-8).

Figure 7-8. Bulging at one end of a composite pipe specimen after axial compression test indicating liner-composite debonding

For the G $\text{\$}$ -ACL specimens, the axial strain at final failure between different specimens. This can be seen from the large standard deviation for the axial strain at failure (Table 7-2 and Figure 7-7). The different behavior shown by the test specimens is due to the difference in the fiber distribution around the grooves. The current fabrication process was not able to produce composite pipes with consistent fiber distribution around the grooves (Section 7.1.2).

7.4.Conclusions

The effects of two mechanical surface treatment methods of metal liners on the metal-composite bonding in composite pipes have been studied. For the grooving surface treatment method, the grooves were cut into the liner at the same angle as the filament winding angle. This allowed the carbon fibers to be placed in the grooves to act as reinforcement for the liner. Under lateral compressive loading, the pipe specimens with liner treated with grooving had higher pipe stiffness. However, the

applied load at first failure was lower than that of specimens with liner treated with grit blasting. For axial compression tests, the grooving method produced specimens that have a higher maximum compressive strength than the grit blasting method. The surface treatment method used also affected the stress at which yielding of the liner occurred. In order to further study the effects of the surface treatment on the liner-composite debonding, FE simulations were carried out. The findings from the FE simulations will be discussed in the next chapter.

8. Liner-Composite Interface in Risers (Simulations)

8.1. Surface-Based Cohesive Behavior

The surface-based cohesive behavior available in Abaqus is used to model the liner-composite interface [62]. Surface-based cohesive behavior is suitable for interface with very small thickness. It is easier to implement compared to using cohesive interface elements while providing very similar capabilities.

The traction-separation behavior before damage is described using a linear elastic model. The normal and shear components are assumed to be uncoupled. The elastic behavior is then given as [62]:

$$\boldsymbol{\tau} = \begin{Bmatrix} \tau_1 \\ \tau_2 \\ \tau_3 \end{Bmatrix} = \begin{bmatrix} K_{11} & 0 & 0 \\ 0 & K_{22} & 0 \\ 0 & 0 & K_{33} \end{bmatrix} \begin{Bmatrix} \delta_1 \\ \delta_2 \\ \delta_3 \end{Bmatrix} = \mathbf{K} \boldsymbol{\delta} \quad (8.1)$$

where τ_i is the interface traction, δ_i is the interface separation, K_{ij} is the interface stiffness, and indices $i, j = 1, 2$ denote the shear directions, 3 denote the normal direction.

The damage response of the surface-based cohesive behavior is similar to the model described by Camanho et al. [52] (discussed in Section 2.4.4). Damage initiation was determined by the quadratic traction criterion (equation (2.36)). A linear softening law based on energy dissipated was used for the post-damage behavior. The damage propagation was determined using the B-K criterion (equations (2.38) and (2.39)). The value of 1.6 was used for the η parameter in the B-K criterion. This value has been found to give good predictions for carbon/epoxy composites [64].

The interfacial properties of the liner-composite interface are largely dependent on the epoxy resin. Therefore, the liner-composite interface was assumed to behave

similar to a homogenous material, that is, interface stiffness, K and traction at onset of damage, τ^o are assumed to be the same in all directions. For the critical energy release rate, $G_{IIC} = G_{IIIC} = 2G_{IC}$ was initially assumed.

8.2.Lateral Compression Simulations

The finite element mesh for the simulation of the lateral compression tests is shown in Figure 8-1. The green component is the aluminum liner and the blue component is the carbon/epoxy composite. The loading plates were modeled using rigid surfaces. The composite layers were assumed to be perfectly bonded.

Figure 8-1. Finite element mesh for lateral compression simulations

The values for interface stiffness, K and traction at onset of failure, τ^o were varied to fit the simulations to experimental results. The value for K affected the pipe stiffness whereas τ^o affected the onset of failure. From experiments, the failure at the liner-interface occurred suddenly (similar to brittle failure). This allows the critical energy

release rate to be estimated from K and τ^o . Figure 8-2 shows the load-displacement curves from the simulations compared to experiments. A comparison of some of the properties calculated from simulations to their experimental counterparts is given in Table 8-1. The K and τ^o values determined from the simulations are given in Table 8-2.

Figure 8-2. Lateral compression simulation and experimental results: (a) composite pipe with liner treated with grit blasting; (b) composite pipe with liner treated with grooving

Table 8-1. Comparison of simulation and experimental results for the lateral compression test on composite pipe specimens with liner treated with grit blasting and grooving

Surface treatment	Grit blasting			Grooving		
Specimen label	L\$-LC-D#			G\$-LC-D#		
	Exp	Sim	% Error	Exp	Sim	% Error
Pipe stiffness (MPa)	7.76 ±0.06	7.19	-7.3	10.71 ±0.23	10.78	+0.7
Load at first failure (N/mm)	30.7 ±2.1	32.7	+6.5	26.2 ±4.0	27.7	+5.7
Displacement at first failure (mm)	5.81 ±1.03	5.60	-3.6	2.75 ±0.19	2.61	-5.1

Note: Exp = Experiment, Sim = Simulation, \$ = pipe number, # = specimen number

Table 8-2. Interface properties used in simulations

Surface treatment	Grit blasting	Grooving
Interface stiffness, K (Pa/m)	6×10^{10}	4×10^{11}
Traction at onset of failure, τ^o (MPa)	5	6
G_{IC} (Pa·m)	210	60
G_{IIC} and G_{IIIC} (Pa·m)	230	110

The grooving surface treatment (specimens labeled G\$-LC) resulted in both higher interface stiffness K and traction at onset of damage τ^o compared to grit blasting (specimens labelled L\$-LC). The higher K for grooving resulted in higher tractions at the liner-composite interface when lateral load is applied. This led to the G\$-LC specimens failing at a lower applied load (26.2 N/mm) compared to L\$-LC specimens (30.7 N/mm) even though the τ^o for grooving was higher. From the lateral compression experiments, matrix cracking was observed for the L\$-LC specimens but not for G\$-LC specimens (Figure 7-5). This is due to the damage propagation at the interface. Figure 8-3 shows the debonding and damage propagation at the liner-composite interface from the simulations. The area that has debonded is shown in red and the damage propagation direction is shown by the white arrows. The two plots were taken at the same displacement of 9 mm. The grit blasting surface treatment resulted in a more compliant liner-composite interface and thus slower damage propagation. As a result, the composite layers were still bonded to the liner at the top part of the L\$-LC specimen (blue region at the top in Figure 8-3(a)) after damage has occurred at the liner-composite interface at the sides. The load sharing between liner and composite at the top part of the specimen resulted in higher stresses in the composite and led to matrix cracking. For the grooving surface treatment, the damage propagation occurred more quickly (Figure 8-3(b)). Therefore,

the stresses in the composite were lower and matrix cracking did not occur for the G $\text{\$}$ -LC specimens.

Figure 8-3. Liner-composite debonding (shown in red) from lateral compression simulations: (a) specimen with grit blasting treatment; (b) specimen with grooving treatment

In order to further study the effects of liner-composite interface properties on the lateral compression tests, simulations with the liner-composite interface having perfect bonding and frictionless contact were also carried out. A comparison of the simulation results with different properties at the liner-composite interface is shown in Figure 8-4. The simulations with perfect bonding and frictionless contact give the upper and lower bounds for the load-displacement response. It can be seen that the pipe stiffness for the specimens with grooving is very close to that of perfect bonding. This is because the mechanical interlock due to the grooves prohibits slipping at the liner-composite interface. After failure, the specimens with grooving and grit blasting both behaved very similarly to having frictionless contact at the liner-composite interface.

Figure 8-4. Comparison of lateral compression simulations with different interaction properties at the liner-composite interface

8.3.Axial Compression Simulations

From axial compression simulations, it was found that the liner surface treatment affected the yielding of the liner. In order to simulate this effect, the stress concentration in the aluminum liner needs to be accounted for in simulation. Two approaches were considered for the modeling of the aluminum liner: (i) the slanted contact model and (ii) the sandwich model.

Figure 8-5(a) shows the finite element mesh for the slanted contact model. The liner is shown in green and the composite is shown in blue. The specimen is modeled with uneven height such that the contact between the loading plate (modeled as a rigid surface) and the axial compression specimen occur at a small angle. The contact angle was calculated using the method introduced by Liu et al. [97]. Using the apparent elastic modulus of 43.4 MPa from the axial compression test on the aluminum liner, the angle of 0.346° was determined. The stress distribution in the aluminum liner as a result of the stress concentration in the slanted contact model is shown in Figure 8-5(b). The side with the larger height had higher stress when compressed.

Figure 8-5. Slanted contact model for axial compression simulation

The second approach is the sandwich model shown in Figure 8-6. The two ends of the sandwich model are the soft segments which are considered to have fully yielded while the hard segment in the middle has the properties of normal aluminum as determined in Section 3.3.1. The soft segments are colored orange, the hard segment is colored green and the composite is colored blue in Figure 8-6(a). The total volume fraction of the soft segment was determined as 0.0112 using the findings of Liu et al. [97]. For the sandwich model, homogeneous deformation was assumed in the axial direction. The stress distribution in the aluminum liner modeled using this approach is shown in Figure 8-6(b). Unlike the slanted contact model, the plastic deformation in the liner was contained in the soft segments before yielding occurred in the hard segment and the stresses are more uniform.

Figure 8-6. Sandwich model for axial compression simulation

For the simulation of axial compression test on composite pipe specimens with liner treated with grit blasting (specimens labeled L\$-ACL), the slanted contact model was used. The liner-composite interface properties determined from lateral compression simulations were used (Section 8.2). The simulation results obtained were in good agreement with the experimental results (Figure 8-7). Therefore, for L\$-ACL specimens, the slanted contact model was sufficient in simulating the stress concentration in the liner. After the liner has fully yielded, the carbon/epoxy composite became the main load bearing component. Final failure in the axial compression test occurred when matrix compression failure took place in the composite. This was followed by debonding at the liner-composite interface that was also observed in experiments (Figure 7-8).

Figure 8-7. Axial compression simulation and experimental results for composite pipe with liner treated with grit blasting

For the axial compression test on composite pipe specimens with liner treated with grooving (specimens labeled G $\bar{S}$ -ACL), three simulations were carried out with different methods of modeling the aluminum liner. The simulation results are shown in Figure 8-8. In all three simulations, the liner-composite interface was modeled using the properties determined from the lateral compression simulations (Section 8.2). For simulation 1, the sandwich model was used whereas for simulation 2, the slanted contact model was used. The complete yielding of the aluminum liner for simulation 1 occurred at a higher compressive stress (about 120 MPa) compared to simulation 2 (about 90 MPa) due to plastic deformation initially being contained in the soft segments of the sandwich model. From Figure 8-8, it can be seen that simulations 1 and 2 give the upper and lower boundaries for the stress-strain response in the axial compression tests. In particular, the stress-strain response from simulation 1 was very close to the experimental data for the G1-ACL-D1 specimen.

For simulation 3, a combination of the sandwich model and slanted contact model was used. The model was constructed using the contact angle of 0.173° which is half the value calculated previously. The liner was also modeled with a soft segment of volume fraction 0.00559 which is again half the value earlier, resulting in an apparent elastic modulus of 55.1 GPa for the aluminum liner. Using this combination, the stress-strain response from simulation 3 agreed well with the average stress-strain response from experiments (Figure 8-8). For final failure, the damage mechanism was similar to the specimens with grit blasting, which is matrix compression damage in the carbon/epoxy composite followed by debonding at the liner-composite interface. A summary of the axial compression simulation results is shown in Table 8-3.

Figure 8-8. Axial compression simulation and experimental results for composite pipe with liner treated with grooving

Table 8-3. Comparison of simulation and experimental results for the axial compression test on composite pipe specimens with liner treated with grit blasting and grooving

Surface treatment	Grit blasting			Grooving		
Specimen label	L\$-ACL-D#			G\$-ACL-D#		
	Exp	Sim	% Error	Exp	Sim 3	% Error
Axial linear elastic modulus (GPa)	35.3 ±3.1	38.2	+8.2	31.6 ±2.8	33.9	+7.3
Maximum compressive stress (MPa)	126.6 ±4.2	134.7	+5.9	145.6 ±3.8	148.7	+2.1

Note: Exp = Experiment, Sim = Simulation, \$ = pipe number, # = specimen number

The effects of the grooves on the plastic deformation in the aluminum liner can be studied from the simulations. Figure 8-9 shows a schematic diagram of the plastic flow in the aluminum liner with grooves. The grooves that contain both carbon fiber and epoxy can act as reinforcement in the aluminum liner. The reinforcement can work to contain the plastic deformation due to stress concentration. In Case 1, all the grooves contain both carbon fiber and epoxy resulting in the plastic deformation only affecting a limited area. The liner in this case can be modeled using the sandwich model (simulation 1 in Figure 8-8). In Case 2, some of the grooves only contain epoxy. This allows the plastic deformation to affect a larger area. The effect of the grooves in Case 2 on the liner is similar to modeling the liner with a combination of sandwich model and slanted contact model (simulation 3 in Figure 8-8). As the distribution of carbon fibers in the grooves were not uniform, with some grooves containing only epoxy, the area of the liner affected by the stress concentration varied between specimens. From the results of simulation 3, it can be deduced that about 50% of the grooves in the liner contained carbon fiber. The effect of the grooves in limiting the area affected by stress concentration can be improved by having more consistent

fiber distribution in the grooves. This effect can be useful in prohibiting damage propagation from failure caused by accidental loads in composite risers.

Figure 8-9. Schematic diagram of plastic deformation in aluminum liner with grooves

8.4. Discussion on the Grooving Surface Treatment

The traction at the onset of damage, τ^o for the liner-composite interface with grooving surface treatment was determined as 6 MPa. This value is low compared to the strength of the epoxy resin used. This suggests that the failure at the liner-composite interface is due to slipping at the interface and not due to matrix cracking. The groove depth that was used was too small to prevent this slipping. For liner with a larger thickness, the groove depth can be increased to increase the τ^o for the liner-composite interface.

The τ^o value determined for the grooving surface treatment is 20% larger than that of the grit blasting surface treatment. However, from lateral compression tests, the applied load at the first failure for the composite pipe specimens with liner treated with grit blasting was higher (average of 30.7 N/mm) (Table 7-1). Therefore, additional simulations of the lateral compression test for composite pipe specimens

with liner treated with grooving were carried out. The τ^o value was varied in the additional simulations such that the applied load at first failure can be increased. It was found that by using $\tau^o = 7.5$ MPa, the applied load at first failure similar to that of the specimens treated with grit blasting can be reached. The load-displacement response for the additional simulation is shown in Figure 8-10. Assuming τ^o is directly proportional to the surface area, the grooving parameters to produce $\tau^o = 7.5$ MPa can be calculated. If the depth of the grooves is kept at 0.2 mm, the groove spacing will need to be reduced to 5.6 mm from 15 mm to give $\tau^o = 7.5$ MPa. The liner-composite bonding can also be further improved by using the grooving surface treatment in conjunction with other surface treatment methods such as anodizing.

Figure 8-10. Additional lateral compression simulation

8.5. Conclusions

The surface based cohesive behavior was used to model the liner-composite interface. The experimental results for the axial and lateral compression tests on composite pipe specimens with liner were successfully reproduced in FE simulations. From the simulations, the liner-composite interfacial properties for the two

mechanical surface treatment methods studied were determined. The grooving treatment method produced both higher interface stiffness and traction at the onset of failure. Therefore, the grooving method was found to give better bonding between the aluminum liner and the carbon/epoxy composite.

The failure mechanisms and damage propagation at the liner-interface were also studied in the simulations. The higher interface stiffness for the grooving treatment method also resulted in faster damage propagation similar to brittle fracture. For the grooving surface treatment, the carbon fibers placed in the grooves can act as reinforcement for the aluminum liner. The plastic deformation in the liner due to stress concentration can be contained in a limited portion of the liner due to the carbon fibers in the grooves. This effect can be useful in prohibiting the damage propagation at the liner-composite interface due to accidental loads in composite risers.

9. Progressive Damage Simulations of Composite Risers

9.1. Composite Riser Simulation Setup

Simulations of a full-scale composite riser segment will be discussed in this chapter. The dimensions for the composite riser analyzed are shown in Table 9-1. The riser dimensions are the same as the riser studied by Kim [98]. The length of the riser segment was determined using the length to diameter ratio, $L/D = 12$. This ratio was determined by Martins et al. to give the length that is sufficient to represent an infinite riser [67]. For the carbon/epoxy composite component of the riser, the layup configuration of $[\pm 55^\circ]_{23}$ was used. This configuration was chosen because the $\pm 55^\circ$ configuration has been studied extensively in the current study and also in past experimental and numerical studies. However, for different layup configurations, the same analysis approach can still be applied. For the inner liner, aluminum was used but the analysis method is also applicable for inner liner made of different materials such as titanium and steel. The simulations carried out fall under local analysis in the global-local analysis procedure of the composite riser system.

Table 9-1. Composite riser dimensions

Aluminum inner liner	Inner radius	123.4 mm
	Thickness	6.4 mm
Carbon/epoxy composite	Thickness	24.7 mm
Overall	Thickness	31.1 mm
	Segment length	1900 mm

The loading conditions investigated include inner pressure, outer pressure, axial forces, moisture effects and accidental loads (Figure 9-1). For risers in operation,

lateral forces due to seawater current are also present. However, the lateral forces are small compared to pressure loads and thus are omitted in the local analysis. For the global analysis of the riser, the lateral forces will need to be included to determine the bending deformation of the riser [28]. Simulations were carried out using the material properties of the carbon/epoxy composite without conditioning (dry condition) and with saturation moisture content (wet condition). The simulation results for the composite in dry and wet conditions were then compared to study the effects of moisture. For the composite in wet condition, the properties determined in Chapter 6 were used.

Figure 9-1. Loads acting on a composite riser segment

Figure 9-2 shows the finite element mesh used in the simulations. The model represents 1/8 of the full composite riser segment (shown in the magnified view in Figure 9-1) and was found to give similar results to the full segment model. The composite body is shown in green while the aluminum liner is colored blue. Similar to

the simulations in Chapter 8, the composite-liner interface was modeled using surfaced based cohesive behavior. The interface properties for the grooving surface treatment method (Table 8-2) were used. For the composite riser segment simulations, due to the model size and various failure modes considered in the model, the explicit solver was used. The loading rate was increased to enable the simulations to be completed in a practical number of increments. The total work and internal energies for the simulations were checked to ensure that quasi-static conditions were maintained.

For the boundary conditions, the riser segment is constrained in the axial direction in the middle such that displacement $u_3 = 0$. At the end of the riser segment, the displacement in the radial direction was constrained. The composite body and the aluminum liner were also tied at the end so that they have the same axial displacement. The boundary conditions are also shown in Figure 9-2.

Figure 9-2. Finite element mesh for composite riser simulations

9.2.Failure under Burst Pressure

For burst simulations, inner pressure and axial tensile forces were applied to the riser such that the hoop stress to axial stress ratio, $S = 2H: 1A$. This stress ratio is chosen

because experimental data from past studies are available and can be used for the verification of the simulation results. Final failure of the composite riser segment is taken as the point when fiber failure occurs. The bimodulus-plastic model discussed in Chapter 4 was used for the carbon/epoxy composite to account for the nonlinear pre-failure behavior. For comparison, a burst simulation assuming linear-elastic pre-failure behavior for the composite was also carried out. The simulation results are summarized in Table 9-2.

Table 9-2. Results for burst simulations performed using different models (experimental data included for comparison)

Failure mode	Inner pressure (MPa)		
	Linear-elastic model for pre-failure behavior	Bimodulus-plastic model for pre-failure behavior	Experiment
Liner yielding	40.5	40.5	
Matrix failure through riser wall and possible fluid leakage	114	91.5 (matrix ductile damage) 228 (matrix final failure)	87 (from Soden et al. [42])
Liner-composite debonding	121.5	195	
Final failure	251.6	246	

From the simulations, the different stages of failure can be determined: liner yielding, matrix tensile failure, followed by final failure due to fiber tensile failure. For matrix failure, the failure started at the inner most composite layer and progressed outward. Damage in the matrix through the composite riser wall corresponds to the damage when fluid leakage is observed in burst experiments. For the simulation using linear-elastic model for pre-failure behavior, fluid leakage was predicted to occur at the

inner pressure of 114 MPa, whereas for the simulation carried out with the bimodulus-plastic model, fluid leakage due to matrix ductile damage was predicted at 91.5 MPa. From past experimental studies, for carbon/epoxy composite, matrix damage was observed when the hoop stress reaches about 410 MPa [42]. For the riser dimensions used, this corresponds to an inner pressure of about 87 MPa. The bimodulus-plastic model gave a better prediction compared to the linear-elastic model. For the liner-composite interface, the debonding at the interface occurred following matrix failure and was not the main factor which determined the failure of the riser.

The burst simulation carried out using the bimodulus-plastic model was studied further. Figure 9-3 shows a plot of the hoop stress against axial and hoop strains. The stresses and strains were taken at the outer surface of the composite riser segment model. The plot of the axial strain shows that the riser segment was expanding in the axial direction with increased inner pressure until hoop stress reached about 170 MPa. This corresponds to the point when the aluminum liner started yielding. The riser segment model then contracted in the axial direction until final failure was reached. This behavior was also observed in experimental studies [11]. On the other hand, the hoop strain increased almost linearly with hoop strain despite matrix failure taking place. This shows that the deformation in the hoop direction is mainly determined by the carbon fiber.

Figure 9-3. Plots of hoop stress vs hoop and axial strains from burst simulation using the bimodulus-plastic model (for dry condition)

Another burst simulation was also carried out using the properties for the carbon/epoxy composite in wet condition. Table 9-3 shows a comparison of the burst simulation results for the composite riser segment in dry and wet conditions. For both simulations, the bimodulus-plastic model was used. Besides liner yielding, the different stages of failure occurred at a lower inner pressure for the composite riser segment in wet condition. Fluid leakage for the riser segment in wet condition occurred at 81 MPa compared to 91.5 MPa for the riser segment in dry condition (11.5% lower). As a result of matrix failure, the fibers were required to take more load. This led to the final failure for the riser segment in wet condition occurring at a lower pressure (213 MPa) despite the composite strength in the fiber direction being unaffected by moisture.

Table 9-3. Burst simulation results for composite riser segment in dry and wet conditions

Failure mode	Inner pressure (MPa)	
	Dry condition	Wet condition
Liner yielding	40.5	40.5
Matrix ductile damage through riser wall and fluid leakage	91.5	81
Matrix final failure through riser wall	228	169.5
Liner-composite debonding	195	177
Final failure	246	213

9.3.Failure under Collapse Pressure

For collapse simulations, no forces were applied in the axial direction and only external pressure was applied to the composite riser segment. Collapse simulations were carried out using properties of the carbon/epoxy composite in dry and wet conditions. The bimodulus-plastic model was used for all the collapse simulations. A summary of the collapse simulations results is given in Table 9-4.

Table 9-4. Collapse simulation results for composite riser segment in dry and wet conditions

Failure mode	External pressure (MPa)	
	Dry condition	Wet condition
Liner yielding	17.5	19.0
Matrix ductile damage through pipe thickness and fluid leakage	31.0	29.4
Final failure	38.3	35.0

From the simulations, the stages of failure were determined: liner yielding, matrix ductile damage, and final failure. Due to the external pressure, the composite layers

in the riser body were experiencing compressive stress in the fiber direction and tensile stress in the transverse direction. Therefore, the matrix damage was due to matrix tensile failure. The composite riser collapsed when matrix final failure occurred in all the composite layers. Therefore, final failure in the collapse simulations was determined by matrix failure instead of fiber failure. This can also be seen from the stress-strain response in Figure 9-4. The nonlinear stress-strain response implies that the matrix was the main load bearing component.

Figure 9-4. Hoop stress vs axial and hoop strains from collapse simulation (for dry condition)

From the simulation carried out using the properties of composite in dry condition, the collapse pressure was determined as 38.3 MPa. This agrees well with the collapse pressure of 38.64 MPa for a carbon/epoxy composite tube with similar wall thickness (30.5 mm) that was studied by Ramirez and Engelhardt [15]. For the simulation performed using the properties of composite in wet condition, the collapse pressure was 35.0 MPa. The property degradation due to moisture resulted in a reduction of about 8.6 % in the collapse pressure.

9.4. Effects of Accidental Loads

Accidental loads include damage caused by collision or impact of debris with the composite riser. Figure 9-5 shows the finite element mesh used to study the effect of accidental loads. The aluminum liner is shown in green and the carbon/epoxy composite is shown in blue. A small portion of the outermost layer of the composite body (colored red) was considered damaged due to accidental load. The damaged part was modeled as a perfectly plastic material. The damaged area was about 10 x 10 mm with a thickness of 1.07 mm.

Figure 9-5. Finite element mesh for the study on accidental load

The burst and collapse simulations have shown that the composite riser can withstand high inner pressure but is weak when loaded with pure external pressure. Therefore, the failure of the composite riser under collapse pressure is more critical. Hence, for the simulation of composite riser affected by accidental load, external pressure load was also applied. The material properties for the carbon/epoxy composite in dry condition were used.

Table 9-5 gives a summary of the collapse simulation result for the composite riser segment affected by accidental damage. The results for the collapse simulation of the composite riser segment without accidental damage are also included for

comparison. The accidental damage did not affect the pressure at which liner yielding and matrix ductile damage occurred. For the final failure, the collapse of the riser affected by accidental damage occurred at an external pressure of 33.3 MPa. The reduction in collapse pressure due to the accidental damage was 13.1 %. This is more severe than the effect of moisture (8.6 % reduction in collapse pressure).

Table 9-5. Collapse simulation results for composite riser segments with and without accidental damage

Failure mode	External pressure (MPa)	
	Without accidental damage	With accidental damage
Liner yielding	17.5	17.5
Matrix ductile damage through pipe thickness and fluid leakage	31.1	31.1
Final failure	38.3	33.3

Figure 9-6 shows the contour plots of the matrix damage for the 22nd composite layer which is directly below the layer affected by accidental damage. Red color indicates complete damage ($d_m = 1$). The contour plots were taken at applied external pressures of 32.8 MPa and 33.4 MPa. The 22nd composite layer was required to carry more load due to the accidental damage at the outer layer. This resulted in matrix tensile failure occurring earlier at the area near the accidental damage. The damage then propagates quickly to the other parts of the composite riser causing the collapse of the riser at a lower pressure load.

Figure 9-6. Contour plot of matrix damage at (a) external pressure = 32.8 MPa and (b) external pressure = 33.4 MPa

9.5. Conclusions

Simulations of a full-scale composite riser segment with aluminum inner liner have been carried out. The simulations fall under the local analysis of the global-local procedure. Loads that were considered include inner and external pressure, axial loads, as well as the effects of moisture.

For burst simulations, the failure of the riser occurred in different stages: liner yielding, matrix failure and then final failure due to fiber breakage. The bimodulus-plastic model proposed in this study was able to give a better matrix failure prediction compared to the simulation performed using the linear-elastic model for the composite. For collapse simulations, the matrix failure was the main determining factor in the collapse of the riser due to external pressure. The effects of moisture on

the burst and collapse pressures were also investigated. The composite material property degradation due to moisture led to a decrease in both burst and collapse pressures.

In addition, a collapse simulation on the riser affected by accidental load was also performed. It was found that the reduction in the collapse pressure due to the accidental load was larger than that of the effects of moisture.

10. Conclusions and Recommendations for Future Work

10.1. Conclusions

In this study, the failure of filament wound composite risers with isotropic liner has been investigated (Note: polymer liners are considered isotropic when modeling their mechanical behaviors although polymers are not strictly isotropic due to the extrusion process). A bimodulus-plastic model has been developed to simulate the pre-failure nonlinear behavior in the carbon fiber reinforced polymer composite. The model includes two damage mechanisms that were observed in experimental studies: (i) ductile damage and (ii) plastic deformation in the polymer matrix. Simulations carried out using the model were found to give results that agreed well with experimental results from tensile, compressive and flexural tests. Using this model in FE simulations of composite risers, the pre-failure nonlinear behavior of the composite materials can be taken into consideration in the failure evaluation to give more accurate predictions.

A method to calculate the tensile properties of carbon fiber-reinforced polymer composites under the effects of moisture has been introduced. In the model formulated, the bulk polymer is modeled as having regions with a reduced tensile modulus due to the congregation of water molecules. The tensile modulus and ultimate tensile strength can then be calculated from the reduced tensile modulus. For CFRP laminae, aside from the reduced strength of the matrix, the weakening of the fiber-matrix interface bonding due to moisture is also taken into consideration. The model has been demonstrated to be able to provide good predictions for carbon fiber-reinforced composites with some of the commonly used polymer matrices.

Experiments and simulations have been carried out on CFRP with saturation moisture content. From the experiments, the maximum stresses for the wet CFRP specimens were found to be lower than dry specimens when subjected to both tensile and compressive loadings. The degradation due to moisture was found to be more severe for the compressive properties of the CFRP composite. The strength properties of the wet composites were successfully determined from the simulations. These properties can be used to study the effect of moisture on the failure of composite risers.

The bonding at the composite-liner interface has also been investigated. The effects of two mechanical surface treatment methods of metal liners on the liner-composite bonding have been studied. The treatment methods are grooving and grit blasting. For the grooving surface treatment method, the grooves were cut into the aluminum liner at the same angle as the filament winding angle. This allowed the carbon fibers to be placed in the grooves to act as reinforcement for the liner. In order to further study the effects of the surface treatment methods on the liner-composite interface, FE simulations have been carried out. The surface based cohesive behavior was used to model the liner-composite interface. From the simulations, the liner-composite interfacial properties for the two mechanical surface treatment methods studied were determined successfully. The grooving treatment method produced both higher interface stiffness and traction at the onset of failure. Also, it was found that the plastic deformation in the liner was contained in a limited portion of the liner due to the carbon fibers in the grooves.

Simulations of a full-scale composite riser segment with aluminum inner liner have been carried out. The loads that were considered include inner and external pressure, axial loads, as well as the effects of moisture. The simulations were able to give good predictions for the failure inner and external pressures. In addition, a collapse

simulation on the riser affected by accidental load was also performed. The progressive damage analysis technique was very useful in determining the effects of accidental damage on the damage initiation of the other parts of the composite riser.

10.2. Major Contributions

The nonlinear behavior of CFRP in the transverse and shear directions, the effects of moisture content on CFRP material properties, as well as the bonding between the metal liner and the composite body have been taken into consideration in the failure evaluation of composite risers in this study. These issues are important for the accurate analysis of the mechanical behavior and long term structural integrity of composite risers, but were seldom investigated in depth in the past studies on the topic.

The contributions of this work on the understanding of composite risers, including the details on the considerations given to the issues mentioned above, are given below:

- **Developed the bimodulus-plastic model to account for the nonlinear behavior of CFRP in the pre-failure stage**

The model gives a comprehensive account of the damage mechanisms observed in experiments while only requiring a minimum number of additional material parameters. The model was successfully implemented in a subroutine for FE simulations. The model can improve the failure predictions for load cases where shear stresses are significant by about 26% compared to assuming linear-elastic behavior for the pre-failure stage.

- **Formulated a predictive model to determine the CFRP tensile properties under various moisture contents**

Using this new model, the degradation of CFRP tensile properties due to moisture can be calculated based on their molecular structures. The use of the model can reduce the time and material required in the studies of CFRPs exposed to water or humid environments. This can be particularly useful for studies on the application of CFRPs in marine and offshore structures such as composite risers.

- **Studied the effects of the grooving surface treatment method on the liner-composite interface**

The grooving surface treatment was found to improve the liner-composite bonding compared to grit blasting surface treatment. The carbon fibers in the grooves also add a unique reinforcing effect to the liner and inhibit the plastic flow in the liner due to stress concentrations. This treatment method can be useful in containing the damage on the liner due to accidental loads in composite risers.

- **Demonstrated the failure evaluation of a composite riser segment using FE simulations and progressive damage analysis methods**

A comprehensive approach to the local analysis of composite risers using FE simulation tools has been presented. The failure modes, damage initiation and growth in composite risers can be analyzed to give a precise understanding of the risers' mechanical response and performance.

10.3.Recommendations for Future Work

- Improvements that can be made to the bimodulus-plastic model include: modifying the plastic deformation part of the model so that the plastic deformation is not restricted to the shear direction, and considering a

separate damage parameter for shear so that the effects of fiber and matrix damage on the shear direction can be modeled accurately.

- Study the effects of combinations of mechanical and chemical surface treatment methods on the liner-composite interface (for instance grooving coupled with anodizing).
- Further studies on the water diffusion in CFRP and the effects of moisture on the fiber-matrix interface can be carried out to improve the predictive model for the moisture effect.
- Develop a mathematical model for the prediction of compressive properties of CFRP laminae at different moisture contents.
- Study the water ingress and diffusion at the region around accidental damage and the combined effects of strength degradation due to moisture and accidental damage on the failure of a composite riser segment.
- Incorporate the results of the local analysis of composite riser segment affected by accidental damage in the global analysis of the riser system.

References

- [1] O. O. Ochoa, "Composite Riser Experience and Design Guidance," Technical Report, Offshore Technology Research Center, Texas A&M University, 2006.
- [2] M. M. Salama, "Advanced Composites for the Offshore Industry: Applications and Challenges," in *Composites Materials for Offshore Operations: Proceedings of the First International Workshop*, The National Institute of Standards and Technology, 1993, pp. 269-288.
- [3] H. M. Refat and A. R. El-gamal, "Influence of the Density of Water on the Dynamic Behavior of Square Tension Leg Platform," *American Journal of Civil Engineering and Architecture*, vol. 2, no. 4, pp. 122-129, 2014.
- [4] R. Rafiee, "Experimental and Theoretical Investigations on the Failure of Filament Wound GRP Pipes," *Composites: Part B*, vol. 45, pp. 257-267, 2013.
- [5] B. Melve, P. Fjellheim, S. Raudeberg and S. A. Tanem, "First Offshore Composite Riser Proven on Heidrun," *Offshore*, vol. 68, pp. 52-54, 2008.
- [6] Multiconsult, "Heidrun TLP," [Online]. Available: <https://www.multiconsultgroup.com/projects/heidrun-tlp/>. [Accessed 26 December 2017].
- [7] Det Norske Veritas, *Recommended Practice for Composite Risers*, DNV-RP-FP202, 2010.
- [8] D.-C. Pham, N. Sridhar, X. Qian, A. J. Sobey, M. Achintha and A. Sheno, "A Review on Design, Manufacture and Mechanics of Composite Risers," *Ocean Engineering*, vol. 112, pp. 82-96, 2016.
- [9] C. K. Morooka, J. R. P. Mendes and K. Miura, "Environmental effects, drilling vessel motions evaluated to determine maximum allowable operational conditions," [Online]. Available: <http://www.drillingcontractor.org/study-analyzes-response-of-deepwater-riser-with-suspended-bop-28656>. [Accessed 28 April 2015].
- [10] D. Hull, M. J. Legg and B. Spencer, "Failure of Glass/Polyester Filament Wound Pipe," *Composites*, vol. 9, pp. 17-24, 1978.
- [11] B. Spencer and D. Hull, "Effect of Winding Angle on the Failure of Filament Wound Pipe," *Composites*, vol. 9, pp. 263-271, 1978.

- [12] P. D. Soden, R. Kitching and P. C. Tse, "Experimental Failure Stresses for $\pm 55^\circ$ Filament Wound Glass Fibre Reinforced Plastic Tubes under Biaxial Loads," *Composites*, vol. 20, pp. 125-135, 1989.
- [13] P. D. Soden, R. Kitching, P. C. Tse, Y. Tsavalas and M. J. Hinton, "Influence of Winding Angle on the Strength and Deformation of Filament-Wound Composite Tubes Subjected to Uniaxial and Biaxial Loads," *Composite Science and Technology*, vol. 46, pp. 363-378, 1992.
- [14] P. Mertiny, F. Ellyin and A. Hothan, "An Experimental Investigation on the Effect of Multi-Angle Filament Winding on the Strength of Tubular Composite Structures," *Composite Science and Technology*, vol. 64, pp. 1-9, 2004.
- [15] G. Ramirez and M. D. Engelhardt, "Experimental Investigation of a Large-Scale Composite Riser Tube Under External Pressure," *Journal of Pressure Vessel Technology*, vol. 131, p. 051205, 2009.
- [16] D. C. Pham, S. Narayanaswamy, X. Qian, W. Zhang, A. Sobey, M. Achintha and R. A. Shenoi, "Composite Riser Design and Development - A Review," in *Analysis and Design of Marine Structures V*, Taylor and Francis Group, 2015, pp. 637-645.
- [17] P.-Y. Le Gac, P. Davies and D. Choqueuse, "Evaluation of Long Term Behaviour of Polymers for Offshore Oil and Gas Applications," *Oil and Gas Science and Technology - Rev. IFP Energies nouvelles*, vol. 70, no. 2, pp. 279-289, 2015.
- [18] C. C. Chamis, "Simplified Composite Micromechanics Equations for Hygral, Thermal and Mechanical Properties," NASA-TM-83320, National Aeronautics and Space Administration, U.S.A, 1983.
- [19] L. Minnetyan, P. L. N. Murthy and C. C. Chamis, "Progressive Fracture in Composites Subjected to Hygrothermal Environment," *International Journal of Damage Mechanics*, vol. 1, pp. 60-79, 1992.
- [20] J. E. Barbero and T. M. Damiani, "Phenomenological Prediction of Tensile Strength of E-Glass Composites from Available Aging and Stress Corrosion Data," *Journal of Reinforced Plastics and Composites*, vol. 22, pp. 373-394, 2003.
- [21] C. E. Browning, "The Effects of Moisture on the Properties of High Performance Structural Resins and Composites," AFML-TR-72-94, Wright-Patterson Air Force Base, Ohio, U.S.A., 1972.
- [22] C.-H. Shen and G. S. Springer, "Environmental Effects on the Elastic Moduli of Composite Materials," *Journal of Composite Materials*, vol. 11, pp. 250-264, 1977.

- [23] R. Selzer and K. Friedrich, "Mechanical Properties and Failure Behaviour of Carbon Fibre-Reinforced Polymer," *Composites Part A*, vol. 28A, pp. 595-604, 1997.
- [24] C.-H. Shen and G. S. Springer, "Effects of Moisture and Temperature on the Tensile Strength of Composite Materials," *Journal of Composite Materials*, vol. 11, pp. 2-16, 1977.
- [25] F. W. Crossman, R. E. Mauri and W. J. Warren, "Hygrothermal Damage Mechanisms in Graphite-Epoxy Composites," Contractor Report 3189, National Aeronautics and Space Administration, Washington DC, U.S.A., 1979.
- [26] F. A. Ramirez, L. A. Carlsson and B. A. Acha, "Evaluation of Water Degradation of Vinylester and Epoxy Matrix Composites by Single Fiber and Composite Tests," *Journal of Material Science*, vol. 43, pp. 5230-5242, 2008.
- [27] K. Totten, "Determination of the Tensile Strength of the Fiber/Matrix Interface for Glass/Epoxy and Carbon/Vinylester," Thesis, Florida Atlantic University, Florida, U.S.A., 2015.
- [28] J. Guesnon, C. Gaillard and F. Richard, "Ultra Deep Water Drilling Riser Design and Relative Technology," *Oil and Gas Science and Technology*, vol. 57, pp. 39-57, 2002.
- [29] B. D. Agarwal, L. J. Broutman and K. Chandrashekhara, Analysis and Performance of Fiber Composites, New Jersey, United States of America: John Wiley & Sons, Inc., 2006.
- [30] H. T. Hahn and S. W. Tsai, "Nonlinear Elastic Behavior of Unidirectional Composite Laminae," *Journal of Composite Materials*, vol. 7, pp. 102-118, 1973.
- [31] H. T. Hahn, "Nonlinear Behavior of Laminated Composites," *Journal of Composite Materials*, vol. 7, pp. 257-271, 1973.
- [32] H. Koerber, J. Xavier and P. P. Camanho, "High Strain Rate Characterization of Unidirectional Carbon-Epoxy IM7-8552 in Transverse Compression and In-Plane Shear Using Digital Image Correlation," *Mechanics of Materials*, vol. 42, pp. 1004-1019, 2010.
- [33] W.-P. Lin and H.-T. Hu, "Nonlinear Analysis of Fiber-Reinforced Composite Laminates Subjected to Uniaxial Tensile Load," *Journal of Composite Materials*, vol. 36, pp. 1429-1450, 2001.

- [34] W. Van Paepegem, I. De Baere and J. Degrieck, "Modelling the Nonlinear Shear Stress-Strain Response of Glass Fibre-Reinforced Composites. Part I: Experimental Results," *Composite Science and Technology*, vol. 66, pp. 1455-1464, 2006.
- [35] W. Van Paepegem, I. De Baere and J. Degrieck, "Modelling the Nonlinear Shear Stress-Strain Response of Glass-Fibre Reinforced Composites. Part II: Model Development," *Composite Science and Technology*, vol. 66, pp. 1465-1478, 2006.
- [36] S. W. Tsai and E. M. Wu, "A General Theory of Strength for Anisotropic Materials," *Journal of Composite Material*, vol. 5, pp. 58-80, 1971.
- [37] Z. Hashin, "Failure criteria for unidirectional fibre composites," *Journal of Applied Mechanics*, vol. 47, pp. 329-334, 1980.
- [38] A. Puck and H. Schürmann, "Failure Analysis of FRP Laminates by means of Physically Based Phenomenological Models," *Composites Science and Technology*, vol. 28, pp. 1045-1067, 1998.
- [39] A. Puck, J. Kopp and M. Knops, "Guidelines for the Determination of the Parameters in Puck's Action Plane Strength Criterion," *Composites Science and Technology*, vol. 62, pp. 371-378, 2002.
- [40] A. Puck and M. Mannigel, "Physically Based Non-Linear Stress-Strain Relations for the Inter-Fiber Fracture Analysis of FRP Laminates," *Composites Science and Technology*, vol. 67, pp. 1955-1964, 2007.
- [41] M. J. Hinton and P. G. Soden, "A Comparison of the Predictive Capabilities of Current Failure Theories for Composite Laminates, Judged against Experimental Evidence," *Composites Science and Technology*, vol. 62, pp. 1725-1797, 2002.
- [42] P. D. Soden, M. J. Hinton and A. S. Kaddour, "Biaxial Test Results for Strength and Deformation of a Range of E-Glass and Carbon Fibre Reinforced Composite Laminates: Failure Exercise Benchmark Data," *Composite Science and Technology*, vol. 62, pp. 1489-1514, 2002.
- [43] P. D. Soden, A. S. Kaddour and M. J. Hinton, "Recommendations for Designers and Researchers Resulting from the World-Wide Failure Exercise," *Composites Science and Technology*, vol. 64, pp. 589-604, 2004.
- [44] C. G. Dávila and P. P. Camanho, "Failure Criteria for FRP Laminates in Plane Stress," NASA/TM-2003-212663, National Aeronautics and Space Administration, U.S.A., 2003.

- [45] S. T. Pinho, C. G. Dávila, P. P. Camanho, L. Iannucci and P. Robinson, "Failure Models and Criteria for FRP under In-Plane or Three-Dimensional Stress States including Shear Non-Linearity," NASA/TM-2005-213530, National Aeronautics and Space Administration, U.S.A., 2005.
- [46] P. F. Liu and J. Y. Zheng, "Recent Developments on Damage Modeling and Finite Element Analysis for Composite Laminates: A Review," *Materials and Design*, vol. 31, pp. 3825-3834, 2010.
- [47] M. R. Garnich and V. M. K. Akula, "Review of Degradation Models for Progressive Failure Analysis of Fiber Reinforced Composites," *Applied Mechanics Review*, vol. 62, p. 010801, 2009.
- [48] R. J. Nuismer and S. C. Tan, "Constitutive Relations of a Cracked Composite Lamina," *Journal of Composite Materials*, vol. 22, pp. 306-321, 1988.
- [49] S. C. Tan and R. J. Nuismer, "A Theory for Progressive Matrix Cracking in Composite Laminates," *Journal of Composite Materials*, vol. 23, pp. 1029-1047, 1989.
- [50] S. C. Tan, "A Progressive Failure Model for Composite Laminates Containing Openings," *Journal of Composite Materials*, vol. 25, pp. 556-577, 1991.
- [51] S. C. Tan and J. Perez, "Progressive Failure of Laminated Composites with a Hole under Compressive Loading," *Journal of Reinforced Plastics and Composites*, vol. 12, pp. 1043-1057, 1993.
- [52] P. P. Camanho and F. L. Matthews, "A Progressive Damage Model for Mechanically Fastened Joints in Composite Laminates," *Journal of Composite Materials*, vol. 33, pp. 2248-2280, 1999.
- [53] F. K. Chang and K. Y. Chang, "Post-Failure Analysis of Bolted Composite Joints in Tension or Shear-Out Mode Failure," *Journal of Composite Materials*, vol. 21, pp. 809-833, 1987.
- [54] F. K. Chang and K. Y. Chang, "A Progressive Damage Model for Laminated Composites Containing Stress Concentrations," *Journal of Composite Materials*, vol. 21, pp. 834-855, 1987.
- [55] I. Lapczyk and J. A. Hurtado, "Progressive Damage Modeling in Fiber-Reinforced Materials," *Composites: Part A*, vol. 38, pp. 2333-2341, 2007.
- [56] P. P. Camanho, C. G. Dávila and M. F. de Moura, "Numerical Simulation of Mixed-Mode Progressive Delamination in Composite Materials," *Journal of Composite Materials*, vol. 37, pp. 1415-1438, 2003.

- [57] Z. P. Bazant and B. H. Oh, "Crack Band Theory for Fracture of Concrete," *Materials and Structures*, vol. 16, pp. 155-177, 1983.
- [58] E. J. Barbero, F. A. Cosso, R. Roman and T. L. Weadon, "Determination of Material Parameters for Abaqus Progressive Damage Analysis of E-Glass Epoxy Laminates," *Composites: Part B*, vol. 46, pp. 211-220, 2013.
- [59] P. Maimí, P. P. Camanho, J. A. Mayugo and C. G. Dávila, "A Continuum Damage Model for Composite Laminates: Part I - Constitutive Model," *Mechanics of Materials*, vol. 39, pp. 897-908, 2007.
- [60] P. Maimí, P. P. Camanho, J. A. Mayugo and C. G. Dávila, "A Continuum Damage Model for Composite Laminates: Part II - Computational Implementation and Validation," *Mechanics of Materials*, vol. 39, pp. 909-919, 2007.
- [61] C. A. Rose, C. G. Dávila and F. A. Leone, "Analysis Methods for Progressive Damage of Composite Structures," NASA/TM-2013-218024, National Aeronautics and Space Administration, U.S.A., 2013.
- [62] *ABAQUS version 6.14 User's Manual*, Dassault Systemes, 2014.
- [63] G. Alfano and M. A. Crisfield, "Finite Element Interface Models for the Delamination Analysis of Laminated Composites: Mechanical and Computational Issues," *International Journal for Numerical Methods in Engineering*, vol. 50, pp. 1701-1736, 2001.
- [64] M. L. Benzeggagh and M. Kenane, "Measurement of Mixed-Mode Delamination Fracture Toughness of Unidirectional Glass/Epoxy Composites with Mixed-Mode Bending Apparatus," *Composites Science and Technology*, vol. 56, pp. 439-449, 1996.
- [65] G. C. Eckold, D. Leadbetter, P. D. Soden and P. R. Griggs, "Lamination Theory in the Prediction of Failure Envelopes for Filament Wound Materials Subjected to Biaxial Loading," *Composites*, vol. 9, pp. 243-246, 1978.
- [66] L. A. L. Martins, F. L. Bastian and T. A. Netto, "Structural and Functional Failure Pressure of Filament Wound Composite Tubes," *Materials and Design*, vol. 36, pp. 779-787, 2012.
- [67] L. A. L. Martins, F. L. Bastian and T. A. Netto, "Reviewing Some Design Issues for Filament Wound Composite Tubes," *Materials and Design*, vol. 55, pp. 242-249, 2014.

- [68] M. L. Ribeiro, T. Martins, R. A. Angélico, D. Vandepitte and V. Tita, "Progressive Failure Analysis of Low Energy Impact in Carbon Fiber Filament Winding Cylinders," in *10th World Congress on Computational Mechanics*, vol. 1, São Paulo, Brazil, 2012, pp. 682-701.
- [69] M. L. Ribeiro, V. Tita and D. Vandepitte, "A New Damage Model for Composite Laminates," *Composite Structures*, vol. 94, pp. 635-342, 2012.
- [70] D. E. Rodriguez and O. O. Ochoa, "Flexural Response of Spoolable Composite Tubulars: An Integrated Experimental and Computational Assessment," *Composites Science and Technology*, vol. 64, pp. 2075-2088, 2004.
- [71] N. P. Vedvik and C.-G. Gustafson, "Analysis of Thick Walled Composite Pipes with Metal Liner Subjected to Simultaneous Matrix Cracking and Plastic Flow," *Composites Science and Technology*, vol. 68, pp. 2705-2716, 2008.
- [72] ASTM International, *Standard Test Method for Tensile Properties of Polymer Matrix Composite Materials*, ASTM Standard D3039/D3039M, 2014.
- [73] ASTM International, *Standard Test Method for Flexural Properties of Polymer Matrix Composite Materials*, ASTM Standard D7264/D7264M, 2015.
- [74] ASTM International, *Standard Test Method for Compressive Properties of Rigid Plastics*, ASTM Standard D695, 2010.
- [75] ASTM International, *Standard Test Method for Determination of External Loading Characteristics of Plastic Pipe by Parallel-Plate Loading*, ASTM Standard D2412, 2011.
- [76] G. Rathnaweera, Y. Durandet, D. Ruan and S. Kinoshita, "Characterizing the Material Properties of a Tube from a Lateral Compression Test," *International Journal of Protective Structures*, vol. 2, pp. 465-476, 2011.
- [77] ANSYS, Inc., *ANSYS Release 15.0, Help System, Mechanical APDL Theory Reference*, 2013.
- [78] E. Totry, J. M. Molina-Aldareguía, C. González and J. Llorca, "Effect of Fiber, Matrix and Interface Properties on the In-Plane Shear Deformation of Carbon-Fiber Reinforced Composites," *Composite Science and Technology*, vol. 70, pp. 970-980, 2010.
- [79] P. Moy and F. E. Karasz, "Epoxy-Water Interactions," *Polymer Engineering and Science*, vol. 20, pp. 315-319, 1980.

- [80] R. A. Pethrick, E. A. Hollins, I. McEwan, E. A. Pollock, D. Hayward and P. Johncock, "Effect of Cure Temperature on the Structure and Water Absorption of Epoxy/Amine Thermosets," *Polymer International*, vol. 39, pp. 275-288, 1996.
- [81] P. Nogueira, C. Ramírez, A. Torres, M. J. Abad, J. Cano, J. López, I. López-Bueno and L. Barral, "Effect of Water Sorption on the Structure and Mechanical Properties of an Epoxy Resin System," *Journal of Applied Polymer Science*, vol. 80, pp. 71-80, 2001.
- [82] D. W. Van Krevelen and K. Te Nijenhuis, *Properties of Polymers: Their Correlation with Chemical Structure; Their Numerical Estimation and Prediction from Additive Group Contributions*, 4th Edition, Amsterdam, The Netherlands: Elsevier, 2009.
- [83] E. E. Gdoutos, *Fracture Mechanics*, Netherlands: Springer, 1993.
- [84] Y. Zhong and S. C. Joshi, "Initiation of Structural Defects in Carbon Fiber Reinforced Composites under Hygrothermal Environments," *Journal of Composite Materials*, vol. 50, pp. 1085-1097, 2015.
- [85] Y. Zhong, "Structural Integrity of Reinforced Polymer Matrix Composites Subjected to Hygrothermal Conditioning," PhD Thesis, Nanyang Technological University, Singapore, 2014.
- [86] L. Barral, C. J. López, P. Nogueira, C. Ramírez and M. J. Abad, "Water Sorption in Tetrafunctional Phenol Novolac Epoxy Mixtures Cured with a Diamine," *Journal of Thermal Analysis*, vol. 52, pp. 823-830, 1998.
- [87] R. Selzer and K. Friedrich, "Influence of Water Up-Take on Interlaminar Fracture Properties of Carbon Fiber-Reinforced Polymer Composites," *Journal of Materials Science*, vol. 30, pp. 334-338, 1995.
- [88] B. C. Ray and D. Rathore, "Environmental Damage and Degradation of FRP composites: A Review Report," *Polymer Composites*, vol. 36, pp. 410-423, 2015.
- [89] A. Zafar, F. Bertocco, J. Schjødt-Thomsen and J. C. Rauhe, "Investigation of the Long Term Effects of Moisture on Carbon Fibre and Epoxy Matrix Composites," *Composites Science and Technology*, vol. 72, pp. 656-666, 2012.
- [90] W. J. Mikols, J. C. Seferis, A. Apicella and L. Nicolais, "Evaluation of Structural Changes in Epoxy Systems by Moisture Sorption-Desorption and Dynamic Mechanical Studies," *Polymer Composites*, vol. 3, pp. 118-124, 1982.

- [91] H. S. Choi, K. J. Ahn, J.-D. Nam and H. J. Chun, "Hygroscopic Aspects of Epoxy/Carbon Fiber Composite Laminates in Aircraft Environments," *Composites: Part A*, vol. 32, pp. 709-720, 2001.
- [92] ASTM International, *Standard Test Method for Moisture Absorption Properties and Equilibrium Conditioning of Polymer Matrix Composite Materials*, ASTM Standard D5229/D5229M, 2014.
- [93] P. Molitor, V. Barron and T. Young, "Surface Treatment of Titanium for Adhesive Bonding to Polymer Composites: A Review," *International Journal of Adhesion & Adhesives*, vol. 21, pp. 129-136, 2001.
- [94] B. J. Ho, J. K.-T. Tsoi, D. Liu, C. Y.-K. Lung, H.-M. Wong and J. P. Matinlinna, "Effects of Sandblasting Distance and Angles on Resin Cement Bonding to Zirconia and Titanium," *International Journal of Adhesion & Adhesives*, vol. 62, pp. 25-31, 2015.
- [95] Y. Xu, H. Li, Y. Shen, S. Liu, W. Wang and J. Tao, "Improvement of Adhesion Performance between Aluminum Alloy Sheet and Epoxy Based on Anodizing Technique," *International Journal of Adhesion & Adhesives*, vol. 70, pp. 74-80, 2016.
- [96] P. He, K. Chen and J. Yang, "Surface Modifications of Ti Alloy with Tunable Hierarchical Structures and Chemistry for Improved Metal-Polymer Interface Used in Deepwater Composite Riser," *Applied Surface Science*, vol. 328, pp. 614-622, 2015.
- [97] W. Liu, Y. Huan, J. Dong, Y. Dai and D. Lan, "A Correction Method of Elastic Modulus in Compression Tests for Linear Hardening Materials," *MRS Communications*, vol. 5, pp. 641-645, 2015.
- [98] W. K. Kim, "Composite Production Riser Assessment," Thesis, Texas A&M University, 2007.

Appendices

Appendix A. Composite Material Model in the Fiber Direction

For the composite material model used in the current study, the material behavior in the fiber direction is the same as the model presented by Lapczyk and Hurtado [55].

The damage initiation is determined using the Hashin failure criteria:

Fiber tension ($\sigma_{11} \geq 0$):

$$FI_{ft} = \left(\frac{\sigma_{11}}{X_T}\right)^2 + \alpha \left(\frac{\sigma_{12}}{S_L}\right)^2$$

Fiber compression ($\sigma_{11} < 0$):

$$FI_{fc} = \left(\frac{\sigma_{11}}{X_C}\right)^2$$

For the current study, α was set to 1.

The equivalent displacements and stresses for the fiber failure modes are given by:

Fiber tension ($\sigma_{11} \geq 0$):

$$\delta_{ft,eq} = L_c \sqrt{\langle \varepsilon_{11} \rangle^2 + \alpha \gamma_{12}^2}$$

$$\sigma_{ft,eq} = \frac{L_c (\langle \sigma_{11} \rangle \langle \varepsilon_{11} \rangle + \alpha \sigma_{12} \gamma_{12})}{\delta_{ft,eq}}$$

Fiber compression ($\sigma_{11} < 0$):

$$\delta_{fc,eq} = L_c \sqrt{\langle -\varepsilon_{11} \rangle^2}$$

$$\sigma_{fc,eq} = \frac{L_c \langle -\sigma_{11} \rangle \langle -\varepsilon_{11} \rangle}{\delta_{fc,eq}}$$

The damage parameter for fiber failure is determined from the equivalent displacements:

$$d_I = \frac{\delta_{I,eq}^f (\delta_{I,eq} - \delta_{I,eq}^0)}{\delta_{I,eq} (\delta_{I,eq}^f - \delta_{I,eq}^0)} ; I \in \{ft,fc\}$$

The equivalent displacements at the onset of damage ($\delta_{I,eq}^0$) and at complete failure ($\delta_{I,eq}^f$) can be computed using:

$$\delta_{I,eq}^f = \frac{2G_{I,C}}{\sigma_{I,eq}^0}$$

$$\delta_{I,eq}^0 = \delta_{I,eq} f_I^{sc}$$

where $\sigma_{I,eq}^0$ is the equivalent stress at the onset of damage given by

$$\sigma_{I,eq}^0 = \sigma_{I,eq} f_I^{sc}$$

and f_I^{sc} is a scaling function given by:

$$f_I^{sc} = \frac{1}{\sqrt{FI_I}}$$

Appendix B. IM2A Carbon Fiber Technical Data Sheet

HexTow® IM2A carbon fiber is a continuous, high performance, intermediate modulus, PAN based fiber available in 12,000 (12K) filament count tows. This fiber has been surface treated and can be sized to improve its interlaminar shear properties, handling characteristics, and structural properties. It is suggested for use in prepregging, filament winding, braiding, and pultrusion.

The unique properties of HexTow® IM2A fiber, such as higher tensile strength and modulus, as well as good shear strength, allow structural designers to achieve both higher safety margins for both stiffness and strength critical applications.

Typical Fiber Properties	U.S. Units	SI Units
Tensile Strength 12K	770 ksi	5,310 MPa
Tensile Modulus (Chord 6000-1000)	40 Msi	276 GPa
Ultimate Elongation at Failure 12K	1.7%	1.7%
Density	0.0643 lb/in ³	1.78 g/cm ³
Weight/Length 12K	25.0 x 10 ⁻⁶ lb/in	0.446 g/m
Approximate Yield 12K	3,337 ft/lb	2.24 m/g
Tow Cross-Sectional Area 12K	3.89 x 10 ⁻⁴ in ²	0.25 mm ²
Filament Diameter	0.203 mil	5.2 microns
Carbon Content	95.0%	95.0%
Twist	Never Twisted	Never Twisted

Carbon Fiber Certification

This carbon fiber is manufactured to Hexcel industrial specification HS-CP-3000. A copy of this specification is available upon request. A Certification of Conformance will be provided with each shipment.

Available Sizing

Sizing compatible with various resin systems, based on application are available to improve handling characteristics and structural properties. Please see additional information on available Sizes on our website or contact our technical team for additional information.

©Copyright Hexcel Corporation

*HexTow, Hexcel and the Hexcel logos are registered trademarks of Hexcel Corporation, Stamford, Connecticut.

Packaging

Standard packaging of HexTow® IM2A is as follows:

Filament Count	Nominal Weight		Nominal Length	
	(lb)	(kg)	(ft)	(m)
12K	4.0	1.8	13,350	4,070

Other package sizes may be available on request. The fiber is wound on a 3-inch ID by 11-inch long cardboard tube and overwrapped with plastic film.

Safety Information

Obtain, read, and understand the Material Safety Data Sheet (MSDS) before use of this product.

Important

Hexcel Corporation believes, in good faith, that the technical data and other information provided herein is materially accurate as of the date this document is prepared. Hexcel reserves the right to modify such information at any time. The performance values in this data sheet are considered representative but do not and should not constitute specification minima. The only obligations of Hexcel, including warranties, if any, will be set forth in a contract signed by Hexcel or in Hexcel's then current standard Terms and Conditions of Sale as set forth on the back of Hexcel's Order Acknowledgement.

For more information

Hexcel is a leading worldwide supplier of composite materials to aerospace and other demanding industries. Our comprehensive product range includes:

- Carbon Fiber
- RTM Materials
- Honeycomb Cores
- Carbon, Glass, Aramid and Hybrid Prepregs
- Structural Film Adhesives
- Honeycomb Sandwich Panels
- Special Process Honeycombs
- Reinforced Fabrics

For US quotes, orders and product information call toll-free 1-866-556-2662 and 1-800-987-0658.

For other worldwide sales office telephone numbers and a full address list, please click here:

<http://www.hexcel.com/contact/salesoffice>

Copyright © 2013 – Hexcel – All Rights Reserved.

March 2013

Appendix C. Epolam 5015/5015 Epoxy System Technical Data Sheet

Epolam 5015 Resin

Technical Data Sheet

Epolam 5014 – 5015 – 5016 Hardeners

Laminating system for resin infusion

Tg 176°F (80°C) – Gel times 45 minutes to 4 hours

Description

Epolam 5015 resin is a low-viscosity system designed for resin infusion and offered with a choice of three different speed hardeners. This allows the selection of a pot life suitable to the size of the part being produced.

Applications

- Production of composite parts by resin infusion methods
- Suitable for vacuum-bagging, RTM and filament winding

Properties

- Lloyd's Register of London approved for the construction of marine vessels
- Very low viscosity
- Readily wets out fabrics
- Good mechanical properties
- Good wetting of core materials such as balsa wood and foam
- Suitable for marine applications

Physical Properties					
		Epolam 5015 Resin	Epolam 5014 Hardener	Epolam 5015 Hardener	Epolam 5016 Hardener
Composition		Epoxy	Amine	Amine	Amine
Mix Ratio, by weight		100	34	30	36
Appearance		Liquid	Liquid	Liquid	Liquid
Color		Light amber	Light amber	Colorless	Light amber
Viscosity @ 77°F (25°C) mPa.s	Brookfield LVT	800	20	12	20
Density @ 77°F (25°C) (g/cc)	ISO 1675:1985	1.15	0.94	0.94	0.94
Viscosity, mixed @ 77°F (25°C) mPa.s	Brookfield LVT		225	210	225
Pot life, 500g at 77°F (25°C)			45 minutes	135 minutes	225 minutes
Cured density @ 74°F (23°C) (g/cm ³)	ISO 2781: 1985		1.12	1.10	1.12

PROCESSING

Caution: EPOLAM 5015 system must be cured (2 hrs at 122°F (50°C) at least) to be demolded.

To obtain the desired temperature resistance and the optimal mechanical properties it is necessary to postcure the EPOLAM 5015 system. This step takes place 24 to 48 hours after application depending on the hardener. In order to avoid any distortion risks it is recommended to support the part on a frame before curing.

To allow a good beginning of polymerization of EPOLAM 5015/5016 system it is recommended to work at 68°F (20°C) minimum.

For any further information concerning the Resin Infusion Method and ancillary products provided by Axson, please contact our Technical Support Department.

Page 1/3- February 3, 2008, US Rev 02

AXSON NA USA
1611 Hulst Drive
Eaton Rapids, MI
48827
Tel. (517) 663-8191
Fax (517) 663-0523
Email : info@axson-na.com

AXSON GmbH
Dietzenbach
Tel. (++49) 60 74 407110

AXSON Italie
Saronno
Tel. (++39) 02 96 70 23 36

AXSON IBERICA
Barcelona
Tel. (++34) 93 225 16 20

AXSON UK Limited
Newmarket
Tel. (++44) 1638 660 062

AXSON BRASIL
Sao Paulo
Tel. (++55) 11 5687 7331

AXSON MEXICO
Mexico DF
Tel. (++52) 55 5264 49 22

AXSON SHANGHAI
Shanghai
Tel. (++86)-58 68 30 37

AXSON France
Cergy
Tel. (++33) 1 34 40 34 60
Email : axson@axson.fr

AXSON JAPAN
OKAZAKI CITY
Tel. (++81) 564 26 2591

Cured Properties at 74°F (23°F) ¹					
Epolam 5015 Resin with			Epolam 5014 Hardener	Epolam 5015 Hardener	Epolam 5016 Hardener
Glass Transition Temperature (Tg) • 7 days at room temperature • 16 hours at 122°F (50°C) • 16 hours at 176°F (80°C)	D.S.C. - Mettler	°F (°C)	106 (41) 160 (71) 176 (80)	104 (40) 158 (70) 180 (82)	108 (42) 140 (60) 178 (81)
Hardness	ASTM D-2240	Shore D	85	85	85
Tensile Strength	ASTM D638	psi (MPa)	10,100 (70)	11,600 (80)	10,100 (70)
Elongation at break	ASTM D638	%	7	6	8
Flexural Strength	ASTM D790	psi (MPa)	14,500 (100)	15,200 (105)	13,800 (95)
Flexural Modulus	ASTM D790	psi (MPa)	421,000 (2,900)	435,000 (3,000)	406,000 (2,800)

¹ Average values on laboratory prepared test samples of neat (unreinforced) resin, 24 hours at room temperature plus 16 hours at 176°F (80°C)

Storage Conditions

This product has a shelf life of 24 months as indicated by the expiration date on the container when stored in original unopened containers between 59 – 77°F (15 – 25°C). Any opened can must be tightly closed.

Handling Precautions

Normal health and safety precautions should be observed when handling these products :

- Ensure good ventilation
- Wear gloves, and safety glasses

For further information, please consult the material safety data sheet.

Guarantee

The information contained in this technical data sheet results from research and tests conducted in our Laboratories under precise conditions. It is the responsibility of the user to determine the suitability of AXSON products, under their own conditions before commencing with the proposed application. AXSON guarantees the conformity of their products with their specifications but cannot guarantee the compatibility of a product with any particular application. AXSON disclaims all responsibility for damage from any incident which results from the use of these products. The responsibility of AXSON is strictly limited to reimbursement or replacement of products which do not comply with the published specifications.

Isothermal viscosity profile: 500 g mass, 77°F (25°C) initial material temperature, ambient temperature 70°F (21°C)