EXPERIMENTAL METHODS FOR UNDERSTANDING THE PERFORMANCE OF IMPULSIVELY LOADED CROSS-LAMINATED TIMBER PANELS

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Date Approved: August 12, 2022
Beyond the walls of intelligence, life is defined.

_Nas_
To everyone that was ever told they could not do something or be someone.
ACKNOWLEDGMENTS

For five years I have been lucky to have had the opportunity to break things that should and, less often, should not be broken. These things were often broken to learn more about their behavior and so that hopefully the world may know little more about them too. For this opportunity I am grateful to Dr. Russell Gentry and Dr. Lauren Stewart for giving me a chance all those years ago, always being supportive, and lending me their expertise.

Dr. Lauren Stewart, I could not have ask for a better advisor and your mentorship has been an invaluable asset in completing this program. While research has always been interesting and exciting to me, the concept of pursuing a doctoral degree seemed foreign and I was often discouraged. I’m glad that I ignored those doubts and pursued my passion anyhow. Dr. Lauren Stewart’s enthusiasm made me excited to learn and to learn to become a researcher. She is flexible and adaptable in her mentorship style, she is unwavering in her advocacy for students, and she is a talented engineer. I am glad to have learned so much from you. Thank you.

Dr. Russell Gentry your sense of humor, excellent teaching skills, practicality in implementing experiments, and enthusiasm have been a great help and inspiration throughout my Ph.D. and will continue to be a resource beyond.

I would also like to thank my thesis committee members without whom, this research would not have been possible: Dr. Laurence Jacobs, Dr. Lawrence Kahn, and Dr. Fred Meyer. Thank you for your guidance, feedback, and mentorship. You truly helped to elevate this research.

Experimental research is a large undertaking, requiring many man-hours, materials, and skilled instruction, and it would not have been possible to complete my research without the help of many people. First, I would be remiss if I didn’t thank Jeremy Mitchell, the Structural Engineering and Material Laboratory facilities manager. Jeremy, you helped make sure we were safe, made setting up tests and testing easier and less stressful, and taught
me a few things along the way. Thank you Andy Udell, the Mason Building and Machine Shop facilities manager, for fabricating parts of my testing fixture, providing your practical knowledge to my designs, and letting me work on developing my skills and confidence machining parts. Jeremy Stephens, you were a huge help in completing, assembling, and installing my test fixture and I learned a great deal from you. Much of my knowledge of instrumentation and running experiments started with instruction from Dr. Nan Gao. Nan was always cool during stressful times and always seemed to know exactly what to do even in the most challenging scenarios - I am happy to have worked with you and you are truly an inspiration. Dr. Giovanni Loreto was another great resource in the lab - thank you for always volunteering your time and for always sharing your humor. I would like to thank my graduate student peers for your advice, help, and friendship: Diana Estrada, Rebecca Nylen, Brian Riser, and Maria Warren. Thank you to my fellow students for all of your hardwork and spending countless hours in the heat and cold of the lab helping me fabricate, test, troubleshoot, and lift heavy things: Ammar Alshannaq, Mehmet Bermek, Griffin Fish, Jonathan Johnson, Hanna Kessler, Lixrine Ngeme, Gino Pagliaro, John Respert, Alexander Rice, Larissa Simoes Novelino, Juliet Swinea, Miranda Tan, Thomas Vandiver, and Isaac Wasson.

My family also played a large role in getting me to this point. I would like to thank my parents who each instilled a sense of hard work into me. My parents made many sacrifices, often giving me things that they did not have the means to, in order to ensure that I received a good education and became an accomplished individual. One of my father’s great ambitions is that each of his children receive a great education, something he did not have access to, as he feels that educated people often contribute to a better world. My in-laws, Seth and Sue, also deserve praise - thank you for your amazing support and confidence in me. Thank you to each of my sisters for your love and humor throughout this journey. A big shout out to my friends back home and elsewhere - Jamil, Jay, Miguel, Polanco, Rene, Robbie - you’re my family and your friendship means the world to me.
Finally and most importantly, I would like to thank my partner, Sara. Sara has often been my advocate, my source of courage, my objective truth bearer, and much more. Sara wrangled our son, Barry, listened to my countless presentation rehearsals, and worked her magic editing my work. She was the one who encouraged me to embark on this path. Without Sara, I would not have begun this endeavor and without her continued support I would not have stood a chance at completing it. I’m excited for the future challenges we will overcome, adventures we will embark upon, and the many, many more years of chasing Barry around.
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SUMMARY

Cross-laminated timber (CLT) is an innovative multi-layered engineered wood product with proven performance as a structural material in extreme events, including earthquake, wind, and fire. Although research is limited, CLT has shown great potential for application in the force protection of structures. This research bridges the gap between the quasi-static and intermediate strain rate loading regimes by investigating two areas that have remained unstudied or elusive, i.e., rolling shear failure of CLT under impulsive, blast-like loading and intermediate strain rates in CLT. A novel center-point testing system and methodology was developed that permits the application of impulsive loading in a highly controlled and repeatable manner. The testing system is highly adaptable and is capable of testing a variety of materials of variable widths, lengths, and thicknesses. The impactor is interchangeable to permit changes to the load condition. Realistic boundary conditions can be simulated empirically via changes to the boundary condition rotational rigidity. The testing system was validated and calibrated through a series of validation tests, finite element simulations, and via the development of a new experimental method. The Direct Force Method (DFM) is a new experimental method for empirically determining the force history applied to a specimen that controls for inertial effects that arise during testing. Experiments featured multiple test phases: quasi-static testing of undamaged CLT specimens, impulsive testing of undamaged CLT specimens, and residual capacity testing of damaged CLT specimens. Low span-to-depth ratio CLT specimens are used throughout testing to encourage the development of shear modes of failure. As verified by the test results, the testing system consistently produced shear modes of failure and facilitated the observation of CLT panel behavior under impulsive loading. The testing programs validated several hypotheses including the conditions that elicit shear modes of failure, strain-rate enhancement of CLT mechanical properties in the impulsive loading regime, and the role of boundary condition rigidity in affecting change in CLT panel behavior. The conclusions made on CLT
panel behavior under impulsive loading and CLT panel residual capacity and survivability provide validation for CLT’s implementation as a structural material in force protection applications.
CHAPTER 1
INTRODUCTION

1.1 Problem Statement

Temporary military structures commonly employed in active theaters of operation provide austere living conditions for military personnel, and present increased risk and vulnerability to their inhabitants and to the supply lines that serve them. While base camps have published standards and guiding principles for their design and construction, often execution of these requirements is difficult given the need of base camps to respond to "dynamic mission requirements, unknown status of base camp end states, and ever changing theater policies and guidelines" [1]. This is compounded by varying levels of construction or engineering design experience, man power, and equipment available to the personnel at a base camp. These resources are termed as organic construction capability by the U.S. Army and Marine Corps. Per the Army Technical Publication, ATP 3-37.10 [2], units have varying levels of organic construction capabilities, which often determines the standard to which structures may be constructed. The Army uses construction standards of initial, temporary, and semipermanent based on the expected design life of a structure.

The barracks hut (B-Hut) is the most commonly used temporary structure design in theater, as classified by the Unified Facilities Criteria (UFC) 01-201-01 [3]. The Army uses B-Huts in theater for a variety of applications including barracks, dormitory, laundry, dining halls, administrative offices, and maintenance shops [4, 5]. B-Huts are typically 16-ft. (4.88 m) wide x 32-ft. (9.75 m) long x 8-ft. (2.44 m) high and are constructed of simple wood frame floors and walls, and wood trusses to support the roof. Plywood is used for sheathing and corrugated metal or composite shingles for roofing material. B-Huts typically lack building wrapping or insulation as these materials often present logistical
challenges to transport [4]. As levels of organic construction capability vary by military unit, construction errors often manifest in the improper enclosing of the building envelope. An environmental control unit may be added as an option, however, their use requires large amounts of energy given the lack of insulation and poor sealing of B-Huts. B-Huts are typically constructed by military personnel or workers contracted locally in the host nation, which can add further complications [6]. Given the complexity and large number of components in the current B-Hut design, construction of B-Huts can require 24 to 30 man hours to complete [4, 7]. Alternatives to the traditional wooden construction B-Hut includes the use of CMU, structural insulated panels (SIP), and 3-D printed buildings. While each of these alternatives include positive properties when contrasted to the current wooden design, each alternative has significant drawbacks that make them non-ideal or impractical. For example, significant weight and volume occupied by materials like concrete, masonry, and SIPs are problematic for transportation logistics. SIP structures in particular present additional challenges in the form of combustibility, emission of volatile organic compounds, and requirement of force protection.

The inherent blast resistance of B-Huts is poor and requires that antiterrorism standards be implemented to minimize mass casualties, progressive collapse, and hazardous flying debris [5, 8]. Two sets of initiatives, Blast and Injury Tests (BAITS) and Retrofits and Overpressure Design of Structures (RODS), conducted by the U.S. Air Force demonstrated these vulnerabilities through a series of large scale live blast tests on Southeast Asia Huts (SEA Hut) and other expeditionary structures [9, 10]. Blast loading is often mitigated through the provision of minimum structure separations and large standoff distances, 50-ft. (16m) to excesses of 657-ft. (200m), per UFC 4-010-01 [5]. As B-Huts are often used in forward operating locations, additional mitigation for anti-terrorism and force protection is necessary [8]. The ATP 3-37.10 outlines hardening strategies for base camp structures including the ad hoc provision of sidewall protection, overhead cover, or a combination thereof, Figure 1.1
Sidewall protection involves the construction of simple, granular soil filled walls that are designed to arrest fragments and reduce the effects of blast from near-miss impacts. Overhead cover involves the construction of a supporting structure and a predetonation layer, such as sandbags, to shield against indirect fire, fragmentation, and direct fire delivered from elevated positions. As the overhead cover provided by sandbags can weigh up to 4,000 pounds [2], the implementation of these hardening strategies is often cumbersome and requires skilled engineering. As such, hardening may not always be possible or may be prohibitively expensive [8]. Additionally, while various hardening strategies and protective walls or barriers are possible, these are not always capable of reducing the effects of blasts (e.g., reflected pressures) resulting from threats like large vehicular bombs to acceptable levels for conventional and expeditionary structures like the B-Hut [9]. As highlighted, B-Huts in their existing design are fairly energy inefficient, require significant man-hours to construct, lack resistance to weathering [4, 11], and lack inherent protection against extreme loading such as blast and ballistic threats.

An opportunity exists to address these deficiencies by implementing cross-laminated timber (CLT), a mass timber material with inherent resistance to extreme loading and fire,
opportunities for logistical and construction optimization, and high thermal performance.

CLT is a novel multi-layer engineered wood product that has been exhibiting increased popularity and demand in North America. CLT has high in-plane and out-of-plane strength and stiffness and low weight relative to these properties. CLT has proven performance in extreme events such as earthquake, wind, and fire where well designed CLT buildings have performed well primarily due to the high mass of CLT and its ability to provide lateral force resistance [12, 13, 14]. CLT has been shown to be a suitable material for force protection and has been shown to provide resistance against ballistic and blast threats [15]. Additionally, CLT complements rapid project completion via modular construction and CLT is a good insulator for both sound and temperature [12, 16]. CLT promotes sustainable construction primarily due to its composition of wood, which offsets and sequesters carbon from polluting industrial processes [12, 17]. Moreover, given the layered construction of CLT, a mix of higher and lower quality timbers can be utilized in the panel to balance the global mechanical properties of the panel. This feature, i.e., the utilization of lower quality wood, gives economic value to wood that would otherwise be economically infeasible to extract thereby, increasing the overall health of forests and removing potential fuel for forest fires. Despite the benefits of CLT as a building material, CLT still faces some challenges in its adoption by engineers, contractors, the military, and the public. The use of wood and engineered wood products has traditionally provided a low cost alternative to more traditional building materials such as steel, aluminum, and concrete, thus wood is not viewed as a "heavy construction system" [18]. With regard to incorporation of CLT into military standards, CLT performance in force protection applications is still in its nascent stages of research. Weaver et al. note this deficiency in their research by highlighting a lack of guidance for CLT in the UFC 4-010-01: DoD Minimum Antiterrorism Standards for Buildings [8], requiring structural analysis for each unique building and site [19].

The primary motivation for the research reported herein is to accelerate the adoption of CLT as a robust building material capable of being implemented in force protection
applications. This research seeks to characterize the mechanical behavior of CLT subjected to impulsive, blast-like loading and to understand how the residual capacity of CLT is affected by this loading. Additionally, this research seeks to add to the understanding of boundary condition rigidity and its effect on CLT performance when subjected to impulsive loading.

A secondary motivation involves increasing life safety in civil structures subjected to impulsive loads. Civil structures, as compared to military structures, typically do not have resistance to impulsive loading. Yet, as Jacques et al. [20] highlight, many residential and commercial structures are subject to unacceptable levels of risk simply by being located within the vicinity of high profile structures. Impulsive loads in civil structures are caused by the rapid onset of a load such as a blast or impact loading (e.g., truck crash or construction accident) and can lead to the partial or complete collapse of a structure. The magnitude of impulsive loading caused by blast or impact is often great and may result in the loss of structural members locally, thus requiring redistribution of loading to alternate load paths. When this redistribution is not possible and local member resistance is exceeded, members fail in concert and progressive collapse occurs. Higher incidences of terrorism or accidental explosions expose civil structures to a higher risk of extreme impulsive loads. Events like the 1968 Ronan Point collapse in East London, the collapse of the Alfred P. Murrah building in Oklahoma City in 1995, and the recent 2020 explosion in Beirut, Lebanon highlight the need to incorporate measures to prevent progressive collapse in civil structures. A material like CLT, which inherently provides some level of resistance to impulsive loading may have helped to limit the extent of the destruction and preserve lives in these instances. CLT’s ability to provide impulsive loading resistance will be evaluated by studying its behavior and residual strength following impulsive loading.

1.2 Thesis Outline

This thesis is organized as follows:
1. **Chapter 1: Introduction**

   This first chapter provides the background and motivation for the research and the problem statement that the research aims to answer. This chapter also provides an outline to the Thesis document.

2. **Chapter 2: Literature Review**

   Chapter 2 analyzes published literature that pertains to wood, CLT, high strain rate loading, experimental high-rate loading techniques, and other applicable subjects.

3. **Chapter 3: Research Questions and Objectives**

   Chapter 3 presents research questions to be answered by this research. This chapter will also present the objectives for this research.

4. **Chapter 4: Experimental Testing of CLT Under Quasi-Static Center-Point Loading**

   Chapter 4 presents and discusses the process for designing a quasi-static center-point experimental testing program. Testing materials and methods, results, and conclusions are discussed in this chapter.

5. **Chapter 5: Design of an Impulsive Center-Point Testing System with Realistic Boundary Conditions**

   Chapter 5 discusses the process for designing an impulsive center-point flexural testing system from conception to design, fabrication, and execution. This chapter presents the rationale and goals for the system as well as the testing procedure for the impulsive tests. An experimental method for directly measuring force during impulsive loading and a set of validation tests is also discussed.

6. **Chapter 6: Behavior of Impulsively Loaded Cross-Laminated Timber Panels**
Chapter 6 describes the application the impulsive center-point testing system to the testing of low span-to-depth ratio CLT specimens. The analysis of CLT behavior resulting from impulsive loading and realistic boundary conditions is conducted and discussed. Characterization of damage is conducted using an experimental method described in Chapter 5 and other tools.


Chapter 7 discusses the implementation of an experimental method conduct insitu, quasi-static testing of CLT specimens that were damaged by impulsive loading. Details on the completion of the testing series are presented and analysis of the test results are discussed.

8. Chapter 8: Conclusions and Recommendations for Future Research

Chapter 8 summarizes major contributions and conclusions derived from this research. Recommendations for improvements to the experiments conducted are offered and recommendations for future research are proposed.
CHAPTER 2
LITERATURE REVIEW

2.1 Wood as a Structural Material

Wood is one of the oldest and most widely used building materials. There is evidence of wood being used for fuel, tools, weapons, recreation, and structures since prehistoric times [21, 22]. Wood has numerous benefits as a building material ranging from ease of use, a wide range of useful applications, efficiency, and durability. Wood is a sustainable product with forests capable of providing wood indefinitely when properly managed [23]. Additionally, forests sequester carbon and wood products continue to store this carbon until the wood is burned or deteriorates. When compared with conventional building materials like concrete or steel, wood results in $\frac{1}{21}$ times and $\frac{1}{8}$ times less carbon by weight than steel and concrete production, respectively [23].

Trees are divided into two classes, coniferous and deciduous, commonly referred to as softwood and hardwood, respectively. The commonly used nomenclature for these classes is misnomer as there are trees in each class with varying levels of hardness such that hardwoods are not necessarily harder than softwoods and vice versa. The class hardwood, or deciduous, refers to tree species that are angiosperms (i.e., producing flowers), typically lose their leaves in autumn or winter, and have broad leaves. The class softwood, or coniferous, refers to tree species that are gymnosperms (i.e., conifers), produce cones, and have needle-like evergreen leaves. Coniferous trees tend to be nonporous where deciduous trees tend to be porous as hardwoods contain vessel elements to transport water or sap within the tree [23].

This research focuses on the use of two tree species groups, i.e., Spruce-Pine-Fir-South (SPFs) and Southern Pine (SP), used in Cross-Laminated Timber. The Spruce-Pine-Fir-
South species group contains tree species such as Engelmann spruce (Picea engelmannii) and black spruce (Picea mariana) that are lightweight, easily worked, low to moderate strength and stiffness, low hardness, and low impact resistance [23]. Other tree species in the classification include red spruce (Picea rubens), Sitka spruce (Picea sitchensis), white spruce (Picea glauca), balsam fir (Abies balsamea), jack pine (Pinus banksiana), lodgepole pine (Pinus contorta), and red pine (Pinus resinosa). SPF is similar to Spruce-Pine-Fir (SPF) with the primary difference being growing location and grading agency rules. SPF is grown in the U.S. and subject to Northeastern Lumber Manufacturers Association (NELMA), West Coast Lumber Inspection Bureau (WCLIB), and Western Wood Products Association (WWPA) grading agency rules whereas SPF is grown in Canada and is subject to National Lumber Grades Authority (NLGA) grading agency rules. The Southern Pine group contains several tree species, approximately ten, of which four species comprise the majority of the inventory: longleaf pine (Pinus palustris), shortleaf pine (P. echinata), loblolly pine (P. taeda), and slash pine (P. elliottii) [23]. Longleaf and slash pine are dense, have high strength and stiffness, high hardness, and a moderately high shock resistance, while shortleaf and loblolly tend to be lighter weight woods [23]. The uses of Southern Pine are vast and extend from stringers in factories and warehouses; trusses, beams, and posts; crossties, piles, and timbers supporting mining operations; marine, bridge, and highway construction; and furniture and material handling applications [23, 24]. Southern Pine is a widely available, abundant, and renewable resource that grows in the Southern United States growing from East Texas to Virginia [24]. As of 2016, loblolly pine and slash pine were the most common tree species in the state of Georgia representing 33% and 31% of all-live trees on forest land, respectively [25]. Georgia has the most forested land area of any of the Southern States; it has considerable and stable forest resources with over 65% of the state being forested [26]. Southern Pine is subject to the U.S. Department of Commerce American Softwood Lumber Standard PS 20 and the Southern Pine Inspection Bureau grading and manufacturing rules.
2.1.1 Wood Physical Properties

Wood is a hygroscopic and complex multi-scale composite material [22, 27]. As a material that is derived from living organisms, wood’s microstructure greatly affects the macrostructural properties of wood, for example, flexural strength, tensile and compressive strength, hardness, porosity, and density [23, 27]. Wood is primarily composed of three polymers: cellulose, lignin, and hemicellulose with other materials (e.g., sugars, phenolic units, extractives, and other organic and inorganic compounds) making up less than 10% of wood’s structure. The cell walls are composed primarily of cellulose and hemicellulose which are held in a matrix by lignin. The arrangement of cellulose and hemicellulose is primarily linear within the cell with lignins forming branched, cross-linked networks. The resulting viscoelastic behavior is a result of a combination of each of the polymer responses [28]. The viscoelastic behavior of wood is highly affected by wood’s moisture content and temperature, load rate, and the stress level applied as a percentage of ultimate strength [28, 29]. The viscoelastic behavior of wood is largely negligible under short term and intermediate term load durations as instantaneous deformation dominates the response, while the time dependent deformations are negligible.

The specific gravity and moisture content of wood largely determine many of its mechanical and physical properties of wood. Specific gravity is considered "one of the most important" physical properties [23] as it determines fastener embedment, weight, modulus of elasticity, shear modulus, and mechanical strength. Cellulose and hemicellulose are largely hydrophilic while the lignin that bonds them is hydrophobic, which implies that lignin is the limiting factor for the cells ability to accept water [23]. The presence of water in wood has a profound influence on the mechanical and physical properties of wood, with the magnitude of mechanical properties being inversely proportional with changes in moisture content below the fiber saturation point. Above the fiber saturation point, physical and mechanical properties do not change as a function of moisture content [23]. The moisture exchange that occurs between wood and its surrounding environment is dependent on
the relative humidity and temperature of the air and the moisture content and fiber saturation point of wood. Wood is dimensionally stable when it is above the fiber saturation point (30%) and shrinks or swells based on its moisture content with respect to the fiber saturation point. Wood experiences the greatest dimensional change along the tangential direction, where dimensional changes are twice those of the radial direction [23]. Wood volume exhibits very slight change along the longitudinal direction in response to fluctuations in moisture content [23]. Figure 2.1 illustrates the degree of deformation that occurs in various types of common sawn lumber cross sections given their location in the tree from which it was harvested. Saw pattern, i.e., the cutting pattern used to harvest timber from a tree, is critical in controlling the orientation of the tree’s annular rings with respect to the timber member’s cross section. The orientation of the annular rings will dictate whether there is mostly uniform dimensional change, versus warping across the cross section. Uneven dimensional change leads to a loss in dimensional stability in timber cross sections. This is most evident in the circular, rectangular, and square cross sections illustrated in Figure 2.1. Loss in dimensional stability negatively impacts the utility of wood members. Furthermore, loss in dimensional stability can have a negative impact on the ability to achieve a good bond between glued members, affect mechanical connections, and impact the load path in a structure.

2.1.2 Wood Mechanical Properties

2.1.2.1 Elastic Stress-Strain Behavior

Wood is a heterogeneous and anisotropic material. At scales smaller than the annular ring scale, heterogeneity in the cellular structure often requires the use of multi-scale models to capture the inherent anisotropy. At the annular ring scale, wood’s heterogeneity is typically modeled using an orthotropic material model, i.e., there exist three independent, orthogonal directions along which material symmetries exist. Mechanical properties along the three independent, orthogonal directions, i.e., longitudinal ($e_L$), radial ($e_R$), and tangential ($e_T$),
are unique. The longitudinal direction is parallel to the growing direction of the tree, or fiber direction, while the radial and tangential directions are perpendicular to the longitudinal direction. The radial direction extends from the early growth of the tree at the center, or pith, to the bark. The tangential direction is tangential to the growth rings of the tree, see Figure 2.2.

At the macrostructural scale (e.g., boards and posts), the orientation of the growth rings,
or saw pattern, greatly affects the properties of the board. This is because the material axes are not necessarily coincident with the element axes and the material axes rotate as a function of annular ring shape and distribution, as exhibited in Figure 2.3.

![Figure 2.3: Heterogeneity Inherent in a Tree Section](image)

The physical properties and models derived as described above can be better understood by recalling the stiffness tensor for the generalized Hooke’s Law and general anisotropic elasticity. In the generalized Hooke’s Law and general anisotropic elasticity there are 21 unique elastic constants. Compare this to orthotropic elasticity where there are 12 elastic constants, with 9 independent elastic constants - moduli of elasticity (\(E_L, E_R, E_T\)), shear moduli (\(G_{LR}, G_{LT}, G_{TR}\)), and Poisson’s ratios (\(\nu_{LR}, \nu_{RL}, \nu_{LT}, \nu_{TL}, \nu_{TR}, \nu_{RT}\)). The material model can be simplified further by averaging material parameters about the radial-tangential plane [31]. Averaging is appropriate as the modulus of elasticity in the longitudinal direction is significantly greater than that of the radial and tangential directions and accurately determining parameters such Poisson’s ratio can be difficult and imprecise [23].
Furthermore, averaging is typically done for design applications as it is impossible and impractical to know and consider sawing pattern in general design. As such, many sources of literature do not make a distinction between the radial and tangential axes and simply refer to them as the perpendicular or transverse direction and the fiber growth direction as the longitudinal direction [32].

Wood behaves brittlely under shear and tension, both parallel and perpendicular to the fiber direction. On the other hand, wood exhibits nonlinear and ductile behavior under compressive loading, both parallel and perpendicular to the fiber direction. Wood tends to exhibit low strength in compression due to buckling of the cellulose fibers, which have a mostly hollow, thin-walled tube structure. During long-term loading, wood used in bending elements often experiences crushing on the compression side leading to the tension side of the element taking on additional load [33]. Figure 2.4 presents common failure modes exhibited by clear wood specimens in static bending and in compression parallel to the fiber direction. These failure modes were originally presented in ASTM D143, Standard Test Methods for Small Clear Specimens of Timber [34]. Furthermore, wood exhibits a high degree of variability in material properties even when controlling for species, moisture content, and physical differences between specimen [32, 35, 34]. Murrey [32] displays the variability of clear wood specimen at various moisture contents that are subjected to either tension parallel to the grain or compression perpendicular to the grain.

2.1.3 Wood Grading and Manufacture

Trees are harvested from various forests and then cut to shape using various saw patterns. Each saw pattern has distinct advantages over the other patterns, for example, efficiency in utilization of the tree, quantity of wood elements produced with favorable grain direction, and the amount of labor involved.

A distinction between timber and lumber needs to be made for clarity: lumber is felled and sawn trees to be further processed for various applications and timber is lumber that
is 5-in. (114-mm) or more in the least dimension [36]. Dimensional lumber, a subclass of lumber, is lumber that is 4 in. (38 mm) and smaller in the least dimension. The National Design Specification for Wood Construction (NDS) governs the structural design of wood construction projects in the U.S. Mechanical properties such as modulus of elasticity, shear modulus, flexural strength, compression parallel or perpendicular to grain, tension parallel to grain, shear parallel to grain are determined based on grading requirements set forth by
the U.S. Department of Commerce American Softwood Lumber Standard PS 20. Softwoods and many hardwood wood species are graded under the PS 20 standard [23]. Grading is usually carried out via one of three methods: 1) visual grading, 2) machine grading, and 3) other machine grading techniques. Visual grading sorts wood into various groups, or grades, based on visible and known strength reducing characteristics. Visual grading uses the ideal mechanical properties of a clear wood specimen and makes deductions to these properties based on the observation of characteristics such as knots (size, location, and number), slope of grain, checks, waning, and warping. Historically, visual grading has provided a low cost and efficient method of grading for small and mid-size saw mills [37]. It is also the oldest and most commonly used of the three methods [24].

Machine grading sorts lumber by assigning stress grades to lumber and requiring that certain visual criteria be met. Machine grading uses non-destructive grading equipment testing to determine modulus of elasticity, modulus of rupture, ultimate tensile strength, and known relationships with strength values to grade wood and set delineations in the boundaries of the grade [36, 24]. This is usually followed by visual observation to refine the sorting process that results from machine grading [36]. Machine grading when combined with traditional visual grading can dramatically improve strength prediction with $R^2$ values rising to 0.65 or higher [37]. Machine grading categorizes Machine Stress Rated (MSR) and Machine Evaluated Lumber (MEL) [24]. MSR and MEL are relatively similar - both methods use nondestructive grading equipment and visual checks to sort lumber into strength classes and require daily quality control tests for bending strength and stiffness [24]. MEL differs in that it requires daily quality control checks for ultimate tensile strength in addition to MSR requirements [24]. A criticism of machine grading centers around the design and grading rules established by agencies that use softwood species as a basis and assume that the rules are valid for hardwood species as well [38]. For example, there is a large difference in magnitudes of knot density in knotted wood and clear wood for softwood species, but the same is not necessarily true across hardwood species [37]. An-
other issue is the fact that the relationships between modulus of elasticity and other strength parameters are not as strongly defined in hardwoods as they are in softwoods [38].

The third option can be broad and entails the use of automated visual grading machines that employ the lasers, X-ray attenuation, and other sensors to determine properties such as knot prevalence, density of the wood, and modulus. This advanced equipment is often in addition to the use of non-destructive grading equipment. This method improves precision to $R^2$ values of 0.69 [37]. This grading method works well for softwood species as there is a greater difference in density between knotted wood and clear wood density [37]. Each grading method has drawbacks and seems better suited to certain scenarios, additionally, grading techniques are governed by the local grading agency and as a result certain grading techniques may not be practiced in certain markets [24]. A researcher or designer must be aware of these drawbacks when selecting tree species and decide if their application warrants special attention.

2.1.4 Dynamic Properties of Wood

Wood has historically been used as an impact energy absorbing material and was often used in military applications where high rate loading was involved, although civil and practical applications have also existed. According to Johnson [33], wood has been used in the construction of war ships up until the mid 19th century. Ancient civilizations designed and built wood warships to ram and be rammed by other war ships and more recently, wooden warships were designed to resist 1700 ft·s$^{-1}$ (518.2 m·s$^{-1}$) projectiles [33]. Other high rate loading applications of wood include the use in siege weapons, e.g., land rams, pile driving for dock construction, and protective packaging, e.g., in the transportation of nuclear fuel [39, 33].

Despite the long history of wood used in dynamic loading applications, more research on wood loaded at high strain rates is needed. Research has focused primarily on material scale impact testing of small, clear wood specimens where the effects of inertia are
not considered [40]. As a result, the strain-rate enhancement of mechanical properties may not be representative of the wood used in construction. Jansson [41] reports on the findings of Spencer [42] and states that in commercially available lumber, stronger boards exhibited strain-rate enhancement of strength while, weaker boards exhibited no strain-rate enhancement. Jansson, in the same document, conducts his own research involving drop weight testing of commercial grade SPF 2x4’s in a three-point bending configuration. Jansson reports that commercial grade timber, i.e., with defects such as knots or other indications of weakness, generally exhibited decreases in strength of up to 15%. However, high strength timber specimens exhibited no reduction or an increase in strength. Gerhards [43] developed a method to compare bending test data for Sitka spruce collected from various researchers. Despite the large scatter of rates of load implemented by the various researchers, Gerhards reports a significant effect of increased rates of loading on the increase in strength of Sitka spruce ranging from approximately 13% to 45%. Other researchers have focused their research on small wood specimens and dimensional lumber beam specimens. Liska [44] conducted a series of compression parallel to the grain and flexural tests on Sitka Spruce, Douglas-fir, maple, and birch specimen with a moisture content of 12%. All specimen were straight grained and defect free with compression specimen that measured 1-in. x 1-in. x 4-in. (25.4-mm x 25.4-mm x 101.6-mm) long and flexural specimen that measured 1 to 2-in. x 1-in. x 16-in. long (50.8-mm x 25.4-mm x 406.4-mm). Loading times ranged from 0.3 to 750 seconds. Liska reported increases to the modulus of rupture at the fast load rate ranging of approximately 20% for the softwood species and 30% for hard wood species. Fiber stress at the proportional limit also increased with increasing load rate - the softwood species exhibited approximately 20% to 40% increases, while the hardwood species exhibited approximately 10% to 17% increases. No strain rate enhancement for modulus of elasticity was reported.

Research has also been conducted on strain rate enhancement of mechanical properties related to compression and flexure in wood by comparing quasi-static and high rate loading
tests in various failure modes including compression and flexure. Bragov et al. [45] report increases in true stress with increasing strain rate for three wood species: pine, birch, and lime. The specimens were 0.79-in. (20-mm) in diameter x 0.39-in. (10-mm) thick and were tested on a Split Hopkinson Pressure Bar (SHPB) according to the Kolsky method. Of the three species tested, Bragov et al. note that the modulus of elasticity exhibited strain-rate dependence for pine only, in other words, an increase in modulus was noted. Reid et al. [39] investigates the enhancement exhibited by five wood species (Balsa, Yellow Pine, Redwood, American Oak, and Ekii) loaded in uniaxial compression at very high strain rates, $3 \times 10^5 s^{-1}$. Reid et al. used a pneumatic launcher to accelerate the specimens and impact the end of a Hopkinson pressure bar; impact velocities ranged from 30 to 300 m·s$^{-1}$. Reid et al. demonstrate that each of the wood species exhibited an enhancement in crushing strength along both the parallel to grain and perpendicular to grain axes. Dynamic crushing strengths increased by a consistent factor of about 2.3 and up to a factor of 20 for the parallel and perpendicular directions, respectively. Failure is delayed by microinertial effects resisting the motion of the wood cells in the direction of the impact force, which delayed buckling or bending mode failure in the parallel to grain and perpendicular to grain directions, respectively [39]. Later studies by Harrigan et al. [46, 47] build upon the work performed by Reid et al. by improving modeling techniques for wood subjected to shocks due to impact loading and confirm the results for crushing strength enhancement found by Reid et al. Other researchers have conducted SHPB tests on wood and have also shown that high strain rate loading has a significant affect on wood behavior and improvement of its properties, e.g., strength and energy absorption, [48, 49]

Work on the impact loading of wood at the element scale and within the context of a structural system has also been conducted. Lacroix et al. [50] subjected light-weight spruce-pine-fir (SPF) wood stud wall retrofits to simulated blast loading using a shock tube resulting in average reflected impulses ranging from 8 psi-ms (51 kPa-ms) to 49 psi-ms (337 kPa-ms). Lacroix et al. compared the resistance and stiffness provided by each
retrofit using data from the experiment and an SDOF model. Jacques et al. [20] also studied individual SPF wooden studs under very low strain rate loading on the order of $10^{-6} \text{s}^{-1}$ to intermediate strain rate loading ($10^{-1} \text{s}^{-1}$). Reflected impulses of 23 psi-ms (157 kPa-ms) to 32.2 psi-ms (222 kPa-ms) were applied to wall retrofits. Their experimental data showed a large degree of scatter and no discernible enhancement to any of the wood stud’s properties. The scatter was attributed to material variability among the specimens. An iterative SDOF solution was used to solve for the dynamic modulus of rupture, modulus of elasticity, strain at rupture, and ultimately to find the dynamic increase factors for each property. DIF’s of 1.40 and 1.18 were found for the modulus of rupture and modulus of elasticity, respectively.

Strain rate enhancement is shown to occur once strain rate has exceed a certain threshold, which appears to be $> 10^{-1} \text{s}^{-1}$ in this case. Viau et al. [51] later used a shock tube to dynamically load walls constructed of SPF wood studs and wooden sheathing. The walls were primarily subjected to reflected impulses ranging from 52 psi-ms (355 kPa-ms) to 95 psi-ms (654 kPa-ms), which caused average strain rates on the order of $10^{-1} \text{s}^{-1}$. Viau et al. used SDOF modeling with dynamic increase factors derived from the work of Lacroix et al. to accurately predict the response of the walls tested in their experiments. This work thereby further validated the conclusion that the wood in the wood stud walls exhibited strain rate enhancement of the modulus of rupture and modulus of elasticity.

2.2 Cross-Laminated Timber

2.2.1 Cross-Laminated Timber Background

Cross-laminated timber (CLT) is a relatively new engineered wood material classified as "mass-timber", or massive timber, and is rapidly gaining popularity in North America. CLT presents several advantages as a building material. Indeed its size to weight ratio, in-plane and out-of-plane strength and stiffness, and good thermal and acoustic performance make CLT a suitable replacement for conventional materials (e.g., steel, concrete, masonry) in certain applications [18]. CLT is largely prefabricated allowing for faster project comple-
tion, increased construction safety, fewer skilled workers, a greater degree of precision, and fewer on-site modifications to facilitate installation [18, 14]. CLT is also easily handled and its lighter weight, relative to conventional building materials, means that buildings constructed using CLT typically require lighter foundations and smaller cranes for handling [18]. Furthermore, contractors have established a track record of successfully delivering cost competitiveness and rapid project completion with mid and high-rise building projects that have utilized CLT as the primary building material in European and Canadian markets [18].

CLT was first developed in the early 1990s and introduced widely in the mid-1990s in Austria and Germany [12]. In the early 2000s, the usage of CLT increased markedly as a result of several changes in manufacturing processes and changes to public perception of the material [14]. Namely, confidence in CLT as building material has been bolstered by the public’s perception that CLT is comparable to materials like concrete and masonry given its larger mass [18]. CLT’s large mass distinguishes it from previous negative perceptions of wood as a light use material meant for light-weight construction, such as, residential framing and truss structures [52]. Also, an increased interest in sustainable building practices, improved manufacturing efficiencies, code changes in Sweden and the Netherlands, and broader approvals for the use of wood in various structure types by international building codes aided in CLT’s gains in popularity [18, 14].

Much testing and effort has been expended determining the combustibility of mass timber and CLT under various conditions [53, 54, 55, 56, 57, 58], simplified analytical methods to aid design (e.g., $\gamma$-method [59] and shear analogy method [60]), effects of creep on CLT, building physics related to the use of CLT (e.g., fire propagation, sound, airtightness) [52], and other mechanical properties necessary for long-term service life of CLT. The introduction of CLT to the North American market began with the introduction of the Canadian and U.S. CLT Handbooks in 2011 and 2013, respectively. In late-2011, the ANSI/APA PRG 320-11 CLT fabrication standard was approved. This document defines
standard requirements for fabrication and qualification as well as performance criteria for CLT products. Together these codes contributed to the inclusion of provisions in the 2015 ANSI accredited NDS 2015 and the 2016 supplement to the Canadian Standard for Engineering Design in Wood (CSA O86) [18, 12, 61]. These standards have been instrumental to the burgeoning success of CLT in North America. Three new construction types will be introduced to the 2021 International Building Code (IBC) allowing for a maximum of 18-story wood buildings constructed of mass timber products and other non-combustible materials [62]. This will facilitate a global approvals process of the use of CLT in mid-rise to high-rise buildings rather, than the resource intensive process of requiring individual approvals at local levels as is currently required [63].

2.2.2 Cross-Laminated Timber Fabrication

CLT is a layered plate composite that is suitable for structural floors, walls, and roofs. Each layer is composed of visually or mechanically graded and kiln dried lamellas, or dimensional boards of lumber, that have been shaped by four-sided planing and sanding and laid side by side. Finger joints are typically machined into edges of lamellas in order to achieve longer lengths longitudinally. Scarf joints are also allowed in lieu of finger joints by the ANSI/APA PRG320-19. Each layer, or lamina, is arranged in alternating 90° orientations with respect to neighboring laminae and adhered, see Figure 2.5.

CLT panels are fabricated with layups typically consisting of an odd number of laminae and with a minimum of three (3) laminae with the extreme laminae oriented parallel to the spanning direction. The ANSI/APA PRG 320-19 requires that layups for non-custom CLT products be balanced and symmetrical about the neutral axis and layers must alternate and be of the same thickness [64] The layup of CLT gives it the capability of resisting in-plane and out-of-plane loads and as such it is primarily implemented in one of two ways: 1) plate with out-of-plane loads causing bending (e.g., floor slab), and 2) as a plate with out-of-plane and in-plane loads causing bending and tension/compression (i.e., diaphragm action).
Common CLT layups are 3, 5, and 7 laminae with thicker panels and custom layups also possible [12, 65], see Figure 2.6. Custom layups may be designed to optimize the panel for a particular loading, for example, adding longitudinal laminae to a layup to achieve additional flexural capacity. Karacabeyli et al. [12] provide additional layup configurations for 3, 5, and 7 laminae CLT as well as thicker CLT panel layups.

Lamellas are approximately 0.625-in. to 2.00-in. (15.87-mm to 50.8-mm) thick and 2.4-in. to 9.5-in. (60.96-mm to 241.3-mm) wide [12]. The ANSI/APA PRG 320-19 sets
lamella thickness at 1.375-in. (34.925-mm) for its standard layups; sets a lamella width at 1.75 times lamella thickness for parallel laminae, with respect to loading direction; and 3.5 times for transverse layers [12]. Brandner [52] recommends at least a multiplier of 4 for lamellas in transverse laminae in CLT panels loaded out-of-plane in order to minimize the effects of rolling shear on CLT strength and stiffness. Rolling shear is a shear force that occurs in the radial-tangential plane and develops as an interlaminar shear stress in CLT. CLT panel thickness is limited to 20 in. (508 mm) per the ANSI/APA PRG 320-19 [64], although European standards allow for thicker panels. CLT panels are available in widths of 2-ft., 4-ft., 8-ft., and 10-ft. (0.61-m, 1.22-m, 2.44-m, and 3.05-m) with lengths ranging from up to 60-ft. to 75-ft. (18.3-m to 22.9-m) [12, 52]. Length is typically limited by the mode of transportation selected for delivery.

Polyurethane adhesives (PUR) are usually used for adhesion; and, less commonly, melamine-urea-formaldehyde (MUF) and phenol-resorcinol-formaldehyde (PRF) based adhesives are used as well [14]. PUR adhesives are also generally implemented in engineered wood products because one component polyurethane cures rapidly at room temperature, does not require mixing, it produces a clear bond line, and does not produce toxic emissions like formaldehyde as found in MUF and PRF [66]. The moisture content of all lumber used in fabrication can significantly affect the mechanical properties of CLT, in particular delamination resistance [67]. ANSI/APA 320-19 [64] PRG 320 specifies that the moisture content of all lumber must be 12% ± 3% at the time of manufacture. CLT panels typically have their side faces glued with narrow face gluing, or edge gluing, being less common given the increased manufacturing cost associated with it. Figure 2.7 provides commonly used nomenclature and terms.

CLT panels require pressure during assembly as well as sustained pressure for a time that is dependent on the adhesive type. Typical times for pressure application vary from several minutes to several hours [12]. The four common methods for applying pressure are vacuum press, compressed air press, use of fasteners (e.g., screws, staples, nails), or
hydraulic press - the most common method. Hydraulic presses allow for pressures ranging from 14.5 psi to 145 psi (0.10-1.00 N/mm$^2$) or higher to be applied; vacuum presses allow for 7.25 psi to 14.5 psi (0.05-0.10 N/mm$^2$); and fasteners allow for 1.45 psi to 29 psi (0.01-0.20 N/mm$^2$) [52]. The pressure applied to a panel, however, is not straightforward as it is a function of wood species, adhesive type, dimensions of the panel, surface geometry of the boards, and ambient temperature and humidity. Panels are typically machined and cut again prior to shipment once the adhesive has properly cured.

2.2.3 Cross-Laminated Timber Mechanical Performance

CLT mechanical performance can be qualified by many different mechanical properties including flexural and shear strength and stiffness, serviceability factors (deflections and long term loading), vibration, fire resistance, sound insulation, and durability [12]. Karacabeyli et al. [12] classify the following properties as being important for floor and roof CLT elements: 1) in-plane and out-of-plane flexural strength, shear strength, and stiffness; 2) instantaneous deflection, long-term deflection (creep), and long-term strength for permanent loads; 3) vibration performance; 4) compression perpendicular to the grain; 5) fire performance; 6) sound insulation; 7) durability. For wall elements, Karacabeyli et al. [12]
propose that the following properties are key: 1) load bearing capacity; 2) in-plane and out-of-plane shear and bending strength; 3) fire performance; 4) sound insulation; 5) durability. This research will primarily focus on the in-plane and out-of-plane shear and flexural strength of CLT.

The PRG 320 has standardized the determination of these mechanical properties, although there are many methods that are currently in use and stem from two approaches: 1) using bearing models and individual board or laminae properties, or 2) testing of CLT elements [52].

As indicated earlier, CLT’s mechanical performance is highly dependent on the wood species and moisture content, and adhesive and pressing procedure. Despite this correlation, CLT significantly improves the mechanical performance of wood that on its own would otherwise be unsuitable for structural applications. This improvement can be attributed to many factors along with CLT’s homogenizing effect on individual timber board properties, which affects woods typical behavior attributed to high levels of variability, heterogeneity, and anisotropy present [52]. This factor has contributed to low-value wood species - those with lower densities and/or lower mechanical properties - being used in the production of CLT. There have been many studies conducted to investigate the use of various tree species and adhesive combinations in CLT panels. These studies have primarily demonstrated the utility of local tree species in the manufacture of CLT in an effort to help develop product standards for these tree species including low-value tree species [68].

Kramer et al. [69] studied the mechanical properties CLT panels constructed of hybrid poplar clone (Pacific Albus) and a phenol-resorcinol adhesive. Kramer et al. found that hybrid poplar CLT met and exceeded the PRG 320 E3 grade for shear and bending strength. While the hybrid poplar CLT panels did not meet stiffness requirements, the test results demonstrated that this low-density wood species could be used in CLT with higher-density wood species. Mohamadzadeh and Hindman [70] conducted a study to demonstrate the viability of a hardwood species, i.e., yellow poplar (liriodendron tulipifera), in the man-
ufacture of CLT panels. The mechanical properties of yellow poplar CLT were found to be significantly higher than the comparable PRG 320 CLT V1 and V2 grades. A potential reason for this phenomenon was the high shear strength found for yellow poplar tested. Hindman and Bouldin conducted mechanical property tests on southern pine CLT based on the ANSI/PRG 320-12 standard [67]. Southern pine's relatively high specific gravity relative to other tree species categorized in the NDS provide it with favorable mechanical properties for use in CLT. The values Hindman found experimentally for southern pine CLT exceeded the published values for the V3 grade in PRG 320-12. This author notes that the values currently published in PRG 320-19 have been reduced further as compared to the values published in the 2012 edition and used in Hindman’s and Bouldin’s research. Some factors in their test series may have affected the results seen including specimen with edge gluing (not typically seen in CLT panel construction in North America), span-to-depth ratios that deviated from the ASTM standards, reinforcement of the lap splices, and location of load adjacent to the lap splices.

Product standards like the PRG 320 have been instrumental in the adoption of uniform manufacturing processes and mechanical performance, and adoption into other codes governing timber design and building code, i.e. the NDS and IBC. Pei et al. provide a thorough history of CLT implementation into engineering codes and design in North America [68].

2.2.4 Grading of CLT in North America

Mechanical properties of CLT products vary between fabricators. There are several factors that dictate these differences including the tree species used, the grading method selected for the lamellas, layup, adhesive used, and specific lamella aspect ratios used within an individual fabricator’s CLT product. The ANSI/APA PRG 320-19 Standard for Performance-Rated Cross-Laminated Timber (PRG 320) [64] establishes performance criteria that define several approved CLT grades and layups. Grades not conforming to these performance criteria are designated as custom CLT grades and layups requiring additional approvals from
local agencies. The standard limits the use of lumber of tree species to softwood tree species recognized by PS20 or the Canadian Lumber Standards Accreditation Board under CSAO141. The lumber must have a specific gravity of at least 0.35 and minimum grade of 1200f-1.2E MSR or visual grade No. 2 for longitudinal laminae and visual grade No. 3 for transverse laminae [64]. CLT grades are designated by a letter and number, for example, "E1" or "V1". The letter E designates a CLT product composed of mechanically stress rated lumber while the letter V designates one composed of visually graded lumber. The numbers designate the tree species and grade of the lumber used in the product. Table 2.1 provides CLT grades and descriptions.

The PRG 320 also specifies testing methods to be used for determining allowable values for various mechanical properties as discussed in the following sections.

2.2.5  Flexural and Shear Strength in Flatwise Bending

The process of cross laminating provides high in-plane and out-of-plane flexural and shear strength and stiffness in both major and minor strength directions. This is due to the transverse layers behaving as reinforcement for the longitudinal layers. This feature allows CLT to have two-way spanning action similar to reinforced concrete slabs [71]. The PRG 320 specifies the use of four-point flatwise bending tests for the evaluation of flatwise bending strength in the major and minor strength directions. The standard further states that the tests must conform to the ASTM D198 and ASTM D4761 standards. Specimen must measure at least 12-in. (304.8-mm) wide with a span-to-depth ratio of 30:1 in the major direction and 18:1 in the minor direction. The standard further states that tests conducted to determine flatwise shear strength in the major and minor strength directions must similarly conform to the ASTM D198 and ASTM D4761 standards. Specimen used to determine in-plane shear strength must measure at least 12-in. (304.8-mm) wide with a span-to-depth ratio of 5:1 to 6:1. Flexural and in-plane shear strength values derived from testing using the ASTM standards are to be multiplied by 2.1 for comparison with the PRG 320 published
Table 2.1: ANSI/APA PRG 320-19 CLT Grades and Layups (Adapted from [64]).

<table>
<thead>
<tr>
<th>Grade</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>E1</td>
<td>1950f-1.7E Spruce-pine-fir MSR lumber in all longitudinal layers and No. 3 Spruce-pine-fir lumber in all transverse layers.</td>
</tr>
<tr>
<td>E2</td>
<td>1650f-1.5E Douglas fir-larch MSR lumber in all longitudinal layers and No. 3 Douglas fir-Larch lumber in all transverse layers.</td>
</tr>
<tr>
<td>E3</td>
<td>1200f-1.2E Eastern Softwoods, Northern Species, or Western Woods MSR lumber in all longitudinal layers and No. 3 Eastern Softwoods, Northern Species, or Western Woods lumber in all transverse layers.</td>
</tr>
<tr>
<td>E4</td>
<td>1950f-1.7E Southern pine MSR lumber in all longitudinal layers and No. 3 Southern pine lumber in all transverse layers.</td>
</tr>
<tr>
<td>E5</td>
<td>1650 f-1.5E Hem-fir MSR lumber in all longitudinal layers and No. 3 Hem-fir lumber in all transverse layers.</td>
</tr>
<tr>
<td>V1</td>
<td>No. 2 Douglas fir-Larch lumber in all longitudinal layers and No. 3 Douglas fir-Larch lumber in all transverse layers.</td>
</tr>
<tr>
<td>V1(N)</td>
<td>No. 2 Douglas fir-Larch (North) lumber in all longitudinal layers and No. 3 Douglas fir-Larch (North) lumber in all transverse layers.</td>
</tr>
<tr>
<td>V2</td>
<td>No. 1/No. 2 Spruce-pine-fir lumber in all longitudinal layers and No. 3 Spruce-pine-fir lumber in all transverse layers.</td>
</tr>
<tr>
<td>V3</td>
<td>No. 2 Southern pine lumber in all longitudinal layers and No. 3 Southern pine lumber in all transverse layers.</td>
</tr>
<tr>
<td>V4</td>
<td>No. 2 Spruce-pine-fir South lumber in all longitudinal layers and No. 3 Spruce-pine-fir South lumber in all transverse layers.</td>
</tr>
<tr>
<td>V5</td>
<td>No. 2 Hem-fir lumber in all longitudinal layers and No. 3 Hem-fir lumber in all transverse layers.</td>
</tr>
</tbody>
</table>

values. Bending and shear stiffness do not require a modification factor.

The ASTM D198 standard [72] outlines reliable and repeatable testing methods for evaluating the strength and stiffness of lumber and wood products subjected to flexure, compression, tension, and torsion. The standard is meant to provide a detailed and accurate set of testing methods that are intended to be used in scientific research, establishing design values, and quality assurance. Flexural testing is carried out using a four-point loading
fixture with loads applied at $l/3$, where $l$ is span length, until first rupture of the specimen is reached. Figure 2.8 illustrates a four-point bending fixture per ASTM D198.

![Four-Point Bending Fixture](image)

**Figure 2.8: Four-Point Bending Fixture. Copyright by [72], used with permission.**

While four-point bending is recommended, three-point bending may be performed to evaluate shear properties or bending stiffness. Four-point bending is utilized to create a zone of pure bending between the points of loading so that a shear-free modulus of elasticity may be determined. In wood materials the modulus of elasticity often cited is the apparent modulus of elasticity, which includes the effects of shear deformation. The effects of shear deformation are often more severe in specimens with low span-depth ratio and low shear modulus. The rate of loading is determined via a trial and error approach to find a rate that allows the maximum load to be reached at an average of at least 4 minutes. A constant rate of outer strain of 0.0010 in./in. · min. is recommended for wood or wood products with a high degree of variability or a small sample set. The standard provides methods to calculate various physical and mechanical properties of the specimen, but specifies that more rigorous calculations are needed for sandwich or composite specimen. While testing of shear properties is not outlined, a method for determining shear stiffness of solid lumber is outlined. The ASTM D4761 [73] standard is similar to the D198 standard in many
respects, but has more relaxed criteria for testing procedures (e.g., the testing fixture, load rate, reporting, accuracy) allowing more flexibility.

These testing methods present challenges and limitations: 1) obtaining the recommended span-to-depth ratio of 30:1 for flatwise bending properties requires a large amount of laboratory space to both fabricate and test specimens and 2) fabrication of specimen of this size requires a large hydraulic press with high capacity, specialized manufacturing equipment, or specially designed fixtures (e.g., as seen in [69]) [70]. The requirement of a large span-to-depth ratio can be attributed to two main factors. The first is, while the PRG 320 standard exists to set performance criteria for the manufacture and testing of CLT, the testing standards that it references are based on methodologies for solid sawn lumber. Second, the assumption used for determining shear stiffness (GA) is based on standards and assumptions for solid sawn lumber. Under this assumption, the ratio of modulus of elasticity to shear modulus (E:G) is 16:1 for the longitudinal laminations - with some variations by tree species having been reported [12, 74, 64]. As a result, the bending deflection as a percentage of total deflection reaches 90% at a span-to-depth ratio of 30:1, as demonstrated by Fellmoser and Blass [75] and Hindman [74] using the shear analogy design method. Fellmoser and Blass [75] also found that the influence of shear deformations on the effective stiffness of the CLT panels was observed at span-to-depth ratios less than 30 and 20 for out-of-plane bending in the major span and minor span directions, respectively. An additional challenge is the determination of the modulus of elasticity. While ASTM D198 provides a method to determine the shear free modulus of elasticity using four-point bending, the test only allows for the evaluation of the material under pure bending between the load points. The use of this limited zone of material tends to lead to errors in the calculation of the shear free modulus of elasticity as the deflections are small and the method does not account for the global behavior of the specimen and any localized weak zones that may exist, as is common in wood materials [76].

To overcome the challenges imposed by this testing requirement, i.e., large span-to-
depth ratio, splicing of multiple panel specimen can be performed, but splicing may also present challenges in the form of failures at the splice joints [67]. Mohamadzadeh [70] proposed the use of a combination of five-point and four-point bending tests in conjunction with analysis of test data to determine properties of CLT as a means of overcoming these challenges. Note, the "five-point bend test" is a test that involves a continuous beam with two equal spans and a concentrated load of equal magnitude at the center of each span.

Mohamadzadeh’s testing method was originally developed by Bradtmueller et al. as a means of an efficient and cost effective method of testing properties for structural wood composites [77]. Later Bradtmueller conducted experimental work on structural composite lumber to further validate this method. A direct comparison of properties obtained via Bradtmueller’s method and the methods approved by ANSI/APA PRG 320-19 has not been made. Sharifnia and Hindman [66] also employed five-point bending tests to evaluate changes in manufacturing criteria used to fabricate Southern Pine CLT. Sharifnia reported significantly higher values for bending and shear stiffness than resulted from the PRG 320 procedure, which was attributed to the PRG 320 reporting of mechanical properties[66]. Indeed, the values published in the PRG 320 are derived analytically using the Shear Analogy Model [60] and validated with testing [64]. Hindman and Bouldin performed multiple regression analysis on experimental data of various span CLT beams based on ASTM D198 and performed a simultaneous solution of PRG 320 deflection equations to evaluate shear and bending stiffness of Southern Pine CLT [74]. The study determined that there was very little difference in the shear stiffness values determined via the experimental data and the simultaneous solution. Furthermore, it was shown that for CLT using the simultaneous solution, the percentage of bending deflection as a percentage of total deflection reaches 90% at a span-to-depth ratio of 18:1 vs. 30:1 [74]. Hindman also reported flexural stiffness to shear stiffness ratios ($E_{eff}/G_{eff}$) ranging from 162:1 to 187:1 for different CLT beam configurations [74]. This finding has the potential to dramatically reduce the cost of evaluating CLT properties and introducing new wood species to CLT fabrication. Cost
may be reduced since it was shown that testing of long CLT specimens is not necessarily required to evaluate CLT properties, which will reduce the need for large amounts of laboratory space, high capacity actuators, and more materials.

2.2.6 Rolling Shear Strength and Stiffness

Rolling shear (RS) is a shear stress occurring in the radial-tangential (RT) plane (Figure 2.9a). It derives its name from the fact that fibers in composite materials "roll" or slide past each other as rolling shear stresses develop and get progressively larger. In CLT, rolling shear often develops as an interlaminar shear stress in the transverse laminae in panels that are loaded out-of-plane. Rolling shear develops in CLT due to 1) the structural configuration, or layup, often used in CLT, and 2) because the shear modulus of wood in the RT direction \( \left( G_{RT} \right) \) is very small compared to the longitudinal shear modulus \( \left( G_L \right) \).

The longitudinal shear modulus to RT shear modulus ratio is approximately 10 in CLT \( \left( G_L:G_{RT} \right) \) [78]. For softwood species, the longitudinal modulus of elasticity to RT shear modulus ratio \( \left( E_L:G_{RT} \right) \) can be as high as 200 [31]. Rolling shear failures tend to be more ductile than longitudinal shear failures in beams [79, 80, 81]. In timber, mechanical properties typically improve as density increases; however, RS properties have been shown to increase as density increases as shown by Bendtsen [82], while others [83, 84] have not found a significant correlation.

The global response of a CLT panel loaded out-of-plane is heavily affected by shear deformations of the transverse laminae resulting from this difference in RS modulus. Shear deformation in CLT panels is dependent on the loading configuration, span-to-depth ratio, boundary conditions, and rolling shear modulus as a function of tree species. Consideration of rolling shear is an important factor in CLT ultimate limit state and serviceability limit state design, and in the stability of a CLT panel [13]. Given the highly heterogeneous nature of wood, and in the introduction of further heterogeneity via cross lamination, accurately and reliably determining RS properties can be challenging. Many researchers have
Figure 2.9: Rolling Shear in Timber
introduced methods to evaluate strength and stiffness of the transverse laminae of CLT and RS (i.e., \( e_{RT} \) and \( e_{TR} \)) properties in wood. These methods have focused on these properties at different material scales: 1) ring scale, 2) element/board scale and boards acting as a system (i.e., laminae), and 3) beam scale. Caution should be used in the application of these methods as they may not scale appropriately across length scales. Aicher and Dill-Langer [83] noted significant differences in rolling shear modulus measured at the ring scale as compared to the board scale. Aicher and Dill-Langer further stipulated that RS modulus is not an intrinsic material property, but a global equivalent stiffness that is highly dependent on the physical properties of the annular rings (density and width), sawing pattern of a board, and the size and geometry of the board.

At the ring scale, several experimental methods have been used to evaluate rolling shear properties with varying degrees of success. The isolation of pure and uniform shear stress states in wood has often made evaluating the shear strength and stiffness of wood quite challenging. A test fixture that accomplishes this task and allows the evaluation of various material orientations paired with the practical matter of machining specimen with properly oriented material axes has further complicated the endeavor. Dahl and Malo [84] compared several methods including: 1) the ASTM D143 [34] notched shear block test, 2) short beam shear test, 3) the Iosipescu shear test, and 4) the Arcan shear test. Each of these methods are displayed in Figure 2.10.

The notched shear block test described by ASTM D143 [34] has been widely used to determine the shear strength of wood and wood products. It involves the application of an eccentric load to a longitudinally oriented clear wood specimen with notches until the notched portion of the specimen is shear off. This method has received acute criticism from many authors [84, 85, 86, 87] due to the following: 1) a pure shear stress state is precluded by the presence of normal stresses caused by bending moment due to eccentric loading, 2) shear and normal stresses are not uniform over the critical section, and 3) stress concentrations are introduced by the presence of the notches. The use of short beam shear
test, introduced by Kollmann and Côté [88], involves the application of a uniform load over a critical section with free or clamped boundary conditions. This test similarly presented issues with incorrect failure modes due to stress concentrations at discontinuities (e.g., corners) and an impure shear stress state caused by bending moments that introduced normal stresses. Next, the Iosipescu shear test requires a rectangularly shaped wood specimen with $90^\circ$ notches on the top and bottom of the specimen located at the centerline. This testing fixture prevents the introduction of bending moment at the critical section and allows for a pure shear stress state at the critical section. Furthermore, this testing fixture facilitates the use of specimen with different material axes allowing the study of different loading and material direction configurations. While some issues have been reported by some authors [86, 89], Dahl and Malo conclude that the Iosipescu test fixture provides adequate results.
for shear moduli. Dahl and Malo also caution that this method may be subject to incorrect failure given the presence of normal stresses in sections adjacent to the critical section. This method may not be appropriate for evaluating failures in the radial and tangential plane for this reason as well as the relatively low tensile capacities for this plane [84]. Finally, the Ar- can shear test method implements two discrete rigid clamps to which a "butterfly" shaped specimen is affixed, either via chemical adhesive or mechanical anchors. A tensile load is applied to the fixture along the centerline of the specimen. Given the configuration of the fixture a pure shear stress state is induced; specimen may be machined to study different material axes orientations; and the fixture may be rotate by an angle $\phi$ to permit the study of combined stress states. Dahl and Malo concluded that the Arcan shear test was the most reliable method for testing shear in wood and Arcan has also be validated for wood by other researchers [85, 90, 91, 92].

Dahl and Malo tested the shear properties of Norway spruce using the Arcan shear test for six loading and material configurations to determine shear moduli for three orthotropic planes (i.e., $G_{LT}, G_{LR}, G_{RT}$). The loading sequence employed involved an elastic load phase to 40% of specimen capacity, an unload phase, and a reload phase that continued until failure. The shear moduli were determined using a least sum square error and then corrected for configuration effects using an FEM analysis. Their data exhibited relatively high coefficients of variance (COV) for each of the mean shear moduli determined, although small differences in COV for symmetrical material and load configurations (i.e., $G_{ij}$ to $G_{ji}$) were found. This supports material symmetry as expected in a continuum orthotropic material model. Finally, Dahl and Malo reported higher shear moduli in the unloading and reloading phases of the tests for all orientations tested. Rolling shear, $G_{RT}$, in particular exhibited a significant increase in average shear modulus with a 23.8% increase from loading phase to unloading phase and a 5% increase from loading to reloading phase. These differences can be indicative of strain rate effects for the the radial-tangential plane. As part of the same testing series, Dahl and Malo later published a paper [93] analyzing the
nonlinear regime of the shear stress-strain curves for each material axis and load configuration. Dahl and Malo showed that rolling shear specimen displayed more nonlinearity as compared to the other shear planes, $G_{LT}$, $G_{LR}$, of which $G_{LT}$ was relatively linear. Rolling shear specimen also exhibited more ductility, albeit at a lower level of stress; rolling shear specimen exhibited up to ten times more strain at ultimate stress. They reported a value of 1.6 MPa (237.9 psi) for average rolling shear ultimate stress.

Board scale testing setups have been argued to be more conducive toward determining rolling shear properties than tests on clear wood specimen. At the board scale, testing fixtures can be designed to more closely imitate the stress state found in CLT and for the observation of relationships between rolling shear strength and stiffness to parameters such as sawing pattern and aspect ratio. Ehrhart [94] argues that ring scale tests, including the ones discussed above, only succeed in determining rolling shear modulus and not strength as there is some variability to the location of failures resulting in inconsistent failures at the critical section designed in the experiment. Aicher and Dill-Langer [83] conducted numerical analyses at the board scale for various sawing patterns in a softwood species board in a FEM simulated two-plate shear test. The rolling shear modulus at the board scale was found to be between 50 MPa (7.3 ksi) - 200 MPa (29 ksi) and dependent on the angle and configuration of the annual rings; an input rolling shear modulus of 50 MPa for the wood material model was used. Fellmoser and Blass [75] later studied the effect of span-to-depth ratio in 3-ply CLT panels using the shear analogy method and used three-point bending vibration tests to determine the rolling shear modulus of Norway Spruce boards. Rolling shear modulus values were found to be between 40 MPa (5.8 ksi) and 80 MPa (11.6 ksi), therefore, supporting values found by other researchers. At the board scale, rolling shear test setups have been adapted from the longitudinal shear test setup described in EN 408 [95] and used by Mestek [96]. This test setup involves a fixture set at a 14° angle and the application of a compression load to a board specimen, with its grain aligned in the direction of the load, via two stiff transfer plates bonded to the board. The test, as described
in EN 408, is intended for determining longitudinal shear strength and shear modulus, but has been used for investigating the effect of other parameters such as timber species, aspect ratio, sawing pattern, inclination angle sensitivity, and transverse stresses [94]. The EN 408 test method was used initially by Mestek [96] and subsequently by several researchers including, [97, 98], to study rolling shear and has been used with boards that are oriented with their grain direction perpendicular to the shearing plane, see Figure 2.11.

![Figure 2.11: EN 408 Based Rolling Shear Test. Copyright by [94], used with permission.](image)

Others have implemented similar two-plate shear tests to determine RS strength and stiffness for multiple layer CLT specimen. Zhou et al. [80, 81] compared RS strength and stiffness results from short three-point bend tests (span-to-depth ratios of 5.5, 6.5, 8.5, 14) and ASTM D2718 based two-plate shear tests oriented at 3.4° on black spruce (Picea mariana) CLT specimen and steel-wood-steel specimen. Zhou et al. found that the average RS modulus and strength were 72.61 MPa (COV 17.2%) and 2.74 MPa (COV 8.8%), respectively, and rolling shear failures tended to be more ductile than longitudinal shear failures. Zhou et al. found that the three-point bend test was more effective at determining RS strength while the two-plate shear tests were more effective at determining RS stiffness. Moreover, Zhou et al. found that the shear analogy method is effective at accurately determining CLT deflection when an experimentally measured RS modulus is used and RS modulus for lower value span-depth ratios(< 8.5) while the two-plate shear test was
found to be more appropriate for larger span-depth ratios (> 14). Zhou et al. [80] later found the rolling shear modulus and strength to be 136 MPa (COV 16.5%) and 1.09 MPa (COV 31.57%), respectively, via the two-plate shear test. Similarly, Cao [99] used a two-plate shear fixture oriented at 18° and three-point bend tests to test 3-ply Southern Pine CLT specimen and determine the effect of knots on RS strength. Cao observed that the two-plate shear tests exhibited higher RS strength than the three-point bend tests. It was suggested that the larger angle, compared with other researchers, led to higher compressive normal forces, which increased the RS strength. Li [100] also conducted a series of tests to determine the effect of cross lamination thickness on RS strength in CLT. Li used short-span (L/h = 6) three-point bend tests and two-plate shear tests oriented at 6° based on the Australian and New Zealand AUS/NZ Standard 2269.1-2012. Li observed that both test methods produced very similar results for each lamination thickness, 20-mm (0.79-in.) and 35-mm (1.38-in.), ranging from approximately 3%-4% between test methods. Furthermore, COV values ranged from approximately 12% to 13.5% with average RS strengths of 1.98 MPa (287.2 psi) and 2.39 MPa (346.6 psi) for 35-mm (1.38-in.) and 20-mm (0.79-in.) thick laminations, respectively, when data from both tests methods were combined. This suggests that increases to board aspect ratio (from 4.71 to 9.75), leads to a significant increase in RS strength, approximately 20%. Li suggested that the 35-mm (1.38-in.) laminations had a larger wood volume under RS stress, which increased the probability of strength-reducing defects being present in those laminations as compared to the 20-mm (0.79-in.) laminations. Li further validated the two testing methods and demonstrated the upward trend related to increasing RS strength and increasing board aspect ratio [101].

Ehrhart and Brandner offer a summative analysis of rolling shear testing methods at the ring and board scale as well as present results from a large experimental campaign on European coniferous and deciduous tree species in [94, 102]. Ehrhart and Brandner conducted a series of two plate shear tests on five European tree species including Norway Spruce (Picea abies (L.) Karst), pine (Pinus sylvestris L.), poplar (Populus tremula L.), European
beech (Fagus sylvatica L.), and European birch (Betula pendula R.). Various geometries were tested: single boards (60-mm (2.36-in.), 120-mm (4.72-in.), and 180-mm (7.09-in.)), single layers of boards (2x1 and 4x1), and multiple parallel layers of boards (4x3). Additional variables investigated were sawing pattern (distance from the pith) and board aspect ratio (width:thickness). Ehrhart and Brandner conclude that the two-plate shear test based on the EN 408 test method, as discussed above, provides a reliable, consistent, and simple testing method that produces stresses in the cross layers sufficiently close to CLT panels subjected to out-of-plane bending. Ehrhart and Brandner also draw several conclusions based on their experimental campaign including: sawing pattern and board aspect ratio have a great influence on RS modulus while only board aspect ratio influences RS strength; the influence of density on RS properties was more prominent in hardwood species; and system effects have a homogenizing effect on RS modulus as evinced by lowering COV values. System effects, i.e., single or multiple layers of boards, displayed deleterious effects on RS strength and did not have a homogenizing effect. The results from this study generally build upon the test results of others explores more precise control over variables and often supports the test results of past researchers.

The two plate shear test method has a minor drawback as described in [94], namely, its inability to create a pure shear stress state - there are two forces present in the board given the inclination and compression force, i.e., a compression force perpendicular to the board and a shear force in the plane of the board. Mestek found a positive correlation between increases to this compression force and increases in rolling shear strength [96, 94]. Other researchers have found that the positive correlation of compressive stresses perpendicular to the shear plane are not as significant for RS strength as they are for longitudinal shear strength, as described by Ehrhart [94]. Additionally, despite this criticism, Cao, Li, and Zhou have each demonstrated the successful applicability of the testing method particularly when paired with the shear analogy method and short span bending tests.

Additonal testing methods for at the board scale include work by Franzoni [103, 104]
and Lam [105]. Franzoni [103, 104] used double-lap shear tests of Norway spruce boards adhered to steel plates to determine transverse shear properties including rolling shear modulus. This test involves the simultaneous testing of two boards in shear. There is a possibility for the redistribution of stresses to the other board in the test setup, thereby influencing the results for failure modes observed and RS properties [94]. Despite this, the rolling shear modulus, 110 MPa (15.95 ksi), determined was in agreement with values found by other researchers. Lam [105] conducted torque tests on cylindrical specimen machined from SPF CLT panels. The test creates a pure shear stress state in the cross layers of the specimen, however, high rolling shear strengths (3.86 MPa (559.8 psi) and 4.83 MPa (700.5 psi) for 3-ply and 5-ply CLT, respectively) are reported by the tests. Additionally, interpretation of the data requires complex analysis, e.g., Monte Carlo and FEM, rather than the use of analytical solutions.

Some researchers believe that studying the system effects present in a cross lamina provides results that are more applicable to CLT RS properties. Testing on the system effects involved in cross laminations have been investigated by Perret, Frazoni, and Görlacher and Blass [94]. Perret [31] tested sandwich beams in four-point bending to determine the equivalent layer RS stiffness for CLT panels. Ten Norway Spruce boards with edge gluing are sandwitched between carbon fiber reinforced polymer (CFRP) skins as a means to isolate the cross lamina in order to characterize its shear behavior. Given the high contrast between the CFRP skin longitudinal stiffness and the wood cross layer transverse shear stiffness, the beam’s bending stiffness is primarily due to the CFRP’s properties and the shear stiffness for the beam is primarily due to the wooden core. While this test method included multiple boards, which is similar to other test methods, it does report an equivalent cross layer RS modulus of 124 MPa with a low value COV of 6.71%. This is similar to results for system effects in cross-wise oriented boards found by Ehrhart and Brandner [94] for 2x1, 4x1, and 4x3 edge glued specimen with median RS moduli of 140 MPa (COV 14%), 130 MPa (COV 7%), and 124 MPa (COV 10%), respectively. Perret’s lower COV value may be due
to the presence of more boards, which may have had a further homogenizing effect thereby lowering COV.

### Table 2.2: Softwood RS Properties Determined Experimentally

<table>
<thead>
<tr>
<th>Source (Year)</th>
<th>Species</th>
<th>Test Description</th>
<th>$G_{IC}$ (MPa (COV))</th>
<th>$f_{IC}$ (MPa (COV))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bendtsen (1976) [82]</td>
<td>Balsam Fir&lt;sup&gt;a&lt;/sup&gt; <em>A. balsamea</em></td>
<td>ASTM C273 Two-plate compression; Machined board segments (0.5 in. x 2 in. x 6 in.); $w_l/t_l = 4$; $\rho_{mean} = 21.8$ lbs/ft$^3$ (349 kg/m$^3$) at 12% M.C.; 33 specimens</td>
<td>60 (43.5%)</td>
<td>1.49 (22%)</td>
</tr>
<tr>
<td>Bendtsen (1976) [82]</td>
<td>Sugar Pine&lt;sup&gt;a&lt;/sup&gt; <em>P. lambertiana</em></td>
<td>$\rho_{mean} = 22.15$ lbs/ft$^3$ (355 kg/m$^3$) at 12% M.C.; 34 specimens</td>
<td>69.2 (37%)</td>
<td>2.02 (22%)</td>
</tr>
<tr>
<td>Bendtsen (1976) [82]</td>
<td>Red Spruce&lt;sup&gt;a&lt;/sup&gt; <em>P. rubens</em></td>
<td>$\rho_{mean} = 25.2$ lbs/ft$^3$ (404 kg/m$^3$) at 12% M.C.; 31 specimens</td>
<td>68 (18.7%)</td>
<td>1.78 (20%)</td>
</tr>
<tr>
<td>Dahl and Malo (2009) [84, 93]</td>
<td>Norway Spruce <em>P. abies K.</em></td>
<td>Arcan shear test; $\rho_{mean} = 25$ lbs/ft$^3$ (398 kg/m$^3$) at 12% M.C.; 31 specimens</td>
<td>30 (28%)</td>
<td>1.64 (25%)</td>
</tr>
<tr>
<td>Mestek (2011) [96]</td>
<td>Norway Spruce <em>P. abies K.</em></td>
<td>EN 408 Based Two-plate compression test; board segments (20 mm x 200 mm); $w_l/t_l = 10$; $\rho_{mean} = 29$ lbs/ft$^3$ (466 kg/m$^3$) at 12% M.C.; 5 specimens with stress relief, 10 specimens total</td>
<td>-</td>
<td>2.13 (-)</td>
</tr>
<tr>
<td>Zhou et al. (2014) [80]</td>
<td>Black Spruce <em>P. mariana</em></td>
<td>EN 408 Based Two-plate compression test; board segments (9 mm x 36 mm x 36 mm); $w_l/t_l = 4$; $\rho_{mean} = 29$ lbs/ft$^3$ (466 kg/m$^3$) at 12% M.C.; 6 specimens</td>
<td>72.61 (17.2%)</td>
<td>2.09 (11%)</td>
</tr>
</tbody>
</table>

Continued on next page
Table 2.2 continued

<table>
<thead>
<tr>
<th>Source (Year)</th>
<th>Species</th>
<th>Test Description</th>
<th>(G_{RT}) MPa (COV)</th>
<th>(f_{RT}) MPa (COV)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Li (2017) [100]</td>
<td>Radiata Pine (P.\ radiata)</td>
<td>AS/NZS 2269.1 Based Two-plate compression test and bend tests (not shown); CLT specimen (165 mm x 50 mm x 60 mm, 165 mm x 50 mm x 105 mm); (w_l/t_l = 8.25) and (w_l/t_l = 4.71); (\rho_{mean} = 27.84) lbs/ft(^3) (446 kg/m(^3)) and (\rho_{mean} = 28.4) lbs/ft(^3) (455 kg/m(^3)) at 10.9% and 11.6% M.C.; 30 specimens</td>
<td>-</td>
<td>1.99 (12%) 2.33 (13%)</td>
</tr>
<tr>
<td>Perret (2017) [31]</td>
<td>Norway Spruce (P.\ abies K.)</td>
<td>CFRP/Wood Sandwich Composite, 4-pt. bend test; board segments (30 mm x 40 mm x 100 mm); (w_l/t_l = 3.33); 10-13% M.C.; 12 specimens</td>
<td>124 (6.71%)</td>
<td>-</td>
</tr>
<tr>
<td>Cao et al. (2019) [99]</td>
<td>Southern Pine (P.\ spp.)</td>
<td>Two-plate compression test and bending tests (not shown); CLT specimen (105 mm x 136 mm x 342 mm); (w_l/t_l = 3.88); (\rho_{mean} = 27.5) lbs/ft(^3) (440 kg/m(^3)) at 15% M.C.; 54 specimens</td>
<td>-</td>
<td>2.00 (11-14%)(^d) 2.55 (11-12%) 2.48 (8-14%)</td>
</tr>
</tbody>
</table>

\(^a\) Species printed in this table based on membership in Southern Pine or Spruce-Pine-Fir/Spruce-Pine-Fir-South groups. Species with lower COV in a group are printed. Full tests results available in [82].

\(^b\) Red spruce is printed for comparison with European species, i.e., Norway Spruce.

\(^c\) Specimen with stress relief.

\(^d\) Values from top to bottom: specimen with no knots, decayed knots, and sound knots.

At the element scale, beam tests have been implemented, often paired with the use of analytical equations, to determine the shear properties of CLT and the effect of certain conditions or features in the CLT specimens or timber used in the specimens. In some cases, researchers have used a combination ring scale tests, e.g., two-plate shear tests, and beam bending tests, e.g., three-point or five-point bending tests, to ensure accuracy in RS properties determined. Element scale tests have been carried out on CLT specimens mounted
in two-plate shear test fixtures to study system effects on RS properties and shear on other element planes. While these tests have been successful at producing the desired failure modes in a repeatable, reliable, and cost effective fashion, a criticism of the methods is that they fail to capture the "true" conditions of a CLT element under loading. Indeed, the purpose of such test fixtures is to isolate a pure, or nearly pure, shear stress state along the desired plane. Given this goal, boundary conditions, application of load, contact forces, and interaction of stresses normally seen in out-of-plane bending are non-existent in these tests. Additionally, observing volume effects present at the element scale may not be possible. ASTM D2718 has an option to use five-point bend tests, developed originally by Bradtmueller [77, 106] to determine RS modulus and strength in structural panels (e.g., plywood, OSB, laminated veneer lumber (LVL)). Bradtmueller’s method requires testing of specimen in both four-point and five-point bending configurations to determine the shear free modulus of elasticity, shear modulus, modulus of rupture, bending and in-plane shear strength. Alternatively, ANSI/APA PRG 320 recommends the use of ASTM D198 center-point load method for the evaluation of interlaminar shear stresses. The ANSI/APA PRG 320 modified ASTM D198 center-point load method involves the use of quasi-static center-point bend tests (a special case of three-point bend tests) on specimen with span-to-depth ratios of 5-6 to induce a greater degree of shear warping and hence a stronger rolling shear response in test specimens and ensure a high probability of shear failure. ASTM D198 recommends that the center-point method only be used for evaluating shear modulus. Despite this recommendation, researchers have been successful at using center point bend tests to determine RS strength with some researchers citing that specific shear testing methods are better suited for determining RS modulus. Norlin et al. [79] used three-point bending tests to determine study static RS and longitudinal shear strengths, fatigue performance, and to calibrate a damage accumulation model for LVL specimens. Norlin et al. found that three-point bend tests produced consistent RS strengths and that RS strength exhibits duration of load effects. Later, Mestek [78] used the shear analogy (SA) method and FEM analysis to
compare normal and shear stress distributions in simply supported CLT panels with \( L/d \geq 20 \) and concentrated loading. He concluded that there is significant shear deformation in the area within a distance \( d \) of the load. Furthermore, the SA method correctly determines longitudinal stresses, and while shear stress distributions adjacent to the load are not accurate, rolling shear stresses at a distance \( d \) from the load are accurate. Zhou et al. [80, 81] found that short span center-point bend tests were effective at determining RS strength and was less sensitive to material heterogeneity than two-plate shear tests. Moreover, Zhou et al. found that the short span bend tests provided advantages in simplicity of experimental implementation and specimen preparation as well as providing conditions that closely mimic loading conditions experienced in design applications. Li et al. [100, 101] compared short span \( (L/h = 6) \) center-point bend tests and SA method to a two-plate shear test and found that it was a suitable method for evaluating RS strength and investigating effects of changes to local parameters in the timber, i.e., board aspect ratio, board thicknesses, and tree species, on RS strength. Cao et al. [99] used data from center-point bending tests of CLT specimens and the SA method to compare results with two-plate shear tests. Cao et al. further confirmed that the RS strengths determined from the center-point bend tests and SA are accurate for CLT constructed of clear as well as knotted timber, and produces conservative results where variances were observed. Crovella [107] further demonstrated that the SA method and center-point bend tests can be used to determine apparent flexural stiffness with relatively high accuracy \( (\leq 5\%) \) given accurate timber species \( E/G \) ratios.

### 2.2.7 CLT Design: Analytical Methods

The mismatch in shear stiffnesses across laminae creates shear warping through the section of CLT panels subjected to bending (see Figure 2.9b). As such, the Euler-Bernoulli theory on beams is inappropriate for CLT undergoing bending and more rigorous approaches must be used. There are three analytical methods that have been used historically: 1) the \( \gamma \)-Method [108, 109, 110, 111, 112], 2) the Shear Analogy (SA) Method [60], and 3) the
Timoshenko beam shear correction method [113, 114]. These methods produce comparable and reasonable results for CLT panels with applied out-of-plane loading and are discussed further in this section.

Euler-Bernoulli beam theory assumes that plane sections that are perpendicular to the neutral axis prior to deformation, remain plane and perpendicular to the beam’s neutral axis during deformation. The theory thereby assumes that shear stresses are low in comparison to flexural stresses and that shear warping across the section in beams is negligible. These assumptions make Euler-Bernoulli beam theory inappropriate for analyzing and designing thick plates and beams or sandwich composites like CLT. As discussed, the following analytical methods incorporate shear deformations: 1) the $\gamma$-Method, 2) the Shear Analogy Method, and 3) the Timoshenko shear correction for prismatic bars. The $\gamma$-Method was developed by Mohler and later improved by Schelling and is currently featured as "Mechanically Jointed Beams" in Annex B of Eurocode 5 [59]. The theory featured in Eurocode 5 is a special case of Mohler’s theory and treats the longitudinally oriented laminae as load carrying members with the transverse laminae behaving as flexible shear connectors. A factor $0 \leq \gamma \leq 1$ is used to define transverse lamina stiffness based on degree of connectivity. The $\gamma$-method provides the state of stress and deformation for simply supported beams with sinusoidal loading [12, 115].

Next, the Shear Analogy method uses a plane frame model consisting of two virtual beams that are connected and offset by vertical web connectors, see Figure 2.12. Beam A is assigned the bending stiffness, $B_A$, of all of the laminae in the panel and has infinite shear rigidity (i.e., no shear deformations), $S_A$.

\[
B_A = \sum_{i=1}^{n} E_i \cdot I_i = \sum_{i=1}^{n} E_i \cdot b_i \cdot \frac{h_i^3}{12} \quad (2.1)
\]

\[
S_A = \infty \quad (2.2)
\]
Where:

\( b_i = \) width of lamina. Unit strip method can be used for CLT panels.

\( h_i = \) thickness of each lamina.

Beam B is assigned the shear stiffnesses, \( S_B \), of all of the laminae and the increase to moment of inertia due to each lamina’s distance away from the neutral axis, \( B_B \), calculated using the parallel axis theorem.

\[
S_B = \frac{a^2}{\sum_{i=2}^{n-1} \frac{1}{c_i} + \frac{h_1}{2 \cdot G_1 \cdot b_1} + \sum_{i=2}^{n-1} \frac{h_i}{G_i \cdot b_i} + \frac{h_n}{2 \cdot G_n \cdot b_n}}
\]

\( B_B = \sum_{i=1}^{n} E_i \cdot A_i \cdot z_i^2 \)

Where:

\( c_i = \) the flexible connection between the \( i \)th and \( (i+1) \)th layer. The links between the laminae are assumed to be perfectly rigid so \( c_i = 1 \). \[99\]

\( z_i = \) the distance between the centroid of each lamina and the neutral axis of the panel.

\( A_i = \) the cross sectional area of each lamina.

\( E_0 = \) the parallel to grain direction timber modulus of elasticity and used for longitudinal laminations (i.e., parallel to the grain direction), \( E_{90} \) is the perpendicular to grain direction timber modulus and used for cross laminations and assumed to be \( E_{90} = E_0/30 \).

\( G = \) the timber shear modulus and should be used for the longitudinal laminae, while the rolling shear modulus, \( G_{RT} \), is used for cross laminations.
The effective bending stiffness, \( EI_{\text{eff}} \), and apparent bending stiffness, \( EI_{\text{app}} \), may be determined as follows:

\[
EI_{\text{eff}} = B_A + B_B \tag{2.5}
\]

\[
EI_{\text{app}} = \frac{EI_{\text{eff}}}{1 + \frac{K_s EI_{\text{eff}}}{S_B L^2}} \tag{2.6}
\]

Where:

\( K_s \) is a constant that corrects apparent bending stiffness to include the effects of shear deformation. [12]. Karacabeyli et al. provide solved values for \( K_s \) for a beam with concentrated loading at midspan as \( K_s = 14.4 \) (pinned support) and \( K_s = 57.6 \) (fixed support).

Next, the axial stresses for each lamina can be found via the CLT moments and axial forces, \( M_A \) and \( N_B \) as follows:

\[
M_{A,i} = M_A \cdot \frac{E_i \cdot I_i}{B_A} \tag{2.7}
\]

\[
N_{B,i} = \pm M_B \cdot \frac{E_i \cdot A_i \cdot z_i}{B_B} \tag{2.8}
\]

\[
\sigma_{A,i} = \pm \frac{M_{A,i}}{I_i} \times \frac{h}{2} = \pm \frac{M_A}{B_A} \times \frac{E_i}{2} \times \frac{h}{2} \tag{2.9}
\]

\[
\sigma_{B,i} = \pm \frac{N_{B,i}}{A_i} = \frac{M_B}{B_B} \times \frac{E_i \cdot z_i}{A_i} \tag{2.10}
\]

Next, the shear stresses may be found via shear force, \( V_A \) as follows:
\[ V_{A,i} = V_A \cdot \frac{E_i \cdot I_i}{B_A} \]  
\[ \tau_{A,i} = -V_A \cdot \frac{E_i}{B_A} \cdot \left( \frac{e_i^2}{2} - \frac{h_i^2}{8} \right) \]  
\[ \tau_{A,max,i} = \frac{3}{2} \cdot \frac{V_A}{b \cdot h_i} \]  
\[ \tau_{B,i} = -\frac{V_B}{B_B} \cdot E_i \cdot \left( e_i - \frac{h_i}{2} \right) \cdot z_i \]  
\[ \tau_{B,max,i} = \frac{V_B}{B_B} \cdot E_i \cdot h_i \cdot z_i \]  

Where \( e_i \) is the distance to the area of interest from the neutral axis.

Note that the \( h/2 \) terms in \( \sigma_{A,i} \) are applicable to the specific case where a lamina with centroid at \( h/2 \) is used. The appropriate distance would be used from centroid to extreme fiber of the material for laminae with geometries other than rectangular. The vertical connectors have infinite axial rigidity and connect the two virtual beams ensuring that the beams share the same deflections. Beams may be discretized to analyze complex loading including multiple loads or to analyze continuous beams; superposition is then used to determine results. The U.S. Edition of the CLT Handbook [12] recommends the use of the shear analogy method; the PRG 320 also uses the shear analogy method in the determination of its reference design values; and the NDS features the method as of the 2015 edition.

\[ T_x = T_y = T_z = 0 \]
\[ R_x = R_y = 0 \]

**Figure 2.12: Shear Analogy Method Basic Framework (Adapted from [115])**

Finally, Timoshenko’s theory involves the application of a correction factor to the dif-
ferential equation for prismatic bars in bending to account for the effects of shear and "ro-
tary"/rotational inertia. Timoshenko’s theory assumes that plane sections remain plane like
Euler-Bernoulli, however, it does not maintain that the sections must remain perpendicular
to the neutral axis. It has been argued that the $\gamma$-method and the Shear Analogy method may
be more appropriate than Timoshenko’s theory due to it neglecting shear warping across
the section [115]. Bogensperger conducts a thorough comparison of the methods with a
high order FEM model in [115]. Some conclusions to be highlighted: Bogensperger et al.
concludes that all methods produce nearly identical results, varying by a few percentage
points, for single span CLT beams with $L/h \geq 30$ and uniform loading. Other researchers
have been able to subsequently show that the methods are comparable CLT beams with
$L/h \geq 15$ and similar boundary and loading conditions [52, 74]. Of the methods compared
by Bogensperger et al., the SA method most closely matches the FEM solution with ideal
pinned supports for all boundary condition configurations and load distributions (i.e., con-
centrated and uniform loading). Bogensperger notes that the error increases significantly
in the analytical methods as $L/h$ ratio is reduced. This occurs because at lower values of
$L/h$ ratio, the cross sectional planes are critically affected by shear deformation, while at
greater values the effects of shear deformation are not as severe. While all of the methods
produce comparable results with respect to the FEM solution, no experimental data was
used in the study for comparison. Since this study several researchers have since compared
the analytical methods with experimental data and have further validated the methods, in
particular the SA method [78, 96, 80, 81, 116, 104, 107, 99, 117].

2.2.8 Dynamic Properties of Cross-Laminated Timber

Live and simulated blast testing have been conducted on CLT and have confirmed the en-
hancement of CLT’s performance under high rate loading. To date, live and simulated blast
testing has primarily focused on the far-field blast loading. Additionally, much of the re-
search has involved the application of repeated applications of impulsive loading. This can
cause damage accumulation in the CLT specimens even though the panels may seemingly respond elastically. This phenomenom was observed in Poulin et al.’s experiments [118]. Weaver et al. [19] performed live-blast testing on three full-scale, two story CLT buildings at Tyndall Air Force Base. The three structures were constructed using a different grade of CLT for each structure (i.e., V1, V4, and E1). The first and second story heights were 12’-0” (3.658-m) and 14’-4” (4.369-m), respectively. As the walls for the structures were three lamination CLT, this resulted in a span-to-depth ratio of about 35 for the walls in each story. The structures were subjected to three surface burst loads of differing intensities using TNT weights of 32-lbs, 67-lbs, and 199-lbs. The first two shots were designed to load the structures elastically, while the third shot was designed to cause a plastic, post-peak response. The three shots resulted in respective incident impulses of 10.9 psi-ms (75.2 kPa-ms), 18 psi-ms (124.1 kPa-ms), and 33.3 psi-ms (229.6 kPa-ms) and peak reflected impulses of 19.9 psi-ms (137.2 kPa-ms), 32.9 psi-ms (226.8 kPa-ms), and 65.2 psi-ms (449.5 kPa-ms), respectively. In all testing, the structures performed as predicted and maintained their structural integrity - connections were intact and load carrying capacity was not adversely affected. Ductility ratios of 1 to 2 were generally reported for panels without openings. Weaver et al. conducted SDOF modeling and utilized a dynamic increase factor (DIF) of 1.25, viscous damping of 2%, idealized pin-roller boundary conditions, and the effect of axial loading was ignored. In general, the SDOF model overpredicted the response of the structures, particularly in the panels with openings. This error can be attributed to several factors including: model boundary conditions not being representative of the field conditions; simplification of load distribution (i.e., one-way versus two-way action); accounting for viscous damping despite rapid loading of the structures; and the effect of the axial load on panel stiffness is ignored. Weaver et al. states that an SDOF model provides a coarse approximation for the multiple degrees of freedom present in the buildings. Despite the results of the numerical modeling, the live blast tests demonstrated CLT’s efficacy in resisting a range of blast loading. CLT was demonstrated to be capable of sustaining its load
carry capability after local damage (e.g., rupture of the extreme laminae). Finally, the documented PRG 320 strength values for visually graded CLT and for the minor direction of 3-ply CLT were shown to be too conservative based on test data.

Poulin et al. [118] dynamically tested twelve spruce-pine-fir (SPF) CLT panels using a shock tube to simulate impulsive four-point bending tests. Most specimens were subjected to a low magnitude reflected impulse that would elicit an elastic response and then subjected to a higher reflected impulse to cause failure in the specimen. Reflected impulses that resulted in failure ranged from 56 psi-ms (380 kPa-ms) to 101 psi-ms (690.7 kPa-ms). The reflected impulses for the three lamination specimens were similar to those recorded by Weaver et al. [19] on their structures. The authors reported a DIF of 1.28 for the strength of the panels loaded at an intermediate strain rate loading ($10^{-2}$ to $10^{-1}$ s$^{-1}$), whereas no enhancement to stiffness was observed. The authors hypothesize that strain rate enhancement to strength occurs only in the longitudinal laminae of CLT allowing rolling shear failures to occur. However, this hypothesis does not adequately explain the inconsistency in failure modes (i.e., some flexural and some rolling shear) observed for similar loading conditions and applied energy. A possible explanation for the variation in failure modes can be attributed to the span-to-depth ratios used in this experiment. Poulin et al. utilize panels with span-to-depth ratios of 12.77 and 21.29 for three laminae and five laminae panels. At these ratios, a mix of flexural and shear failure modes can be expected. Additionally, the delivery of multiple reflected pulses with a shock tube may have caused damage accumulation to occur and it is possible that the rolling shear failure mode is sensitive to damage accumulation [119]. The delivery of multiple pulses also makes the observation of the final failure modes difficult and the investigator must rely on the limited perspective provided by the high speed camera footage. Other variables such as boundary conditions, strain rate, and material variability can also affect failure mode. Using the dynamic increase factor the authors are able to reasonably predict the response of CLT panels using an SDOF model.

Sanborn [15] developed a blast analysis tool for CLT that determines the CLT panel
response, level of protection, and expected damage. The tool uses an SDOF model and inputs for explosive properties, static loading, CLT geometry and material properties, structure properties, and UFC 3-340-30 data. Sanborn validated the tool using live blast testing data from Weaver et al. The DIF reported by Poulin et al. [118] is also used and seems to improve the tool’s prediction for inbound displacement at large blast loads. The tool provides good predictions for the inbound response for CLT panels tested in the elastic regime. Sanborn identifies areas of research that could improve the tool’s predictive ability including: testing of CLT’s post-peak response; inclusion of the negative phase of a blast load; furthering understanding of the rolling shear failure mode and blast response; and further understanding of strain rate enhancement that occurs in CLT under high strain rate loading.

Viau et al. [120] investigated the blast performance of connections of varying rigidity in SPF CLT. Realistic and idealized boundary conditions are explored in full-scale CLT panel specimen and in short, rigid CLT specimen that isolated connection behavior. Full-scale and component-scale CLT panels had a span-to-depth ratio of approximately 12 and 2.6, respectively. The full-scale specimens were subjected to impulsive four-point bending and reflected impulses ranging from 88 psi-ms (601 kPa-ms) to 120 psi-ms (824 kPa-ms), while the components were subject to reflected impulses ranging from 56 psi-ms (381 kPa-ms) to 102 psi-ms (700 kPa-ms). The testing reported a significant increase in ultimate resistance, DIF of 1.55, of the connections that permitted yielding of the steel angle and crushing of the wood. The DIF for CLT was consistent to that reported by Poulin et al. Additionally, a two degree of freedom model was developed to predict CLT response with high accuracy while accounting for the boundary condition linear stiffness. Based on their findings, Viau et al. recommended modifying timber blast design standards to allow for greater ductility in the connections, thereby allowing the connections to yield and absorb energy and redirect failure from the CLT panels. While this approach has merit, a balanced approach needs to be pursued that considers CLT post-peak behavior as well as added connection ductility in or-
der to prevent progressive collapse in CLT structures. Cote et al. [119] also tested the blast
performance of full-scale SPF CLT with realistic boundary conditions using a shock tube to
impulsively load specimens in a four-point bending configuration. The authors investigated
two dowel type connections and two bearing type connections: end-grain screws, double
threaded screws, thin and thick angle brackets, and balloon construction. Five lamination
CLT panels with a span-to-depth ratio of 11.9. Only one shot was applied per specimen to
avoid the effects of damage accumulation in the CLT panels. It was found that the dowel
type connections failed before the capacity of the CLT specimen could be reached. The
thin angle connection allowed for significant energy dissipation via steel angle yielding
and wood crushing, which corresponds with Viau et al.’s findings [120]. The thick angle
connection had slippage of about 0.70-in. (18-mm), but allowed rotation at the ends of
the specimen, thereby behaving as an idealized pin-pin connection. Balloon construction
forced failure into the panel as the connections prevented displacement or significant rota-
tion from occurring; flexural failure of the panel was the failure mode observed. Ultimately,
the connections tested were not conducive to simplified numerical models such as an SDOF
and require modeling of the connection.

Lopez-Molina et al. [121] performed research on steel strap and glass fibre-reinforced
polymer (GFRP) fabric retrofits on SPF CLT panels specimens. The specimens used in this
study were taken from the same batch as those used by Poulin et al. [118] and featured
similar dimensions leading to similar span-to-depth ratios for the three and five lamination
panels. The specimens were subjected to reflected impulses ranging from 84 psi-ms (578
kPa-ms) to 183 psi-ms (1259 kPa-ms). The CLT and GFRP experienced strain rates on
the order of $10^{-1}\, \text{s}^{-1}$ and the steel experienced strain rates on the order of $10^{-1}\, \text{s}^{-2}$. Hor-
izontal and vertical steel straps locations and order were varied in the specimens tested. In
the GFRP specimens, different layup configurations were also tested. Lopez-Molina et al.
found that steel straps did not have an affect on the ultimate strength of the specimens, but
had a significant effect on the post-peak resistance and ductility. Stiffness was improved
by 10-28% in steel strap specimens while post-peak resistance (i.e., equal to 50% of peak resistance) was improved by an average of 27% to 443% and ductility was improved by an average of 49% to 65%. The steel straps were effective at reducing or eliminating rolling shear and at acting as a containment system for wood debris. GFRP specimens showed an improvement to stiffness of 30% and 50% for three and five lamination panels. GFRP specimens also showed an improvement of 51% to peak resistance, 6640% to post-peak resistance, and 270% to ductility on average. Furthermore, a ductility ratio of 5 was reported for one of the GFRP specimens. GFRP specimens exhibited higher localization of failure as compared to unretrofitted specimens; little to no failure in the CLT; and flexural modes controlled where failure did occur. Overall, the retrofits were successful at improving the overall performance of the CLT panels, containing wood debris, and reducing or eliminating rolling shear as a failure mode.

Recently, Van Le [122] subjected Radiata pine CLT panels to simulated blast loading on a shock tube. Five lamination panels of two thicknesses, 5.12-in. (130-mm) and 5.7-in. (145-mm), were subjected to uniform pressure from the shock tube and span-to-depth ratios were 13.8 and 12.4 respectively. The panels were each subjected to four shots of increasing intensity and reflected impulses ranging from 258 kPa-ms to 1400 kPa-ms were applied to the specimens. The strain rate in the specimens was not reported. The panels remained elastic throughout all tests and no damage was reported. SDOF and numerical models were produced to predict a DIF to modulus of elasticity ranging from 1.12 to 1.57 and 1.05 to 1.43, respectively.

2.3 Experimental High Strain Rate Loading Techniques

As discussed previously, materials exhibit different mechanical responses based on the rate of loading applied. Many techniques have been developed to test materials over a spectrum of strain-rates. Table 2.3 summarizes strain rate regimes, mechanical behavior that is expected at those regimes, and techniques applicable to test within those regimes.
Table 2.3: Strain Rate Regimes (Adapted from [123, 124])

<table>
<thead>
<tr>
<th>Strain Rate ((s^{-1}))</th>
<th>Loading Regime</th>
<th>Experimental Technique</th>
<th>Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>(10^{-8}) to (10^{-6})</td>
<td>Creep and stress relaxation</td>
<td>Conventional servo-hydraulic testing machine</td>
<td>Inertial effects negligible.</td>
</tr>
<tr>
<td>(10^{-6}) to (10^{0})</td>
<td>Quasistatic</td>
<td>Conventional servo-hydraulic testing machine</td>
<td>Inertial effects negligible.</td>
</tr>
<tr>
<td>(10^{0}) to (10^{2})</td>
<td>Intermediate</td>
<td>Specialized testing machine</td>
<td>Inertial effects important.</td>
</tr>
<tr>
<td>(10^{2}) to (10^{4})</td>
<td>High</td>
<td>Conventional Hopkinson bar</td>
<td>Inertial effects important.</td>
</tr>
<tr>
<td>(10^{4}) to (10^{6})</td>
<td>Very High</td>
<td>Special Hopkinson bar or Taylor Impact</td>
<td>Inertial effects important.</td>
</tr>
<tr>
<td>(10^{6}) to (10^{8})</td>
<td>Ultra High</td>
<td>Plate impact</td>
<td>Inertial effects important and 1D stress impossible.</td>
</tr>
</tbody>
</table>

In the context of blast loading, intermediate to high strain rates, \(10^{0}\) to \(10^{2}s^{-1}\), are typically considered for far-field and close-in explosives. In the context of this research, impulsive loading that results in intermediate to high strain rate loading and the resulting behavior of CLT is of interest. Impulsive loading that generates strain rates ranging from \(10^{0}\) to \(10^{4}s^{-1}\) typically results in elasto-plastic behavior [125]. Zukas et al. [125] describe three regimes that exist for impacted solids: 1) elastic regime, 2) plastic regime, and 3) the shock wave regime. Per Zukas et al., in the elastic regime, elastic stress waves travel through a medium and the stress remains below yield stress for the material. Additionally, Hooke’s law is applicable in this case. In the plastic regime, large deformations, heating, and often failure occurs in the impacting and impacted solids through a variety of mechanisms. In the shock wave regime, solids are impacted at sufficiently high velocities that induce stresses that are several orders of magnitude higher than the impacted solid’s material strength. Shock wave loading results in solids behaving hydrodynamically and required the consideration of fundamental mechanics and physics extending beyond the mechanical properties of a material.

Testing that results in strain rates \(\geq 10s^{-1}\) in the test specimens will require testing methods that are more specialized than servo-hydraulic actuators. An additional consid-
eration for this research is the objective to experimentally test CLT at a structural scale. While many experimental methods exist to subject specimens to the full range of strain rate regimes, experimental techniques are limited that able to induce the strain rate regimes to structural scale specimens as is required for this research.

2.3.1 *Hammer-Type Tests*

Intermediate to high strain rates in structural scale specimens may be achieved via the implementation of a impact hammer testing. Several variations of impact hammer tests exist [126]: 1) drop hammer tests, 2) compressed gas driven hammer tests, and 3) electric rotary hammer tests. Drop hammer tests involve the elevation of a mass that is typically supported and guided by a frame to a measured height and at the initiation of a test the mass is allowed to drop freely. Fundamental physics equations and instrumentation of the test may be used to determine various quantities to be determined for the test and specimen, including energy applied, velocity, and strain rate. The compressed gas driven hammer is similar to the drop hammer test except that the hammer is accelerated by a piston in a compressed gas machine. Firing of the compressed gas driven hammer is typically used to subject specimens to compressive loading. An electric rotary flywheel machine utilizes an electric motor to rotate a flywheel to accelerate a rotary hammer that subjects a gage length specimen to tensile loading. Constant velocity of the specimen is ensured by having a hammer of sufficient mass. The primary limitation to these test methods lies in the degree of control over the impacting mass once the mass has been fired. To this author’s knowledge, no impulsive impact testing has been conducted on cross-laminated timber. Limited impact testing with hammer-type tests exist for wood composites. Li et al. [127] used drop hammer tests to apply dynamic compressive loading to 0.98-in. x 0.98-in. (25-mm x 25 mm) bamboo parallel strand lumber specimens. An impact striker was allowed to drop under the influence of gravity allow and impact the specimens. The bamboo parallel strand lumber specimens were subjected to strain rates on the order of $10 \, s^{-1}$ and exhibited
strain rate dependent behavior. As the specimens were small, it was not possible for the testing to observe structural scale effects.
CHAPTER 3
RESEARCH QUESTIONS AND OBJECTIVES

This research seeks to develop a better understanding of the behavior of CLT flexural members subjected to impulsive out-of-plane loading. Characterization of how mechanical properties of CLT are affected by blast loading is fundamental to its wider adoption as a force protection material. Currently, a lack of data for CLT loaded in the intermediate strain rate loading regime is a limiting factor.

This research seeks to provide data on several areas that remain unstudied or elusive. A high-speed hydraulic actuator called a blast generator, was used to investigate the conditions that promote shear failure modes. Boundary conditions, loading condition, and geometry of the CLT specimens were varied or optimized, as appropriate, to facilitate this study. The effect of boundary condition rotational rigidity on the overall behavior of the CLT specimens was investigated. Rotational rigidity was varied during testing to observe the degree to which energy was absorbed and dissipated by CLT via fracture of the laminae versus distribution to the boundaries. Next, residual capacity of CLT specimens following impulsive loading was determined experimentally. Residual capacity testing provided data on the effect of impulsive loading on properties such as strength, flexural stiffness, and ductility.

3.1 Research and Methodology Questions

The following questions served as a guide to this research and are answered by this research program. Questions are categorized and numbered according whether they are a research question (RQ) or experimental method question (MQ).

RQ. 1 Does loading condition and span-to-depth ratio in CLT loaded out-of-plane promote
a higher likelihood of shear modes of failure in impulsively loaded CLT?

Rolling shear has been observed either inconsistently or not at all in dynamic testing of CLT flexural members. Past research has focused on investigating CLT with large span-to-depth ratios, which promotes a largely flexural response by reducing the influence of shear deformations. Additionally, past research has sought to quantify stiffnesses along various material directions and has thus focused on loading conditions that create shear free spans, e.g., four-point bend tests. This research seeks to directly study the dynamic shear strength of CLT by optimizing the loading condition and geometry of the panel to encourage shear modes of failure.

**RQ. 2 Is strain-rate dependence observed in CLT shear strength and apparent stiffness values?**

While strain-rate dependence has been observed in CLT’s shear strength at low strain rates (i.e., \(\dot{\varepsilon} \approx 10^{-6}\)), amplification of shear strength due to intermediate to high strain rates (i.e., \(\dot{\varepsilon} \approx 10^{0} \text{ to } 10^{2}\)) is relatively unstudied. It is of interest to understand how mechanical properties such as strength, stiffness, and ductility change following impulsive loading. Knowledge is also desired on how these properties change based on the characteristics of the impulsive load, such as, peak force, load duration, and energy. In-situ quasi-static residual capacity testing is conducted following impulsive loading to study CLT’s strain-rate dependence.

**RQ. 3 How is the ductility of impulsively loaded CLT affected by rolling shear as a failure mode? Does rotational rigidity provided at the boundary conditions affect ductility?**

It is expected that CLT has greater ductility when rolling shear is the controlling mode of failure. This phenomenon has been observed in CLT that is statically loaded [84]. The relationship of change in ductility to increasing levels of impulse is studied. High levels of ductility (i.e., \(\mu \geq 3 \text{ to } 4\)) have not been observed in dynamically loaded...
CLT flexural members. A hypothesis is that CLT panels have not been tested with loading of sufficient magnitude to elicit high ductility. A secondary hypothesis is that in testing that utilized connections, connections with high rigidity redistributed and absorbed the energy delivered to the CLT members. Rotational rigidity is varied during dynamic testing of CLT flexural specimens in order to assess this hypothesis.

**MQ. 1 How do we advance the use of instrumentation to measure the input of impulse and output reaction impulse?**

Dynamic load cells can be used to determine the force transferred to a test specimen without affecting the response of the specimen. This is the first known instance of direct force measurement of the input impulse. This information is used to study the level of energy that causes varying levels of damage in CLT.
CHAPTER 4

EXPERIMENTAL TESTING OF CROSS-LAMINATED TIMBER PANELS
UNDER QUASI-STATIC CENTER-POINT LOADING

This research is divided into two phases: 1) Quasi-Static Testing and 2) Impulsive testing and Residual Capacity testing. In the first phase of this research, a series of quasi-static center-point flexural tests were conducted on low span-to-depth ratio CLT panel specimens. This phase of research serves to provide points for comparison to the dynamic tests conducted in second phase of testing and allows for the study of the specimens’ mechanical properties. The methodology, results, and their implications are discussed in this chapter.

In the second phase of this research, CLT specimens of similar geometry to those in this phase are impulsively loaded and subsequently tested for residual capacity. To achieve these goals, two testing fixtures were designed. The first fixture was a preliminary design that facilitated the three-point testing of CLT panels and is described in this chapter. The second fixture is an evolution of the first fixture’s design and is described in the next chapter.

4.1 Testing Rationale

4.1.1 Short-Span Flexural Tests

Spruce-pine-fir-south (SPFS) CLT specimens were subjected to short-span flexure to determine mechanical properties relevant for the design of the impulsive loading phase of this research and for the evaluation of the specimens’ performance overall. The ANSI/APA PRG 320-19 [64] provides recommendations for determining CLT panel structural performance via the use of representative test specimens of a certain quantity and dimension. As discussed in Chapter 2, a combination of four-point bending and center-point bending tests
are recommended by the ANSI/APA PRG 320 for determining various mechanical properties for CLT including rolling shear (RS) strength and stiffness. Note that center-point testing refers specifically to three-point tests with loading applied at the center, or midspan, of the specimen. Specifically, apparent bending stiffness, shear strength, and RS strength may be determined using center-point bending tests per ASTM D198 [72], while four-point bending tests must be used to determine RS stiffness, bending strength, and the shear-free modulus of elasticity. As characterization of the RS stiffness and bending strength of CLT panels is not a goal of this research, nor is deriving mechanical properties that are comparable to gross CLT panels, a fixture that facilitated the testing of CLT panels in a center-point configuration was determined to be appropriate.

CLT is a massive timber composite composed of laminae with vastly different moduli of elasticity ($E_{90} = E_0/30$). CLT panels that undergo out-of-plane bending experience greater shear deformations, i.e., shear warping, than flexural elements composed of other materials, e.g., steel. The effect of shear deformations is more pronounced at low values of span-to-depth ratio, $L/h \leq 14$ [81], $L/h \leq 15$ [115], or $L/h \leq 18$ [74]. At particularly low values of span-to-depth ratio, the effect of the shear deformations dominates the response of a CLT panel loaded out-of-plane, controlling 50%-60% of the overall deflection. Furthermore, at low values of $L/h$ interlaminar stresses in the cross laminations increase significantly, leading to a higher likelihood of RS failures. Zhou et al. [81] showed that the experimental methods they employed to determine rolling shear modulus in the cross layers of CLT overestimated the modulus when values for $L/h \geq 8$. This indicates that a value of $L/h$ approximately equal to eight or less appears to be an appropriate range to consider for experimental testing where RS modulus and strength are of interest. The ANSI/APA PRG 320-19 recommends the use of a center-point flexural test, a special case of three-point flexural tests, with a CLT specimen having a span-to-depth ratio of $5 \leq L/h \leq 6$. Short span center-point flexural tests have been used by several researchers to induce RS failures and determine RS strength in CLT [78, 80, 81, 128, 99, 107]. Experimental data and the
shear analogy method are used to determine the desired mechanical properties of the CLT specimens in this research.

4.2 Materials

Three-laminae and five-laminae SPFS CLT panels produced by SmartLam LLC using SPFS timber were sourced from Montana. The SmartLam SPFS CLT panels were manufactured to meet the ANSI/APA PRG 320-12 V4 grade for a custom layup consisting of SPFS lamellas [15]. Table 4.1 and Table 4.2 give the Required Characteristic Test Values and Allowable Design Properties per the PRG 320-12.

Table 4.1: Required Characteristic Test Values for CLT Grades and Layups per ANSI/APA PRG 320-12 (Adapted from [15]).

<table>
<thead>
<tr>
<th>CLT Grades</th>
<th>Major Strength Direction</th>
<th>Minor Strength Direction</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$f_{b,0}$</td>
<td>$E_0$</td>
</tr>
<tr>
<td>SL-V4</td>
<td>1,628</td>
<td>1.1</td>
</tr>
</tbody>
</table>

* U.S. manufacturer, SmartLam’s customized CLT grade

Table 4.2: Allowable Design Properties for Laminations Used in CLT Grades per ANSI/APA PRG 320-12 (Adapted from [15]).

<table>
<thead>
<tr>
<th>CLT Grades</th>
<th>Major Strength Direction</th>
<th>Minor Strength Direction</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$F_{b,0}$</td>
<td>$E_0$</td>
</tr>
<tr>
<td>SL-V4</td>
<td>775</td>
<td>1.1</td>
</tr>
</tbody>
</table>

The panels were cut into several specimens yielding specimens with 16-in. (406.4-mm) widths; thicknesses of 4.125-in. (104.78-mm) and 6.875-in. (174.63-mm) for three and five
laminae panels, respectively; and lengths varying based on a span-to-depth ratio of approximately $5 \leq L/h \leq 7$. Table 4.3 provides a summary on specimen geometry including span length ($L$), specimen width ($W$), and specimen depth ($h$). The naming convention used for the CLT specimens is CLT(No. of laminations) - (Test No. in the series)S, for example, CLT3-1S is a three-laminae panel and is the first test in the quasi-static test series. Specimens CLT3-1S ($L/h = 9.2$), CLT3-2S ($L/h = 9.2$), CLT5-1S ($L/h = 6.7$), and CLT5-2S ($L/h = 6.7$) were designed with a longer span to study the sensitivity of the span-to-depth ratio on the likelihood of shear failure. CLT3-3S and CLT3-4S ($L/h = 5.7$), CLT3-5S to CLT3-9S ($L/h = 6.3$), CLT5-3S and CLT5-4 ($L/h = 5.5$), and CLT5-5S to CLT5-10S ($L/h = 5.6$) were designed with a span-to-depth ratio that would elicit shear failure.

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Specimen Name</th>
<th>Geometry</th>
<th>L/h</th>
<th>L (in.)</th>
<th>W (in.)</th>
<th>h (in.)</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT3 1S &amp; 2S</td>
<td>9.2</td>
<td>38</td>
<td>16</td>
<td>4.125</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3S &amp; 4S</td>
<td>5.7</td>
<td>23.5</td>
<td>16</td>
<td>4.125</td>
<td></td>
<td></td>
</tr>
<tr>
<td>5S to 9S</td>
<td>6.3</td>
<td>25.875</td>
<td>16</td>
<td>4.125</td>
<td></td>
<td></td>
</tr>
<tr>
<td>CLT5 1S &amp; 2S</td>
<td>6.7</td>
<td>46</td>
<td>16</td>
<td>6.875</td>
<td></td>
<td></td>
</tr>
<tr>
<td>3S &amp; 4S</td>
<td>5.5</td>
<td>38</td>
<td>16</td>
<td>6.875</td>
<td></td>
<td></td>
</tr>
<tr>
<td>5S to 10S</td>
<td>5.6</td>
<td>38.5</td>
<td>16</td>
<td>6.875</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The ANSI/APA PRG 320-19 [64] recommends that bending specimens have at least a width of 12-in. (304.5-mm) and span-to-depth ratio of 30 and shear specimen have at least a width of 12-in. (305-mm) and span-to-depth ratio of 5-to-6. The laminae were face-glued with polyurethane adhesive [15] and finger joints were used for the lamellas where required. Laminae thicknesses, $t_l$, are approximately 1.375-in. (35-mm) with lamella widths, $w_l$, at approximately 4-in. (101.6-mm) yielding $w_l/t_l = 3$. Dimensions for this initial set of tests were selected to maximize the number of specimen cut from each panel, while
ensuring that each specimen had two lamellas across the width in the longitudinal laminae. Work by Crovella [107] and Steiger [129], has shown that the width used for testing CLT strip specimens behaving as beam-like structural elements is critical. Steiger has shown that at narrow widths, \( w \leq 11.8\)-in. (300-mm), local heterogeneities and defects affect a given specimen’s load-bearing behavior. The span-to-depth ratio was varied to observe the sensitivity of the ratio on the specimens’ probability of shear failure. As such, two tests at an \( L/h = 9.2 \) were conducted to observe if fewer shear failures occurred.

A handheld two-pin moisture meter, DelmHorst BD-10, was used to take measurements on the exterior surface of each face of the CLT specimen. The mean moisture content of the CLT panel specimens was 8.02%, which conforms to the ANSI/APA PRG 320-19 requirement that CLT specimens have a moisture content of 8% or higher. The panels had been stored in a climate controlled facility for several months, which provides an explanation for the relatively low moisture content found.

### 4.3 Methods

#### 4.3.1 Design of the Quasi-Static Center-Point Test Fixture

In the second phase of the research, the panels are tested impulsively using the Blast Simulator located at the Structures Laboratory at the Georgia Institute of Technology. Given the configuration of the Blast Simulator, impulsive testing requires that the panels be supported in a vertical orientation. Next, studying the effect of realistic boundary conditions on the behavior of CLT under impulsive loading was desired. To accomplish these goals, a clamping assembly was designed that would secure the CLT specimens in a vertical orientation; model pinned supports by permitting rotation of the specimen about the support; and allow for changes to rotational rigidity at the supports via clamping pressure. Although the quasi-static tests in the first phase involve loading of CLT specimens in a horizontal orientation, the clamping assembly was also used in this phase to permit a direct comparison to the tests conducted in the second phase.
The primary material used for the quasi-static test fixture was steel of various grades: ASTM A36 for plates, ASTM A992 for W-sections, ASTM A193 Gr. B7 for threaded rod, and ASTM A325 for bolts. Steel was selected to ensure that the fixture was sufficiently rigid for the quasi-static setup and the second phase of testing. Bolted connections were preferred in order to facilitate the repair of test fixture components in the event of damage. The fixture is best described as a two part assembly: 1) a clamping assembly, and 2) a supporting assembly. Figure 4.1 shows conceptual drawings of the testing fixture including an isometric view of the fixture and detail views of the clamp assembly and steel plate. Figure 4.2 shows the testing fixture following fabrication and assembly.

Figure 4.1: Design of Quasi-Static System: (a) Isometric View, (b) Clamp Assy. Side View, and (c) Clamp Assy. Bottom View
The fixture was designed so that the clamping assembly could be readily swapped between the quasi-static supporting assembly and the impulsive test supporting assembly, thus eliminating the need for additional material and design. A steel spacer is placed between the top of the W12x50 support beams and the bottom of the clamping assemblies for bolt clearance. The clamping assembly consists of two clamps composed of a set of 1.50 in. diameter solid steel rods acting as pins that are welded to steel plates. A set of rods and plates are connected via a threaded rod that is anchored to the bottom plate via double nuts. The rod size was selected to reduce the high-stress concentrations that develop in the specimen at the points of contact during testing. The clamps are able to accommodate panels of widths < 24-in. (609.6-mm), with the support extending across the entire width of the specimen. The span length is varied by moving the supports to accommodate the desired span length. The length of the specimen overhang was kept minimal (≤ 6-in. (152.4-mm)). While some have cited ASTM D198 [72] with regard to overhang length potentially affecting the shear properties of CLT, other studies have found no effect on the shear properties due to overhang length. Christiansen et al. [130] determined that overhang length did not affect the shear properties of glass-fiber polyester resin composite beams subjected to three-point bending. Furthermore, no effects resulting from overhang length, e.g., failures absent at or arresting prior to the overhangs, were noted during testing.
The load is applied via an impactor with a rounded face as a means to reduce high-stress concentrations caused by the concentrated load. Concentrated loads have been shown to affect the shear deformation of CLT in the area of the load, thereby changing the longitudinal strain [78, 72]. A relatively large diameter structural tube section, HSS10.75x0.5, was selected to achieve a radius to lamella thickness ratio of 4 (i.e., 5.375 in. / 1.375 in.). This radius was selected to conform to the recommendations in ASTM D198, radius of curvature 2-to-4 times the depth, and Hou [131], radius of curvature 2.5-to-3 times the depth. The tube was cut in half, two steel plates were welded to the tangent of the tube, and the assembly was completed by bolting to a rectangular plate as shown in Figure 4.3.

![Figure 4.3: Design of Impactor: (a) Isometric View, (b) Side View](image)

Rotational rigidity at the supports is provided via clamping force introduced into the clamping assembly. The clamping force is introduced via torque applied to the nut securing the top plate. The torque force is measured using a torque wrench and a conversion is applied to the torque reading to account for the use of an open ended tight-clearance offset socket, or "crowfoot wrench" (Figure 4.4); torque equation is given as Equation 4.1.
Figure 4.4: Torque Wrench

\[ T_W = \frac{T_A \cdot L}{L + A} \]  \hspace{1cm} (4.1)

Where:

\( T_A \) = Torque exerted at the end of the adapter

\( L \) = Distance between square drive and hand position

\( T_W \) = Wrench scale reading

\( A \) = Length of adapter or extension

The torque applied to the wrench is then converted to a bolt pretension force, or preload, which is assumed to be equal to the compressive force applied to the panel. Equation 4.2 from Bickford [132] describes a simple relationship between bolt pretension and applied torque.

\[ T = KDFP \]  \hspace{1cm} (4.2)
where:

\[
T = \text{the applied torque (} \text{in.-lbs., } N \cdot m) \\
D = \text{the nominal diameter of the bolt (} \text{in., mm}) \\
F_p = \text{the preload developed in the bolt (} \text{lbs, N}) \\
K = \text{a dimensionless constant called the nut factor, assumed to be 0.3.}
\]

While there is a potential for a high level of inaccuracy from this method of up to 30% [132], the method was deemed sufficient for this application. Greater accuracy in the pretension load and torque relationship may be achieved via experimentally establishing a relationship between the bolt and faying surface using a statistically significant population of bolt/faying surface specimen and directly measuring the applied torque and preload developed. Nut factor is assumed to be 0.3 for a stainless steel material contacting carbon steel, assembled in an "as received" condition per Table 1 in [132]. A torque of 15 in-lbs (1695 N-mm), unless noted otherwise, was applied to each of the (8) threaded rods in the clamping fixtures to allow for leveling of the clamping fixture plates and consistency in support alignment across tests. This applied torque produces an exerted torque of approximately 17 in-lbs (1921 N-mm) via Equation 4.1. Equation 4.2 yields a bolt pretension of 113 lbs. (502.6 N) per bolt and a total of 453 lbs. (2015 N) of clamping force per fixture, which amounts to 28.33 lbs./in. (4.96 N/mm) applied to the CLT specimens. This support condition was deemed to be "simply supported" given the relatively low clamping force; the potential for the force to be significantly less than calculated [132]; and the ability of the ends of the CLT specimens to uplift and rotate about the longitudinal pin axis under these conditions.
4.3.2 Quasi-Static Test Implementation

The testing fixture with specimen is fixed to support blocks that are centered below a hydraulic bottle jack with an impactor and load cell. A rigid steel frame is used to support the hydraulic bottle jack (Enerpac RRH606), load cell, and impactor above the testing fixture. Once the specimen has been placed in the testing fixture and properly aligned, the top plates on the clamp assemblies are leveled and the top nuts are torqued to 15 in-lbs. (1695 N-mm). The actuator stroke is controlled via a hand operated pump (Powerteam P300D Model B), which allows for load to be applied in a stepwise manner. The actuator load rate is controlled by the operator while observing a load-displacement curve and strain, i.e., longitudinal compressive and tensile. Figure 4.5 shows the quasi-static test fixture prior to loading.

Figure 4.5: Quasi-Static Assembly Prior to Test
4.3.3 Instrumentation of the Quasi-Static Tests

The applied force is measured via a load cell, Interface Model No. 1120AQ-50K, with a 50 kip (222.4 kN) capacity in compression. Prior to testing, the load cell was calibrated using an MTS 810 55 kip (244.7 kN) capacity uniaxial servo-hydraulic testing machine. An external excitation voltage of 5V was applied and the load to voltage slope was determined. Displacement was measured using string potentiometers, Celesco No. PT101-0010-111-1110 and PT1A-50UP-500-M6-SG, with maximum strokes of 20-in. (508-mm) and 50-in. (1270-mm), respectively. String potentiometers were placed at the midspan and at the quarter span. The string potentiometer at midspan was placed below a length of solid sawn lumber to prevent damage to the instrument. The signal to displacement slope was determined for the string potentiometers at the middle of the stroke within a range of 0-to-6-in. (152.4-mm), for example, 25-in. (635-mm) to 31-in. (787.4-mm) for the potentiometer with a 50 in. (1270-mm) stroke. The slope was determined as such because the maximum displacement for the panels was calculated to be within 2-to-3-in. (50.8-to-76.2-mm) and the middle of the stroke is the range that would be used during testing. An excitation voltage of 5V and the MTS 810 testing machine were used to determine the displacement to voltage slope. Strain gauges, Tokyo Measurement Labs No. PFL-20-11-3LJC, were applied to the tensile and compressive faces of the specimens. Two strain gauges at the midspan and the quarter span were applied to the tensile face and a strain gauge offset from the midspan was applied to the compressive face. The strain gauges were primarily used to capture data on strain rate and observe if there were any differences in strain rate on the compressive and tensile faces. Signal voltage data was recorded using a National Instruments NI cDAQ-9178 data acquisition system with NI9219 and NI9237 modules. A function was written in LabView to provide the mean for the data sampled at a sampling rate of 18,000 samples per second. The LabView function also subtracts the initial signal reading from the subsequent readings setting a baseline reading. An external power supply, BK Precision DC 1711, was used to provide power to the load cell and two string potentiometers.
4.3.4 *Quasi-Static Testing Procedure*

Panels were tested during the Summer of 2019 at the Structures Laboratory at Georgia Tech. The following procedure was followed to test the CLT specimen.

1. Mark specimens for placement of strain gauges and string potentiometers. A layer of polyester adhesive was used to fill the voids in the wood and bond the strain gauges to the surface of the CLT specimen. The adhesive was allowed to cure for a minimum of 3 hours. Wood screw eye hooks were to anchor the string potentiometers.

2. Mark and place support blocks according to the span length of the specimen to be tested. Testing fixture was aligned with respect to the impactor to ensure center placement along width and length of the specimen.

3. Affix testing fixture to the support blocks to prevent lateral movement of the fixture.

4. Mount CLT specimen to the testing fixture, clamp the specimen, and connect string potentiometers to the specimen. The CLT specimen was aligned on the testing fixture. Next, the top clamp plate in the clamp assembly was leveled and torque was applied with a torque wrench while ensuring that each threaded rod received the same amount of torque. Once a torque of 15 in-lbs (1695 N-mm) per threaded rod is achieved, the torque was checked at each rod and the final level of the top clamp plate was checked.

5. A load of approximately 100 lbs. (444.8 N) was applied and the impactor alignment was checked with respect to the specimen.

6. Load was applied via the hand operated pump while observing the load-displacement curve and longitudinal tensile and compressive strains to ensure consistent application of load. Load was applied until a post-peak load equal to approximately 20% of the peak load was read. Time to failure targeted was approximately 3-to-6 minutes.
4.3.5 Second Phase of Quasi-Static Testing

In the Spring of 2022, additional quasi-static tests were conducted to obtain additional data for undamaged CLT specimens. A total of 11 additional tests were completed: five tests on three laminae CLT and six on five laminae CLT. The clamping assemblies that provide the boundary conditions for the CLT specimens in this research and that were used in the first phase of quasi-static testing (Summer 2019) were also used in this phase. A servo-hydraulic actuator, rigid beam, and rigid testing frame were used to load specimens in this test series (Figure 7.8). Modifications to the loading fixture and support fixtures were implemented for the following reasons: 1) a servo-hydraulic actuator was used as it allowed for the control of loading rate with higher precision, 2) the modification to the support fixtures and loading fixture simplified the execution of testing and increased the repeatability of tests conducted. Instrumentation of the CLT panels in the second test phase included load cell and displacement readings taken by an MTS 407 controller. Strain measurements were not taken.
The procedure used for the second phase of quasi-static testing was similar to the first phase with some modifications. The following modified procedure was followed to find the capacity of the undamaged CLT panel specimens:

1. Bond bearing plates to both sides and ends of the specimen.

2. Mount and align the CLT panel specimen in the test fixture. The alignment of the specimen is checked to ensure that each specimen is centered in both horizontal directions over the supports and under the load head.

3. Clamp the specimen at the boundary conditions. The top clamping assembly plate is lowered onto the specimen, leveled, and a torque of 65 in-lbs is applied to each
threaded rod in each clamping assembly.

4. Load is applied at a rate of 0.1 in./min. A test is terminated once the post-peak capacity of the CLT specimen has reached approximately 20% of the peak load.

4.4 Theory and Calculation

4.4.1 Shear Analogy Method

Kreuzinger’s Shear Analogy Method (SAM) as outlined in the CLT Handbook [12] was used to determine the rolling shear strength of each of the specimens and to compare the experimental results to the analytical solution for Apparent Stiffness. The process will be briefly discussed and demonstrated with an example using data for specimen CLT3-1S. The methodology used is similar to that described in Section 2.2.7.

The Shear Analogy Method uses the concept of a plane frame model consisting of two virtual beams, Beam A and Beam B, connected by rigid vertical connectors to model the behavior of a CLT panel (Figure 4.7).

![Figure 4.7: Shear Analogy Method Virtual Beams (Adapted from [115]).](image)

The rigid vertical connectors act as the web members in the plane frame model; they ensure that there is continuity between the deflections in Beam A and Beam B and are assigned zero flexural stiffness at their ends (i.e., pinned connections). Beam A is assigned the flexural stiffness of the laminae in the panel, $B_A$, and assumed to have zero shear
deformation, \( S_A \) (also referred to as \( G \cdot A \)):

\[
B_A = \sum_{i=1}^{n} E_i \cdot I_i = \sum_{i=1}^{n} E_i \cdot b_i \cdot \frac{h_i^3}{12}
\]  

(4.3)

\[
S_A = \infty
\]  

(4.4)

Where:

\( b_i \) = width of panel. Unit strip method can be used for CLT panels.

\( h_i \) = thickness of each lamina.

Beam \( B \) is assigned the flexural stiffness due to the increase in moment of inertia due to the distance of a layer’s neutral axis from the panel’s neutral axis (i.e., parallel axis theorem or "Steiner" points per Kreuzinger), \( B_B \), and the shear stiffness of the panel, \( S_B \) (also referred to as \( GA_{\text{eff}} \)).

\[
S_B = \frac{a^2}{\sum_{i=2}^{n-1} \frac{h_i}{2 \cdot G_i \cdot b_i} + \sum_{i=2}^{n-1} \frac{h_i}{G_i \cdot b_i} + \frac{h_n}{2 \cdot G_n \cdot b_n}}
\]  

(4.5)

\[
B_B = \sum_{i=1}^{n} E_i \cdot A_i \cdot z_i^2
\]  

(4.6)

Where:

\( z_i \) = the distance between the centroid of each lamina and the neutral axis of the panel.

\( A_i \) = the cross sectional area of each lamina.

\( E_0 \) = the parallel to grain direction timber modulus of elasticity and used for longitudinal laminations (i.e., parallel to the grain direction), \( E_{90} \) is the perpendicular
to grain direction timber modulus and used for cross laminations and assumed to be \( E_{90} = E_0 / 30 \).

\( G \) is the timber shear modulus and should be used for the longitudinal laminae and assumed to be \( G_{90} = E_0 / 16 \). The rolling shear modulus, \( G_{RT} \), is used for cross laminations and assumed to be \( G_{90} = G_0 / 10 \).

\( E_{90} \) is very small for cross layers without edge gluing and is often set to zero to better model this condition. Material testing of the timber used in the cross laminae may be conducted to improve the analytical results, however, is not employed in this research.

To derive the apparent bending stiffness, \( EI_{\text{app}} \), the equation for the total deflection of a beam under a single center-point load (Equation 4.7) and the equation for deflection ignoring shear deflection (Equation 4.8) are set equal [72]. Note, the total deflection includes the flexural and shear deflection:

\[
\delta = \frac{PL}{48EI_{\text{eff}}} + \frac{PL}{4KGA_{\text{eff}}} \tag{4.7}
\]

\[
\delta = \frac{PL^3}{48EI_{\text{app}}} \tag{4.8}
\]

\[
\frac{PL^3}{48EI_{\text{app}}} = \frac{PL^3}{48EI_{\text{eff}}} + \frac{PL}{4KGA_{\text{eff}}} \tag{4.9}
\]

Per ASTM D198, \( K \) is the shear coefficient and is a reciprocal of the shape factor, a dimensionless quantity dependent on the cross-section of a specimen. \( K \) is 5/6 for a rectangular section. The above equation may be solved for apparent bending stiffness as follows:

\[
EI_{\text{app}} = \frac{EI_{\text{eff}}}{1 + \frac{12EI_{\text{eff}}}{15/6GA_{\text{eff}}L^2}} \tag{4.10}
\]
Note, the CLT Handbook uses a shear deformation constant, $K_s$, and solves for various load and boundary conditions. $K_s$ is equivalent to $12/(5/6) = 14.4$ and is the same value used in the equation above.

The effective bending stiffness, $EI_{\text{eff}}$, may be determined as follows:

$$EI_{\text{eff}} = B_A + B_B$$  \hfill (4.11)

$$B_A = \sum_{i=1}^{n} E_i \cdot \frac{b_i h_i^3}{12}$$

$B_A = 2(3,812,760.42 \text{lbf-in}^2) + 0.127 \text{lbf-in}^2$

$= 7,625,520.96 \text{lbf-in}^2$

### 4.4.2 Example Computation for a Three Layer Panel

The geometric and material properties, and Shear Analogy terms are summarized for a SPFS 3-Layer CLT panel in Table 4.4.

**Table 4.4: Three Layer CLT Panel**

<table>
<thead>
<tr>
<th>Layer</th>
<th>$E_i$ (x 10^6 psi)</th>
<th>$z_i$ (in.)</th>
<th>$b_i$ (in.)</th>
<th>$h_i$ (in.)</th>
<th>$E_i b_i h_i^3/12$</th>
<th>$E_i A_i z_i^2$</th>
<th>$B_A + B_B$ (lbf-in^2)</th>
<th>$G_i$ (psi)</th>
<th>$h_i/(G_i b_i)$ (1/psi)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.1</td>
<td>1.375</td>
<td>16</td>
<td>1.375</td>
<td>3,812,760</td>
<td>45,753,125</td>
<td>49,565,885</td>
<td>68,750</td>
<td>1.25E-06</td>
</tr>
<tr>
<td>2</td>
<td>0.037</td>
<td>0</td>
<td>16</td>
<td>1.375</td>
<td>127,000</td>
<td>0</td>
<td>0.127</td>
<td>6875</td>
<td>1.25E-05</td>
</tr>
<tr>
<td>3</td>
<td>1.1</td>
<td>1.375</td>
<td>16</td>
<td>1.375</td>
<td>3,812,760</td>
<td>45,753,125</td>
<td>49,565,885</td>
<td>68,750</td>
<td>1.25E-06</td>
</tr>
</tbody>
</table>

Using the values in Table 4.4, the following are computed:

$$B_A = \sum_{i=1}^{n} E_i \cdot \frac{b_i h_i^3}{12}$$

$$B_A = 2(3,812,760.42 \text{lbf-in}^2) + 0.127 \text{lbf-in}^2$$

$$= 7,625,520.96 \text{lbf-in}^2$$
\[ B_B = \sum_{i=1}^{n} E_i \cdot A_i \cdot z_i^2 \]
\[ B_B = 2(45,753,125 \text{ lbf-in}^2) + 0 \]
\[ = 91,506,250 \text{ lbf-in}^2 \]  
(4.14)

\[ S_B = \frac{a^2}{\left[ \sum_{i=2}^{n-1} \frac{h_i}{2 \cdot G_1 \cdot b_1} + \sum_{i=2}^{n-1} \frac{h_i}{G_i \cdot b_i} + \frac{h_n}{2 \cdot G_n \cdot b_n} \right]} \]
\[ = \frac{(2 \cdot 1.375 \text{ in.})^2}{1.25E-06 \text{ psi} + 1.25E-05 \text{ psi} + 1.25E-06 \text{ psi}} \]
\[ = 550,000 \text{ lbf} \]  
(4.15)

\[ EI_{eff} = B_A + B_B = 1,672,465.28 \text{ lbf-in}^2 + 91,506,250 \text{ lbf-in}^2 \]
\[ = 99,131,771 \text{ lbf-in}^2 \]

\[ EI_{app} = \frac{EI_{eff}}{1 + \frac{EI_{eff} \cdot K_s}{GA_{eff} \cdot L^2}} \]
\[ = \frac{99,131,771 \text{ lbf-in}^2}{1 + \frac{99,131,771 \text{ lbf-in}^2 \cdot 14.4}{550,000 \text{ lbf} \cdot (38 \text{ in.})^2}} \]
\[ = 35,437,071.17 \text{ lbf-in}^2 \]  
(4.16)

An analytical or numerical method (e.g., FEM model) can be used to solve for the forces in the plane frame model, including the shear forces in each of the virtual beams. A plane frame model was created in MASTAN2 [133] (Figure 4.8) to verify the shear forces in each of the virtual beams using Kreuzinger’s method as outlined in the CLT Handbook. The stiffnesses for each of the virtual beams computed above were assigned to the corresponding element in the model. Virtual values for the beam areas and moments of inertia were determined to produce the calculated virtual beam stiffnesses. The plane frame model produced the following shear forces: \( V_A = 231.8 \text{ lbf} \) (1031.1 N) and \( V_B = 2776 \text{ lbf} \) (12,348 N).
Bogensperger and the CLT Handbook rely on numerical or analytical methods to solve the plane frame model for the requisite forces. An analytical method that could directly solve for the shear forces was desired for simple implementation. While other authors [99] have derived equations for directly determining shear in the virtual beams, a drawback to this method is its use of apparent stiffness to calculate stress values. Cao [99] uses the ratio of the virtual beam B stiffness to the total apparent stiffness of the panel multiplied to the maximum real shear force to obtain the virtual beam B shear force. In contrast to Cao’s method, using the effective stiffness produced more appropriate results that were consistent with the CLT Handbook and Bogensperger methodologies. Additionally, using the effective stiffness produced rolling shear strengths comparable to those determined by others [134] for SPF CLT with lamellas of similar properties to those used in this research. Another observation is that using apparent stiffness is more appropriate for deflection or vibration based calculations where a reduction to effective stiffness is warranted in order to account for the greater deflection attributed to shear deformations in the panel.

The shear forces were determined using the effective flexural stiffness as demonstrated
below.

\[ V_A = V_{\text{total}} \cdot \frac{B_A}{EI_{\text{eff}}} \]
\[ = \frac{20650 \text{lbf}}{2} \cdot \frac{1,672,465.28 \text{ lbf-in}^2}{99,131,771 \text{ lbf-in}^2} \]
\[ = 794.23 \text{lbf} \quad \text{(4.17)} \]

\[ V_B = V_{\text{total}} \cdot \frac{B_B}{EI_{\text{eff}}} \]
\[ = \frac{20650 \text{lbf}}{2} \cdot \frac{91,506,250 \text{ lbf-in}^2}{99,131,771 \text{ lbf-in}^2} \]
\[ = 9530.80 \text{lbf} \]

The shear forces can then be used to determine the shear stresses in each of the virtual beams, which can be used to determine the shear stress at any point along the thickness of the panel. The rolling shear stress may be determined by using the maximum load resisted by the panel to determine the maximum shear stress. The following stresses are computed at the neutral axis where the maximum shear stress occurs in a three layer panel, per Bogensperger [115].

\[ \tau_{A,i} = -V_A \cdot \frac{E_i}{B_A} \cdot \left( \frac{z_i^2}{2} - \frac{h_i^2}{8} \right) \]
\[ = 794.23 \text{ lbf} \cdot \frac{36700 \text{ psi}}{11,672,465.28 \text{ lbf-in}^2} \cdot \left( \frac{1.375^2}{2} - \frac{1.375^2}{8} \right) \text{ in}^2 \]
\[ = 2.71 \times 10^{-6} \text{ psi} \]

\[ \tau_{B,i} = \frac{V_B}{B_B} \cdot E_i \cdot \left( z_i - \frac{h_i}{2} \right) \cdot e_i \]
\[ = \frac{9530.80 \text{ lbf}}{91,506,250 \text{ lbf-in}^2} \cdot 1,100,000 \text{ psi} \cdot (1.375 \text{ in.} - 1.375 \text{ in.}/2) \cdot 2.75 \text{ in.} \]
\[ = 216.61 \text{ psi} \]
\[ \therefore \tau_{RS} = \tau_A + \tau_B = 216.61 \text{ psi} \]

Where:
\[ e_i = \text{the distance the centroids of the extreme laminae in the panel.} \]

The results of applying the shear analogy method to the remaining quasi-static tests are summarized in Tables 4.5, 4.6, 4.7, 4.8.

Table 4.5: Shear Analogy Method Analysis for Three Laminae CLT

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>( B_A ) kip-in(^2 )</th>
<th>( B_B ) kip-in(^2 )</th>
<th>( E I_{\text{eff}} ) kip-in(^2 )</th>
<th>( E I_{\text{app}} ) kip-in(^2 )</th>
<th>( K_s ) in</th>
<th>L in.</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT3-1</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>35,000</td>
<td>14.4</td>
<td>38.000</td>
<td></td>
</tr>
<tr>
<td>CLT3-2S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>35,000</td>
<td>14.4</td>
<td>38.000</td>
<td></td>
</tr>
<tr>
<td>CLT3-3S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>17,000</td>
<td>14.4</td>
<td>23.500</td>
<td></td>
</tr>
<tr>
<td>CLT3-4S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>17,000</td>
<td>14.4</td>
<td>23.500</td>
<td></td>
</tr>
<tr>
<td>CLT3-5S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>20,300</td>
<td>14.4</td>
<td>25.875</td>
<td></td>
</tr>
<tr>
<td>CLT3-6S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>20,300</td>
<td>14.4</td>
<td>25.875</td>
<td></td>
</tr>
<tr>
<td>CLT3-7S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>20,300</td>
<td>14.4</td>
<td>25.875</td>
<td></td>
</tr>
<tr>
<td>CLT3-8S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>20,300</td>
<td>14.4</td>
<td>25.875</td>
<td></td>
</tr>
<tr>
<td>CLT3-9S</td>
<td>7,600</td>
<td>92,000</td>
<td>99,000</td>
<td>20,300</td>
<td>14.4</td>
<td>25.875</td>
<td></td>
</tr>
</tbody>
</table>

\(^{a}\) Simply supported beam with a concentrated load at midspan per Table 2 Section 2.1.3 in [12].
<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>$B_A$ kip-in$^2$</th>
<th>$B_B$ kip-in$^2$</th>
<th>$EI_{eff}$ kip-in$^2$</th>
<th>$EI_{app}$ kip-in$^2$</th>
<th>$K_s^a$</th>
<th>L in.</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT5-1S</td>
<td>11,000</td>
<td>369,000</td>
<td>380,000</td>
<td>113,000</td>
<td>14.4</td>
<td>46.000</td>
<td></td>
</tr>
<tr>
<td>CLT5-2S</td>
<td>11,000</td>
<td>369,000</td>
<td>380,000</td>
<td>113,000</td>
<td>14.4</td>
<td>46.000</td>
<td></td>
</tr>
<tr>
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<td>11,000</td>
<td>369,000</td>
<td>380,000</td>
<td>86,000</td>
<td>14.4</td>
<td>38.000</td>
<td></td>
</tr>
<tr>
<td>CLT5-4S</td>
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<td>369,000</td>
<td>380,000</td>
<td>85,000</td>
<td>14.4</td>
<td>38.000</td>
<td></td>
</tr>
<tr>
<td>CLT5-5S</td>
<td>11,000</td>
<td>369,000</td>
<td>380,000</td>
<td>87,000</td>
<td>14.4</td>
<td>38.500</td>
<td></td>
</tr>
<tr>
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<td>369,000</td>
<td>380,000</td>
<td>87,000</td>
<td>14.4</td>
<td>38.500</td>
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</tr>
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<td>369,000</td>
<td>380,000</td>
<td>87,000</td>
<td>14.4</td>
<td>38.500</td>
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</tr>
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<td>380,000</td>
<td>87,000</td>
<td>14.4</td>
<td>38.500</td>
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<td>380,000</td>
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<td>14.4</td>
<td>38.500</td>
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</tr>
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<td>380,000</td>
<td>87,000</td>
<td>14.4</td>
<td>38.500</td>
<td></td>
</tr>
</tbody>
</table>

$^a$ Simply supported beam with a concentrated load at midspan per Table 2 Section 2.1.3 in [12].
Table 4.7: Shear Analogy Method Analysis for Three Lamine CLT

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>$P_{\text{max}}/2$</th>
<th>L/h</th>
<th>$V_A$</th>
<th>$V_B$</th>
<th>$\tau_A$</th>
<th>$\tau_B$</th>
<th>$\tau_{RS}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT3-1S</td>
<td>10,325.50</td>
<td>9.20</td>
<td></td>
<td>794.23</td>
<td>9,530.80</td>
<td>2.71E-06</td>
<td>216.61</td>
<td>216.61</td>
</tr>
<tr>
<td>CLT3-2S</td>
<td>10,617.00</td>
<td>9.20</td>
<td></td>
<td>816.54</td>
<td>9,798.50</td>
<td>2.78E-06</td>
<td>222.69</td>
<td>222.69</td>
</tr>
<tr>
<td>CLT3-3S</td>
<td>19,677.00</td>
<td>5.70</td>
<td></td>
<td>1,513.46</td>
<td>18,161.50</td>
<td>5.16E-06</td>
<td>412.76</td>
<td>412.76</td>
</tr>
<tr>
<td>CLT3-4S</td>
<td>16,006.50</td>
<td>5.70</td>
<td></td>
<td>1,231.15</td>
<td>14,773.80</td>
<td>4.20E-06</td>
<td>335.77</td>
<td>335.77</td>
</tr>
<tr>
<td>CLT3-5S</td>
<td>15,208.00</td>
<td>6.30</td>
<td></td>
<td>1,170.00</td>
<td>14,040.00</td>
<td>3.99E-06</td>
<td>319.09</td>
<td>319.09</td>
</tr>
<tr>
<td>CLT3-6S</td>
<td>12,190.00</td>
<td>6.30</td>
<td></td>
<td>937.69</td>
<td>11,252.30</td>
<td>3.20E-06</td>
<td>255.73</td>
<td>255.73</td>
</tr>
<tr>
<td>CLT3-7S</td>
<td>12,453.00</td>
<td>6.30</td>
<td></td>
<td>958.08</td>
<td>11,496.90</td>
<td>3.27E-06</td>
<td>261.29</td>
<td>261.29</td>
</tr>
<tr>
<td>CLT3-8S</td>
<td>13,661.50</td>
<td>6.30</td>
<td></td>
<td>1,050.77</td>
<td>12,609.20</td>
<td>3.58E-06</td>
<td>286.57</td>
<td>286.57</td>
</tr>
<tr>
<td>CLT3-9S</td>
<td>13,671.75</td>
<td>6.30</td>
<td></td>
<td>1,051.54</td>
<td>12,618.50</td>
<td>3.58E-06</td>
<td>286.78</td>
<td>286.78</td>
</tr>
</tbody>
</table>
### Table 4.8: Shear Analogy Method Analysis for Five Laminae CLT

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>( P_{\text{max}}/2 ) L/h</th>
<th>( V_A ) lbf</th>
<th>( V_B ) lbf</th>
<th>( \tau_A ) psi</th>
<th>( \tau_B ) psi</th>
<th>( \tau_{RS} ) psi</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT5-1S</td>
<td></td>
<td>18,483.50</td>
<td>6.70</td>
<td>567.63</td>
<td>17,917.40</td>
<td>6.31E-06</td>
<td>201.92</td>
</tr>
<tr>
<td>CLT5-2S</td>
<td></td>
<td>14,054.50</td>
<td>6.70</td>
<td>431.60</td>
<td>13,623.40</td>
<td>4.80E-06</td>
<td>153.53</td>
</tr>
<tr>
<td>CLT5-3S</td>
<td></td>
<td>19,594.00</td>
<td>5.50</td>
<td>601.72</td>
<td>18,993.30</td>
<td>6.69E-06</td>
<td>214.05</td>
</tr>
<tr>
<td>CLT5-4S</td>
<td></td>
<td>20,720.00</td>
<td>5.50</td>
<td>636.26</td>
<td>20,083.70</td>
<td>7.07E-06</td>
<td>226.34</td>
</tr>
<tr>
<td>CLT5-5S</td>
<td></td>
<td>19,256.50</td>
<td>5.60</td>
<td>591.28</td>
<td>18,663.70</td>
<td>6.57E-06</td>
<td>210.33</td>
</tr>
<tr>
<td>CLT5-6S</td>
<td></td>
<td>17,447.50</td>
<td>5.60</td>
<td>535.85</td>
<td>16,914.20</td>
<td>5.96E-06</td>
<td>190.62</td>
</tr>
<tr>
<td>CLT5-7S</td>
<td></td>
<td>17,629.50</td>
<td>5.60</td>
<td>541.38</td>
<td>17,088.60</td>
<td>6.02E-06</td>
<td>192.58</td>
</tr>
<tr>
<td>CLT5-8S</td>
<td></td>
<td>16,803.50</td>
<td>5.60</td>
<td>516.04</td>
<td>16,289.00</td>
<td>5.74E-06</td>
<td>183.57</td>
</tr>
<tr>
<td>CLT5-9S</td>
<td></td>
<td>19,749.50</td>
<td>5.60</td>
<td>606.48</td>
<td>19,143.50</td>
<td>6.74E-06</td>
<td>215.74</td>
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<tr>
<td>CLT5-10S</td>
<td></td>
<td>18,238.50</td>
<td>5.60</td>
<td>560.11</td>
<td>17,679.90</td>
<td>6.23E-06</td>
<td>199.25</td>
</tr>
</tbody>
</table>

### 4.5 Experimental Results and Discussion

#### 4.5.1 Quasi-Static Test Results

Nineteen CLT panels, nine three-laminae and ten five-laminae, were quasi-statically tested in a three-point testing configuration until failure. Panels were tested to failure where a panel was considered to have reached failure when its recorded post-peak load carrying capacity dropped below 20% of its maximum load.

Tables 4.9, 4.10, 4.11 and 4.12 summarize the results from the experimental series.
Table 4.9: Three Laminae CLT Static Test Results Summary

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>$L_{span}^a$ in.</th>
<th>$P_{max}^b$ kips</th>
<th>$\Delta_{peak}^c$ in.</th>
<th>$\Delta_{fail}^d$ in.</th>
<th>$\mu^e$</th>
<th>$t_f^f$ min.</th>
<th>$\dot{\epsilon}^g$ s$^{-1}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT3-1S</td>
<td>38.00</td>
<td>20.65</td>
<td>0.38</td>
<td>2.37</td>
<td>6.30</td>
<td>3.99</td>
<td>5.16E-05</td>
<td></td>
</tr>
<tr>
<td>CLT3-2S</td>
<td>38.00</td>
<td>21.23</td>
<td>0.41</td>
<td>1.80</td>
<td>4.41</td>
<td>2.77</td>
<td>1.14E-05</td>
<td></td>
</tr>
<tr>
<td>Average</td>
<td>-</td>
<td>20.94</td>
<td>0.39</td>
<td>2.08</td>
<td>5.36</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>COV$^h$</td>
<td>-</td>
<td>1%</td>
<td>4%</td>
<td>14%</td>
<td>18%</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>CLT3-3S*</td>
<td>23.50</td>
<td>39.35</td>
<td>0.43</td>
<td>1.00</td>
<td>2.29</td>
<td>2.62</td>
<td>5.86E-05</td>
<td></td>
</tr>
<tr>
<td>CLT3-4S</td>
<td>23.50</td>
<td>32.01</td>
<td>0.35</td>
<td>2.00</td>
<td>5.77</td>
<td>3.588</td>
<td>-**</td>
<td></td>
</tr>
<tr>
<td>Average</td>
<td>-</td>
<td>35.68</td>
<td>0.46</td>
<td>1.50</td>
<td>3.23</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>CLT3-5S</td>
<td>25.88</td>
<td>30.42</td>
<td>1.06</td>
<td>1.75</td>
<td>1.65</td>
<td>17.47</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>CLT3-6S</td>
<td>25.88</td>
<td>24.38</td>
<td>0.68</td>
<td>1.15</td>
<td>1.69</td>
<td>11.48</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>CLT3-7S</td>
<td>25.88</td>
<td>24.91</td>
<td>0.77</td>
<td>2.01</td>
<td>2.61</td>
<td>20.13</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>CLT3-8S</td>
<td>25.88</td>
<td>27.32</td>
<td>0.95</td>
<td>1.65</td>
<td>1.74</td>
<td>16.51</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>CLT3-9S</td>
<td>25.88</td>
<td>27.34</td>
<td>0.88</td>
<td>1.34</td>
<td>1.53</td>
<td>13.43</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>Average</td>
<td>-</td>
<td>26.87</td>
<td>0.87</td>
<td>1.58</td>
<td>1.84</td>
<td>15.80</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>COV</td>
<td>-</td>
<td>8%</td>
<td>15%</td>
<td>19%</td>
<td>21%</td>
<td>19%</td>
<td>-</td>
<td></td>
</tr>
</tbody>
</table>

$^a$ Span Length  
$^b$ Maximum Load  
$^c$ Midspan displacement at the maximum load  
$^d$ Midspan displacement at failure of the panel  
$^e$ Ductility Ratio ($\Delta_{fail}/\Delta_{peak}$)  
$^f$ Time to failure of the panel  
$^g$ Strain Rate  
$^h$ Coefficient of Variation  
* Midspan string pot. did not record load. Quarter span data is reported here.  
** Tensile strain gauge was damaged during testing.
<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>Span Length (L_{\text{span}}^{a}) in.</th>
<th>Maximum Load (P_{\text{max}}^{b}) kips</th>
<th>Midspan Displacement at the Maximum Load (\Delta_{\text{peak}}^{c}) in.</th>
<th>Midspan Displacement at Failure of the Panel (\Delta_{\text{fail}}^{d}) in.</th>
<th>Ductility Ratio ((\Delta_{\text{fail}}/\Delta_{\text{peak}}))</th>
<th>Time to Failure of the Panel (t_{\text{f}}^{f}) min.</th>
<th>Strain Rate (\dot{\epsilon}_{g})</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT 5</td>
<td>CLT5-1S</td>
<td>46.00</td>
<td>36.97</td>
<td>0.45</td>
<td>3.02</td>
<td>6.69</td>
<td>4.69</td>
<td>4.41E-05</td>
</tr>
<tr>
<td></td>
<td>CLT5-2S</td>
<td>46.00</td>
<td>28.11</td>
<td>0.32</td>
<td>2.81</td>
<td>8.74</td>
<td>5.80</td>
<td>4.04E-05</td>
</tr>
<tr>
<td></td>
<td>CLT5-3S</td>
<td>38.00</td>
<td>39.19</td>
<td>0.36</td>
<td>2.77</td>
<td>7.62</td>
<td>4.06</td>
<td>3.25E-05</td>
</tr>
<tr>
<td></td>
<td>CLT5-4S</td>
<td>38.00</td>
<td>41.44</td>
<td>0.34</td>
<td>2.78</td>
<td>8.11</td>
<td>5.75</td>
<td>4.96E-05</td>
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<td>COV</td>
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<td>13%</td>
<td>4%</td>
<td>10%</td>
<td>14%</td>
<td>-</td>
</tr>
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<td>2.44</td>
<td>26.89</td>
<td>-</td>
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<tr>
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<td>CLT5-6S</td>
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<td>1.13</td>
<td>2.32</td>
<td>2.06</td>
<td>23.23</td>
<td>-</td>
</tr>
<tr>
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<td>CLT5-7S</td>
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<td>1.11</td>
<td>2.20</td>
<td>1.98</td>
<td>22.00</td>
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<td>CLT5-8S</td>
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<td>3.08</td>
<td>3.59</td>
<td>30.84</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>CLT5-9S</td>
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<td>39.50</td>
<td>1.07</td>
<td>2.75</td>
<td>2.57</td>
<td>27.51</td>
<td>-</td>
</tr>
<tr>
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<td>CLT5-10S</td>
<td>38.50</td>
<td>36.48</td>
<td>1.14</td>
<td>2.49</td>
<td>2.19</td>
<td>24.91</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>Average</td>
<td>-</td>
<td>36.38</td>
<td>1.07</td>
<td>2.59</td>
<td>2.47</td>
<td>25.90</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>COV</td>
<td>-</td>
<td>6%</td>
<td>9%</td>
<td>11%</td>
<td>22%</td>
<td>11%</td>
<td>-</td>
</tr>
</tbody>
</table>

\(^{a}\) Span Length  
\(^{b}\) Maximum Load  
\(^{c}\) Midspan displacement at the maximum load  
\(^{d}\) Midspan displacement at failure of the panel  
\(^{e}\) Ductility Ratio \((\Delta_{\text{fail}}/\Delta_{\text{peak}})\)  
\(^{f}\) Time to failure of the panel  
\(^{g}\) Strain Rate  
\(^{h}\) Coefficient of Variation
Table 4.11: Three Laminae Static Test Results Summary

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>$M_{max}^a$ in-kips</th>
<th>$K_b^b$ kip/in.</th>
<th>$E_{app}^c$ psi</th>
<th>$EI_{app}^d$ lbf-in²</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT3-1S</td>
<td>196.20</td>
<td>66.32</td>
<td>810.35E+03</td>
<td>75.8E+06</td>
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</tr>
<tr>
<td>CLT3-2S</td>
<td>201.71</td>
<td>57.22</td>
<td>699.23E+03</td>
<td>65.4E+06</td>
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</tr>
<tr>
<td>Average</td>
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<td>61.77</td>
<td>754.79E+03</td>
<td>70.6E+06</td>
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</tr>
<tr>
<td>COV</td>
<td>1%</td>
<td>7%</td>
<td>7%</td>
<td>7%</td>
<td></td>
</tr>
<tr>
<td>CLT3-3S</td>
<td>231.14</td>
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<td>458.30E+03</td>
<td>42.9E+06</td>
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</tr>
<tr>
<td>CLT3-4S</td>
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<td>133.44</td>
<td>385.32E+03</td>
<td>36.1E+06</td>
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</tr>
<tr>
<td>Average</td>
<td>209.59</td>
<td>146.08</td>
<td>421.81E+03</td>
<td>39.5E+06</td>
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</tr>
<tr>
<td>COV</td>
<td>10%</td>
<td>9%</td>
<td>9%</td>
<td>9%</td>
<td></td>
</tr>
<tr>
<td>CLT3-5S</td>
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<td>46.18</td>
<td>178.02E+03</td>
<td>16.70E+06</td>
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</tr>
<tr>
<td>CLT3-6S</td>
<td>157.69</td>
<td>46.61</td>
<td>179.68E+03</td>
<td>16.80E+06</td>
<td></td>
</tr>
<tr>
<td>CLT3-7S</td>
<td>161.12</td>
<td>46.77</td>
<td>180.32E+03</td>
<td>16.90E+06</td>
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</tr>
<tr>
<td>CLT3-8S</td>
<td>176.71</td>
<td>48.25</td>
<td>186.00E+03</td>
<td>17.40E+06</td>
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</tr>
<tr>
<td>CLT3-9S</td>
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<td>45.65</td>
<td>175.97E+03</td>
<td>16.50E+06</td>
<td></td>
</tr>
<tr>
<td>Average</td>
<td>173.82</td>
<td>46.69</td>
<td>179.99E+03</td>
<td>16.80E+06</td>
<td></td>
</tr>
<tr>
<td>COV</td>
<td>8%</td>
<td>2%</td>
<td>2%</td>
<td>2%</td>
<td></td>
</tr>
</tbody>
</table>

$^a$ Maximum Moment

$^b$ Experimental Elastic Stiffness, i.e., slope of force displacement curve ($P/\Delta$)

$^c$ Apparent Modulus of Elasticity

$^d$ Apparent Stiffness

$^e$ Rolling Shear Strength per Kreuzinger’s Shear Analogy Method
Table 4.12: Five Laminae Static Test Results Summary

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>$M_{max}^a$ in-kips</th>
<th>$K^b$ kip/in.</th>
<th>$E_{app}^c$ psi</th>
<th>$EI_{app}^d$ lbf-in$^2$</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT-5S</td>
<td>425.12</td>
<td>94.30</td>
<td>441.24E+03</td>
<td>191.20E+06</td>
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</tr>
<tr>
<td>CLT-5-1S</td>
<td>323.24</td>
<td>93.39</td>
<td>437.00E+03</td>
<td>189.30E+06</td>
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</tr>
<tr>
<td>CLT-5-2S</td>
<td>372.34</td>
<td>131.34</td>
<td>346.65E+03</td>
<td>150.20E+06</td>
<td></td>
</tr>
<tr>
<td>CLT-5-3S</td>
<td>393.72</td>
<td>126.90</td>
<td>334.92E+03</td>
<td>145.10E+06</td>
<td></td>
</tr>
<tr>
<td>Average</td>
<td>378.61</td>
<td>111.48</td>
<td>389.95E+03</td>
<td>169.00E+06</td>
<td></td>
</tr>
<tr>
<td>COV</td>
<td>10%</td>
<td>16%</td>
<td>12.7%</td>
<td>12.7%</td>
<td></td>
</tr>
<tr>
<td></td>
<td>CLT-5-4S</td>
<td>370.62</td>
<td>54.69</td>
<td>150.02E+03</td>
<td>65.00E+06</td>
</tr>
<tr>
<td></td>
<td>335.88</td>
<td>50.63</td>
<td>138.88E+03</td>
<td>60.20E+06</td>
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</tr>
<tr>
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<td>339.34</td>
<td>61.22</td>
<td>167.93E+03</td>
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</tr>
<tr>
<td></td>
<td>323.46</td>
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<td>125.99E+03</td>
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<tr>
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<td>380.15</td>
<td>48.88</td>
<td>134.10E+03</td>
<td>58.10E+06</td>
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</tr>
<tr>
<td></td>
<td>351.08</td>
<td>49.57</td>
<td>135.98E+03</td>
<td>58.90E+06</td>
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</tr>
<tr>
<td>Average</td>
<td>350.09</td>
<td>51.82</td>
<td>142.15E+03</td>
<td>61.60E+06</td>
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</tr>
<tr>
<td>COV</td>
<td>6%</td>
<td>10%</td>
<td>10%</td>
<td>10%</td>
<td></td>
</tr>
</tbody>
</table>

$^a$ Maximum Moment
$^b$ Experimental Elastic Stiffness, i.e., slope of force displacement curve ($P/\Delta$)
$^c$ Apparent Modulus of Elasticity
$^d$ Apparent Stiffness
$^e$ Rolling Shear Strength per Kreuzinger’s Shear Analogy Method

The force and displacement data for the tests is plotted in Figures 4.9 and 4.10. All panels exhibited rolling shear (Figure 4.11 and 4.12).
Figure 4.9: Load Versus Displacement Data for SPFS Three Laminae CLT

Figure 4.10: Load Versus Displacement Data for SPFS Five Laminae CLT
Figure 4.11: Three Laminae CLT Specimen Failures: (a) CLT3-1S to CLT3-4S (*Top to Bottom*), (b) CLT3-5S to CLT3-9S (*Top to Bottom*)
Across all panel specimens, failures generally initiated in the cross layer(s) in the form of diagonal cracking and/or Z-shaped cracks, i.e., cracks that traverse the cross layer(s) and progress along the interlaminar boundaries. The cracks were typically followed by crack growth and propagation, longitudinal shear cracks nucleated from rolling shear cracks and propagated along the interlaminar boundaries causing separation of the cross layer(s) from the longitudinal layers. Longitudinal shear cracks are characterized by cracks that propagate along or adjacent to the interlaminar boundaries and often result in the loss of composite action. Longitudinal shear cracks observed in this testing occurred through the wood fibers and not the adhesive boundary layer. Failure of the wood fibers in the longitudinal
shear cracks indicates that the failures were not caused by failure of the adhesive. Shear modes of failure were followed by tensile rupture of the tensile face lamina and often crushing of the compressive face lamina. Generally, reductions observed in the CLT specimens’ load carrying capacity can be attributed to shear cracking such as rolling shear cracks and longitudinal shear cracks.

In the case of the three laminae panels, tests CLT3-1S and CLT3-2S exhibited a greater degree of ductility post peak load. On the other hand, tests CLT3-3S and CLT3-4S exhibited much higher peak forces than the first two tests as well as brittle, rapid loss in strength post peak. While the CLT5-3S and CLT5-4S tests did have somewhat higher peak loads than CLT5-1S and CLT5-2S, a clear relationship was not evident. This lack of relationship may be attributable to the similarity in span-to-depth ratio, 6.70 vs. 5.50, and may indicate that the tests were too similar in design. With regard to the effect of overhang on shear failures, no effect was noted as failures often initiated between the point of load and the supports and often extended to the edge boundary of the panels. Crushing of the wood contacting the rods was observed and resulted in the addition of wood bearing plates to prevent crushing.

Specimens tested in the Spring of 2022 were tested in using a modified testing setup, per Section 4.3.5. The specimens tested in the Spring of 2022 experienced markedly less experimental elastic stiffness and ductility than earlier tests. As the implementation of the servo-hydraulic actuator allowed for greater control over the load rate, a lower and constant load rate was possible. The lower load rate appears to have significantly reduced the stiffness of all specimens. Additionally, the displacement at maximum load also increased markedly thus reducing the ductility of the panels tested in the Spring. While the strain rates for both rounds of testing are well within the range of the quasi-static loading regime, the variation in stiffness and ductility highlight the high sensitivity of these properties to strain rate in CLT. Maximum load was markedly less in three laminae panels tested in the second round of testing, however, maximum load was similar for five laminae specimens in both rounds of testing. The testing conducted in Spring 2022 resulted in more consistent
test results across those specimens as noted by the lower coefficient of variation for most data gathered.

The experimental elastic stiffness, $K$, for each specimen was determined using the slope of the force-displacement curve for each respective specimen. The slope was determined by performing a linear regression of a region of the linear elastic portion of the force-displacement curves that corresponded to 30% to 50% of the maximum applied load. In the case of a clear bilinear-elastic region, the slope for each linear-elastic region was determined and the larger slope was selected [72]. The experimental elastic stiffness was then used to compute the Apparent Modulus of Elasticity and the Apparent Stiffness of the specimens per Equation 4.19.

\[
\delta = \frac{PL^3}{48EI} \Rightarrow E_{\text{app}} = \frac{PL^3}{\delta 48I} = \frac{PL^3}{\delta 4bd^3}
\]

\[
\Rightarrow E_{\text{app}} = \frac{K L^3}{4bd^3}
\]

\[
\Rightarrow EI_{\text{app}} = \frac{KL^3}{48}
\]

Ductility within the context of this research will be defined as a ratio that describes the post-peak structural behavior of CLT as is convention for research in blast, shock, and impact research. Ductility ratio, in this research, is defined as the ratio of deflection at complete failure of the panel, $\delta_{\text{fail}}$, to the deflection at the peak load, $\delta_{\text{peak}}$, see Equation 4.20.

\[
\mu = \frac{\delta_{\text{fail}}}{\delta_{\text{peak}}}
\]

The Apparent Modulus of Elasticity, Apparent Stiffness, and ductility ratio were used for comparison with results from the residual capacity tests following impulsive loading in the impulsive testing phase.
4.5.2 Rolling Shear Strength

Table 4.13 summarizes three quantities: the mean rolling shear strength, apparent stiffnesses obtained via the shear analogy method, and the apparent stiffness obtained via the experimental test data. The mean rolling shear strength was computed individually for three laminae and five laminae specimens. The resulting average rolling shear values were 288.59 psi (1.99 MPa) COV 20% and 198.79 psi (1.37 MPa) COV 10% for the three and five laminae specimens, respectively. These values are respectively, 54.24% and 6.25% greater than the RS strength found using a two plate shear test with SPFS CLT specimens having 0.157-in. (4-mm) gaps as reported by Wang et al. [134], i.e., 187.1 psi (1.29 MPa). While the data reported by Wang et al. was derived using a two plate shear test, a significant difference between three-point bend tests and two plate shear tests has not been found [99]. Cao found a 13.33 % difference in mean RS strength between the two test methods. The cause of the significant difference in average rolling shear strengths for the three and five laminae specimens is uncertain, however, some hypotheses include: 1) the smaller depth of the three laminae specimens results in fewer defects in the timber material in the panels, thus reducing the potential for failure to occur; 2) the boundary condition rotational rigidity may have resulted in a higher maximum load to be reached in only the three laminae specimens.

4.5.3 Apparent Stiffness

In general there is low agreement between the two sets of apparent stiffness values with percent differences ranging between 40% and 60%. A likely explanation for this low agreement is that the value for elastic modulus for each of the laminae is based on characteristic values provided by SmartLam. The characteristic values were not specifically determined for this batch of material. Instead, the characteristic values are the tabulated values for 2-in. to 4-in. (50.8-mm to 101.6-mm) thick SPFS No. 2 visually graded dimensional lumber listed in Table 4A in the National Design Specification (NDS) Supplement 2018.
Additionally, this research uses conventional design assumptions for elastic and shear modulus ratios for CLT [12]. Some examples of the elastic and shear modulus ratios referred to are $E_T/E_L$, $G_{LT}/E_L$ and $G_{RT}/E_L$. Greater agreement may be derived from using elastic modulus and shear ratios specific to SPFS and conducting wood testing to determine the longitudinal modulus of elasticity for the lamellas used in the CLT specimens. For example, the Wood Handbook’s [30] elastic ratios for SPFS may be used in lieu of the CLT Handbook generalized elastic ratios (i.e., $E_T/E_L = 0.033$, $G_{LR}/E_L = 0.0625$, $G_{RT}/E_L = 0.00625$).

Table 4.14 compares the apparent stiffness derived using the Wood Handbook’s elastic ratios, where the elastic ratios for Engelmann Spruce are used: $E_T/E_L = 0.059$, $G_{LR}/E_L = 0.124$, $G_{LT}/E_L = 0.120$, $G_{RT}/E_L = 0.01$. Note, although $G_{LR}/E_L$ is used in this case, it is very similar to $G_{LT}/E_L$, 0.124 vs. 0.120. (3.33% difference). With the change in elastic ratios used, agreement between the experimentally and analytically derived apparent stiffnesses improved for some specimens, but not for others. It is possible that the published values for rolling shear modulus, $G_{RT}$, may be the source of the low agreement. Multiple test methods for determining rolling shear modulus have been developed in recent years that account for the mechanical behavior of the cross laminae in CLT at a structural scale rather than a material scale with a relatively low coefficient of variation. Past testing, cited in the Wood Handbook, generally does not account for structural scale wood behavior and wood with defects. Greater accuracy in the SAM results may be derived by employing a test method suitable for the determination of rolling shear modulus. For example, Erhrart et al. [94] performed a literature review of several test methods as well as testing using the two-plate shear test.
Table 4.13: Comparison of SAM and Quasi-Static Test Data

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>$\tau_{RS}$ psi</th>
<th>$E_{I_{app}}^a$ kip-in$^2$</th>
<th>$E_{I_{app}}^b$ kip-in$^2$</th>
<th>Difference$^c$ (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT-3</td>
<td>CLT3-1S</td>
<td>216.61</td>
<td>76,837.72</td>
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</tr>
<tr>
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</tr>
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<tr>
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<tr>
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<td>COV</td>
<td></td>
<td></td>
<td></td>
<td>20%</td>
</tr>
<tr>
<td>CLT-5</td>
<td>CLT5-1S</td>
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<td>191,175.33</td>
<td>113,000</td>
<td>40.65</td>
</tr>
<tr>
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<td>CLT5-2S</td>
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<td>189,336.44</td>
<td>113,000</td>
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<tr>
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<tr>
<td></td>
<td>COV</td>
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<td></td>
<td>10%</td>
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</table>

$^a$ Quasi-Static Test Data.

$^b$ Shear Analogy Method.

$^c$ Difference between Quasi-Static Test Data and Shear Analogy Method Apparent Stiffnesses.
Table 4.14: Updated SAM Values with Wood Handbook Elastic Moduli

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>$EI_{app}^a$ kip-in$^2$</th>
<th>$EI_{app}^b$ kip-in$^2$</th>
<th>Difference$^c$ (%)</th>
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</thead>
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<td>CLT-3</td>
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<td>47,000</td>
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<td>CLT3-3S</td>
<td>42,890.19</td>
<td>26,000</td>
<td>39.38</td>
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<tr>
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<td>CLT3-4S</td>
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<td>26,000</td>
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<td>29,000</td>
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<td>29,000</td>
<td>76.09</td>
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<td>18.77</td>
</tr>
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<td>CLT5-4S</td>
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<td>15.92</td>
</tr>
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<td>CLT5-5S</td>
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<td>124,000</td>
<td>90.77</td>
</tr>
<tr>
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<td>58,915.99</td>
<td>124,000</td>
<td>110.47</td>
</tr>
</tbody>
</table>

$^a$ Quasi-Static Test Data
$^b$ Shear Analogy Method
$^c$ Difference between Quasi-Static Test Data and Shear Analogy Method Apparent Stiffnesses.
4.6 Conclusions

Center-point quasi-static loading was applied to undamaged, low span-to-depth ratio spruce-pine-fir-south CLT panel specimens. Two layups were tested, three and five laminae, with span-to-depth ratios ranging from $5.5 \leq L:h \leq 9.2$. Mechanical properties including apparent flexural stiffness and rolling shear strength for the panels were computed using experimental data and the Shear Analogy Method. The testing fixture and methodology used were found to cause shear and flexural modes of failure.

The primary conclusions are as follows:

1. The testing fixture and methodology were successful in causing shear modes of failure in the short span CLT panels. A combination of rolling shear and flexural failures were noted in the tests that were conducted. Rolling shear and longitudinal shear failures preceded the development of flexural modes of failure.

2. Rolling shear strength and apparent flexural stiffness were determined using the Shear Analogy Method. The five laminae CLT average rolling shear strength showed good agreement with the literature - compare 198.96 psi (COV 10%) compared to the rolling shear strength found by Wang et al. [134] of 187.10 psi for SPFS CLT, a 6.25% difference. However, the average rolling shear strength determined for the three laminae CLT specimens was significantly higher than the SPF CLT rolling shear strength published in the literature - compare 288.59 psi (COV 20%) to 187.10 psi, a 54.24% difference. A possible explanation for the significant difference in rolling strengths computed for the three laminae and five laminae specimens may be that the clamping assemblies provided greater restraint than anticipated for the three laminae specimens.

3. The apparent flexural stiffnesses determined with the Shear Analogy Method showed poor agreement with the experimental data. Values for the elastic ratios for Engelmann Spruce from the Wood Handbook were used to determine the shear modulus,
or $G_{LR}$, and rolling shear modulus, $G_{RT}$, specific to SPFS timber. Although agreement between the SAM and experimental apparent flexural stiffness did not improve, it was determined that the published rolling shear modulus for the SPFS group is too high and additional material testing is required to derive an accurate rolling shear modulus for SPFS.
CHAPTER 5
DESIGN OF AN IMPULSIVE CENTER-POINT TESTING SYSTEM WITH REALISTIC BOUNDARY CONDITIONS

A novel impulsive center-point testing system, i.e., three-point testing with loading applied at the midspan, was designed to experimentally study structural element scale cross-laminated timber specimens subjected to impulsive, intermediate to high strain-rate loading. The testing system was implemented in an experiment that consisted of two phases: 1) impulsive loading of CLT flexural specimen in a center-point testing loading condition, and 2) quasi-static loading of the specimen following impulsive loading. This chapter discusses the testing concept, goals for the system, and the methods to analyze, design, fabricate, validate, and calibrate the testing system.

5.1 Impulsive Testing System

5.1.1 Testing Rationale

Experimental testing of CLT is a maturing field, yet the performance of CLT subjected to impulsive loading is not well understood. Rolling shear (RS) strength and stiffness are tied to a CLT panel’s out-of-plane bending performance. The global out-of-plane bending response for CLT panels is significantly affected by cross laminae strength and stiffness. Brandner et al. [13] stated that RS is an important factor in the consideration of CLT ultimate limit state and serviceability limit state design. For example, premature failure of the panel may be caused if the cross laminations fail due to low RS strength. Premature failure in this mode would cause the panel to lose its ability to transfer shear flow and thus, its capability for composite action. Despite this, it seems that CLT’s shear behavior under impulsive loading is relatively unstudied. Sanborn [15] stated that RS failures have
been observed inconsistently in shock tube testing and not at all in live blast testing. To date, testing that has subjected CLT flexural specimens to shock has focused on specimens with large span-to-depth ratios (approximately 12 to 35) [19, 118, 122]; specimens with complex boundary conditions [19, 120, 119]; and low strain-rates on the order of $10^{-1}\,s^{-1}$ [118, 119, 120, 121, 122]. A few hypotheses to explain Sanborn’s statement include:

1. Experimental shock tests on CLT have been designed to either load specimens elastically or to meet a specification for a certain level of damage, thus avoiding the application of sufficient energy to produce a significant post-yield response.

2. Test specimens have been designed with large span-to-depth ratios (i.e., $\geq 12$), which elicit a largely flexural response. Decreasing ratios would elicit increasing shear deformation in CLT panels subjected to out-of-plane bending. Therefore, a mix of flexural and shear failure modes are expected at the lower end of the range (i.e., $\approx 12$) while mostly flexural failure modes are expected at the higher end of the range (i.e., 35). These expectations are supported by the literature, as discussed in Chapter 2.

3. Complex and/or stiff boundary conditions redistribute the stresses induced by shock loading, thus precluding the initiation of shear failure modes.

4. Material testing on wood at the material scale on Split Hopkinson Pressure Bars and drop weight testing of sawn lumber in bending, indicate that strain-rate enhancement occurs at intermediate to very high strain-rates ($10^{1}\,s^{-1}$ to $10^{5}\,s^{-1}$), as discussed in Chapter 2. To date, most experiments that have subjected CLT to simulated blasts have focused on far-field blasts that subject CLT to intermediate strain-rates. These tests have primarily relied on numerical modeling to approximate strain-rate enhancement to strength and stiffness. It is hypothesized that greater strain-rate enhancement may be evident in the longitudinal and transverse laminations at intermediate to high strain-rates (i.e., $10^{0}\,s^{-1}$ to $10^{2}\,s^{-1}$).
To address these hypotheses, additional testing is required at span-to-depth ratios that are likely to elicit a greater shear response, i.e., span-to-depth ratios in the 5 to 6 range. Testing must incorporate sufficient levels of energy and sufficiently high strain-rates to produce a significant post-yield response and thus allow for the observation of high ductility levels in CLT specimens, i.e., ductility ratios greater than 3 to 4. The observation of high ductility ratios has not been pursued to date, which may further explain the inconsistent or absent RS failures in live blast testing. Dahl and Malo [84] provide support for this hypothesis as they have shown that a relationship exists between higher levels of ductility and RS failures in statically loaded CLT specimens. Finally, a variety of boundary conditions should be studied in order to better understand the effect of connection rigidity on RS failures versus idealized pinned/roller boundary conditions.

An experiment was designed to implement the following strategies to study these areas: 1) implementing an impulsive one-way, out-of-plane bending test program of CLT panel specimens with low span-to-depth ratios, and 2) quasi-static residual capacity testing of CLT panel specimens following impulsive loading.

CLT specimens were impulsively loaded at varied energy levels to determine how applied impulse affects damage progression in the specimens and the energy level required for failure. Following impulsive loading, in situ quasi-static testing was used to quantify the residual shear capacity of the specimens. As research on the RS properties of CLT has suggested that RS modulus may be strain-rate dependent [84], the quasi-static (QS) residual capacity and impulsive tests were vital in verifying this and any other conclusions on RS properties and strain-rate-dependence. Studying specimens at high ductility ratios was included in this testing. High ductility was evoked by including specimens with a higher probability of RS failure and by loading at energy levels that resulted in complete failure of the specimens. As a point of reference, in Chapter 4, higher levels of ductility were observed in specimens with lower span-to-depth ratios during quasi-static testing. The carry over of this effect, i.e., increased ductility in low span-to-depth ratio CLT specimens, and
its applicability was also evaluated for low span-to-depth ratio specimens subjected to high strain-rates.

5.1.2 Impulsive Testing System Design Concept

The impulsive center-point testing system was designed to meet the goals described in the previous section. The system is composed of several components that can be divided into two categories, the impact components and the reaction components. The impact components consist of a high-velocity actuator anchored to a reaction wall and an impacting mass that is fastened to the actuator piston rod (Figure 5.1). The reaction components consist of a center-point flexural testing fixture, a steel mounting plate, and a modular reaction block wall to which all of the previous reaction components are anchored (Figure 5.2). Finally, the instrumentation consists of high-frame rate cameras, a portable data acquisition system (DAQ) that is capable of recording at high sampling rates, and piezoelectric load cells.

Figure 5.1: Testing Area
During a test, the actuator initiates and accelerates the impacting mass to a desired constant velocity. Once the desired velocity is reached, the impacting mass strikes the target specimen and reaches a programmed actuator displacement before retracting. This process describes the delivery of a "punch", where hydraulic pressures and valve timings are programmed such that a "singular pulse", as defined by Freidenberg [136], is delivered to the specimen. The delivery of a "punch" requires a rapid retracting of the actuator following impact to achieve this. Piezoelectric load cells are "sandwiched" between the actuator and impacting mass to facilitate direct force measurement of the impact. Additional piezoelectric load cells are fastened behind the specimen supports to allow for reaction force measurement. Direct measurement of force input and output as well as measurement of the deformation captures the specimen response to a given impulse.

Following impulsive loading, each specimen is loaded quasi-statically using a hydraulic

Figure 5.2: Reaction Components
cylinder. The residual capacity testing system was used to determine the residual capacity of the specimen.

5.1.3 Design Challenges

5.1.3.1 Adaptability of the System

The primary objective of the testing system is to facilitate the impulsive testing of flexural specimens of various shapes and dimensions. The testing system was designed as a two part system consisting of a rigid support frame and a clamping fixture (Figure 5.4). The rigid support frame is mounted onto the steel mounting plate and reaction wall. Moveable supporting cross beams in the rigid support frame were designed to permit adjustability to the span length of a specimen. Clamping of the specimen is achieved via two clamping fixtures, front and back supports, that are mounted to the rigid support frame. Given the configuration of the testing area and the high-velocity actuator, a vertical orientation is required. Further adjustability is built into the clamping system which allows for specimens of widths up to 24-in. (609.60-mm) and various depths to be clamped.

5.1.3.2 Inertial Effects

Inertial effects can be significant in high rate load tests. When the natural frequency of each of the components in a testing fixture is not sufficiently high to produce a period shorter than the duration of the test, a phenomenon called load ringing occurs [124]. In load ringing, the response of the component(s) with the lower natural frequency dominates the measured results; the mass, stiffness, and damping of the component(s) are important factors. Such factors were demonstrated by Huh et al. [137] in their high rate loading tests. The high mass of the gripper jig in their experiment effectively decreased the natural frequency of the gripper jig. Given the decreased natural frequency, stress waves from the high rate loading caused different rates of deformation in their testing system’s components. The load cell data were negatively impacted as oscillations were introduced.
Load ringing and other inertial effects are considerations for the testing system in this research. The impactor has been designed to directly measure force via a series of piezoelectric load cells *sandwiched* between the impactor and the high speed actuator pusher plate. Low natural frequencies in any of the impact components causes load ringing and corrupt the measured force-time history data. The impactor has an additional element of complexity, as the inertia from the acceleration of the actuator is also captured in the force-time history. On the reaction side, load ringing and inertial load resulting from the response of the support assembly to an impact are also concerns.

To mitigate load ringing, the natural frequency of the support frame with CLT specimen of various lengths has been estimated (Table 5.1). The calculated natural period for each specimen tested in Chapter 4 is less than the duration of the impacts observed in the dynamic testing described later in this chapter. This indicates that load ringing will not be an issue.
Table 5.1: Estimated Natural Frequency and Period for CLT Panel Specimens

<table>
<thead>
<tr>
<th>Panel Category</th>
<th>Test Number</th>
<th>Volume in$^3$</th>
<th>Specific Gravity</th>
<th>$\gamma_w$ $^b$</th>
<th>$\rho$ $^c$</th>
<th>$K$ $^d$</th>
<th>$f$ $^e$</th>
<th>$T$ $^f$</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT3-1</td>
<td>CLT3-1</td>
<td>2,508</td>
<td>0.36</td>
<td>0.013</td>
<td>0.0845</td>
<td>66,319.09</td>
<td>886.55</td>
<td>1.13</td>
</tr>
<tr>
<td>CLT3-2</td>
<td>CLT3-2</td>
<td>2,508</td>
<td>0.36</td>
<td>0.013</td>
<td>0.0845</td>
<td>57,224.39</td>
<td>823.52</td>
<td>1.21</td>
</tr>
<tr>
<td>CLT3-3</td>
<td>CLT3-3</td>
<td>1,551</td>
<td>0.36</td>
<td>0.013</td>
<td>0.0522</td>
<td>158,714.80</td>
<td>1,744.01</td>
<td>0.57</td>
</tr>
<tr>
<td>CLT3-4</td>
<td>CLT3-4</td>
<td>1,551</td>
<td>0.36</td>
<td>0.013</td>
<td>0.0522</td>
<td>133,440.50</td>
<td>1,599.13</td>
<td>0.63</td>
</tr>
<tr>
<td>CLT5-1</td>
<td>CLT5-1</td>
<td>5,060</td>
<td>0.36</td>
<td>0.013</td>
<td>0.1702</td>
<td>94,300.26</td>
<td>744.27</td>
<td>1.35</td>
</tr>
<tr>
<td>CLT5-2</td>
<td>CLT5-2</td>
<td>5,060</td>
<td>0.36</td>
<td>0.013</td>
<td>0.1702</td>
<td>94,300.26</td>
<td>740.68</td>
<td>1.35</td>
</tr>
<tr>
<td>CLT5-3</td>
<td>CLT5-3</td>
<td>4,180</td>
<td>0.36</td>
<td>0.013</td>
<td>0.1406</td>
<td>131,342.00</td>
<td>966.41</td>
<td>1.035</td>
</tr>
<tr>
<td>CLT5-4</td>
<td>CLT5-4</td>
<td>4,180</td>
<td>0.36</td>
<td>0.013</td>
<td>0.1406</td>
<td>126,895.20</td>
<td>949.91</td>
<td>1.053</td>
</tr>
</tbody>
</table>

\(^a^\) Specific gravity for oven-dry Spruce-Pine-Fir-South per NDS 2018 [135]

\(^b^\) Unit weight

\(^c^\) Mass

\(^d^\) Stiffness

\(^e^\) Natural frequency

\(^f^\) Natural period

Additionally, the geometrical design and use of steel have been leveraged in order to achieve a high overall stiffness for the testing assembly and to mitigate load ringing. Components such as the piezoelectric load cells are designed with a high stiffness, low mass, and to operate over a wide range of frequencies so load ringing is already mitigated. To account for the inertial force caused by acceleration captured in the force-time history, an experimental method for filtering out this data was developed and is described in Section 5.3.
5.2 Impulsive Testing Fixture

This section describes the design of the impulsive testing fixture.

5.2.1 High Speed Actuator

Testing was conducted in the testing area of the Blast, Shock, and Impact Laboratory located in the Structural Engineering and Materials Laboratory at the Georgia Institute of Technology. Images of the laboratory space and testing area may be found in [138]. The Blast, Shock, and Impact Laboratory consists of the following components: control center, high-speed actuator anchored to a reaction wall, rail system, movable modular reaction block for mounting test specimens, and safety system. The safety system includes a reconfigurable and impact resistant polycarbonate safety wall to contain any flying debris resulting from testing, CCTV surveillance, public address audio system, and additional barriers to limit access of personnel during testing. During a test the CCTV surveillance and audio systems are used to monitor and warn personnel of the status of a test and to monitor/control access to the testing area. Additional information regarding the overall configuration of the testing area, safety features, and laboratory facilities and capabilities are provided by Stewart et al. [138].

Shock loading is simulated using an ultra-fast, computer-controlled, hydraulic actuator called a Blast Generator (BG). A BG delivers an impulse by impacting a specimen with a variable mass at a controlled rate and duration and in a highly repeatable manner. The BG at Georgia Tech is a model BG25 manufactured by MTS Systems Corporation. The BG25 is similar to the BGs located at the University of California, San Diego and is capable of accelerating a 50 kg. (110 lbs.) mass up to 34 m/s (112 ft/s) [139]. Detailed information on the concept of simulating blast with BGs is provided by Hegemier et al. [140] and further explanation, validation, and applications are provided by [136, 139, 141, 142].

In this research, the actuator is used to deliver a singular pulse via a "punch" type im-
impact. To accomplish this, an impactor is connected to the BG piston rod via a bolted connection made to an aluminum pusher plate and mass adapter and remains attached throughout the impact. When a test is initiated, the BG accelerates the piston rod with the attached impactor toward the specimen until the target velocity is reached. The impactor and pusher assembly are guided by the rail system to ensure that the assembly travels on a path parallel to the floor and that there is no rotation of the assembly. The BG is programmed such that the target velocity is reached at impact with the specimen. Impact with the specimen and programming of the BG will cause deceleration of the mass and retraction of the piston rod and impactor. The process of decelerating and retracting prevents the introduction of additional pulses due to multiple impacts and allows for the loading history to be tailored to be blast-like in duration and shape. Careful programming of the BG controls is required as multiple impacts can be caused by the specimen response.

5.2.2 Impactor

The impactor used in the impactor assembly was previously used in the quasi-static testing (Figure 5.3). The mass of the impactor is 111.52 lbs (50.46 kg). Four piezoelectric load cells were anchored to the impactor plate using beryllium copper mounting studs threaded into the plate. The four load cells were aligned and placed in a square, diamond pattern. The impactor and load cells were mounted to the actuator pusher plate using four 0.50-in. (12.70-mm) through-bolts. The through bolts serve multiple functions in addition to forming a connection. Each through bolt is paired with 0.625 in. (15.875-mm) (O.D.) thin-walled tubing that extends the length of the bolt shank and threading. As the impactor collides with a specimen, the load cells are compressed between the impactor plate and the pusher plate causing each of the plates to slide along the bolt shank or threading. The addition of thin-walled tubing minimizes friction and prevents the plates from catching along the bolt to pusher plate contact interface. Preloading of the load cells is applied to ensure that each load cell receives equal loading from an impact and to remove any delay
in load cell response due to excess space. Preloading also maintains the impactor level and motion rigid.

Figure 5.3: Impactor Design: (a) Perspective View, (b) Side View, and (c) Back View with Pusher Plate in Foreground

### 5.2.3 Supporting Reaction Frame

The supporting reaction frame, as shown in Figure 5.4 and 5.5, provides support for the specimen and clamps it in a vertical orientation. The reaction frame is divided into an upper and a lower support frame. Two discrete frames were designed in order to prevent visual obstruction of a specimen’s response to an impact. The reaction frame beams are designed with slotted holes to enable the adjustment of the specimen length by moving the cross beams. Four built-up beams act as guides and supports to the cross beams. The cross beams are fixed during testing via two double angle clip connections per cross beam.
All beams in the reaction frame are designed as built-up beams to satisfy the clearance requirement of the waterjet cutter table at the Mason Building machine shop and facilitate cutting slotted holes into the beams’ webs. The built-up beams consist of two C12x20.7 channel sections and one 0.625-in. (15.875-mm) thick plate for each beam flange. There are two clamping assemblies, top and bottom, and each is affixed to the supporting reaction frame via six 0.50-in. (12.70-mm) diameter bolts. Three piezoelectric load cells are fixed to the top of each cross beam. During an impact, the specimen deforms and distributes load to the clamping assembly supports, which move toward the steel reaction plate. This movement compresses the load cells between the clamping assembly support and the top of the cross beams. Each clamping assembly has four threaded rods that are tightened to a specified torque value according to the test specifications. The clamping force provided by each assembly limits the rotation of the specimen at the support, thereby setting a control on rotational rigidity. Torque is measured using the same method described in Chapter 4. For the experiment, three levels of torque (65 in-lbs, 130 in-lbs, and 200 in-lbs) were selected for study in order to represent a wide range of rotational rigidities at the boundary conditions of the specimens.
Figure 5.4: Supporting Reaction Frame Design: (a) Perspective View, (b) Side View

Figure 5.5: Supporting Reaction Frame Top View
5.2.4 Fabrication and Installation

The testing assembly and impactor were fabricated at the School of Civil and Environmental Engineering’s machine shop. Each of the structural shapes including channel sections, angles, plates, and bars were cut to length using a horizontal band saw. Slotted holes in the channel section webs were cut using a waterjet cutting table. Coping of the cross beams was also performed on the waterjet. Steps were taken to increase accuracy in machining parts and ensure a snug fit within $\pm 0.125$-in. ($3.175$-mm) of tolerance. Built-up beams were assembled and clamped, and holes were machined using a radial arm drill in order to ensure proper hole alignment throughout the beam components. Cutting of the parts on the waterjet table was preferred in order to provide better tolerances and less deformation of the parts during fabrication. Following the completion of the testing assembly, the reaction assembly was fully assembled and all bolts were torqued to be wrench tight. The design of the assembly ensured that the frame would become square upon assembly and torquing of hardware. The lower and upper reaction frame assemblies were individually lifted by the lab crane and positioned in front of the reaction plate. Shim plates were installed between the reaction frame and the reaction plate in order to ensure vertical alignment of the frame. Once each frame was installed and leveled, load cells were mounted and the clamping assemblies were mounted and leveled such that loading would be applied evenly across the face of the reaction load cells. Next, the impactor was installed and leveled such that loading would be applied evenly across the face the impact load cells. Care was taken to ensure gaps between the impactor and load cells were eliminated and that each load cell received similar levels of torque. Figure 5.7 shows the partial installation of the testing assembly in the Blast, Shock, and Impact Laboratory.
Figure 5.6: Fabrication: (a) Angles Cut to Length with Drilled Holes, (b) Completed Channel Sections, (c) Radial Arm Drilling Holes into Cross Beams (In Process), (d) Completed Vertical and Cross Beams Ready for Assembly

Figure 5.7: Installation: (a) Impactor Attached to BG, (b) Reaction Frame Mounted on Steel Reaction Plate and Modular Concrete Reaction Wall
5.3 Impulsive Testing Experimental Method

5.3.1 Instrumentation of the Impulsive Loading

The force time histories were sensed via a series of Dytran 1061V6 piezoelectric load cells produced by Dytran Instruments, Inc. Four load cells were sandwiched between the pusher plate and impactor assembly and three load cells were anchored behind each clamping fixture and top of the cross beams, totaling ten load cells. The load cells are capable of sensing 50,000 lbf. (222.4 kN) of compressive force and 10,000 lbf. (44.48 kN) of tensile force. The load cells were paired with Dytran 6217 impact caps to protect the load bearing surface of the load cells during testing. Force histories were recorded by the Synergy DAQ produced by Hi-Techniques. The response of the specimens and overall system were captured using two high frame rate cameras, a Phantom Miro C110 and Phantom Miro M310. The Phantom Miro C110 has a maximum resolution of 1280x720 at a frame rate of 1,295 frames per second (fps) and the Phantom Miro M310 has a maximum resolution of 1280x800 at a frame rate of 3,200 fps. The Phantom Miro M310 was used to capture the overall response of the system including the support frame, specimen, and impactor. The Phantom Miro C110 was used to capture a view of the thickness of the specimen at midspan. The images captured by the Miro M310 were used to produce and analyze strain fields in these areas via the application of digital image correlation (DIC). The Miro M310 was selected for this task as the higher frame rate was expected to minimize motion blur, thereby producing images with minimized noise and bias for DIC. Additionally, motion tracking software, ProAnalyst Professional Edition [143], was used to post-process video and determine inbound and rebound displacements of the specimens. The load cells, cameras, and DAQ were triggered by an MTS controller which synchronized the data output by the instruments.
5.3.2 Impulsive Testing Procedure

The CLT panel specimens were tested in the blast simulator using the procedure outlined below:

1. Move and secure the reaction frame cross beams to achieve the span length required by the test specifications.

2. Check that the bolts connecting the clamping assembly to the reaction frame are level and evenly torqued using a turn-of-nut method. Check clamping assembly back plate level. Check for gaps between the load cell impact caps and the clamping assembly back plate as this is indicative of uneven placement of the clamping assembly. This step will ensure that the reaction load cells are preloaded equally.

3. Mount the CLT specimen in the reaction frame and ensure that it is centered in the vertical and horizontal directions. Ensure that the specimen is not rotated across the supports by checking level. Tighten the threaded rods in the clamping assemblies using a torque wrench. Check the clamping assembly front plate level to ensure that the specimen’s ends are clamped with even pressure.

4. Add targets for motion tracking to CLT specimen at premarked locations. Check that the impactor targets are in place.

5. Ensure the bolts connecting the impactor to the pusher plate are evenly torqued. This step shall be repeated after each test and will ensure that the impactor load cells are preloaded equally.

6. Connect strain gages to Hi-Techniques SY-BI breakout box to sense strains with Synerregy DAQ.

7. Commence pre-test instrumentation preparation, pre-test safety inspections, system initialization, and warm-up procedure for the BG as outlined in [144].
8. Verify specimen conditions and verify instrumentation settings.

9. Execute firing sequence per [144].

10. Visually assess level of damage and determine if residual capacity test is required.

5.4 Method for Direct Force History Measurement

5.4.1 Background

The blast generator is often used to conduct parameter studies and to validate computer models for different materials, geometries, and boundary conditions. Apriori calculation of the force-time history applied to a specimen is difficult particularly when a programmer is used. Additionally, varying levels of rigidity, material tolerances, and other factors often frustrate apriori calculations. Despite this challenge, the determination of the force-time history is essential for the accurate numerical modeling of structural behavior observed in experiments and furthering understanding.

In a punch type test in the blast simulator, the impacting mass remains attached throughout the impact and the actuator is programmed to pull back on the mass near the end of the collision preventing multiple impacts. The force imparted onto the specimen can be described by the following function:

\[
F(t)_{\text{net}} = ma(t)_{\text{net}} = F(t)_{\text{specimen}} - F(t)_{\text{hydraulic}}
\] (5.1)

In Equation 5.1, the specimen force, \( F(t)_{\text{specimen}} \), is the force-time history on the rear face of the impacting mass, i.e., the mass and piston rod connection interface. This force is not necessarily the same as the force delivered to the specimen at the impactor and specimen contact interface. As a programmer is typically used on the impacting mass, the unique properties of the programmer typically preclude the front and rear faces from having similar forces. Freidenberg [136] describes methods for ensuring that no energy loss occurs
due to the programmer. The term, $F(t)_{\text{hydraulic}}$, describes the hydraulic force acting on the impacting mass. The hydraulic force is determined by taking the difference between the force on the piston rod resulting from the acceleration chamber oil pressure and the force on the piston rod resulting from the deceleration chamber nitrogen pressure, see Figure 5.8. This calculation is illustrated with an example by Freidenberg [136].

![Figure 5.8: Schematic of the Blast Generator [139]](image)

Freidenberg [136] contrasted three methods for determining force delivery to a beam specimen in a four-point bending configuration subjected to a punch type collision. In Freidenberg’s work, a beam was supported by a frame with dynamic load cells affixed to the rear of the frame to measure reaction force. The impacting mass was instrumented with accelerometers and the impact face of the beam specimen was instrumented with a dynamic load cell that measured force at the impact interface. Freidenberg investigated three methods for subjecting the beam specimen to an accurate force-time history in a numerical simulation: 1) apply the force-time history recorded by the front load cell to the specimen, 2) use accelerometers and BG hydraulic data with Equation 5.1 to determine $F(t)_{\text{specimen}}$, and 3) use BG hydraulic data in an LS-DYNA model as a load input to drive the simulation. The third method produced the best results of the three methods with the LS-DYNA derived force-time and displacement-time histories matching the experimental data closely. Method 3 also benefited from not requiring consideration of the mass and stiffness nor modeling of the impact load cell as opposed to Methods 1 and 2. Freidenberg suggests
that method 3 should be used unless a numerical model is not to be made, in which case, method 2 is recommended for achieving an estimate of the specimen force-time history.

Despite achieving acceptable and excellent results for methods 2 and 3, respectively, both methods require additional analysis and/or instrumentation to determine the force-time history delivered to the specimen. An experimental method to directly measure the force history applied to a specimen was desired. To this author’s knowledge, no experimental method has been developed that directly measures the force history delivered to a specimen during an impact. Similar methods developed required the use of both a numerical model and experimental testing, which require extensive knowledge of the piezoelectric load cell design and modeling of the load cell. In addition to developing an experimental method to accomplish this goal, this research seeks to verify if the forces on the rear and front faces of the impactor are similar.

5.4.2 Methods and Materials

This section details the instrumentation of the impactor and the use of the impactor to accurately capture the force-time history. As described previously, four piezoelectric, dynamic load cells were attached to the back of the impactor and the impactor was attached to the pusher plate. This configuration effectively sandwiches the load cells between the pusher plate and impactor back plate (Figure 5.9).
In this configuration, when the impactor collides with a specimen, the load cells are compressed between the set of plates. The force recorded by the load cells is the force applied by the pusher to the impactor. The impactor is assumed to be rigid given that it is significantly stiffer than the specimen being tested. As such, it may be assumed that the force on the rear face of the impactor is the same as the force on the impacting side of the impactor. Validation for this assumption is provided by finite element modeling of the experiment and is discussed in Section 5.4.4. The force-time history, $F(t)_{\text{specimen}}$, for this design is determined by summing the force-time histories of the four load cells (Equation 5.2).

$$F(t)_{\text{specimen}} = \sum_{i=1}^{4} F(t)_i$$  \hspace{1cm} (5.2)

As multiple load cells are being used, ensuring that the load cells are being actuated in phase and receiving symmetrical loading is critical. Testing was carried out to ensure that these conditions were being met for the impact components of the experiment. The reaction
frame and impactor were prepared according to the impulsive testing procedure outlined in Section 5.3.2. A steel bar measuring 0.625-in. x 3-in. x 60-in. (15.875-mm x 76.2-mm x 1524-mm) was placed in the reaction frame. A steel bar was used in order to minimize the variables introduced by material and structural element heterogeneity inherent in CLT and to simplify numerical modeling of the tests. A torque of 65 in-lbf (113 N-mm) was applied to each nut on the threaded rods in each clamping assembly. The span length was set to 41-in. (1041.4-mm) on the reaction frame, and the bar was centered over the supports. The tests consisted of firing the BG at a specified velocity at the steel bar and then subsequently firing the BG at the same test specifications with no specimen in the reaction frame, i.e., a dry fire test. This process gives force-time history data for a test with impact and for a test with no impact, or dry fire test. Furthermore, this process allows for direct comparison and quantification of the force delivered to the specimen while allowing for the removal of inertial force recorded by the load cells when the mass is accelerated to the target velocity. A total of sixteen tests were conducted on the steel bar specimen. Table 6.3 outlines the test matrix. The test naming convention is based on a letter V (validation test) and a sequential numbering scheme. Note, several of the tests were repeated multiple times for verification, and their results are not discussed due to redundancy.
**Table 5.2: Impactor Validation Test Matrix**

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Impacting Mass kg</th>
<th>Target Velocity m·s(^{-1})</th>
<th>Test Specimen</th>
</tr>
</thead>
<tbody>
<tr>
<td>V-24</td>
<td>50.46</td>
<td>4.952</td>
<td>Dry Fire</td>
</tr>
<tr>
<td>V-27</td>
<td>50.46</td>
<td>4.952</td>
<td>Steel</td>
</tr>
<tr>
<td>V-28</td>
<td>50.46</td>
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<td>6.029</td>
<td>Dry Fire</td>
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<tr>
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</tr>
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</tr>
</tbody>
</table>
5.4.3 Experimental Results of the Validation Tests

5.4.3.1 Results for Tests V-24 to V-31

The load cell force histories were summed for tests with and without impact, which produced two distinct curves. The difference was then taken between the curves to produce a third curve, which represents the net force history applied to the specimen with inertial forces subtracted. Figure 5.12 presents representative results. The difference curve can then be integrated to determine the impulse history (Figure 5.13).

Note that the force histories show values of positive and negative force. The sign convention used for this experiment is that compressive forces, stresses, and strains are positive. Normally negative values would then be assumed to be tensile, however, this assumption does not apply in this case. As the load cells are preloaded via bolt tension, there is an existing compressive force applied to the load cells prior to the initiation of a test, and this preload force is effectively set to zero, or tared, by the DAQ. During the test, varying levels of compression are applied to and recorded by the load cells. When the compression force recorded at a given time falls below the value of the preload force, the force is recorded as being negative. Therefore, the negative forces do not represent tensile forces, but rather lesser levels of compression with respect to the preload compressive force. A study was conducted in order to quantify the preload compressive force that was applied at different levels of bolt torque. It was necessary to vary the levels of torque as it was not clear at the onset of the experiment what level of torque would be necessary for the connection of the impactor. In this study, a static load cell was placed in between the impactor and pusher plate while varying levels of torque were applied to each of the four bolts in the connection (Figure 5.10).
An Interface Model 1010-C load cell and Vishay Measurements Group Model P-3500 strain indicator were used to sense and report the compressive force generated by each level of torque (Figure 5.11). Two methods of torque application were employed: 1) a turn-of-nut method, and 2) use of a torque wrench. Prior to torquing the bolts, the plates were leveled, the load cell was placed in the center of the plate assembly, and each of the nuts were turned until *hand tight*. In the first method, each nut was marked with a permanent marker and turned one full rotation. In the second method, two levels of torque were chosen: 65 in-lbs and 130 in-lbs. For each of the methods, a minimum of four trials were run and the loads were recorded for each. A summary of the results is presented in Table 5.3. The study revealed that the range of compressive preload force was approximately 2,000 lbf to 6,500 lbf. This range of values is representative of the range of negative forces observed in the various force time histories and supports the explanation provided.
Table 5.3: Summary of Precompression Load Study

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5.4.3.2 Results for Tests V-24 to V-31

Representative data for tests V-24 to V-31 is presented in Figures 5.12, 5.13, and 5.14.
The validation tests V-24 to V-31 revealed that the individual force-time histories captured by each of the load cells were out-of-phase, Figure (5.14).
The data was analyzed to determine the cause of the out-of-phase load cell force histories. The load cells aligned on the vertical axis, $y$, see Figure 5.15, were mostly in phase and captured most of the force delivered. The horizontal axis, $x$, load cells were in phase with each other, but out-of-phase with the vertical axis load cells. The reasoning for this is that with this geometry, the vertical load cells received most of the load as they were directly in front of the specimen during an impact whereas the horizontal axis load cells were offset from the specimen (Figure 5.15).
To ameliorate the phase difference, the torque on the bolts connecting the impactor to the pusher plate was increased using a turn-of-nut method, described previously, and the bolts were checked for tightness prior to each test. The increase in bolt torque improved the phasing of the load cells in the next series of tests.

5.4.3.3 Results for Tests V-36 to V-41

Although phasing of the load cells was improved from the previous series of tests, the load cell force-time histories were going out-of-phase at about 30 ms (Figure 5.16).
High speed camera footage was reviewed to determine what was occurring when the load cell force histories went out-of-phase. It was determined that the load cells were going out-of-phase prior to impact occurring. The load cell mounting pattern was modified following analysis of the data by rotating the diamond 90-degrees to a square pattern (Figure 5.17).

The test results following this modification indicate that the orientation of the load
cell group had a slight, if not negligible, effect on the phasing as shown in Figure 5.18. Despite this phase difference, it was believed that applying an even bolt pretension to the four bolts securing the impactor to the BG would improve phasing. The modified load cell pattern permitted even application of the preload across all load cells given their symmetric placement with respect to the test specimens.

![Figure 5.18: Test V40: Force Time Histories for Impact Load Cells](image)

**Figure 5.18: Test V40: Force Time Histories for Impact Load Cells**

### 5.4.3.4 Results for V-44 to V-47

The goal for this series of tests was further improvement of load cell phasing and verification of the repeatability of the data collected. The impactor bolts were torqued prior to this test series to check bolt torque’s effect on the time series phasing. The impactor bolt torque was not applied with a torque wrench as the required tool attachment for the tight clearance was not available at the time of testing. This led to the implementation of a turn-of-nut method to tighten the bolts and preload the load cells. An attempt was made to ensure that bolt torque was relatively similar to ensure even preloading of the load cells. Nuts were marked with an oil pencil and turned an equal number of rotations. This method seemed to improve load cell phasing markedly (Figure 5.19). The four load cells are in
phase throughout the time history with the exception of a period between 40 ms and 60 ms. During this period one of the load cells goes out-of-phase following impact with the specimen, which was deemed to be the result of bolt pretension not being well calibrated.

Figure 5.19: Test V45: Force Time Histories for Impact Load Cells

Figure 5.20: Tests V44 and V45: Impulse and Difference Force Time Histories for Impact Load Cells
In order to validate the method, finite element simulations were conducted in LS-DYNA [145] to determine if the force-time history is accurate and free from inertial effects. This model is discussed further in the following section.

5.4.4 Finite Element Simulation of Impulsive Center-Point Flexural Testing System

A finite element model was created with the objective of validating the experimental results obtained from the blast simulator. This section describes the loading parameters for the test with a target velocity of 6.029 m/s (19.78 ft/s). The finite element model was simulated in LS-DYNA with explicit time integration and the mesh was generated with TrueGrid [146]. The test geometry was simplified significantly from the experimental test fixture and blast generator used in the laboratory setting (Figure 5.22).
The model included the pusher plate, impactor load cells, impactor, clamping assembly with steel bar specimen, and reaction load cells. Bolts and other details are not included in this version of the model. Additionally, half of the geometry for the experiment is meshed in order to increase computational efficiency (Figure 5.23).
Each of the components are meshed with quadratic elements with an average element size of 0.5-in. (12.7-mm) and a 5:1 element width to thickness ratio is maintained throughout the model, as feasible. A type -1 fully integrated selective reduced solid element (efficient formulation) is used throughout the model. The impactor is placed at an initial position of 8.25-in. (209.55-mm) as in the experiment. The impactor’s motion is controlled using displacement control, and the BG’s displacement-time history data as recorded by the MTS controller.

The BG’s motion is an arc-like, stepwise displacement-time function with fine gradations. This function is problematic for a numerical simulation as each horizontal step, when differentiated, leads to zero values for velocity, thereby causing sudden stopping and starting of the impactor and extreme inaccuracy in the simulation. To correct this issue, smoothing of the displacement-time history was applied using a 6th order polynomial with a coefficient of determination, $R^2 = 0.9997$. The materials were assumed to be elastic perfectly plastic, which was deemed appropriate given that the specimen response during testing was mostly elastic and nearly no plastic deformation was noted. The keyword MAT_PLASTIC_KINEMATIC was used to model the ASTM A36 and A529 steel and the 6061-T6 aluminum used for the test setup, bar specimen, and pusher plate, respectively.

No clamping pressure is applied to the clamping assembly. The clamping assembly plates and rods are fixed at their ends and the back of each of the reaction load cells are also fixed (Figure5.24). Contact between the plates and the load cells is simulated using CONTACT_AUTOMATIC_SURFACE_TO_SURFACE_TIEBREAK with Option 1 to inhibit breaking of ties. CONTACT_AUTOMATIC_SURFACE_TO_SURFACE is used for the remaining parts. Force-time histories in the model’s load cells were recorded using the following keywords: DATABASE_CROSS_SECTION_SET and DATABASE_HISTORY_SOLID_SET.
The force-time history data in the simulation is captured by the load cells and the contact interface between the impactor and the specimen and the contact interfaces between the clamping assembly steel rods and the specimen front and back faces. Impulse is calculated from the LS-DYNA force-time history using Equation 5.3, where $F$ is force and $t$ is time.

The first 70-ms of the experiment was simulated to reduce computational resources and as impact has occurred and the specimen has had time to respond allowing reaction forces to develop.

$$I = \int F \, dt$$  \hspace{1cm} (5.3)

An additional step is required for processing the force-time history at the clamping assembly to specimen interface, i.e., the reaction force-time history. As there are two primary components to the clamping assembly, a top plate with welded rod and a similar bottom plate with welded rod, there are two objects restraining the specimen’s translation. Given these constraints, as the specimen deforms due to an applied force at its midspan, forces will develop on both the front and bottom faces of the specimen due to lever action. The forces that develop must be balanced in order to derive the reaction force-time history. Figure 5.25 illustrates the forces that develop on the specimen, i.e., $F_1(t)$ and $F_2(t)$, as a result of the boundary conditions and an applied force history, i.e., $P(t)$. The resultant force is
derived by subtracting the forces $F_1(t)$ and $F_2(t)$, i.e., $R_Z = F_2(t) - F_1(t)$. Conceptually, the resultant force derived using the LS-DYNA model data is equivalent to the force recorded by the reaction load cells in the experiment.

![Figure 5.25: Correction to LS-DYNA Reaction Force](image)

Results from the initial version of the LS-DYNA model were plotted and compared to experiment data. For this comparison, Test 03 and Test 04 completed on March 1, 2021 and LS-DYNA simulation of Test 03 were used. Test 03 involves impact with a steel bar specimen and Test 4 involves a dry fire test. The force-time history is calculated as described previously. Figure 5.26 plots the LS-DYNA impactor to specimen contact interface force history and the sum of the force histories for the four impact load cells used in the experiment. The negative impact force values in the experiment were set to zero as they indicate lesser levels of compression not applied force. Note that setting the negative force values to zero is necessary for comparison to the LS-DYNA model. The force history developed by the model involves the use of the impactor to specimen contact interface and as such, only values of compression and zero force, i.e., loss of contact, can be captured by this interface in the model. The mass-scaling and scaling of the stiffnesses of the impactor
and rods in the clamping assembly was employed to increase computational efficiency and to improve the simulated behavior at the contact and support interfaces, respectively. Comparing the model to the experiment, the impact force histories are out-of-phase with each other by approximately 7-ms and the peak forces are 5,126.46 lbf versus 6,416.63 lbf for the experiment compared to the model, respectively - a difference of 25.2%.

Figure 5.26: Impact Force Histories for LS-DYNA Model and Experiment

Figure 5.27 plots the LS-DYNA model’s corrected clamping assembly to specimen contact interface force history for the upper support. The sum of the force histories for the three reaction load cells under a given support in the experiment is also plotted. Note, that the force history for the lower support in the experiment is not plotted for clarity. Similarly, the reaction force-time histories are out-of-phase by approximately 7-ms and the peak forces are 4,614.24 lbf and 6,290 lbf for the experiment and LS-DYNA model, respectively - a difference of 36.3%.
Next, in order to better assess the model’s ability to capture the experimental results and specimen behavior, the impact and reaction impulse histories are plotted for the simulation and experiment. The experimental impulses were calculated as described previously and processed using a Butterworth 6th order lowpass filter with a cutoff frequency of 100,000 Hz, i.e., 10% of the sampling frequency for the DAQ. Additionally, the force values at the beginning of the history, 0 to 10 ms, and negative force values are set to zero. The signal between 0 to 10 ms occurs before impact with the target and can be ruled out as noise. Figure 5.28 plots the impact impulse histories for the model and the experiment. The impact impulse histories are out-of-phase by approximately 9-ms. The LS-DYNA impact impulse is approximately 30% to 98.6% lower than the experimental impact impulse.

Figure 5.27: Reaction Force Histories for LS-DYNA Model and Experiment
Figure 5.28 plots the reaction impulse histories for the model and the experiment. The LS-DYNA reaction impulse history is out-of-phase with the Upper and Lower Reaction support impulse histories by approximately 8-ms. The LS-DYNA reaction peak impulse is 127.11 psi-ms, compare this to the peak reaction impulses for the upper, 212.45 psi-ms, and lower supports, 161.23 psi-ms, in the experiment. The LS-DYNA peak impulse is 21.16% and 40.17% less than the peak impulses for the experimental reaction impulses for the upper and lower supports, respectively.
Figure 5.29: Reaction Impulse Histories for LS-DYNA Model and Experiment

Overall this version of the model exhibited poor agreement with the experimental data. Further refinement of the model was required to capture the physical behavior of the experiment and enable its use as a validation tool. Multiple iterations of the model were created and run. A summary of the changes include:

1. Removal of mass-scaling and increased stiffness applied to the clamping assembly rods and the impactor. Mass-scaling was initially used to increase the minimum time step and decrease model run-time. Also, stiffnesses for the impactor and supports were increased to observe whether a higher stiffness was needed to account for the significant difference in stiffness between the steel bar specimen and those members. The accuracy of the solution was improved.

2. Update to the static and kinetic friction coefficients for steel on steel contact for all
contact keywords, with the exception of tied contact keywords. Values of 0.65 and 0.42 were used for the static and kinetic friction coefficients, respectively.

3. Update to the contact interface between the reaction load cells and the clamping assembly backing plate from a tied contact to a surface-to-surface contact.

4. Update to the displacement history data to use the data captured by the high-speed camera footage and tracking software. This update alleviated the impact history timing discrepancy.

5. The reaction assembly was updated to remove the single point constraints placed on the ends of the assembly plates (Figure 5.24). Bolts and threaded rods were meshed and included in the model instead. Single point constraints were applied to the ends of the bolts to more accurately model the boundary conditions (Figure 5.30).

![Figure 5.30: LS-DYNA Model Boundary Conditions with Refinements (1 = Fixed; 0 = Free).](image)

The refinements discussed above led to markedly improved results. Figure 5.31 plots the force histories for the impactor to specimen contact interface in LS-DYNA and the
sum of the force histories for the four impact load cells used in the experiment. The force histories are in-phase and the peak force values are 5,585 lbf and 5,126 lbf for the model and the experiment, respectively. This is a difference of 8.95%. Similarly, the reaction force histories (Figure 5.27) are in-phase and several similarities are present in the force histories indicating better simulation of the experiment. Positive reaction peak force values were 7,340 lbf and 4,626 lbf for the model and experiment, respectively; a difference of approximately 58%. While the reaction peak forces in the model and experiment vary significantly, the reaction impulse histories for the model and experiment indicate more accurate model behavior.

![Impact Force Histories for LS-DYNA Model and Experiment](image)

**Figure 5.31**: Impact Force Histories for LS-DYNA Model and Experiment
Figure 5.32: Reaction Force Histories for LS-DYNA Model and Experiment

Figure 5.33 plots the reaction impulse histories for the upper and lower reaction load cells and the LS-DYNA specimen to rod contact interface impulse history. In general, the impulse histories are in-phase and closer agreement in the histories exists indicating higher levels of accuracy. The LS-DYNA impulse history curve shape exhibits many similarities with the experimental lower reaction impulse, which indicates that the level of impulse and timing correlates closely to the experiment. Furthermore, the peak impulse for the lower reaction impulse history and the model reaction impulse history are 161.23 psi-ms and 139 psi-ms, respectively; a difference of approximately 16%. The experimental upper reaction impulse varies markedly from the LS-DYNA model, however. This marked difference may be due to a variety of issues in the experiment including: uneven loading of the upper reaction load cells due to the supports being out-of-level marginally; uneven loading of the specimen; or tolerances used in fabrication of the experimental setup.
Figure 5.33: Reaction Impulse Histories for LS-DYNA Model and Experiment

Figure 5.34 plots the impact impulse histories for the experiment and the model. The impact impulse histories also exhibited no time discrepancy and improved accuracy in capturing the impulse transferred to the specimen during the experiment as evinced in the similar curve shapes. The peak impulses at 72 ms (i.e., the end of the simulation), were 125.3 psi-ms and 116.3 psi-ms for the experiment and the model, respectively; a difference of 7.75%. Despite the improved accuracy, further refinement to the model may be made by adjusting the friction coefficients used for the contact interfaces and introducing a compression force into the clamping assembly to simulate the compression introduced by the torquing of the threaded rod nuts.
Figure 5.34: Impact Impulse Histories for LS-DYNA Model and Experiment

Given these results, it may be concluded that the model captures the experiment behavior with sufficient accuracy to be used as validation tool and answer questions about the implementation of the Direct Force Method. The primary question that arises on the implementation of the Direct Force Method is with regard to the placement of the load cells behind the impactor: *Does the location of the impact load cells behind the impactor accurately capture the impulse that is being transferred by the impactor to the specimen at their contact interface, at the front of the impactor?* Given the results of the LS-DYNA model, it may be concluded that the impactor the impulse at the location of the load cells and at the front of the impactor are indeed the same. The impact impulses (Figure 5.34) demonstrate that the impulses are similar for the experiment where force is sensed behind the impactor and for the LS-DYNA model where force is queried at the contact interface between the impactor front and specimen. Further evidence is provided by the reaction impulses (Figure 5.33) for the experiment and the model. The similarities in the reaction impulses indicate that the model accurately captures the force transferred from the impact to the supports.
Furthermore, the experiment and model both show increased reaction impulse as compared to the impact impulse. The increased reaction impulse is likely due to the momentum of the specimen and clamping assemblies following an impact. A percentage increase of approximately 19% and 28% in peak reaction impulse was observed in the experiment and model, respectively. Note that the percentage increase in reaction impulse is similar for the model and for the experiment, which indicates accurate simulation of the experiment. Further analysis of this phenomenon will be discussed in the next chapter where impulsive testing of CLT is discussed.

5.5 Conclusions

A novel impulsive center-point testing system was designed and developed with the goal of subjecting CLT panels to impulsive out-of-plane loading. The testing system is highly versatile and adaptable:

- Specimens of various thicknesses, widths, and span lengths may be accommodated.
- Specimens of different materials may be tested.
- Boundary condition rotational rigidity is adjustable.
- The impactor is interchangeable thus allowing for various loading conditions to be tested.

This chapter analyzed the testing system and testing methodology and their performance under impulsive loading. The evaluation of the testing system’s performance required the development of an experimental method, i.e., the Direct Force Method. The Direct Force Method used the analysis of piezoelectric load cell data to determine the force history applied to a test specimen during an impulsive impact. LS-DYNA was used to conduct finite element analyses that were used to verify that the summed impact load cell force histories were similar to the force history at the impactor to specimen contact interface.
The primary conclusions are as follows:

1. The development of the Direct Force Method addresses the need for an experimental method that directly measures the force history delivered to a specimen during an impulsive test. Prior to the development of the Direct Force Method, existing techniques had three primary issues: 1) the requirement of sensitive hydraulic data, 2) the use of accelerometers and signal conditioning that had varying levels of success dependent on the mass and stiffness of the testing system and specimen, and 3) the requirement of both experimental data and finite element simulations. The Direct Force Method provides a simplified and accurate method for determining the net impact force history delivered and avoids the issues of methods used previously.

2. The LS-DYNA model results closely simulate the impact to a steel bar specimen and the subsequent response. Several comparisons to the model and the experimental data have been made and it has been demonstrated that the model is capable of simulating the impact and reaction force and impulse histories with a high degree of accuracy. The validated model was used to provide evidence that the placement of the load cells behind the impactor in the experiment do indeed capture the impulse transferred from the impactor front to the specimen. The validated model, therefore, provides validation for the Direct Force Method and its use as an experimental method to advance the use of instrumentation.
CHAPTER 6
BEHAVIOR OF IMPULSIVELY LOADED CROSS-LAMINATED TIMBER PANELS

An experimental test series was developed to investigate the behavior of low span-to-depth ratio CLT panels subjected to impulsive loading in a center-point bending loading condition. The goal of this test series was to subject CLT panels to a variety of impulses in a loading condition that would promote shear modes of failure. Impact impulses were varied to permit the observation of varied levels of damage ranging from elastic and inelastic to complete failure. An additional goal of the testing was to observe how varied levels of boundary condition rotational rigidity would affect CLT panel behavior. Two CLT layups were tested - three and five laminae - and a total of 35 tests were conducted using the impulsive center-point testing system. This chapter describes the materials and methods used to complete the test series, analysis of the collected data, and significant observations and conclusions from the test series.

6.1 Materials

6.1.1 CLT Panel Specimen

The CLT panels used for this test series were from the same lot of CLT panels described in Chapter 4. Three-laminae and five-laminae SPFS CLT panels produced by SmartLam and manufactured to meet the ANSI/APA PRG 320-12 V4 grade were used to produce specimens. The CLT panels supplied by SmartLam measured 4-ft. (1.219-m) x 8-ft. (2.438-m) and test specimen were cut from these panels. Test specimens were cut to measure approximately 16-in. (406.4-mm) ± 1/8-in (3.175-mm) wide and were cut to different lengths in order to achieve the span-to-depth ratio required for a given test. Panels were cut using
a large diameter circular saw, reciprocating saw, and carpentry layout tools. A reciprocating saw was required to complete cuts in the thicker five-laminae specimen and led to some specimen having rough textured cut faces. Figure 6.1 shows the specimen cutting in process.

Figure 6.1: CLT Panel Specimen Processing: (a) Cutting in Progress, (b) Partial Cut with Circular Saw, (c) Completed Test Specimen

Given the selected method of producing specimens, i.e., cutting from larger panels, the ability to control specimen attributes such as the location of gaps between lamellas, location of joinery, number of lamellas per lamina, and location of lamella defects was limited. Custom fabrication of CLT specimen to the required dimensions would have been ideal in comparison as it would have allowed for greater control. Despite the advantages to custom fabrication, the fabrication of CLT panel specimens is time, labor, and resource intensive as it requires sorting timber, material tests of the timber to be used in the panel, tests to verify adequate bond strength, the layup and bonding of the panels, and specialized manufacturing equipment to apply sufficient uniform pressure (e.g. specialized hydraulic press, clamping fixture, etc.). Therefore, while not ideal, the fabrication of panels in a laboratory setting has been deemed out of scope for this research.

Following cutting of the specimens to size, moisture content readings were taken at multiple locations for each specimen. A handheld two-pin moisture meter, DelmHorst BD-10, with a hammer electrode, DelmHorst 26-Es, was used to facilitate moisture readings up to 1.125-in. (28.575-mm) deep. Moisture content readings were taken at the narrow edge of each lamina at least 2-in. (50.8-mm) from the end of the panel (Figure 6.2) and on each
side face of the specimen. Initially, moisture content readings were taken at the locations labelled 1, 2, and 3 as illustrated in Figure 6.3. Consistent moisture content readings within a specimen and across multiple specimens highlighted that only moisture content readings at the 1 and 3 locations were necessary. The moisture content readings for each panel were recorded and their average was taken. The average moisture content for the specimens ranged from approximately 8% to 9%. The weight of each specimen was also taken and recorded in order to have an indication of individual specimen unit weight. Tables 6.1 and 6.2 list each specimen’s weight and average moisture content.

Figure 6.2: Moisture Content Readings in CLT: (a) Readings Taken at the Narrow Face of Each Lamina, (b) Readings Taken at the Side Face
Figure 6.3: Moisture Content Reading Locations: (1) Narrow Face Each Lamina, (2) Narrow Face Each Lamina - Opposite End, (3) Side Face

Figure 6.4: Base Plate and Strain Gage Locations
Table 6.1: Three Laminae Specimen Weights and Average Moisture Contents

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Table 6.2: Five Laminae Specimen Weights and Average Moisture Contents

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<th>Weight</th>
<th>Panel Name</th>
<th>Average M.C.</th>
<th>Weight</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT5-1</td>
<td>8.5</td>
<td>83.0</td>
<td>CLT5-10</td>
<td>8.1</td>
<td>74.8</td>
</tr>
<tr>
<td>CLT5-2</td>
<td>7.8</td>
<td>85.8</td>
<td>CLT5-11</td>
<td>7.8</td>
<td>74.4</td>
</tr>
<tr>
<td>CLT5-3</td>
<td>8.4</td>
<td>82.4</td>
<td>CLT5-12</td>
<td>8.0</td>
<td>77.8</td>
</tr>
<tr>
<td>CLT5-4</td>
<td>8.1</td>
<td>81.6</td>
<td>CLT5-13</td>
<td>8.1</td>
<td>78.5</td>
</tr>
<tr>
<td>CLT5-5</td>
<td>8.1</td>
<td>82.4</td>
<td>CLT5-14</td>
<td>8.4</td>
<td>88.1</td>
</tr>
<tr>
<td>CLT5-6</td>
<td>7.7</td>
<td>82.0</td>
<td>CLT5-15</td>
<td>8.5</td>
<td>86.6</td>
</tr>
<tr>
<td>CLT5-7</td>
<td>8.3</td>
<td>80.8</td>
<td>CLT5-16</td>
<td>8.5</td>
<td>91.5</td>
</tr>
<tr>
<td>CLT5-8</td>
<td>8.0</td>
<td>78.4</td>
<td>CLT5-17</td>
<td>8.1</td>
<td>93.0</td>
</tr>
<tr>
<td>CLT5-9</td>
<td>7.9</td>
<td>79.6</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Plywood base plates, 4-in. (101.6-mm) x 16-in. (406.4-mm), were cut for each of the specimen, four per specimen (Figure 6.4). The base plates were adhered to the specimens at the points where they made contact with the steel rods in the clamping assembly. Base plates were adhered to eliminate localized damage in the specimens resulting from load application.

6.2 Impulsive Testing Experimental Method

The impulsive center-point testing system described in Chapter 5 was used to complete experimental testing of the CLT panel specimens and study their behavior under impulsive load. Figure 6.5 illustrates the impulsive center-point testing system following an impulsive test. The impulsive testing procedure outlined in Chapter 5 was implemented to complete each of the tests with no significant changes made to the procedure. A modification to the impactor was made to add a felt programmer prior to the completion of the impulsive tests on the CLT specimens. The rationale for the modification will be discussed in Section 6.4.

A total of 35 impulsive tests were completed: 18 tests on three laminae CLT and 17 tests on five laminae CLT. Table 6.3 details the test matrix for this test series. Similar to the tests conducted in Chapter 5, the BG was used to deliver a punch, i.e., a singular pulse to each specimen. The velocities used for each test were determined using a combination of a single degree of freedom (SDOF) model for each layup and observations of damage during testing. The stiffness values calculated from the testing conducted in Chapter 4 were used in SDOF models to determine the impulse that would initiate damage in a CLT specimen, which allowed for the determination of a lower limit for impact velocity. The upper limit for the velocities tested was determined via observations of levels of damage that the CLT specimens exhibited during testing and residual strengths available in the specimens. Tests were conducted at intermediate velocities to permit the observation of the variation of damage at varied levels of impulse.
Figure 6.5: Testing Fixture Following an Impulsive Test
6.2.1 Felt Programmer Modification

Following the testing conducted on the steel bar specimen to validate the Direct Force Measurement method, the testing fixture was unmounted and disassembled prior to completion of the tests on the CLT specimens. Prior to testing of the CLT specimens, the testing system was re-installed, realigned, and additional validation tests using a steel bar specimen were completed to ensure optimal performance. During the course of the second round of validation testing, excessive oscillations, or signal noise, in the impact force history data was noted. The cause was believed to be a combination of the following: impactor alignment, torque of the bolts connecting the impactor to the BG, and high frequency vibrations isolated to the contact interface between the specimen and the impactor. The following modifications to the impactor and procedure corrected the issue: the impactor level and bolt torques were checked prior to each test, bolt torque was increased, and a 1/2-in. x 12-in. x 12-in. (12.7-mm x 304.8-mm x 304.8-mm) sheet of firm felt type F5 (Durometer 25A) was adhered to the impactor front (Figure 6.6). The modifications eliminated the signal noise.
Table 6.3: Impulsive Center-Point Test Matrix for Three Laminae CLT

<table>
<thead>
<tr>
<th>Test Number</th>
<th>Span Length in.</th>
<th>L:h&lt;sup&gt;a&lt;/sup&gt;</th>
<th>Clamping Torque in-lbf</th>
<th>Impact Velocity m·s&lt;sup&gt;-1&lt;/sup&gt;</th>
<th>Impact Energy kJ</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT3-1</td>
<td>27</td>
<td>6.55</td>
<td>65</td>
<td>9.86</td>
<td>4.80</td>
</tr>
<tr>
<td>CLT3-2</td>
<td>27</td>
<td>6.55</td>
<td>65</td>
<td>12.54</td>
<td>7.77</td>
</tr>
<tr>
<td>CLT3-3</td>
<td>27</td>
<td>6.55</td>
<td>65</td>
<td>12.76</td>
<td>8.06</td>
</tr>
<tr>
<td>CLT3-4</td>
<td>27</td>
<td>6.55</td>
<td>65</td>
<td>11.22</td>
<td>6.23</td>
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<tr>
<td>CLT3-5</td>
<td>27</td>
<td>6.55</td>
<td>65</td>
<td>10.98</td>
<td>5.96</td>
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<tr>
<td>CLT3-6</td>
<td>27</td>
<td>6.55</td>
<td>65</td>
<td>8.48</td>
<td>3.56</td>
</tr>
<tr>
<td>CLT3-7</td>
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<td>6.55</td>
<td>65</td>
<td>7.45</td>
<td>2.74</td>
</tr>
<tr>
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<td>6.55</td>
<td>65</td>
<td>7.63</td>
<td>2.88</td>
</tr>
<tr>
<td>CLT3-9</td>
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<td>65</td>
<td>6.13</td>
<td>1.86</td>
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<td>CLT3-10</td>
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<td>65</td>
<td>5.79</td>
<td>1.66</td>
</tr>
<tr>
<td>CLT3-11</td>
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<td>6.55</td>
<td>200</td>
<td>5.64</td>
<td>1.57</td>
</tr>
<tr>
<td>CLT3-12</td>
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<td>6.55</td>
<td>200</td>
<td>5.33</td>
<td>1.41</td>
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<tr>
<td>CLT3-16</td>
<td>27</td>
<td>6.55</td>
<td>200</td>
<td>6.99</td>
<td>2.41</td>
</tr>
<tr>
<td>CLT3-17</td>
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<td>2.40</td>
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<td>1.41</td>
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<tr>
<td>CLT3-13</td>
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<td>14.55</td>
<td>65</td>
<td>7.40</td>
<td>2.71</td>
</tr>
<tr>
<td>CLT3-14</td>
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<td>14.55</td>
<td>65</td>
<td>10.25</td>
<td>5.19</td>
</tr>
<tr>
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<td>60</td>
<td>14.55</td>
<td>65</td>
<td>9.84</td>
<td>4.79</td>
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</tbody>
</table>

<sup>a</sup>Span-to-depth ratio.
Table 6.4: Impulsive Center-Point Test Matrix for Five Laminae CLT

<table>
<thead>
<tr>
<th>Test Number</th>
<th>Span Length in.</th>
<th>L:h&lt;sup&gt;a&lt;/sup&gt;</th>
<th>Clamping Torque in-lbf</th>
<th>Impact Velocity m·s&lt;sup&gt;−1&lt;/sup&gt;</th>
<th>Impact Energy kJ</th>
</tr>
</thead>
<tbody>
<tr>
<td>CLT5-1</td>
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<td>5.15</td>
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<td>65</td>
<td>6.24</td>
<td>1.92</td>
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<td>6.40</td>
<td>65</td>
<td>12.31</td>
<td>7.50</td>
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<td>65</td>
<td>11.32</td>
<td>6.34</td>
</tr>
<tr>
<td>CLT5-5</td>
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<td>9.12</td>
<td>4.11</td>
</tr>
<tr>
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<td>4.45</td>
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<td>11.53</td>
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<tr>
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<td>6.14</td>
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<td>10.90</td>
<td>5.87</td>
</tr>
<tr>
<td>CLT5-13</td>
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<td>11.99</td>
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<tr>
<td>CLT5-14</td>
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<td>CLT5-15</td>
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<td>200</td>
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<td>1.73</td>
</tr>
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<td>CLT5-16</td>
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<td>1.44</td>
</tr>
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<td>6.40</td>
<td>200</td>
<td>4.68</td>
<td>1.09</td>
</tr>
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</table>

<sup>a</sup> Span-to-depth ratio.
6.2.2 Instrumentation

The instrumentation used for CLT impulsive test series was similar to that described in Chapter 5 with some additions. The additional instrumentation is described in the sections that follow.

6.2.2.1 Strain Gages

Uniaxial strain gages were installed at the midspan of each CLT specimen near the edge of the extreme laminae (Figures 6.4 and 6.7). Two brands of strain gages were utilized: Tokyo Measurement Labs (TML) PFL-20-11-3LJC and Micro-measurements C4A-06-235SL-120-39P foil strain gages. The brand and type of strain gage was changed to a local manufacturer when the supply of TML strain gages was exhausted due to supply chain disruptions. The strain gages were used in combination with a Hi-Techniques SY-BI-120 breakout box, which provided half bridge completion and connection to the Synergy
DAQ. Initial impulsive tests featured strain gage data, however, a malfunction in one of the Synergy DAQ’s modules made it impossible to collect strain gage data for later tests.

Figure 6.7: Strain Gage Placement

6.2.2.2 Implementation of Digital Image Correlation

Digital Image Correlation (DIC) is a non-contact experimental tool for obtaining high fidelity, full-field deformation gradients for test specimens. Strain gradients may also be calculated by post-processing the images to differentiate the displacement data. DIC uses correlation algorithms to compare subsets of pixel values between a series of digital images taken sequentially before, during, and after the deformation of a specimen. Interpolation of the pixel values allows for sub-pixel levels of accuracy to be obtained with DIC when optimal conditions are present. Calibration of the system to obtain diffuse lighting with adequate illumination, images with high contrast (i.e., high grey level counts), high quality speckling pattern, a lens with minimal lens distortion (e.g., a middle focal length lens), and
camera placement are a few of the variables that can be manipulated to achieve optimal conditions for DIC [147, 148, 149, 150, 151, 152, 153]. Given DIC’s level of accuracy, there have been many applications for DIC with more evolving over time. For example, per Blaber [154] DIC has been applied to biological materials, metal alloys, shape memory alloys, porous metals, polymers, and polymer foams. DIC has also been applied to concrete [155], masonry [156], and CLT [69, 157]. DIC may be implemented to determine deformation gradients for two-dimensional and three-dimensional surfaces. In 2D DIC, a single fixed camera is used to capture digital images of a planar surface thereby providing in-plane deformations for an object’s surface. In 3D DIC, on the other hand, the principle of binocular stereovision [158] is used to capture deformation of a curved surface or of out-of-plane displacements of a planar surface.

As 2D DIC was used in this research, a few advantages of 2D DIC will be discussed, as originally presented by Pan et al. [158]. First, experimental setup is simple. A setup only requires one fixed camera oriented with its optical axis normal to the specimen surface. Next, the setup requires the application of a speckle pattern, which may be applied using household paints to achieve a random grey intensity. Third, the requirements for lighting are low and only require adequate diffuse white or natural lighting, which allows for the implementation of DIC in the laboratory or in the field. Fourth, as digital images are processed by DIC algorithms, a wide range of devices that acquire high resolution digital images may be used. This aspect makes DIC rather flexible and, per Pan et al., permits the implementation of DIC in applications such as optical microscopy, scanning electron microscopy, and high-speed photography. Therefore, small length scale measurements (i.e., micro to nano) as well as measurements of specimens involved in dynamic, short time duration events are possible. While CCD sensor machine vision cameras are sometimes recommended [158] for DIC, several researchers [155, 157] have been able to obtain accurate results for their application using consumer grade, CMOS sensor cameras.

Some additional benefits to DIC include the simplification of instrumentation and the
successful application of DIC in materials and full-scale element testing. Instrumentation of an experiment is often simplified greatly when DIC is used as DIC generates full-field gradient maps of displacement and strain. This level of detail would require many strain gauges and other instruments and would often be impractical. DIC has been used in several structural engineering applications to identify or predict failure. Gencturk [159] was able to use DIC generated strain fields to map load paths in a prestressed concrete beam and predict crack growth via localized strains. Golewski [160] demonstrated that their DIC measurements predicted crack formation, showing crack formation prior to physical instrument measurements and before they were visible. Tung et al.[161] also showed similar results in early crack identification and prediction. Ji et al. [155] used DIC on repaired reinforced concrete beams to map strain, identify damage zones, and validate finite element modeling. DIC has been used on non-cementitious composites to identify micrometer scale cracking in silicon carbide (SiC) by Roux et al. [162]. LeBlanc et al. [163] used DIC in a structural health monitoring application to create full-field displacement and strain mappings of composite wind turbine blades and monitor the progression of damage around previously identified damaged areas.

This research implemented 2D DIC in the experiment to obtain in-plane displacement and strain measurements of CLT panel specimens. Full-field displacement and strain measurements were generated on the depth plane, i.e., the $e_1 - e_2$ plane, of the CLT specimens during testing (Figure 6.8).

Figure 6.8: Depth Plane of CLT Specimen, $e_1 - e_2$

Mapping of displacements and principal strains assisted with identifying failure initia-
tion, cracking patterns and their evolution through the thickness of the specimens, characteriza-
tion of failure mode(s), and level of damage in the specimens.

An open-source 2D DIC software package developed by Blaber et al. [154], NCorr, was used in conjunction with a high speed camera, i.e., Phantom Miro M310. This system facilitated the creation of full-field deformation gradients for the specimens during impulsive loading. The DIC results were validated using tracking software, Proanalyst [143]. An application of DIC using a representative specimen, CLT5-10, is discussed in further detail in the Appendix C.

6.2.2.3 Speckling CLT Specimen for DIC

Following the installation of the impulsive center-point testing system, the Phantom Miro M310 camera was positioned as it would be for testing and measurements of the camera’s field of view were taken. An assistant operator held measuring tape in a horizontal and then a vertical orientation at the depth of field where the speckled CLT specimen face was to be located. The recorded measurements, L = 51.5-in. (1308.1-mm) x W = 37.5-in. (952.5-in.), and camera resolution, 1280 x 800 pixels, were used to determine the image scale along the vertical and horizontal axes: 21 pixels per inch (0.83 pixels per mm) and 24 pixels per inch (0.94 pixels per mm), respectively. Jones et al. [164] indicates that a physical speckle size between three to five pixels is preferred as speckles that are smaller than three pixels risk being aliased, thereby, adding error to the DIC results. Jones et al. further states that speckles larger than five pixels are also undesirable as they will require larger subsets thereby, minimizing the spatial resolution of displacements and strains plotted. In DIC analysis, an image is divided into several subsets in order to calculate individual displacement values. Given the image scale and target range of pixels per speckle, maximums and minimums for the height and width of the speckle dot size may be computed (Table 6.5). Based on these calculations and the average image scale, a speckle size of 0.22-in. (5.59-mm) was selected.
Table 6.5: Physical Speckle Size Selection

<table>
<thead>
<tr>
<th>Target Pixels per Speckle</th>
<th>Width (in.)</th>
<th>Height (in.)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum - 5 Pixels</td>
<td>0.208</td>
<td>0.238</td>
</tr>
<tr>
<td>Minimum - 3 Pixels</td>
<td>0.125</td>
<td>0.143</td>
</tr>
<tr>
<td>Selected Speckle Size</td>
<td>0.22 in.</td>
<td></td>
</tr>
</tbody>
</table>

The speckle size was input into a computer application, Speckle Generator [165], which generates randomized speckle patterns with inputs for units, speckle diameter, density, and variation. The pattern was output to a PDF document and a laser cutter was used to cut the pattern into adhesive vinyl sheets. A flat, matte white household paint was used to coat one depth face on each CLT specimens. Once the white paint was cured, the cut vinyl sheets were applied to the face and a flat, matte black household spray paint was applied over the vinyl. The vinyl was subsequently removed. Figure 6.9 illustrates the resulting speckle pattern applied to a CLT specimen.
6.3 Theory and Calculation

6.3.1 Impulsive Loading

The impulsive loading applied to each of the specimens was determined using force recorded by a series of four piezoelectric, dynamic load cells attached to the back of the impactor and the Direct Force Method described in Chapter 5. Two tests are conducted for each specimen, a test with impact and a dry fire (no impact) test. The force histories for the four load cells on the impactor are summed for each test and the difference is taken to arrive at the net force applied to the specimen (Equation 6.1).

\[
F(t)_{\text{specimen}} = \left( \sum_{i=1}^{4} F(t)_i \right)_{\text{impact}} - \left( \sum_{i=1}^{4} F(t)_i \right)_{\text{dry fire}}
\]  

(6.1)

The reaction force histories due to the impulsive loading were similarly recorded via a
series of three piezoelectric, dynamic load cells per support. The reaction force histories are summed by support, i.e., upper support reaction force history and lower support reaction force history and as a total reaction force history (Equation 6.2).

\[
F(t)_{\text{upper reaction}} = \sum_{i=1}^{3} F(t)_i \\
F(t)_{\text{lower reaction}} = \sum_{i=1}^{3} F(t)_i \\
F(t)_{\text{total}} = F(t)_{\text{upper reaction}} + F(t)_{\text{lower reaction}}
\]  

(6.2)

Impact and reaction impulse are calculated by taking the integral of each respective force-time history (Equation 6.3).

\[
I = \int F dt
\]

(6.3)

Figures 6.10, 6.11, 6.12, 6.13, and 6.14 illustrate the product of completing these calculations for a representative test.
Figure 6.10: Individual Impact Load Cell Readings for CLT5-10 Specimen

Figure 6.11: Summed Force Histories for CLT5-10 Specimen
Figure 6.12: Net Impact Force and Impulse for CLT5-10 Specimen

Figure 6.13: Reaction Forces and Impulse for CLT5-10 Specimen
6.3.2 CLT Panel Displacement History

High speed camera footage was used to determine displacement history of each specimen and verify the velocity of the impactor at the point of impact. ProAnalyst [143] was used to process the high speed camera footage and analyze the motion of the targets and speckles on the specimens and impactor. ProAnalyst permits sub-pixel accuracy motion tracking and the field of view used, provided an image resolution of 0.04-in./pixel and 0.015-in./pixel for the Phantom Miro M310 and C110, respectively. While no specific literature is available on the level of sub-pixel accuracy achievable by ProAnalyst, 0.1 pixel subpixel estimation is generally accepted as a “rule of thumb” [166]. This implies that the motion tracking software with sub-pixel accuracy is capable of providing data that is accurate to 0.004-in. and 0.0015-in. for the Miro M310 and C110, respectively. Representative image tracking is shown in Figure 6.15. Tables 6.6 and 6.7 provide a summary of pertinent displacements and ductility for each specimen. Ductility is defined as a ratio that describes the post-
peak structural behavior of CLT as is convention for research in blast, shock, and impact research. For the impulsively loaded CLT, the ductility ratio is defined as the ratio of the maximum inbound displacement of the panel, $\delta_{\text{max}}$, to the deflection at the peak resistance, $\delta_R$.

The displacement history was used to determine maximum and residual displacements in each specimen (Figure 6.16). Additionally, the panel displacements were used with the moment-curvature equations to obtain the strain rate for each of the specimens. As CLT panels exhibit shear warping across their cross sections, they do not behave as Euler-
Bernoulli beams and the assumption that *plane sections remain plane* during deformation does not hold. While not an accurate representation of the strain rate in the panels, the moment-curvature equations were used to provide an estimate for strain rate in this case due to equipment malfunction during testing.

\[ \kappa = \frac{1}{\rho} \]

\[ \epsilon = -\kappa \cdot y \]

(6.4)

![Figure 6.16: CLT5-10 Displacement History](image)

### 6.3.3 Development of Resistance Function

The determination of the resistance function for CLT panels loaded out-of-plane may be accomplished via analytical methods or design methods. As data on the applied load
Table 6.6: Displacement Summary for Three Laminae CLT

<table>
<thead>
<tr>
<th>Test Number</th>
<th>( \delta_{max}^a )</th>
<th>( \delta_{Residual}^b )</th>
<th>( \delta_R^c )</th>
<th>( \mu^d )</th>
<th>( \dot{\epsilon}^e )</th>
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</thead>
<tbody>
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<td>7.54</td>
</tr>
<tr>
<td>CLT3-2</td>
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<td>2.70</td>
<td>1.05</td>
<td>4.85</td>
<td>11.77</td>
</tr>
<tr>
<td>CLT3-3</td>
<td>5.63</td>
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<td>3.11</td>
<td>2.04</td>
<td>2.35</td>
<td>1.32</td>
<td>11.75</td>
</tr>
<tr>
<td>CLT3-6</td>
<td>2.42</td>
<td>1.79</td>
<td>0.47</td>
<td>5.16</td>
<td>6.77</td>
</tr>
<tr>
<td>CLT3-7</td>
<td>1.79</td>
<td>1.42</td>
<td>0.55</td>
<td>3.25</td>
<td>6.75</td>
</tr>
<tr>
<td>CLT3-8</td>
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<td>1.16</td>
<td>0.44</td>
<td>3.92</td>
<td>6.43</td>
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<td>CLT3-9</td>
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<td>0.31</td>
<td>0.43</td>
<td>2.82</td>
<td>4.55</td>
</tr>
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<td>CLT3-10</td>
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<td>0.26</td>
<td>0.49</td>
<td>2.04</td>
<td>5.33</td>
</tr>
<tr>
<td>CLT3-11</td>
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<td>0.49</td>
<td>0.28</td>
<td>2.82</td>
<td>3.59</td>
</tr>
<tr>
<td>CLT3-12</td>
<td>0.71</td>
<td>0.12</td>
<td>0.62</td>
<td>1.14</td>
<td>3.62</td>
</tr>
<tr>
<td>CLT3-13</td>
<td>1.93</td>
<td>0.15</td>
<td>0.84</td>
<td>2.29</td>
<td>1.42</td>
</tr>
<tr>
<td>CLT3-14</td>
<td>3.97</td>
<td>0.56</td>
<td>0.87</td>
<td>4.56</td>
<td>1.74</td>
</tr>
<tr>
<td>CLT3-15</td>
<td>3.91</td>
<td>2.07</td>
<td>1.00</td>
<td>3.89</td>
<td>1.54</td>
</tr>
<tr>
<td>CLT3-16</td>
<td>1.32</td>
<td>0.72</td>
<td>0.41</td>
<td>3.20</td>
<td>5.12</td>
</tr>
<tr>
<td>CLT3-17</td>
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<td>1.47</td>
<td>0.43</td>
<td>4.08</td>
<td>5.36</td>
</tr>
<tr>
<td>CLT3-18</td>
<td>0.80</td>
<td>0.13</td>
<td>0.44</td>
<td>1.81</td>
<td>4.36</td>
</tr>
</tbody>
</table>

\(a\) Maximum panel displacement  
\(b\) Residual panel displacement  
\(c\) Panel displacement at peak resistance  
\(d\) Ductility calculated as \( \mu = \frac{\delta_{max}}{\delta_R} \)  
\(e\) Strain Rate
Table 6.7: Displacement Summary for Five Laminae CLT

<table>
<thead>
<tr>
<th>Test Number</th>
<th>$\delta_{\text{max}}$</th>
<th>$\delta_{\text{Residual}}$</th>
<th>$\delta_R$</th>
<th>$\mu$</th>
<th>$\dot{\varepsilon}$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>(in.)</td>
<td>(in.)</td>
<td>(in.)</td>
<td></td>
<td>(s$^{-1}$)</td>
</tr>
<tr>
<td>CLT5-1</td>
<td>1.21</td>
<td>0.17</td>
<td>0.52</td>
<td>2.31</td>
<td>1.45</td>
</tr>
<tr>
<td>CLT5-2</td>
<td>0.75</td>
<td>0.10</td>
<td>0.50</td>
<td>1.48</td>
<td>3.29</td>
</tr>
<tr>
<td>CLT5-3</td>
<td>7.87</td>
<td>-</td>
<td>1.08</td>
<td>7.31</td>
<td>10.75</td>
</tr>
<tr>
<td>CLT5-4</td>
<td>5.23</td>
<td>4.59</td>
<td>0.84</td>
<td>6.21</td>
<td>7.40</td>
</tr>
<tr>
<td>CLT5-5</td>
<td>2.88</td>
<td>2.22</td>
<td>0.98</td>
<td>2.93</td>
<td>5.24</td>
</tr>
<tr>
<td>CLT5-6</td>
<td>3.67</td>
<td>3.26</td>
<td>0.75</td>
<td>4.89</td>
<td>6.51</td>
</tr>
<tr>
<td>CLT5-7</td>
<td>3.77</td>
<td>3.49</td>
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<td>4.86</td>
</tr>
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</tr>
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<td>CLT5-9</td>
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<td>3.78</td>
<td>0.72</td>
<td>7.07</td>
<td>5.49</td>
</tr>
<tr>
<td>CLT5-10</td>
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<td>0.73</td>
<td>0.56</td>
<td>5.74</td>
<td>4.18</td>
</tr>
<tr>
<td>CLT5-11</td>
<td>3.73</td>
<td>3.52</td>
<td>0.28</td>
<td>13.24</td>
<td>4.77</td>
</tr>
<tr>
<td>CLT5-12</td>
<td>3.73</td>
<td>3.16</td>
<td>0.64</td>
<td>5.81</td>
<td>5.81</td>
</tr>
<tr>
<td>CLT5-13</td>
<td>7.25</td>
<td>4.57</td>
<td>1.07</td>
<td>6.78</td>
<td>8.40</td>
</tr>
<tr>
<td>CLT5-14</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>CLT5-15</td>
<td>0.72</td>
<td>0.046</td>
<td>0.31</td>
<td>2.30</td>
<td>3.28</td>
</tr>
<tr>
<td>CLT5-16</td>
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<td>0.10</td>
<td>0.48</td>
<td>1.22</td>
<td>3.61</td>
</tr>
<tr>
<td>CLT5-17</td>
<td>1.06</td>
<td>0.20</td>
<td>0.58</td>
<td>1.84</td>
<td>1.18</td>
</tr>
</tbody>
</table>

- $\delta_{\text{max}}$: Maximum panel displacement
- $\delta_{\text{Residual}}$: Residual panel displacement
- $\delta_R$: Panel displacement at peak resistance
- $\mu$: Ductility calculated as $\mu = \delta_{\text{max}} / \delta_R$
- $\dot{\varepsilon}$: Strain Rate
and dynamic reactions is available (i.e., from the experiment), an analytical method was employed. Furthermore, design equations are typically employed for structural materials where there is a greater understanding of the dynamic material behavior, e.g., steel, reinforced concrete, or reinforced masonry. An expression for the dynamic reactions may be derived in terms of the resistance and applied force by considering dynamic equilibrium of the CLT panels used in the experiment. The boundary conditions for the panels tested in this research are most closely modeled by simple supports with rotational springs, where the rotational spring stiffness, $k_R$, is unknown. Consider the CLT panel has uniform mass and stiffness and behaves as a simply supported beam with rotational springs at each support and a concentrated load applied at midspan per Figure 6.17.

\[
M_R = k_R \theta(t)
\]

The approximate angle of rotation at the boundary conditions, $\theta$, may be determined via Figure 6.17: Free Body Diagram for Dynamic Force Equilibrium

The approximate angle of rotation at the boundary conditions, $\theta$, may be determined via
high speed video footage and motion tracking software. Dynamic equilibrium requires that
the inertia of the beam be considered. The beam’s inertial force has the same distribution
along the beam length as the deflected shape of the beam per Equations 6.5, where $\rho$ is the
mass density, $A$ is the cross sectional area, $\ddot{v}$ is acceleration, $A_1$ is a constant, and $\phi(y)$ is
the deflected shape of the beam.

$$F_{\text{inertia}} = \rho A \ddot{v}$$

$$\ddot{v} = A_1 \phi(y)$$

(6.5)

As the inertial force is proportional to the deflected shape of the beam, the differential
equation for the deflected shape of the beam in Figure 6.17 may be derived and used to
determine the location, $zL$, of the resultant of the inertial force, $I(t)$. Note $zL$ is a multiple
of L and $z$ is may be determined by finding the centroid of the differential equation for
deflection. Finally, moments may be taken about the location of the resultant inertial force
to determine the equation for the dynamic reactions. Given the rotational springs at the
boundary conditions of beam under consideration, the differential equation for the deflected
shape and the dynamic reaction equation will yield equations with complex expressions in
terms of rotational spring stiffness, $k_R$, and angle of rotation, $\theta$. Additionally, using this
approach would require some approximations as the following assumptions that must be
made to derive the differential equation for the deflected shape do not hold for CLT panels:
1) The differential equation of the deflection curve for a prismatic beam assume that shear
deformations are negligible and considers that deformations are due to pure bending; 2) If
the assumption is made that a CLT panel’s deflected shape is approximately the same as that
provided by the differential equations up until fractures develop, then the deflected shape
would not be the same following the initiation of any fracture in the CLT panel. Given the
complexity of the final equation of the dynamic reactions, the uncertainty of $k_r$ and $\theta$, and
approximations that must be made to derive an analytical solution, an approximate method
was employed to determine the equation for the dynamic reactions.
The rigidity of the boundary conditions in the test fixture can be interpolated between the rotation rigidity provided by an ideal simple support \((k_R = 0)\) and an ideal fixed support \((k_R = \infty)\). To approximate the level of rotational rigidity provided by the test fixture’s boundary conditions, an SDOF model was created to simulate Validation Test V-44 (Chapter 5), i.e., an impulsive test on a steel bar specimen with dimensions 0.625 in. x 3.00 in. x 60 in. (15.88 mm x 76.20 mm x 1524 mm). The physical parameters used in the SDOF model are provided in Table 6.8. The stiffnesses for a steel beam with ideal fixed and ideal pinned boundary conditions were determined analytically using the following procedure:

1. Determine the ultimate moment capacity:
   
   \(M_{u,dyn} = f_{y,dyn}Z_x\)

   (b) Note, \(f_{y,dyn} = (SIF)(DIF)f_y\), where \(SIF\) and \(DIF\) are the static and dynamic increase factors, respectively.

2. Determine the ultimate resistance:
   
   (a) (Ideal Pinned) \(R_u = \frac{4M_{u,dyn}}{L}\)

   (b) (Ideal Fixed) \(R_u = \frac{4}{L} (M_{ps} + M_{pm}) = \frac{4}{L} (M_{u,dyn} + 2M_{u,dyn}) = \frac{12M_{u,dyn}}{L}\)

   (c) Note, \(M_{ps}\) and \(M_{pm}\) are the ultimate moment capacities at the support and midspan, respectively, as provided by Biggs in Table 5.2 in *EM 1110-345-415: Design of Structures to Resist the Effects of Atomic Weapons, 1957*, as cited in [167].

3. Determine the displacement:
   
   (a) (Ideal Pinned) \(x_e = \frac{R_uL^3}{48EI}\)

   (b) (Ideal Fixed) \(x_e = \frac{R_uL^3}{192EI}\)

4. Determine the elastic stiffness, \(k = \frac{R_u}{x_e}\).
Table 6.8: SDOF Parameters and Results

<table>
<thead>
<tr>
<th>SDOF Model Parameters</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Mass, ( kip \cdot s^2/in )</td>
<td>8.30E-05</td>
</tr>
<tr>
<td>Peak Force, ( kip )</td>
<td>4.10</td>
</tr>
<tr>
<td>Duration, ( sec. )</td>
<td>0.012844</td>
</tr>
<tr>
<td>Pinned B.C.’s Stiffness, ( kip/in. )</td>
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</tr>
<tr>
<td>Fixed B.C.’s Stiffness, ( kip/in. )</td>
<td>4.93</td>
</tr>
<tr>
<td>Rotational Spring B.C.’s Stiffness, ( kip/in. )</td>
<td>1.85</td>
</tr>
</tbody>
</table>

Given the low mass, low stiffness, and the homogeneity of the steel bar specimen at a structural material scale, it is reasonable to assume that the majority of the resistance was provided by the test fixture’s boundary conditions. Using the SDOF model, it was possible to determine the stiffness of the steel bar with the testing fixture boundary conditions. The calculated resistance for the steel bar with rotational springs did not reflect test conditions, i.e., the calculated resistance suggested that the bar should have exhibited more plasticity than was reflected by the test. As such, the resistance was set to an arbitrary resistance that was sufficiently high to model the steel bar behavior during the test. The stiffnesses for each boundary condition (i.e., ideal pinned, ideal fixed, and the test fixture) were used as a basis to interpolate between the constants in the dynamic reaction equations for a pinned and fixed beam in the elastic strain range (Equation 6.6) as derived by Biggs in *EM 1110-345-415: Design of Structures to Resist the Effects of Atomic Weapons, 1957*, as cited in [167]. Finally, it was assumed that the rotational rigidity provided by increasing the bolt torques in the clamping assemblies would follow a linear relationship. The final equations for the dynamic reactions for the testing fixture at each level of torque are provided in Equation 6.7.

\[
V(t) = 0.78R(t) - 0.28F(t) \quad \text{(Ideal Pinned)} \\
V(t) = 0.71R(t) - 0.21F(t) \quad \text{(Ideal Fixed)}
\]
\[ V(t) = 0.768R(t) - 0.268F(t) \quad (T = 65 \text{ in-lbs}) \]
\[ V(t) = 0.757R(t) - 0.257F(t) \quad (T = 130 \text{ in-lbs}) \quad (6.7) \]
\[ V(t) = 0.744R(t) - 0.244F(t) \quad (T = 200 \text{ in-lbs}) \]

The reaction force and net impact force history data from the experiment were used to determine the resistance force history for each specimen. Next, the resistance function was plotted and the linear-elastic region of the resistance function curve was used to derive the dynamic apparent flexural stiffness for each specimen. Representative resistance history and resistance function is presented in Figures 6.18 and 6.19.

Figure 6.18: CLT5-10 Resistance History
6.4 Experimental Results and Discussion

6.4.1 Effect of Impulsive Loading on CLT Panel Dynamic Apparent Flexural Stiffness

The effect of impulsive loading on CLT panels was studied by evaluating changes to their mechanical properties and characterizing damage caused in relation to variations in rotational rigidity at the boundary conditions and impact energy. Understanding of how CLT flexural stiffness is affected by impulsive loading is crucial in furthering understanding on the response of CLT panels to blasts. The dynamic apparent flexural stiffness, $EI_{app,dyn}$, was determined for each specimen by taking the slope of the resistance function for the elastic region of the resistance function. In addition to selecting the linear, pre-peak region of the resistance function, high speed video footage was reviewed to ensure that no visible damage had initiated in each specimen prior to the development of the peak impact force. The dynamic apparent flexural stiffness was compared to the average apparent flexural stiffness for the quasi-statically loaded specimens, $EI_{app,QS}$. These experiments
showed that the dynamic apparent flexural stiffness increased significantly in both the three laminae and five laminae CLT specimens (Figures 6.20 and 6.21).

Figure 6.20: Dynamic Apparent Stiffness Ratio for Three Laminae Specimens
At energies ranging from 1,000 J to 2,000 J, the five laminae specimens exhibited a higher amplification to their dynamic apparent flexural stiffness than the three laminae specimens. The higher amplification in dynamic apparent flexural stiffness in five laminae CLT may be due to a combination of the following factors: redundancy inherent in the presence of additional longitudinal laminae in five laminae specimens; increased stiffness due to the geometry of the five laminae specimens; and amplification of the shear modulus in the cross laminae (i.e., rolling shear modulus, $G_{RT}$) due to high rate impulsive loading.
and the presence of multiple cross layers in the five laminae specimens.

At higher impact energies, there was a marked decrease in the stiffness ratio for the five laminae CLT. The three laminae specimens, however, displayed a consistent stiffness ratio between approximately 3.0 to 4.5 for most impact energies tested. In both the three and five laminae CLT, stiffness ratios are higher at lower energy levels and drop dramatically once a certain level of impact energy is reached, thereby creating distinct impact energy zones. A similar phenomenon is also noted in the three laminae specimen with L:h = 14.55 - additional tests are required to determine a trend for these specimens in particular. In the three laminae CLT specimens, the transition between zones occurs at a higher level of impact energy, $E_{impact}$ ($6,500 \leq E_{impact} \leq 7,500$) as compared to the five laminae CLT specimens ($2,000 \leq E_{impact} \leq 4,000$).

Next, at higher levels of boundary condition rotational rigidity, the three laminae CLT exhibited a greater increase in dynamic stiffness (6% to 47%) compared to specimens tested at the baseline torque (i.e., $T = 65$ in-lbs) and similar impact energies. Increased boundary condition rotational rigidity in five laminae CLT did not appear to affect the dynamic stiffness and no clear relationship was noted.

The findings discussed above indicate the following conclusions:

1. Impulsive loading dramatically increases the apparent stiffness of CLT panels.

2. Stiffness ratios were fairly consistent in both CLT layups up to a certain level of impact energy above which, the stiffness ratios dropped dramatically. The transition zone in impact energy appears be between $6,500 \leq E_{impact} \leq 7,500$ for three laminae CLT and $2,000 \leq E_{impact} \leq 4,000$ for five laminae CLT.

3. Higher boundary condition rotational rigidity in three laminae CLT leads to a greater increase in apparent stiffness as compared to specimens tested at the baseline torque.
6.4.2 Damage Characterization

Different degrees of damage were observed following impulsive loading with higher degrees of damage being a function of the impact energy and impulse applied. All specimens were tested at a range of 1.0 to 8.0 kJ of impact energy and exhibited slight damage to complete panel failure. In this research, slight damage is characterized as little to no damage in the form of visible shear or flexural cracks, zero or low residual displacement, and a residual load capacity that is approximately the same (within 10% to 20%) as the average quasi-static load capacity. Heavy damage is characterized by partial rupturing of the extreme tensile and compressive laminae, presence of shear cracks, high residual displacement, and significant loss in residual capacity ($\geq 50\%$ of quasi-static capacity). Complete failure is characterized by failure of all lamellas in the tensile face lamina oriented in the major strength direction of the CLT panel.

6.4.2.1 Evaluation of Observed Failure Modes in Low Span-to-Depth Ratio Specimens

This section analyzes the progression of damage in low span-to-depth ratio specimens ($6.40 \leq L : h \leq 6.55$). Most specimens fall into this category with the exception of three specimens, i.e., CLT3-13 to CLT3-15, which are discussed in the following section. In general, each specimen exhibited a mixture of shear and flexural failure modes, however, all specimens consistently exhibited rolling shear and longitudinal shear failures that occurred at and immediately following impact. The shear failures were often followed by widening of the rolling shear cracks, the nucleation of additional rolling shear cracks, and the growth of longitudinal shear cracks. Shear failures in CLT led to the loss of composite action and resulted in load being increasingly redistributed to the longitudinal laminae, followed by complete failure of the specimen - in specimens that experienced failure. Complete failure was categorized by flexural failure modes, i.e., tensile rupture and/or compressive crushing, in the longitudinal laminae, which occurred several milliseconds following impact. The testing fixture proved to be effective at consistently eliciting rolling shear failure
modes in every low span-to-depth ratio specimen regardless of test parameters such as peak force, duration of load, impact energy, or boundary condition rotational rigidity. Figures 6.22 and 6.23 provide images of CLT specimens being loaded at varied impact energies and prior to the development of flexural failure modes. Figure 6.22 shows specimens CLT3-2 \( (E_{\text{impact}} = 7.77kJ) \), CLT3-5 \( (E_{\text{impact}} = 5.96kJ) \), and CLT3-18 \( (E_{\text{impact}} = 1.41kJ) \) several milliseconds following impact. In each specimen, a combination of rolling shear and longitudinal shear cracking are evident. In panels that exhibited moderate damage to complete failure, flexural failures such as, longitudinal cracking and rupture, were noted in the lamellas oriented in the major strength direction. Flexural failures occurred several milliseconds after impact and the development of shear cracking.

![Figure 6.22: Comparison of Damage in Three Laminae CLT at Varied Impact Energies. From Left to Right: CLT3-2 \( (E_{\text{impact}} = 7.77kJ) \), CLT3-5 \( (E_{\text{impact}} = 5.96kJ) \), CLT3-18 \( (E_{\text{impact}} = 1.41kJ) \)](image)
In panels that exhibited failure and experienced heavy damage to complete failure, a dramatic loss in resistance was noted following the development of the initial rolling shear cracks. As resistance falls, longitudinal shear cracks develop, which causes partial to complete delamination and allows the laminae to slip thus resulting in loss of composite action. As composite action is lost, the longitudinal laminae begin to rupture. In panels that do not exhibit failure and exhibit slight damage, loss in resistance is not due to rolling shear failures and is instead due to load reversal caused by the impactor retracting. Note, longitudinal shear cracks observed throughout this research occurred through the wood fibers and not the adhesive boundary layer indicating that the CLT panels met ANSI/PRG320-19 fabrication standards. Failure of the wood fibers in the longitudinal shear cracks indicates that the failures were not caused by failure of the adhesive. Longitudinal cracks with a smooth failure surface and relatively little wood fibers would indicate a failure of the adhesive.

Figure 6.24 shows the resistance history for a representative test where the specimen
experiences complete failure, specimen CLT3-2. At 40.79 ms peak resistance is reached and rolling shear and longitudinal shear cracks have just developed and have resulted in some delamination and slip between the laminae (Figure 6.25). Following this time, the resistance drops dramatically and by 42.70 ms the longitudinal laminae begin to rupture.

![Resistance History for CLT3-2](image)

Figure 6.24: Resistance History for CLT3-2
6.4.2.2 Evaluation of Observed Failure Modes in Moderate Span-to-Depth Ratio Specimens

Test specimens CLT3-13, CLT3-14, and CLT3-15 were cut to the dimensions 16-in. W x 66-in. L (406.40 mm x 1676.40 mm) to produce a span-to-depth ratio of $L:h = 14.55$. Tests on more slender specimens were desired to provide a point for comparison to the low span-to-depth ratio specimens. All three tests had the clamping fixtures torqued to 65 in-lbs. Testing on specimens of this span-to-depth ratio was only conducted on the three
laminae CLT layup as the testing fixture was not designed to accommodate the span length required for a five laminae layup. Additionally, designing to accommodate a five laminae layup would have required re-positioning of the blast generator actuator, which was not feasible for this testing.

Test CLT3-13 was impacted with a velocity of 7.40 m/s (24.29 ft/s) and experienced some plastic deformation with a residual displacement of 0.15 in. The damage noted included rolling shear, longitudinal shear, and flexural cracks. Figure 6.26, shows CLT3-13 following impulsive testing - note that damage caused by impulsive loading is highlighted in red ink. The laminae in the major strength direction remained intact despite some loss in composite strength due to the development of longitudinal shear cracks.

![Figure 6.26: CLT3-13 Post Dynamic Test](image)

Test CLT3-14 was impacted with a velocity of 10.25 m/s (31.62 ft/s). More extensive damage was noted than the previous test: extensive RS shear cracking throughout the height of specimen, longitudinal shear cracks extending on both boundary layers of the cross lamina for nearly the full height of the specimen, and complete rupturing of the tensile lamina (Figure 6.27). The compressive lamina remained intact. The residual displacement was 0.56-in.

![Figure 6.27: CLT3-14 Post Dynamic Test](image)

Test CLT3-15 was impacted with a velocity of 9.84 m/s (32.28 ft/s). Heavy damage was noted: several RS cracks and longitudinal shear cracks that extended down to the boundary and complete rupturing of the tensile and compressive laminae (Figure 6.28). The residual
displacement was 2.07-in.

Figure 6.28: CLT3-15 Post Dynamic Test

The development of damage in this set of tests was similar to that described in the low span-to-depth ratio tests. Residual strength tests were conducted on CLT3-13 and CLT3-14. No residual strength testing was conducted on CLT3-15 as the laminae in the major strength directions were completely ruptured.

CLT3-13 was compared to short span-to-depth ratio tests with similar impact velocities and clamping torque of 65 in-lbs: CLT3-7, 7.45 m/s (24.43 ft/s) and CLT3-8, 7.63 m/s (25.02 ft/s). Figure 6.29 plots the maximum inbound displacement and residual, or permanent, displacement versus impact energy. CLT3-13 exhibited approximately the same maximum inbound displacement as the lower span-to-depth ratio specimens, however, the residual displacement following dynamic loading for CLT3-13 (0.15-in.) was approximately 10% of the CLT3-7 (1.42-in.) and CLT3-8 (1.16-in.) residual displacements. A possible explanation for the CLT3-13 specimen behavior is that the greater flexibility of CLT3-13 allowed the panel to transfer more of the dynamic loading to the supports and dissipate the applied energy without damage as compared to the other specimens. Additionally, as the span-to-depth ratio of CLT3-13 is significantly greater than that of CLT3-7 and CLT3-8, a higher percentage of the deformation in CLT3-13 is due to flexural bending versus shear deformation. Low levels of damage in the laminae in the major strength direction in CLT3-13 provided more elasticity and greater strength.
Figure 6.29: Displacements for L:h = 14.55 Specimens
CLT3-14 and CLT3-15 were compared to short span-to-depth ratio tests with similar impact velocities and clamping torque of 65 in-lbs: CLT3-4, 11.22 m/s (36.83 ft/s), and CLT3-5, 9.93 m/s (32.58 ft/s). CLT3-14 and CLT3-15 exhibited similar maximum displacements, 3.97-in. and 3.91-in., respectively. Given the complete rupture of the laminae in the major strength direction for CLT3-15, its permanent displacement was greater than CLT3-14. The damage in specimen CLT3-15 was similar to CLT3-4 and CLT3-5 and it was observed that the residual displacement for all three specimens was similar. CLT3-14 had a residual displacement following dynamic loading of 0.56-in., which is approximately 27% of the residual displacements of CLT3-4 (2.08-in.), CLT3-5 (2.04-in.), and CLT3-15 (2.07-in.).

Figure 6.30: Residual Load for L:h = 14.55 Specimens
6.4.2.3 Net Force History as a Damage Identification Tool

The following section illustrates the use of the net impact force history and high speed camera footage to characterize damage in representative three and five laminae specimens. Given the implementation of the Direct Force Method, it was possible to derive the net impact force history applied to each specimen. The net impact force history provides data on the precise moment of contact between the impactor and specimen and the duration of the impact. The net impact force history made it possible to observe the development of new damage in the specimens based on fluctuations in the forces sensed by the impact load cells. These data paired with the review of the high speed video footage allowed for the characterization the failure modes.

Figure 6.31 illustrates net impact force history for specimen CLT5-10 with an impact that occurred at 11.19 m/s (36.72 ft/s). Significant fluctuations in the force throughout the history were observed to be correlated with the development of new damage, e.g., a shear crack, or the growth of damage that occurred earlier in the impact event. In Figure 6.31, several significant fluctuations in force are labeled and discussed below.
1. $t = 29.82$ ms: Contact between the felt programmer and specimen is made. Load rapidly increases beyond this point in time.

2. $t = 31.23$ ms: Peak force occurs at this time. Rolling shear cracks located above and below the mid-span develop and widen during the load duration.

3. $t = 31.23$ to $32.51$ ms: Crushing in the lower and upper support plywood bearing plates is observed. Additional rolling shear cracks and longitudinal cracks develop. The crushing of the bearing plates and development of shear cracks promotes the rapid decline of force.

4. $t = 32.51$ to $34.55$ ms: Crushing of bearing plate plywood continues. Tensile rupturing commences in a single lamella in the extreme tensile lamina and longitudinal flexural cracks develop in the extreme compressive lamina.

5. $t = 34.55$ ms to $37.44$ ms: Progressive tensile rupturing of the lamellas in the extreme tensile lamina. Fall and rise of force is indicative of failure of individual CLT
components or occurrence of damage and the subsequent redistribution of load.

6. $t = 37.44$ ms to $43.35$ ms: Complete failure of the extreme tensile lamina occurs. Rapid oscillations in the net force history is caused by bending of the wood fibers in the lamella in the extreme tensile lamina.

7. $t = 43.35$ ms to $46.65$ ms: Peaks at and after this point occur due to the deceleration and retraction of the impactor and the wobbling of the aluminum pusher plate toward and away from the direction of impact.

Figures 6.32 and 6.33 show frames from the high speed video at the approximate time associated with the net impact force history times described above. Note that the timing of each frame is approximately associated with the force history as the frame rate of the high speed camera is significantly less than the sampling rate of the Synergy data acquisition device - 3200 fps versus $10^6$ samples per second. Figure 6.38 shows CLT5-10 post test.
Figure 6.33: CLT5-10 Damage Progression (Continued)
The damage progression for a three laminae CLT specimen developed in a similar fashion to the five laminae specimens with rolling shear cracks typically preceding longitudinal shear cracks, followed by the progressive rupturing of the tensile lamellas, and the eventual failure of the specimen. Figure 6.35 illustrates the net impact force history for specimen CLT3-5 with an impact that occurred at 9.93 m/s (32.58 ft/s). Significant fluctuations in force are labeled and discussed below.
1. $t = 41.556$ ms: Contact between the felt programmer and specimen is made. Load rapidly increases beyond this point in time.

2. $t = 42.493$ ms: Rolling shear cracks develop in the lower half of the specimen. A RS crack merges with a longitudinal shear crack that spreads the lower boundary in the beam causing partial loss of composite action.

3. $t = 42.600$ ms: Peak force occurs at this time.

4. $t = 43.431$ ms: RS cracks that previously developed continue to widen. A longitudinal flexural crack develops in the compressive lamina and a lamella in the tensile lamina ruptures. Following this point in time, lamellas in the tensile lamina progressively rupture until complete failure of the panel.

5. $t = 47.649$ ms: Tensile lamina completely fails.

6. $t = 49.211$ ms: Compressive lamina ruptures.
7. t = 49.211 ms to 190 ms: Fluctuations in force history are primarily due to decel-
eration and retraction of the impactor and the resulting wobbling of the aluminum
pusher plate toward and away from the direction of impact.

8. t = 68.362 ms: Contact between the impactor and specimen ends.

Figures 6.36 and 6.37 show frames from the high speed video at the approximate time
associated with the net impact force history times described above. Figure 6.38 shows
CLT3-5 post test. An important observation from this study is that in both specimens the
time the specimen is under contact is far longer (≥ 10 ms) than the duration of any of the
peaks labeled in the net impact force histories. During the time under contact, the impactor
is advancing toward the specimen and contact is lost once the impactor begins to retract.
It may be concluded that each change in force history is indicative of the development of
damage in the specimen or a change in momentum of the impactor as was highlighted in
the damage progression descriptions in this section.
Figure 6.37: CLT3-5 Damage Progression (Continued)

Figure 6.38: CLT3-5 Post Dynamic Test
Table 6.9: Three Laminae Specimens Impact Velocity and Boundary Condition Summary

<table>
<thead>
<tr>
<th>Test Number</th>
<th>Impact Velocity $m \cdot s^{-1}$</th>
<th>Impact Energy $J$</th>
<th>Clamping Torque in-lbf</th>
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</thead>
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<td>CLT3-18</td>
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<td>1,407.50</td>
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6.4.2.4 Influence of Boundary Condition Rotational Rigidity on CLT Damage

Several test series were conducted at varying velocities and at three levels of torque for each lay-up. Varied levels of torque were tested to adjust the clamping pressure provided at the boundary conditions and provide data on the effect of boundary condition rotational rigidity on panel behavior. Tables 6.9 and 6.10 summarize the velocities tested, level of torque applied, and the specimen lay-up.

Figures 6.39 and 6.40 plot the maximum inbound displacements as a function of impact energy for the three laminae and five laminae specimens, respectively, with 65 in-lbs of torque as the baseline torque parameter.
Table 6.10: Five Laminae Specimens Impact Velocity and Boundary Condition Summary

<table>
<thead>
<tr>
<th>Test Number</th>
<th>Impact Velocity $m \cdot s^{-1}$</th>
<th>Impact Energy $J$</th>
<th>Clamping Torque in-lbf</th>
</tr>
</thead>
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<td>65</td>
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<td>CLT5-15</td>
<td>5.91</td>
<td>1,729.20</td>
<td>200</td>
</tr>
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<td>CLT3-16</td>
<td>5.39</td>
<td>1,438.30</td>
<td>200</td>
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<td>65</td>
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<td>5,873.40</td>
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<td>130</td>
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<td>CLT5-11</td>
<td>11.59</td>
<td>6,640.20</td>
<td>130</td>
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<td>CLT5-9</td>
<td>11.14</td>
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<td>200</td>
</tr>
<tr>
<td>CLT5-10</td>
<td>11.19</td>
<td>6,194.30</td>
<td>200</td>
</tr>
</tbody>
</table>
\[ \Delta_{\text{max}} = 0.0006354E_k - 0.001508 \text{ [in, J]} \]

Figure 6.39: Maximum Inbound Displacements for Three Laminae Specimens
A linear regression of the data shows a coefficient of determination of $R^2 = 0.85$ and $R^2 = 0.90$ for maximum inbound displacement as a function of impact energy for the three and five laminae specimens, respectively. Given the high degree of linearity of the trend lines, the maximum inbound displacement is a linear function of the impact energy and may be described by Equations 6.8 and 6.9 for the three and five laminae CLT specimens, respectively. The relationships described by Equations 6.8 and 6.9 are logical as they state...
that the maximum displacements in the CLT panels increase with increasing impact energy.

\[ \Delta = 6.354 \times 10^{-4} E_k - 0.001508 \text{ [in,J]} \]  

(6.8)

\[ \Delta = 9.971 \times 10^{-4} E_k - 0.6186 \text{ [in,J]} \]  

(6.9)

Figure 6.41: Residual and Maximum Inbound Displacements for Three Laminae Specimens

Figure 6.41 plots the maximum inbound and residual displacements for the three lam-
in CLT panel tests. The three laminae specimens were tested at two torques, 65 in-lbs and 200 in-lbs, and several velocities, 5.70 m/s (±0.40 m/s), 7.30 m/s (±0.30 m/s), 8.48 m/s, 9.86 m/s, 10.53 m/s (±0.69 m/s), and 12.65 m/s (±0.11 m/s). A torque of 65 in-lbs is used as the baseline torque parameter. At 65 in-lbs of torque the restraining moment at the boundary conditions is relatively low and specimens tested impulsively at this level of torque behave mostly as simple beams. Section 6.4 showed the similarity in resistance functions for a beam with ideal simple boundary conditions versus the test fixture’s boundary conditions thus demonstrating that specimens behave mostly as simple beams at the baseline torque. At impact energies of 3,500 J and higher, three laminae specimen experienced complete failure. The maximum inbound displacement for specimens torqued to 200 in-lbs was less than the maximum inbound displacement for specimens torqued to 65 in-lbs and tested at similar impact energies. The residual displacements showed a similar trend for most impact energies tested.
Figure 6.42 plots the maximum inbound and residual displacements for the five laminae tests. The five laminae specimens were tested at three torques, 65 in-lbs, 130 in-lbs, and 200 in-lbs, and several velocities, 4.90 m/s ($\pm 0.24$ m/s), 5.82 m/s ($\pm 0.43$ m/s), 10.29 m/s ($\pm 1.25$ m/s), 9.12 m/s, 11.65 m/s ($\pm 0.35$ m/s), 12.31 m/s. At impact energies greater than 6,100 J, five laminae specimens generally experienced complete failure. There was some variability in the impact energies that caused complete failure, as contrasted to the three laminae tests, which showed greater consistency. It is hypothesized that the larger
size and greater number of redundant cross and longitudinal laminae in five laminae CLT provided increased material heterogeneity and a greater possibility for load redistribution. Additionally, Steiger et al. [129] indicate that mechanical properties, e.g., bending stiffness, can vary strongly within a given CLT panel. Given the conclusion provided by Steiger et al. it is possible to conclude that there is a high level of heterogeneity that exists within a given CLT panel and as a result mechanical properties can have high variation in strips cut from a panel. The hypothesis provided by this author and the observation provided by Steiger et al. may explain the higher degree of variability in test outcomes for five laminae specimens that was noted.

Tests on five laminae CLT where torque was varied were conducted at two impact energy ranges: 1,000 J to 2,000 J and 6,000 J to 6,500 J. At lower impact energies, i.e., 1,000 J to 2,000 J, specimens tested with 200 in-lbs of torque exhibited 4.0% to 43.1% lower maximum inbound displacement than the baseline tests torqued to 65 in-lbs. While the residual displacements for the specimens tested with 200 in-lbs of torque were lower than those of the baseline tests (0% to 75%), they were fairly similar in magnitude and indicated that minimal damage occurred at lower impact velocities. At higher impact energies, i.e., 6,000 J to 6,500 J, lower maximum inbound displacements were observed in specimens tested with 130 in-lbs (12.24% to 28.68 %) and 200 in-lbs (2.3% to 38.43%) of torque. Similarly, lower residual displacements were observed in specimens tested with 130 in-lbs (12.24% to 28.68 %) and 200 in-lbs (2.3% to 38.43%) of torque. Additional tests at 130 in-lbs and 200 in-lbs of torque are required at the tested velocities and middle velocities not tested in order to establish whether the above observations hold at other velocities and if there is a function that describes the relationship between displacement and energy for the higher torques tested.

The observations on the maximum and residual displacement data are indicative of lower levels of damage in specimens with higher levels of rotational rigidity at the boundary condition in both CLT panel layups tested.
6.5 Conclusions

Impulsive loading was applied to CLT Panels in a center point loading condition. A novel testing fixture was used to mount and provide restraint for the CLT specimens and an ultra, high velocity actuator was used to apply impulsive loading at the midspan of the panels. Two layups of spruce-pine-fir-south CLT panels were tested, three and five laminae. The test series was successful at promoting shear modes of failure and providing data on several parameters that were unstudied in CLT loading in flatwise bending at an impulsive loading regime. Low span-to-depth \((6.40 \leq \frac{L}{h} \leq 6.55)\) CLT panels were tested to promote shear modes of failure. Moderate span-to-depth ratio \((\frac{L}{H} = 14.55)\) CLT panels were tested to provide data on the effect of moderate span-to-depth ratios on CLT behavior and the prevalence of shear modes of failure. Three levels of boundary condition rotational rigidity were provided to study the effect of boundary condition rotational rigidity on CLT panel behavior. The resistance function for each CLT panel was developed to determine the increase in dynamic apparent flexural stiffness and to study damage development in the CLT panels. Damage was analyzed by studying high speed video footage, the physical CLT specimens, using the Direct Force Method, and panel displacements.

The primary conclusions are as follows:

1. The test fixture was successful at eliciting rolling shear and longitudinal interlaminar shear failures in all CLT panels tested. Analysis of the resistance history verified that resistance dropped dramatically following the development of shear cracking. No dependency was noted on material geometrical parameters, such as CLT layup, or test parameters, such as, boundary condition rotational rigidity, span-to-depth ratio, impact energy, peak load, load duration, or impact impulse. Shear modes of failure have been largely absent from CLT panels that undergo live blast testing. The results and observations from these experiments suggest that the occurrence of shear modes of failure in impulsively loaded CLT panels may be due to the load condi-
tion and span-to-depth ratio rather than the boundary conditions, CLT panel layup and geometry, or intensity and duration of loading. The literature further supports this hypothesis as shear modes of failure have been observed in shock tube testing of CLT where a four-point bending loading condition was employed [118]. A uniform loading condition, as may be applied by live blast loading, should be explored.

2. Strain rate effects were noted in the apparent flexural stiffness of all specimen tested. The dynamic apparent flexural stiffness of CLT was 1.3 to 7.2 times greater than the quasi-static apparent flexural stiffness. For the three laminae specimens, stiffness ratios were consistent across most kinetic energies tested with ratios the stiffness ratio dropping at the higher end of the energies tested. For the five laminae specimens, stiffness ratios were consistent at the lower range of kinetic energies tested, i.e., 1.000 to 2.000 kJ, with stiffness ratios exhibiting a decreasing trend with increasing kinetic energies above a kinetic energy of approximately 4.000 kJ. There appears to be a transition zone in the stiffness ratios for kinetic energies ranging from 2.000 kJ to 4.000 kJ; testing was not conducted in this energy range and as a result additional testing is required to capture the change in stiffness in this range.

3. The presence of shear modes of failure resulted in the dramatic loss of resistance in all specimens tested. The resistance histories provided a method for correlating loss in resistance to the development of damage. Additionally, the Direct Force Method may be used to aid in the characterization of damage in CLT. The development of a resistance function that captures CLT post-peak behavior is required as experimental data and analytical tools are limited.

4. Lower levels of damage were observed in specimens with higher levels of boundary condition rotational rigidity. This observation implies that the provision of stiffer boundary conditions can reduce damage in impulsively loaded CLT panels. Additionally, relationships that correlate impact kinetic energy with maximum inbound
displacement were developed for both CLT layups tested, Equation 6.8 (Three Laminae) and Equation 6.9 (Five Laminae).
CHAPTER 7
QUASI-STATIC RESIDUAL CAPACITY TESTING OF CROSS-LAMINATED
TIMBER PANELS

Following impulsive loading, insitu residual capacity tests were conducted on CLT panel specimens that did not completely fail during the impulsive loading. A method for insitu quasi-static center-point loading was implemented to load CLT specimens to failure. The primary goal of this test series was to qualify and quantify damage in CLT panels that were tested impulsively and did not exhibit complete failure. An additional goal of the test series was to quantify the change in the residual mechanical properties of CLT following impulsive loading and understand the degree to which these properties were affected by impulsive loading. Two CLT layups were tested - three and five laminae - and a total of 21 tests were conducted using the impulsive center-point testing system and insitu quasi-static testing system. This chapter describes the materials and methods used to complete the test series, analysis of the collected data, and significant observations and conclusions from the test series.

7.1 Materials

This test series employed the CLT panels that were impulsively loaded in the impulsive testing series as described in Chapter 6. Panels that survived impulsive loading and did not exhibit complete failure were quasi-statically loaded to failure. Complete failure in impulsively loaded CLT was defined as fracture, via tensile rupture or compressive crushing, through the entire thickness of all laminae oriented in the major strength direction, i.e., the longitudinal direction. A total of 21 residual capacity tests were conducted: 11 tests on three laminae CLT and 10 on five laminae CLT. A summary is provided in Tables 7.1 and 7.2 of the specimens tested with the specimens grouped by common applied impact
energies.

Table 7.1: Quasi-Static Center Point Residual Capacity Test Matrix for Three Laminae CLT

<table>
<thead>
<tr>
<th>Test Number</th>
<th>L:h</th>
<th>$T_{\text{clamp}}^a$</th>
<th>$P_{\text{max},i}^b$</th>
<th>$P_{\text{max},r}^c$</th>
<th>$i_{\text{impact}}^d$</th>
<th>$i_{\text{reaction}}^e$</th>
<th>$\dot{\varepsilon}^f$</th>
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</thead>
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<td>147,737</td>
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</tr>
</tbody>
</table>

a Clamping torque
b Peak impact force during dynamic testing
c Peak reaction force during dynamic testing
d Peak impact impulse during dynamic testing
e Peak reaction impulse during dynamic testing
f Strain rate during residual testing

7.2 Methods

7.2.1 Quasi-Static Residual Strength Testing System Design Concept

Quasi-static (QS) residual capacity testing was employed by using a method for insitu quasi-static residual strength testing designed by Sanborn [168] with some modifications. Figure 7.1 illustrates a conceptual rendering of the modified QS system and Figure 7.2 provides an image of the completed testing system prior to a test.
Table 7.2: Quasi-Static Center Point Residual Capacity Test Matrix for Five Laminae CLT

<table>
<thead>
<tr>
<th>Test Number</th>
<th>L:h</th>
<th>$T_{clamp}$</th>
<th>$P_{max,i}$</th>
<th>$P_{max,r}$</th>
<th>$i_{impact}$</th>
<th>$i_{reaction}$</th>
<th>$\dot{\epsilon}$</th>
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<td></td>
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<td>kips</td>
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<td>39.44</td>
<td>222,100</td>
<td>327,682</td>
<td>6.10E-07</td>
</tr>
</tbody>
</table>

\begin{itemize}
\item[a] Clamping torque
\item[b] Peak impact force during dynamic testing
\item[c] Peak reaction force during dynamic testing
\item[d] Peak impact impulse during dynamic testing
\item[e] Peak reaction impulse during dynamic testing
\item[f] Strain rate during residual testing
\end{itemize}
Figure 7.1: QS Residual Strength Testing System Concept: (a) Side View, (b) Isometric View
A hydraulic cylinder with attached load cell is used as an actuator and is anchored to a W14x145 steel column (Figure 7.3). The actuator-column assembly is positioned in front of the reaction frame and specimen following an impulsive test. The actuator-column assembly anchors to a W12x65 steel beam that is fixed to the laboratory’s strong floor with steel DYWIDAG post tensioning bars and centered in the rail system frame. The design and the laboratory’s 30-ton (27.22-t) overhead crane allows for the actuator-column assembly to be rapidly positioned or removed as necessary per the testing work flow. The actuator-column assembly was used in conjunction with an impactor of similar design to that used in the impulsive tests. The impactor was positioned at the midspan and center of the specimen, and the actuator was used to apply a pre-load and maintain the impactor in place. A universal ball joint was placed between the load cell and the back of the impactor (Figure 7.4) to serve two purposes: 1) To direct the actuator load to the midspan of the
specimen, and 2) as a safety measure to protect the actuator piston rod against eccentric loading. During testing, rotation of the impactor occurred given its round shape and the deflection of the specimen. The circular shape of the impactor and the provision of a universal ball joint, mitigated the rotation of the impactor and the rotation of the actuator by directing the actuator to the specimen.

Figure 7.3: QS Actuator-Column Assembly
7.2.2 Instrumentation of the Quasi-Static Residual Tests

The applied force was measured via a load cell, Interface Model No. 1120AQ-50K, with a 50 kip (222.4 kN) capacity in compression. An excitation voltage of 5V was applied and the load to voltage slope was determined. Midspan displacement measurements were taken on each side of the panel to account for the potential of nonuniform failure across the panel's width. Displacements were measured using two string potentiometers, Celesco No. PT101-0010-111-1110 and PT1A-50UP-500-M6-SG, with a maximum stroke of 20-in.(508-mm) and 50-in. (1270-mm), respectively.
Figure 7.5 shows the placement of the string potentiometers and the HSS steel housing that was fabricated to protect the string potentiometers from inadvertent damage. An excitation voltage of 10V was provided and the displacement to voltage slope was determined, while accounting for the stroke range to be used in this test series. Strain gauges were applied at midspan along the depth of the specimens, near the tensile and compressive faces to record the maximum tensile and compressive normal strains. Two brands of strain gages were utilized: Tokyo Measurement Labs (TML) PFL-20-11-3LJC and Micro-measurements C4A-06-235SL-120-39P foil strain gages. Given supply chain disruptions during the time that testing ramp-up occurred, the brand and type of strain gage was changed to a local manufacturer when the supply of TML strain gages was exhausted. The strain gages were used with a NI9237 module, RJ50 cable, and NI9949 accessory box with a 120Ω precision resistor wired in a quarter bridge configuration. Signal voltage data was recorded using a National Instruments NI cDAQ-9178 data acquisition system with NI9219 and NI9237 modules, and LabView. Power for the instruments was supplied via a
MASTECH Dual Regulated DC power supply HY5005-2.

7.2.3 *Quasi-Static Residual Strength Testing Procedure*

The following procedure was followed to quasi-statically load the CLT specimens.

Prior to mounting specimen for dynamic testing:

1. Mark specimens for placement of strain gauges and string potentiometers. A layer of polyester or two part epoxy adhesive is used to fill the voids in the wood and bond the strain gauges to the surface of the CLT specimen. Allow the adhesive to cure for a minimum of 10 minutes to 3 hours - cure time is dependent on the adhesive used. Wood screw eye hooks are installed to facilitate anchoring to the string potentiometers (Figure 7.6).

![Figure 7.6: Wood Screw Eye Installation](image)

Procedure for residual capacity test following impulsive testing:
1. Check level of clamping fixtures and confirm threaded rod torque using a torque wrench.

2. Connect string potentiometer wires to the CLT specimen.

3. Use overhead crane to lift and position the actuator, load cell, and column assembly over the horizontal W12x65 steel beam. The assembly is bolted to the W12x65 beam.

4. The impactor is positioned at the midspan and center of the CLT panel and held in place with the overhead crane. The universal ball joint is positioned and lifted into place using a bottle jack. Once the impactor and universal ball joint are in position, the impactor alignment is checked with respect to the specimen and modifications are made as necessary (Figure 7.7). A preload of approximately 700 lbf. (3.11 kN) to 900 lbf. (4.00 kN) is applied to compression fit the impactor, joint, and actuator. Following the application of the preload, the bottle jack is lowered so it does not interfere with testing. The overhead crane is lowered and the straps remain attached to the impactor and crane. Sufficient slack is provided to allow for unrestricted range of motion in the actuator and to catch the impactor in the event of a sudden specimen failure.

5. Load is applied via a hand operated pump while observing the load-displacement and strain curves to ensure consistent, quasi-static application of load. Load is applied until the specimen achieves a post-peak load of approximately 10% to 20% of peak load or all laminae in the major strength direction fail. Duration of tests ranged from 10 to 30 minutes.
7.2.4 Quasi-Static Stiffness Testing Procedure

The static stiffness was determined for the five laminae specimen prior to impulsive testing. The stiffness of the five laminae specimens was found following processing. A servo-
A hydraulic actuator and an MTS 407 controller were used to load the specimens to 3.20 kips (14.23 kN) - approximately 10% of the average maximum static load determined experimentally for the 46-in span specimens tested in Chapter 4. The goal of this testing was to determine the undamaged stiffness while ensuring that the specimens would remain well within their elastic range to avoid damage. The clamping assemblies used in the impulsive testing series were fastened to a rigid beam that was fastened to the servo-hydraulic actuator. A 3-in. wide beam was fastened to a reaction frame and was used to apply load to the specimens (Figure 7.8). As the purpose of the tests was to determine the CLT panel stiffness and avoid damage, the use of the rounded impactor was not deemed necessary for these tests. Additionally, the use of the servo-hydraulic actuator was deemed superior in this application as the load rate could be controlled with high precision. An additional benefit of the servo-hydraulic actuator and testing fixture is that the amount of time to execute a test was minimized as compared to the process used for the static tests conducted in 2019 (Chapter 4).
The following procedure was followed to find the stiffness of the undamaged CLT panel specimens:

1. Bond bearing plates to both sides and ends of the specimen.

2. Mount and align the CLT panel specimen in the test fixture. The alignment of the specimen is checked to ensure that each specimen is centered in both horizontal directions over the supports and under the load head.

3. Clamp the specimen at the boundary conditions. The top clamping assembly plate is lowered onto the specimen, leveled, and a torque of 65 in-lbs is applied to each threaded rod in each clamping assembly.
4. Load is applied at a rate of 0.1 in./min. up to approximately 3.20 kips. The specimen is then unloaded at a rate of 0.1 in./min. Three cycles were conducted of load-unload. The third cycle is used for the Experimental Elastic Stiffness calculation. Test duration was approximately 6 minutes.

7.3 Theory and Calculation

7.3.1 Quasi-Static Actuator Alignment

During testing it was noted that the steel support beam anchored to the strong floor slid away from the test fixture and the steel column-actuator assembly rotated clockwise in the perspective provided in Figure 7.9.

![Figure 7.9: Translation and Rotation of Quasi-Static Actuator](image)

This was of concern as the angle of the actuator affects the actual load applied to a
specimen as compared to the load exerted by the actuator and sensed by the load cell. The maximum horizontal and vertical displacements were approximated using still images taken during testing and visual observation. The relative movement of the actuator assembly was studied for the CLT5-14RC test as this specimen had the highest peak force of all residual capacity tests conducted and would therefore provide the greatest movement of the actuator assembly. The rails that are a part of the blast generator rails, in the background and foreground of Figure 7.9, served as a reference point. Given the location of machine screw holes in the rails and the movement of the actuator assembly relative to this feature, it was possible to estimate the vertical and horizontal movement as 0.1875-in., i.e., half the diameter of the machine screw holes. Some error is introduced by this method of estimation given the following factors: 1) distortion introduced by the camera’s lens, 2) angle of focal plane with respect to the subject, and 3) real depth of the rail compared to the actuator assembly in the camera’s depth of field, i.e., the rail and actuator do not lie on the same plane. As a result, the actual vertical and horizontal movement may be greater than 0.1875-in.

Figure 7.10: Actuator Rotation
Figure 7.10 illustrates the movement of the actuator and the resulting change in force. To address this potential for error, a sensitivity study was conducted by varying vertical displacements to evaluate the sensitivity of the actual load applied to the specimen to positive changes in vertical displacement. The calculation of the change in angle is illustrated in Equation 7.1, where $\theta$ is solved in terms of $\delta_y$ for the sensitivity study.

$$
\theta = \sin^{-1} \left( \frac{\delta_y}{x_1 + x_2} \right)
$$

For 0.1875-in. of vertical displacement, the actuator assembly would rotate upward approximately $0.547^\circ$ with respect to the horizontal plane and results in 24.409 kips of load being applied to the specimen. Compare 24.409 kips to the load exerted by the actuator and sensed by the load cell of 24.41 kips for CLT5-14RC, a percent difference of 0.00456%. Significant deviation ($\geq 5\%$) of the applied load from the actuator load was found to occur at a vertical displacement of 6.125-in. It was found that for 6.125-in. of vertical displacement, the actuator assembly would exhibit an angle of $18.19^\circ$ with respect to the horizontal plane and result in 23.19 kips of load being applied to the specimen. Compare 23.19 kips to the load exerted by the actuator and sensed by the load cell of 24.41 kips for CLT5-14RC, a percent difference of 5.00%. As the assembly’s movement was barely perceptible via visual observation, it is not possible that the actuator displaced more than several fractions of an inch in the vertical direction. The small change in load given the approximated vertical displacement, led to the conclusion the movement of the quasi-static actuator did not have a significant effect on the testing or test results.

### 7.3.2 Stiffness of Undamaged Five Laminae CLT

The five laminae CLT panel specimens were quasi-statically loaded prior to impulsively loading them. The specimens were loaded to approximately $10\%$ of their predicted max-
imum load carrying capacity. The experimental elastic stiffnesses, \( K \), were determined using the slope of the force-displacement curve for each specimen. The slope was determined by performing a linear regression of a region of the linear elastic portion of the force-displacement curves that corresponded to 50% to 70% of the maximum applied load. The selected region, i.e., 50% to 70%, was deemed appropriate given the low magnitude of the maximum loads (\( \approx 3.20 \text{ kips (14.23 kN)} \)) and higher order portion of the curve that occurred in all static tests conducted in this research. This higher order curve portion (Figure 7.11) results at the initiation of the static center-point bend tests and was related to the compression of the bearing plates and seating of the load head. It is for this reason that multiple cycles of load-unload were conducted in order to minimize this effect. A summary of results is provided in Table 7.3.
Figure 7.11: Force-Displacement Curves for Undamaged Stiffness Determination

7.3.3 Residual Capacity of Damaged and Undamaged Panels

The experimental values for residual capacity of the CLT panel specimens were compared to the quasi-statically loaded CLT panels (QS CLT) tested to failure in Chapter 4. The maximum load for the QS CLT was determined by using the average value for the maximum loads resisted by the quasi-static tests conducted (Tables 4.9 and 4.10). The Shear Analogy Method (SAM) was used to determine analytical values for maximum load carrying capacity for the tests with higher span-to-depth ratios (L:h = 14.55), i.e., CLT3-13 and CLT3-14, where QS testing was not conducted. The maximum load carrying capacity, $P_{max}$, was determined using the average moment capacity of the quasi-static experimental
Table 7.3: Stiffnesses for Undamaged Five Laminae CLT Specimens

<table>
<thead>
<tr>
<th>Test Number</th>
<th>$L_{span}$ in.</th>
<th>$P_{max}^a$ kips</th>
<th>$\Delta_{max}^b$ in.</th>
<th>$K^c$ kip/in.</th>
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<td>66.46</td>
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<td></td>
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<td><strong>COV</strong></td>
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<td><strong>3.65%</strong></td>
<td><strong>10.74%</strong></td>
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</table>

$^a$ Maximum load  
$^b$ Displacement at the maximum load  
$^c$ Experimental elastic stiffness, i.e., slope of the force displacement curve ($P/\Delta$)

testing, $M_{max}$, and the effective section modulus for the major strength direction, $S_{eff,0}$.

Equation 7.2 provides the equations used to determine $P_{max}$ and $S_{eff}$.

$$S_{eff,0} = \left( \frac{2EI_{eff}}{E_1 \cdot h} \right)$$

$$P_{max} = \frac{4M_{max}}{L}$$

(7.2)

Where:

$EI_{eff} =$ effective stiffness.
\[ E_1 = \text{modulus of elasticity of the laminae in the major strength direction.} \]

\[ h = \text{total thickness of the panel.} \]

\[ L = \text{span length.} \]

The apparent stiffness, \( E_{I_{\text{app}}} \), was determined for the specimens that were tested for residual capacity. The residual apparent stiffnesses for the three laminae specimens were compared to the average apparent stiffness for the three laminae QS CLT (Chapter 4). The residual apparent stiffnesses for the five laminae specimens were compared to the average apparent stiffness determined for the five laminae prior to undergoing impulsive loading (Section 7.3.2). The calculations were similar to those conducted in Chapter 4.

### 7.4 Experimental Results and Discussion

Tables 7.4 and 7.5 provide a summary of results for this test series. Figures 7.12, 7.13, 7.14, and 7.15 plot the load displacement data. Several tests were conducted at similar velocities and varying levels of torque applied to the clamping assembly to assess if there was a change in specimen behavior resulting from changes to rotational rigidity. All test failures included the widening of existing cracks in combination with the growth of cracks in the cross laminae and were followed by the failure of the longitudinal laminae via tensile rupture or compressive crushing and loss of all residual capacity. Sudden changes in the post peak loading is generally indicative of the development of new damage in the cross laminae with dramatic changes generally being indicative of failure in the longitudinal laminae.
Table 7.4: Residual Capacity Result Summary for Three Laminae CLT

<table>
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<tr>
<th>Test Number</th>
<th>L:h</th>
<th>(v_i^a)</th>
<th>(E_k^b)</th>
<th>(T_{\text{clamp}}^c)</th>
<th>(\Delta_{\text{fail}}^d)</th>
<th>(\Delta_{\text{peak}}^e)</th>
<th>(\mu_f^f)</th>
<th>(P_{\text{max}}^g)</th>
<th>(K^h)</th>
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<td>7.45</td>
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<td>1.43</td>
<td>2.22</td>
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<td>1.38</td>
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<td>1.91</td>
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<td>3.82</td>
<td>1.00</td>
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</table>

\(^a\)Impact velocity during impulsive testing
\(^b\)Kinetic Energy of impacting mass
\(^c\)Torque applied to the clamping assembly threaded rods
\(^d\)Maximum displacement
\(^e\)Displacement at peak load
\(^f\)Ductility ratio \((\Delta_{\text{fail}}/\Delta_{\text{peak}})\)
\(^g\)Maximum load
\(^h\)Experimental elastic stiffness, i.e., slope of the force displacement curve \((P/\Delta)\)
Table 7.5: Residual Capacity Result Summary for Five Laminae CLT

<table>
<thead>
<tr>
<th>Test Number</th>
<th>L:h</th>
<th>$v_i^a$</th>
<th>$E_k^b$</th>
<th>$T_{clamp}^c$</th>
<th>$\Delta_{fail}^d$</th>
<th>$\Delta_{peak}^e$</th>
<th>$\mu^f$</th>
<th>$P_{max}^g$</th>
<th>$K^h$</th>
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</thead>
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<td>1.41</td>
<td>2.04</td>
<td>21.99</td>
<td>19.03</td>
</tr>
<tr>
<td>CLT5-5</td>
<td>6.40</td>
<td>9.12</td>
<td>4,114.90</td>
<td>65</td>
<td>4.56</td>
<td>3.27</td>
<td>1.39</td>
<td>4.10</td>
<td>7.46</td>
</tr>
<tr>
<td>CLT5-17</td>
<td>6.40</td>
<td>4.68</td>
<td>1,085.00</td>
<td>65</td>
<td>5.09</td>
<td>1.39</td>
<td>3.66</td>
<td>12.79</td>
<td>11.04</td>
</tr>
</tbody>
</table>

$^a$Impact velocity during impulsive testing  
$^b$Kinetic Energy of impacting mass  
$^c$Torque applied to the clamping assembly threaded rods  
$^d$Maximum displacement  
$^e$Displacement at peak load  
$^f$Ductility ratio ($\Delta_{fail}/\Delta_{peak}$)  
$^g$Maximum load  
$^h$Experimental elastic stiffness, i.e., slope of the force displacement curve ($P/\Delta$)
Figure 7.12: Load-Displacement Plot: Three Laminae Specimens at 1.41 kJ to 1.86 kJ Impact Energy

Figure 7.13: Load-Displacement Plot: Three Laminae Specimens at 2.40 kJ to 2.88 kJ Impact Energy
Figure 7.14: Load-Displacement Plot: Five Laminae Specimens at 4.11 kJ to 6.64 kJ Impact Energy

Figure 7.15: Load-Displacement Plot: Five Laminae Specimens at 1.31 kJ to 1.92 kJ Impact Energy
7.4.1 Residual Mechanical Properties

The effect of impulsive loading on the specimens was evaluated by studying their residual mechanical properties as compared to the average mechanical properties of quasi-statically loaded undamaged specimens (Chapter 4). Three mechanical properties were investigated, namely the maximum residual strength (i.e., residual capacity), residual apparent stiffness, and the ductility of specimens following impulsive loading. Residual rolling shear strength was not investigated as the Shear Analogy Method does not model inelastic behavior in CLT panels. The development of a new analytical method or extension of the Shear Analogy Method is required to capture inelastic behavior and fracture in CLT panels and was deemed out of scope for this research.

7.4.1.1 Residual Apparent Stiffness Ratio

Figure 7.16 plots impact energy with the ratio of the residual Apparent Stiffness to the average undamaged Apparent Stiffness for three laminae CLT. The residual stiffness was generally between 10% to 20% of the undamaged stiffness for the baseline tests (L:h = 6.55, 65 in-lbs). The specimens with higher rotational rigidity provided at the boundary conditions exhibited greater residual apparent stiffness for every energy level tested. At the higher span-to-depth ratio tested, L:h = 14.55, specimens exhibited higher residual apparent stiffness as compared to the L:h = 6.55 specimens. Additionally, the L:h = 14.55 specimens exhibited a greater ability to withstand higher energy loading.
Figure 7.16: Residual Apparent Stiffness Ratio for Three Laminae Specimens

Figure 7.17 plots impact energy with the residual stiffness ratio for five laminae CLT. The residual stiffness was generally between 10% to 40% of the undamaged stiffness for all tests regardless of boundary condition rotation rigidity or impact energy. Some variation in the residual stiffness was noted in the specimens tested at impact energies ranging from approximately 1.50 kJ to 2.00 kJ. Specimens tested with an applied torque of 130 in-lbs survived testing at high impact energies (≈ 6.5 kJ) and retained some residual stiffness as compared to tests conducted at similar impact velocities that exhibited complete failure.
despite having higher (i.e., CLT5-9 and CLT5-10) or lower (i.e., CLT5-4) levels of torque.

Figure 7.17: Residual Apparent Stiffness Ratio for Five Laminae Specimens

Three laminae specimens experienced a dramatic reduction in apparent stiffness following impulsive loading even at low impact velocities whereas five laminae specimens did not show as great of a loss in stiffness. The global apparent stiffness in CLT is highly dependent on the composite action provided by the cross laminae. Therefore, any loss in the integrity of the cross laminae in three laminae CLT specimens had deleterious effects as there is much less redundancy than five laminae CLT.
7.4.1.2 Residual Strength Ratio

Figure 7.18 plots the impact energy with the ratio of the residual strength to the average undamaged strength for three laminae specimens. The plot shows a decreasing trend in residual capacity as impact energy increases. The specimens with L:h = 14.55 exhibit a decreasing trend as well and also show that significantly more energy (2 to 3 times) is required to result similar residual capacities in the L:h = 6.55 specimens. An additional observation is that higher rotational rigidity results in higher residual capacity when compared to the baseline tests (L:h = 6.55, 65 in-lbs) at similar levels of impact energy.
Figure 7.18: Residual Strength Ratio for Three Laminae Specimens

Figure 7.19 plots the impact energy with the ratio of the residual capacity to the average undamaged capacity for five laminae specimens. The plot shows a decreasing trend in residual capacity as impact energy increases. At impact energies ranging from 1.00 kJ to 2.00 kJ, residual capacities generally ranged from 50% to 70%. At impact energies greater than 4.00 kJ, residual capacity seemed to be consistent at about 10%. Higher rotational rigidity at the 1.00 kJ to 2.00 kJ range showed inconsistent results, where one specimen showed a greater ability to hold residual capacity than the other test conducted at the same
rotational rigidity and similar impact energy. Rotational rigidity due to an applied torque of 130 in-lbs appears to have had a positive effect on residual capacity. Specimens tested at 130 in-lbs of torque survived higher impact energies (∼6.50 kJ) and also had a ratio similar to baseline tests conducted at impact energies approximately 40% lower.

Figure 7.19: Residual Strength Ratio for Five Laminae Specimens
7.4.1.3 Ductility Ratio

Ductility ratios for the specimens tested for residual capacity were determined using displacements that included the permanent deformation that resulted from impulsive loading. Equation 7.3 illustrates how the residual ductility ratios were calculated.

\[
\mu_{Residual} = \frac{\delta_{fail} + \delta_{permanent}}{\delta_{peak} + \delta_{permanent}}
\]  

(7.3)

Figure 7.20 plots the impact energy and the ratio of the residual capacity specimens ductility ratios to the average undamaged ductility ratio for three laminae specimens. Note that the specimens with L:h = 14.55 were omitted as there is no data to provide comparison at this span-to-depth ratio. Generally the residual ductility is between 10% to 20% less than the undamaged ductility ratio indicating a modest loss in ductility for most specimens tested. At impact energies ranging from 1.60 kJ to 1.90 kJ, specimens tested at baseline test parameters (L:h = 6.55, 65 in-lbs) exhibited markedly higher ratios than the tests subjected to higher applied torques (200 in-lbs). The higher ratios indicate that the baseline specimens exhibited greater ductility following impulsive loading than the specimens with a higher boundary condition rotational rigidity. At higher impact energies (2.40 kJ to 2.90 kJ), tests at both levels of torque exhibited similar residual ductilities - approximately 80% of the undamaged ductility.
Figure 7.21 plots the impact energy and the ratio of the residual capacity specimens ductility ratios to the average undamaged ductility ratio for five laminae specimens. Specimens tested at the baseline parameters (L:h 6.40, 65 in-lbs) exhibited modest loss in ductility (≈10%) at impact low impact energies (1.00 kJ to 2.00 kJ). Two specimens tested at the baseline parameters exhibited high to very high (50% to 170%) increases in ductility. The higher ductilities may be due to material heterogeneity. At a similar impact energy range, specimens tested at 200 in-lbs of torque showed a significant increase in ductility. At
higher impact energies (4.00 kJ to 4.50 kJ) marked loss in ductility is exhibited (≈ 40% to 55%). Interestingly, specimens tested at 130 in-lbs of torque and approximately 6.50 kJ of impact energy exhibited residual ductilities similar to specimens with baseline parameters (L:h 6.40, 65 in-lbs) and at significantly lower impact energies.

Figure 7.21: Residual Ductility Ratio for Five Laminae Specimens
7.4.1.4 Qualitative Damage in CLT Specimens

Variability in the five laminae specimens residual properties were noted for specimens tested with similar impulsive test parameters for impact energies ranging from 1.00 kJ to 2.00 kJ. For example, CLT5-16 retained much of its residual mechanical properties \(\frac{EI_{\text{App,Residual}}}{EI_{\text{App,undamaged}}} = 0.88\), \(\frac{P_{\text{Residual}}}{P_{\text{undamaged}}} = 0.90\), and \(\frac{\mu_{\text{App,Residual}}}{\mu_{\text{App,undamaged}}} = 1.53\) compared to CLT5-15. Per Figure 7.22, the level of damage that CLT5-16 (200 in-lbs, 1.44 kJ) received during impulsive loading was minimal compared to CLT5-15 (200 in-lbs, 1.73 kJ). Similarly, CLT5-14 retained much of its residual mechanical properties \(\frac{EI_{\text{App,Residual}}}{EI_{\text{App,undamaged}}} = 0.63\), \(\frac{P_{\text{Residual}}}{P_{\text{undamaged}}} = 0.67\), and \(\frac{\mu_{\text{App,Residual}}}{\mu_{\text{App,undamaged}}} = 2.76\) compared to CLT5-1, CLT5-2, and CLT5-17. Per Figure 7.23, the damage that CLT5-14 received was also minimal in comparison to CLT5-1 (65 in-lbs, 1.31 kJ), CLT5-2 (65 in-lbs, 1.92 kJ), and CLT5-17 (65 in-lbs, 1.09 kJ).

Figure 7.22: Damage in CLT5-15 and CLT5-16 Following Residual Capacity Tests. Damage is marked and delineated: "D" - Impulsive Damage, "RC" - Residual Capacity Damage.
Figure 7.23: Damage in CLT5-1, CLT5-2, CLT5-14, and CLT5-17 Following Residual Capacity Tests. Damage is marked and delineated: "D" - Impulsive Damage, "RC" - Residual Capacity Damage.

The residual properties for specimens CLT5-14 and CLT5-16 are commensurate with the level of impulsive damage observed and indicates that the variability in their residual properties may be due to a combination of material heterogeneity, fabrication variability (e.g., placement of finger joints), or layout of the lamellas in the major strength direction. While both CLT5-15 and CLT5-16 had three lamellas on each side face - tensile and compressive - lamellas in the major strength direction for CLT5-16 were wider than the lamellas on both the compressive and tensile faces for CLT5-15 (Figure 7.24). Similarly, CLT5-14 had lamellas that were wider than CLT5-1 and CLT5-17 and comparable to CLT5-2 (Figure 7.25). CLT5-2 and CLT5-17 had a lamella that failed along a finger joint on the tensile face of the panels, which likely contributed to their reduced residual stiffness as compared to CLT5-14. In other specimens tested, finger joint placement in the tensile lamina appeared to affect its performance whereas no adverse effects were noted for finger joint found in the
compressive lamina. The variance in lamella width indicates that the presence of narrower lamellas in the lamina in the major strength direction and joint placement in the tensile lamina had a significant effect on the CLT panel performance. In this research, specimen were produced by cutting from a larger CLT panel and as such, consistent lamella width and control over joint placement was not achievable as readily as with a custom fabrication of CLT specimens. It is recommended that future research employ custom fabrication of CLT specimens in order to elicit greater control over the materials tested.

Figure 7.24: Failure in Lamellas in CLT5-15 and CLT5-16 Following Residual Capacity Tests
7.5 Conclusions

Center point insitu quasi-static loading was applied to CLT specimens following an impulsive load. The test series successfully captured the residual capacity of both the three and five laminae CLT specimens. Residual Apparent Stiffness, Residual Strength, and Residual Ductility were studied to quantify the residual capacity of the specimens at various energy levels and boundary condition rotational rigidities. Residual rolling shear strength was not investigated as the Shear Analogy Method does not model inelastic behavior in CLT panels. The development of a new analytical method or extension of the Shear Analogy Method is required to capture inelastic behavior and fracture in CLT panels. Quasi-static testing of undamaged CLT specimens was used for comparison to determine the degree of damage caused by the impulsive loading. The cumulative damage resulting from both the impulsive loads and residual loads was used to determine the cause of variability in the five laminae
specimens’ properties.

The primary conclusions are as follows:

1. A dramatic and consistent loss in residual stiffness ($\geq 85\%$) occurred for three laminae CLT even at low levels of impact energy. Five laminae specimens also experienced a dramatic loss in stiffness (40% to 90%). In both CLT layups, greater loss in stiffness with increasing kinetic energy was observed. Damage in all specimens resulting from the impulsive loading initiated in the cross laminae given the design of the impulsive experiments. The dramatic and consistent loss in stiffness that occurred in both layups as a result of the impulsive loading paired with the shear cracks that developed in the cross laminae, indicate that the development of even slight damage in the cross laminae has severe outcomes on the global stiffness of the CLT panels. It may be concluded that impulsively loaded CLT that has shear as a primary mode of failure, behaves as a brittle structural element that is shear dependent.

2. Residual strength decreased dramatically in three laminae specimen as compared to five laminae specimen. Three laminae specimens showed a 70% to 90% loss in strength as compared to 30% to 90% for five laminae specimens.

3. Both three and five laminae specimens retained much of their ductility with residual ductility being 45% to 90% of the ductility of undamaged specimens.

4. Specimens tested with increased boundary condition rotational rigidity had higher residual strength and stiffness in both layups tested. However, residual ductility was lower in specimens with higher rotational rigidity. Three laminae specimens with increased boundary condition rotational rigidity exhibited higher residual apparent stiffness and residual strength than the baseline residual capacity tests at all impact energies tested. Five laminae specimens with higher rotational rigidity had higher residual strength and stiffness at most impact energies tested and survived higher impact energies than the baseline test specimens. Three laminae specimens with
increased rotational rigidity also exhibited less ductility than the baseline tests at each impact energy tested, while the converse was true for the five laminae specimens.

5. Five laminae specimens exhibited higher residual strength and stiffness than three laminae specimens at similar impact energies.

6. Global residual mechanical properties were affected by characteristics of local members, i.e., lamella width, and the placement of joints in the tensile lamina. For laminae oriented in the major strength direction of the panel, the presence of finger joints adjacent to the midspan of the panel appeared to alter the location of the failure surface, i.e., the failure surface tended to occur at the location of the joint. Additionally, finger joints located near the midspan appeared to reduce the tensile capacity of the extreme lamina, thereby reducing the global capacity of the panel and the load required for complete fracture of the panel. Given these observations, it is recommended that specifications for CLT to be used for construction applications be written such that finger joints are located away from areas of high stress or are reinforced in some manner. Furthermore, CLT specimens used for experimentation should employ custom fabrication whenever practical in order to control the placement of joints and control the width of the lamellas with greater precision and certainty.
8.1 Conclusions

8.1.1 Experimental Testing of CLT Under Quasi-Static Center-Point Loading

Quasi-static center-point loading was conducted on undamaged, spruce-pine-fir-south CLT with low span-to-depth ratios, $5.5 \leq L:h \leq 9.2$. The quasi-static test series were conducted using similar boundary conditions and a similar loading condition to those used in the impulsive tests. Additionally, the quasi-static test series provided data on the static mechanical properties and the behavior of the CLT panels. The static test data provided a basis for comparison to dynamic test data and facilitated understanding CLT's behavior when subjected to impulsive loading regimes. The Shear Analogy Method (SAM) was used to determine various properties of the CLT specimens and was compared to derivations made from the experimental data. The primary conclusions from this quasi-static center-point testing are summarized below:

1. The testing fixture was effective in testing CLT panels quasi-statically. The testing fixture and methodology employed permitted the determination of the average rolling shear strength, $\tau_{RS}$, and the average static apparent flexural stiffness, $EI_{app,QS}$. The average rolling shear strength was similar to values for rolling shear strength for spruce-pine-fir CLT cited in the literature. The SAM was an effective tool for determining rolling shear strength and apparent flexural stiffness in low span-to-depth ratio CLT loaded in a center-point loading condition. Greater accuracy in SAM predictions can be derived from the use of mechanical properties determined from material scale tests on the timber used in the CLT. For higher levels of accuracy in the determination of rolling shear strength and apparent flexural stiffness, the use of test
methodologies that isolate those properties are recommended (e.g., two-plate shear test).

2. As quasi-static testing was completed in two phases, the load rate was controlled manually in the first phase and digitally in the second phase. The higher accuracy of the digital control permitted higher consistency in testing and drastically lowered the coefficient of variance of all data collected. It was noted that the apparent flexural stiffness was highly sensitive to changes in load rate. While most of the CLT specimen’s properties were affected by the higher load rate resulting from manual control, varying approximately 12% to 15%, the apparent flexural stiffness for CLT loaded at the higher rate was as much as 280% greater than the average apparent flexural stiffness for CLT loaded at the lower rate.

3. The SAM method was an effective tool for determining rolling shear strength in low span-to-depth ratio CLT loaded in a center-point loading condition.

8.1.2 Design of an Impulsive Center-Point Testing System with Realistic Boundary Conditions

An important outcome of this research was the design and development of a novel impulsive center-point testing system that is capable of subjecting CLT to impulsive out-of-plane flexure via the application of high strain-rate loading. The testing system was designed to be highly versatile and adaptable: the testing fixture can accommodate specimens of various thicknesses, widths, and span lengths; specimens of different materials may be tested; boundary condition rotational rigidity is adjustable; and different loading conditions may be tested by interchanging the impactor used. The testing system and methodology allow for high repeatability and for the application of controlled impulses. Similar test parameters for the hydraulic system may be used to deliver nearly identical impulses to different specimens with a high degree of repeatability.
The primary contribution of this research was the development of the *Direct Force Method* (DFM). The DFM provides an experimental method that measures the impulsive force history delivered by the blast simulator to a specimen during a *punch* type test. Implementation of the DFM does not require the use of sensitive data, finite element modeling of complex instrumentation or test fixtures, or signal conditioning inherent in the use of instruments such as accelerometers. Highly detailed force history data results from the implementation of the DFM allowing for detailed observations into the damage that occurs in a specimen. Finite element simulations of test specimens are simplified by the implementation of the DFM as the net force that is derived can be used as the input load for the simulation. As a result, simulation of the impactor and complex physical parameters (e.g., rail friction and momentum of hydraulic fluids) introduced by the blast simulator actuator can be avoided, thereby simplifying the analysis of a given test specimen.

### 8.1.3 Behavior of Impulsively Loaded Cross-Laminated Timber Panels

An important contribution from this research was the implementation of a novel experimental method for the impulsive loading of CLT at the structural element scale. This research employs a testing methodology that consistently causes shear modes of failure in impulsively loaded CLT panels. The study of CLT subjected to blast-like or impulsive loading has been limited to the study of slender, higher span-to-depth ratio specimens that exhibit flexural failure modes primarily and low levels of ductility (i.e., $\mu \leq 3.0$ to 4.0). This research fills a gap in the literature by studying the behavior of CLT panels that exhibit shear failure modes, i.e., rolling shear and longitudinal shear. Primary conclusions from this testing include the following:

1. While span-to-depth ratio was demonstrated to have a significant effect on the occurrence of shear modes of failure in impulsively loaded CLT, the load condition may also play a large role in whether shear failure modes are observed in CLT. Testing conducted in the literature that involves out-of-plane loading on moderate span-to-
depth ratio CLT panels in a four-point bending loading condition [118] seem to support this conclusion.

2. The dynamic apparent flexural stiffness of CLT exhibited a consistent, dramatic increase as a result of strain rate effects at all impact energies tested. A transition zone where a notable decrease in the $EI_{app,dyn}/EI_{app,QS}$ ratio was evident at higher impact energies.

3. Higher level of boundary condition rotational rigidity was correlated to lower levels of damage.

4. The Direct Force Method facilitated the characterization of damage as it developed in the CLT specimens.

8.1.4 Quasi-Static Residual Capacity Testing of Cross Laminated Timber Panels

An additional significant contribution from this research was the experimental study of CLT’s static residual capacity following impulsive loading. Understanding of the behavior of damaged CLT is an area that has been relatively unstudied and is critical towards the implementation of CLT in force protection applications. Primary conclusions from this testing include the following:

1. Residual Apparent Stiffness, Residual Strength, and Residual Ductility each exhibited a downward trend with increasing impact energy. Three laminae specimens generally experienced a greater reduction in residual strength and stiffness than the five laminae specimens. The greater loss in strength and stiffness in three laminae specimens may be explained by the lower redundancy inherent in three laminae CLT, which results in deleterious effects as any composite action is lost. Ductility for damaged specimens was similar to the undamaged specimens for all CLT panels tested.

2. A dramatic and consistent loss in residual stiffness ($\geq 85\%$) occurred for three laminae CLT even at low levels of impact energy. Similarly, five laminae specimens also
experienced a dramatic loss in stiffness (40% to 90%) at low and high energy levels. In both CLT layups, a greater loss in stiffness was observed as kinetic energy increased. Damage in all specimens resulting from the impulsive loading initiated in the cross laminae. The dramatic and consistent loss in stiffness that occurred in both layups as a result of the impulsive loading paired with the shear cracks that developed in the cross laminae indicate that the development of even slight damage in the cross laminae has severe outcomes on the global stiffness of the CLT panels. It may be concluded that impulsively loaded CLT that has shear as a primary mode of failure, behaves as a brittle structural element that is shear dependent.

3. Residual strength decreased dramatically in three laminae specimen as compared to five laminae specimen. Three laminae specimens show a 70% to 90% loss in strength as compared to 30% to 90% for five laminae specimens.

4. Both three and five laminae specimens retained much of their ductility with residual ductility being 45% to 90% of the ductility of undamaged specimens.

5. High levels of boundary condition rotational rigidity were associated with higher residual strength and stiffness in all CLT specimens tested. Ductility was lower in CLT specimens with higher rotational rigidity. This conclusion provides further evidence to the observation that lower levels of damage occurred in panels with higher levels of rotational rigidity.

8.2 Recommendations for Improvements to Testing Systems and Methods

8.2.1 Improvements to the Impulsive Testing System

The impulsive testing system served to gather data in an unstudied loading regime for CLT. During testing, multiple modifications were noted that would benefit future testing: design iteration of the reaction frame’s cross beam connection, additional calibration tests to calibrate the reaction load cells, improvements to instrumentation.
Design Modification to the Cross Beam Connection: In the reaction frame, the design of the connection of the cross beam to the vertical beams may be reevaluated. Changing the span length in the testing fixture was very beneficial, however, simpler connections would improve the ease with which this operation may be completed. Additionally, while the slotted holes permitted a great deal of adaptability in the span lengths specified, finer tuning of the span length is desirable. A potential design concept that may be explored is the design of a rail mechanism that is seated on the web of the vertical reaction frame beams and allows for sliding of each end of the cross beam. The cross beams may have connections that permit locking of the beam ends once the desired span has been reached.

Calibration of the Reaction Load Cells in the Reaction Frame: Additional testing may be completed to permit calibration of the testing system so that similar reaction and impact impulses are consistently achieved. While achieving similar reaction and impact impulses was not a goal or requirement for this research, future research may require the evaluation of phenomena (e.g., evaluation of the loss of momentum during an impact) that require the impact and reaction impulses to be calibrated in such a manner.

Stiffening of the Pusher Plate: The aluminum pusher plate should be stiffened or a similar plate fabricated with a stiffer material should be used. During impulsive testing, the pusher plate exhibited significant out of plane oscillations (i.e., in the plane normal to the direction of movement) at each change in direction of the impactor. These oscillations were sensed by the impactor load cells and even with the Direct Force Method, the inertia induced by the pusher plate oscillations were not always possible to correct numerically. A stiffer pusher plate would reduce the magnitude of the inertia introduced to the force history data and provide higher fidelity data for Direct Force Method.

Improvements to Instrumentation: Strain gage measurements and improvements to the digital image correlation (DIC) setup would provide additional insight into damage characterization of CLT. Despite the failure of the data acquisition system, which precluded the attainment of strain gage data, strain data at the extreme lamina would be invaluable for
furthering understanding of CLT’s behavior under impulsive loads. The experiment would also benefit from the implementation of improvements to the DIC system used. During testing, intermediate to high test velocities presented significant motion blur in the video footage captured. Additionally, high strain analysis using DIC typically requires a high quantity of images taken at a high rate in order to capture the movement of the subject well and increase accuracy. The impulsively loaded CLT specimens exhibited high strains and displacements at a high rate and the accuracy of the DIC analysis would be improved with the implementation of higher frame rate high speed cameras. Higher frame rate cameras would also mitigate motion blur at the higher velocities. Finally, enterprise level DIC software should be considered as many enterprise level DIC software feature calibration for specific testing environments and hardware and may further benefit the DIC analysis to be conducted.

8.2.2 Improvements to the Residual Testing System

The residual capacity testing system could benefit from two modifications: 1) redesign of the loading assembly, and 2) use of a servo-hydraulic actuator.

Modifications to the Existing Load Assembly: To conduct insitu residual capacity testing using the existing methodology, future residual capacity tests should employ a new design for a rapidly deployable QS loading assembly that features a rigid connection to the strong floor. In testing conducted in this research, rotation of the QS loading assembly was observed and while this rotation was not significant in this research, it can alter test results significantly in other scenarios.

Addition of a Servo-Hydraulic Actuator: A servo-hydraulic actuator is recommended to facilitate digital control over the load rate applied to CLT specimens. CLT properties are sensitive to changes in load rate and consistency in rate is necessary to achieve consistent results.
8.3 Recommendations for Future Research

8.3.1 General Recommendations for CLT Testing

*Custom Fabrication of CLT Specimens:* Custom fabrication of CLT specimens is recommended. As CLT is a composite of timber, a heterogeneous, anisotropic material, and as such, there are many variables that are inherent in its testing. Steiger et al. [129] captured the heterogeneity that is present within a given panel and demonstrated that a great level of heterogeneity may be present in samples cut from a larger CLT panel. If cutting from a larger panel is chosen, careful planning of specimen widths should be considered in order to avoid a high level of variance in mechanical properties between specimens. Custom fabrication would allow for greater control of the specimens to be used in testing. The following list includes several factors that a researcher may control during custom fabrication of CLT: timber species and quality of the timber used to fabricate the CLT, joinery of the lamellas, choice of adhesive and chemical composition, geometry of the CLT layup, environmental control during the CLT’s storage. Custom fabrication will also facilitate the control of moisture content to a greater degree. The ANSI/APA PRG 320-19 requires that CLT specimen have a moisture content of at least 8%, however, testing of specimens with a moisture content of 12-15% will permit closer comparison with published values for timber and CLT mechanical properties. Finally, researchers may consider studying the effect of using alternate CLT layups as discussed in Chapter 2. For example, additional CLT specimens may be fabricated with additional longitudinal laminae. The integration of enhancing materials (e.g., composite fabrics) may also be explored as increasing the shear rigidity and strength of the cross laminae would enhance the overall structural behavior of CLT panels loaded out-of-plane. Note, this list is not an exhaustive list.

*Material Scale Testing of Timber Species:* Quasi-static testing of CLT in this research showed that using conventional design assumptions for stiffness ratios, i.e., \( E_T/E_L, G_{LT}/E_L, G_{RT}/E_L \), and characteristic values for CLT was not sufficiently accurate and led to dra-
tic underestimation of apparent flexural stiffness. Timber is inherently a natural material, and it’s physical and mechanical properties are sensitive to several factors influenced by its growing environment and, later, its use environment, e.g., humidity and temperature. Additionally, timber is a multi-scale material, and its structural scale behavior is highly dependent on timber’s micro-structure. As much as is practical, it is important to have knowledge of the material batch that is being used in the fabrication of CLT specimens. Material scale testing of the timber should be conducted in order to determine the mechanical properties that are specific to the batch and improve the accuracy of analytical predictions of CLT’s mechanical properties.

8.3.2 Recommendations for Future Impulsive Testing

Testing Fixture Designed to Test Dynamic Rolling Shear Directly: A limitation of the testing fixture employed in this research is its reliance on the Shear Analogy Method (SAM) to determine the rolling shear strength. As the SAM is not valid for impulsively loaded CLT, it was not possible to determine the dynamic rolling shear strength of the specimens tested. This research used mechanical properties such as the resistance function as a proximate property in an effort to understand changes in strength in each of the specimens. The development of a test fixture that exclusively studies the rolling shear strength and stiffness would provide insight into any strain rate effects that may be present at high strain rates. The two plate shear test is an example of a testing fixture design that is already employed at the quasi-static loading regime and may be adapted for impulsive loading [80, 94]. Alternatively, the design of the test specimens may be altered and implemented in the existing testing fixture. The implementation of sandwich beams where the core consists of timber lamellas oriented orthogonal to the span direction and the skin layers consist of a stiff material, e.g., aluminum or a fiber composite. The implementation of sandwich beams in this fashion would allow for the isolation of rolling shear strength and stiffness. A similar concept was employed by [169].
**Impulsive Testing with Alternate Loading Conditions:** Testing of CLT panels with different loading conditions (e.g., four point bending, uniform loading, and pure moment) is recommended. Additional evidence is required to support the hypothesis that the occurrence of shear modes of failure in impulsively loaded CLT is influenced by the loading condition.

**Additional Blast Generator Testing:** Testing on rotational rigidity at more impact energies may be conducted to observe relationships that may exist. Next, data on high span-to-depth ratio CLT panels (L:h \( \geq 20 \)) tested a center-point impulsive loading condition may be studied further. The study of high span-to-depth ratio CLT panels at high strain rates and in a laboratory setting would provide insight into damage and failure modes observed and would provide data for comparison to tests conducted in this research. Finally, changes in the dynamic properties of damaged CLT panels may be studied via the application of multiple impulsive loads. Multiple impacts to CLT panels would provide insight into the degradation of CLT mechanical properties with increased damage, evolution of damage, and residual properties of CLT following each impact.

**Dissection of CLT Subjected to Live Blast Loading:** Dissection of CLT subjected to live blast loading is required to assess the extent of internal damage caused. Zukas et al. [125] showed that composite plates subjected to impact loading may exhibit little to no visible external damage, yet may exhibit extensive internal damage, including matrix cracking and delamination. Therefore, it is reasonable to hypothesize that internal damage may occur in CLT that is loaded with live blast loading. Despite the lack of observations of rolling shear damage in CLT subjected to live blasts, dissection is required to verify the conclusion that rolling shear does not occur in CLT under this loading condition.

### 8.3.3 Recommendations for Future Residual Strength Testing

**Determination of the Residual Rolling Shear Strength:** A limitation to finding the residual rolling shear strength is that current analytical methods suited for CLT are only valid when
CLT behaves elastic. A testing fixture that determines the rolling shear strength without the need for analytical methods should be designed and validated for CLT’s plastic regime. A potential testing fixture design could employ a modified two plate shear test adapted for fractured specimens or a testing fixture similar to that developed by Perret et al. [169] and modified for fractured specimens loading. Additional analytical tools will be valuable in studying CLT’s plastic behavior.

*Additional tests at the impact energies tested in this research and at intermediate impact energies are required.* A higher quantity of residual capacity tests are required in order to compensate for the material heterogeneity and variability in CLT fabrication. Additionally, testing at intermediate impact energies, i.e., 2.00 kJ to 4.00 kJ and 4.50 kJ to 6.50 kJ for five laminae CLT. More tests are also required for higher rotational rigidities at similar and intermediate impact energies for both layups.

### 8.3.4 Recommendations for Design

**Connection Design:** The results of the impulsive tests and residual capacity tests indicate that higher levels of rotational rigidity improved the rate of survival and resulted in greater residual mechanical properties in CLT panels. These observations may be used as guidance for designing CLT to be used in force protection applications. Connections that provide rotational rigidity should be implemented in order to improve the performance of impulsively loaded CLT and ensure that there is sufficient residual capacity in the CLT structural elements.

**Joints in CLT:** The development of specifications that provide adequate guidance on detailing in impulsively loaded CLT is required. Currently, specifications and guidance on CLT detailing is based on CLT loaded in the low strain rate loading regime, i.e., static loading. These specifications do not appear to adequately address joint details, e.g., location and strength, for CLT that is impulsively loaded. This research has demonstrated that the global capacity of CLT panels is adversely affected by the location of joints in the...
lamellas located in laminae oriented in the major strength direction. Joints located in areas of high stress can induce premature failures in the laminae oriented in the major strength direction. Therefore, specifications that provide recommendations for the placement of the joints away from areas of high stress are required. Additionally, specifications must provide alternative actions in the event that the relocation of the joints is impractical, for example, by providing methods for reinforcing joints located in areas of high stress.
Appendices
APPENDIX A

TESTING SETUP FABRICATION DRAWINGS
1. Channels one side shown. Second channel (not shown) is opposite hand side, i.e., mirror image.
1. C CHANNELS: ONE SIDE SHOWN. SECOND CHANNEL (NOT SHOWN) IS OPPOSITE HAND SIDE, i.e., MIRROR IMAGE.

2. INSTALL 1/2" DIAMETER BOLTS IN SUP CRITICAL CONDITION. MINIMUM BOLT PRETENSION 12 KIPS.

NOTES:

1. C CHANNELS: ONE SIDE SHOWN. SECOND CHANNEL (NOT SHOWN) IS OPPOSITE HAND SIDE, i.e., MIRROR IMAGE.

2. INSTALL 1/2" DIAMETER BOLTS IN SUP CRITICAL CONDITION. MINIMUM BOLT PRETENSION 12 KIPS.

NOTES:

1. C CHANNELS: ONE SIDE SHOWN. SECOND CHANNEL (NOT SHOWN) IS OPPOSITE HAND SIDE, i.e., MIRROR IMAGE.

2. INSTALL 1/2" DIAMETER BOLTS IN SUP CRITICAL CONDITION. MINIMUM BOLT PRETENSION 12 KIPS.
NOTES:
1. ALL DIMENSIONS ARE IN INCHES.
2. ALL NEW WORK TO BE PERFORMED UNDER THIS WORK ORDER IS HIGHLIGHTED BY REVISION CLOUDS. ALL OTHER WORK IS EXISTING.
3. MATERIAL: ASTM A36 STEEL
4. TOLERANCES:
   1. X ± 0.1
   2. XX ± 0.03
   3. XXX ± 0.01
5. TOTAL WEIGHT IS APPROXIMATELY 1,940 LBS
6. **THRU HOLES** (4) 0.813" THRU HOLES
   - ROW 4: (4) 0.813" THRU HOLES
   - ROW 3: (4) 0.813" THRU HOLES
   - ROW 2: (2) 0.813" THRU HOLES
   - ROW 1: (2) 0.813" THRU HOLES

**RIGHTSIDE**
APPENDIX B

EXPERIMENTAL DATA
Figure B.1: Test CLT3-1 | 9.86 m/s
Figure B.2: Test CLT3-2 | 12.54 m/s

![Graph 1: Force, Impulse, and Time](image1)

![Graph 2: Summed Impact LCs, Summed Impact LCs - Dry Fire, and Net Impact Force](image2)

![Graph 3: Total Reaction Impulse and Net Impact Impulse](image3)

![Graph 4: Resistance](image4)
Figure B.3: Test CLT3-3 | 12.76 m/s
Figure B.4: Test CLT3-4 | 11.22 m/s
Figure B.5: Test CLT3-5 | 10.98 m/s
Figure B.6: Test CLT3-6 | 8.48 m/s
Figure B.7: Test CLT3-7 | 7.45 m/s
Figure B.8: Test CLT3-8 | 7.63 m/s
Figure B.9: Test CLT3-9 | 6.13 m/s

- Summed Lower Reaction LCs
- Summed Upper Reaction LCs
- Lower Reaction Impulse
- Upper Reaction Impulse
- Summed Impact LCs - Impact
- Summed Impact LCs - Dry Fire
- Net Impact Force
- Total Reaction Impulse
- Net Impact Impulse
- Resistance (lbf.)
Figure B.10: Test CLT3-10 | 5.79 m/s

- Summed Lower Reaction LCs
- Summed Upper Reaction LCs
- Lower Reaction Impulse
- Upper Reaction Impulse
- Summed Impact LCs - Impact
- Summed Impact LCs - Dry Fire
- Net Impact Force
- Total Reaction Impulse
- Net Impact Impulse
- Resistance, (lbf.)
Figure B.11: Test CLT3-11 | 5.64 m/s
Figure B.12: Test CLT3-12 | 5.33 m/s

- Summed Lower Reaction LCs
- Summed Upper Reaction LCs
- Lower Reaction Impulse
- Upper Reaction Impulse
- Summed Impact LCs - Impact
- Summed Impact LCs - Dry Fire
- Net Impact Force
- Total Reaction Impulse
- Net Impact Impulse
- Resistance, (lbf.)
Figure B.13: Test CLT3-13 | 7.40 m/s
Figure B.14: Test CLT3-14 | 10.25 m/s
Figure B.15: Test CLT3-15 | 9.84 m/s

![Graphs showing various forces and impulses over time.](image-url)
Figure B.16: Test CLT3-16 | 6.99 m/s
Figure B.17: Test CL3-17 | 6.97 m/s
Figure B.18: Test CLT3-18 | 5.34 m/s
Figure B.19: Test CLT5-1 | 5.15 m/s
Figure B.20: Test CLT5-2 | 6.24 m/s
Figure B.21: Test CLT5-3 | 12.31 m/s
Figure B.22: Test CLT5-4 | 11.32 m/s
Figure B.23: Test CLT5-5 | 9.12 m/s

[Graphs showing time vs. force, impulse, and resistance for different reaction and impact forces and impulses.]
Figure B.24: Test CLT5-6 | 9.04 m/s
Figure B.25: Test CLT5-7 | 9.49 m/s
Figure B.26: Test CLT5-8 | 11.53 m/s
Figure B.27: Test CLT5-9 | 11.14 m/s
Figure B.28: Test CLT5-10 | 11.19 m/s
Figure B.29: Test CLT5-11 | 11.59 m/s
Figure B.30: Test CLT5-12 | 10.90 m/s
Figure B.31: Test CLT5-13 | 11.99 m/s
Figure B.32: Test CLT5-14 | 6.21 m/s
Figure B.33: Test CLT5-15 | 5.91 m/s
Figure B.34: Test CLT5-16 | 5.39 m/s
Figure B.35: Test CLT5-17 | 4.68 m/s
Digital Image Correlation (DIC) was used to analyze the deformation gradients of a representative specimen that was subjected to impulsive loading, CLT5-10. DIC was used to calculate the deformation gradients including the planar displacements and normal and shear strains. The displacements in CLT5-10 were compared to the displacement history determined via motion tracking with ProAnalyst and the hi-speed video footage taken during the test. It was not possible to compare the normal strains to data sensed by the strain gages given a malfunction in the data acquisition system that precluded the saving of strain gage data. The midspan displacement determined via DIC and ProAnalyst were 0.817 in. and 1.144, respectively. This indicates that there is a difference of 28.60%. While some error in the ProAnalyst data is possible, the level of error is expected to be similar to the DIC data given that the same video footage was used, so any errors that may have originated as a result of lens distortion, field of view, and depth of field, for example, would be present in both data sets. Despite the error in the displacement gradient data, the data captured using DIC may still be used as a relative measure for displacement. Additionally, while the values of the strain data determined via DIC has not been validated with an additional instrument, the distribution of strain may be interpreted as the actual strain distribution through the thickness of the CLT panels.

Section 6.4.2.3 describes the evolution of damage in CLT5-10; the net force history is reprinted here (Figure C.1) and the evolution of damage is briefly summarized below. Note, that from the time that contact is made between the impactor and specimen (t = 29.82 ms) until contact begins to soften (t \approx 55 ms), contact is not lost. This observation indicates that any fluctuations that are observed in the impact force history are due to changes in the CLT specimen, i.e., the development of damage.
1. $t = 29.82$ ms: Contact is made between impactor and specimen. Note, felt programmer has completely deformed and the impactor has made contact at this time.

2. $t = 31.23$ ms: Peak load in CLT5-10 is reached and the appearance of the first damage occurs at this time. The damage is characterized by a series of diagonal and longitudinal shear cracks in the cross laminae and along the interlaminar boundaries.

3. $t = 32.51$ ms: A dramatic drop in the impact force is observed. At this time the damage observed at 31.40 ms has propagated and further damage has nucleated.

4. $t = 34.55$ ms: Force rises again. Force is approximately 58.33% lower than the peak force at 31.23 ms. This indicates that a loss of approximately 58.33% in force carrying capacity of CLT5-10 has occurred resulting from the shear cracking that developed. Additionally, the failure of CLT5-10 is primarily due to shear failure.

5. $55 \text{ ms} \leq t \leq 63$ ms: Contact between the impactor and specimen begins to soften at approximately 55 ms and is completely lost at approximately 63 ms.
DIC was used to provide further evidence that shear modes of failure dominated the failure of CLT5-10 during impulsive loading. The strain gradients for CLT5-10 are compared to the damage that develops at 32.51 ms in order demonstrate that flexural modes of failure did not contribute to the dramatic loss in force carrying capacity that occurred at this time. Figures C.2, C.3, and C.4 illustrate the respective gradients for the shear strain ($\epsilon_{xy}$) and the normal strains ($\epsilon_{xx}$ and $\epsilon_{yy}$) at 32.40 ms. Additionally, Figures C.2, C.3, and C.4 also illustrate the damage that has developed in CLT5-10 at 32.51 ms.

Figure C.2: Comparison of Damage (Left) and Shear Strain Gradient (Right) for CLT5-10 at 32.51 ms

Comparing the strain gradients to the damage in CLT5-10 several observations may be made: 1) shear strain concentrations are observed to occur only in the cross laminae where damage has occurred or where shear cracking is observed to propagate (e.g., crack
roots) or nucleate at later times; 2) strain concentrations for the normal strains ($\epsilon_{xx}$ and $\epsilon_{yy}$) are only seen to occur in the cross laminae and at the shear cracks and indicate the horizontal and vertical deformation in the cracks. Given the lack of strain concentrations in the longitudinal laminae, it is possible to conclude that no flexural failure modes were present at 32.51 ms, i.e., the time that a dramatic loss in force carrying capacity in CLT5-10 was observed.

As demonstrated, the DIC strain gradients provide rich data on the development of strains and damage resulting from impulsive loading, which, to this author’s knowledge is the first time that such data has been captured. The DIC data may be used to inform future research on the placement of instrumentation to capture relevant strains and DIC systems used in the future may be calibrated to obtain values for strain that have a higher degree of
Figure C.4: Comparison of Damage (Left) and Vertical Normal Strain, $\epsilon_{yy}$, Gradient (Right) for CLT5-10 at 32.51 ms accuracy.
REFERENCES


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