FRACTURE AND BLENDING OF WOOD COMPOSITES
WITH DISCONTINUOUS ADHESIVE BONDS

by

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ABSTRACT

To minimize resin consumption in the manufacture of oriented strand board, while maintaining the mechanical properties of the finished product, several factors must be optimized. The focus of this work is to answer the following questions: what is the optimum droplet diameter and dot-pitch (distance between droplet centres), and how should the rotary drum blender be operated to achieve this? This work examines the effect of droplet diameter and dot-pitch on the Mode I fracture toughness of a discontinuous adhesive bond and presents preliminary results on the blending dynamics of oriented strand board.

The major conclusions from the work are that the fracture toughness appears to increase linearly with resin droplet diameter and with the square root of bonded area, a finding that is supported by an analytical model. Furthermore, the study shows that the fracture toughness is not strongly dependent on the amount of resin applied. Therefore, by modifying the droplet diameter and dot-pitch it should be possible to maintain the current mechanical properties of the board with reduced resin consumption. Since the bond fails in the substrate, a similar square root relationship between the retention of solid wood fracture toughness by the discontinuous bond and the area of solid wood failure is postulated and confirmed. A minimum ratio of bonded area to total area of 0.3 is suggested, below which the bond does not form. To determine the above results a modified flexographic printing technique was used to deposit resin droplets of known size and spacing. The mass of resin applied was relatively constant, however, the mass applied is affected by resin viscosity, relative humidity and substrate moisture content.

The preliminary work on blending examines the effect of blender rotation speed and drum-wall friction on the tumbling behaviour of strands. Increasing the rotation speed causes the strands to pass through various tumbling regimes, from sliding to centrifuging. Experiments on drum-wall friction show the necessity of flights. Initial models in the area ignore the effect of friction, which limits their usefulness.
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INTRODUCTION

Anticipated North American production of oriented strand board (OSB) for 2002 is 2.2 billion square metres on a 9.5 mm basis (Botting, 2002). One of the major material costs for OSB is the resin, which constitutes 2 to 3.5 wt% of the final board mass. Reducing the resin content 0.1 wt%, from 2.0 to 1.9 wt%, will result in an annual savings of $100,000 to $170,000 for a plant with an annual production of 20 million square metres. However, resin reduction must not reduce mechanical properties. The question becomes: what is the minimum amount of resin required to obtain specified mechanical properties, and how should the resin be distributed within the composite to achieve this? To answer this, a better understanding of resin application is required in two main areas: the effect of resin droplet diameter and dot-pitch (distance between droplet centres) on mechanical properties, and the effect that rotary-drum blender design and operation have on the variability in droplet diameter and droplet spacing. The goal is to understand the mechanics of blending such that the desired droplet diameter and dot-pitch can be achieved in industry.

This work is mainly focused on the effect of droplet diameter and dot-pitch on mechanical properties. The Mode I fracture toughness is examined, as fracture toughness is often used to describe the in-situ mechanical behaviour of adhesive bonds. It is currently not feasible to produce an entire oriented strand board with a specific droplet diameter and dot-pitch. Therefore, a less complicated fracture geometry the double cantilever beam (DCB) was chosen. This is in comparison with an internal bond strength sample, which is the industry standard typically used to quantify the quality of resin distribution. The applicability of linear-elastic fracture mechanics to discontinuous adhesive bonds may be questioned. However, linear-elastic fracture mechanics simply states that load-displacement behaviour of a cracked body can be reduced such that the calculated fracture toughness is constant with crack length. It will be shown that this is the case with discontinuous adhesive bonds in wood.

A literature review on the blending dynamics of oriented strand board and related preliminary experimentation are also presented. These give some understanding in the second area discussed above, and provide insight into the parameters that will affect industries ability to achieve the outlined goal.
The thesis is organized as follows: Chapter 2 presents a literature review on the fracture of solid wood and wood composites. The review on solid wood will provide insight into the behaviour of bonded fracture specimens, since the substrate is solid wood in both cases. The study on wood composites will provide the background and guidance for the present work. The development of the method for applying specific droplet diameters and dot-pitches on wood adherends is described in Chapter 3. The effect of droplet diameter and dot-pitch on fracture toughness and a mathematical model that describes the results based on bond geometry is presented in Chapter 4. Chapter 5 is a literature review on the blending dynamics of oriented strand board and describes preliminary experimental work in the area. Conclusions and future work are detailed in Chapter 6. Three appendices are attached, which contain the design of experiments, Appendix A, the load-CMOD (crack mouth opening displacement) curves for all bonded tests, Appendix B, and the results of all the solid wood tests, Appendix C.
2 - LITERATURE REVIEW: FRACTURE OF SOLID WOOD AND WOOD COMPOSITES

This chapter provides a literature review on fracture of solid wood, from the molecular to the macroscopic level, and the fracture of wood composites, from the wood-adhesive joint level to the macroscopic level.

2.1 FRACTURE OF SOLID WOOD

Fracture of solid wood can be examined on a variety of levels from molecular through cellular to macroscopic. An understanding of the mechanism of fracture at each level will give greater insight into the fracture of solid wood in general. Numerous authors have applied fracture mechanics to solid wood as a means of understanding the failure and predicting the strength of wooden structural members with review articles written by Patton-Mallory and Cramer (1987) and Barrett (1981). The applicability of fracture mechanics is derived from the fact that it is typically assumed that wood behaves as a brittle solid and fails through crack propagation rather than plastic yielding. A brittle material is one that fails without significant plastic deformation. This is valid for solid wood if the moisture content of the sample is sufficiently low. Since wood is a natural material with inherent variability, the associated physical and mechanical properties have coefficients of variation on the order of 20 to 25 percent. This variability leads to a wide range of quoted fracture toughness values even within a given species, thus creating difficulties in using these values for design purposes. In practice, the properties of the fifth percentile are used.

In studying the fracture mechanics of solids, three pure modes of crack propagation are typically examined. As well, mixed-mode fracture, which involves a combination of two to three of the modes, is sometimes studied. The three pure modes of fracture are:

Mode I: Opening or tensile mode, where the crack surfaces move directly apart.

Mode II: Sliding or in-plane shear mode, where the crack surfaces slide across one another in the direction perpendicular to the leading edge of the crack.

Mode III: Tearing or antiplane shear mode, where the crack surfaces move parallel to the leading edge of the crack.

Since wood is a highly anisotropic material these fracture modes are further divided by the three principal axes of anisotropy: radial (R), tangential (T) and longitudinal (L). The plane
and direction of crack propagation are identified by a pair of letters. The first denotes the direction normal to the crack surface and the second the direction of crack growth with a total of six possible crack propagation directions. The fracture of wood can be simplified if the specimen is taken a sufficient distance from the tree centre such that the curvature of the growth rings can be ignored. The wood then behaves as an orthotropic material.

2.1.1 Molecular Scale

At the molecular level, wood consists of cellulose and lignin and other organic molecules such as hemicelluloses and uronic acids. Cellulose is a semi-crystalline polysaccharide based on 1,4 linked β-D-glucose molecules (Figure 2.1a). The molecular weight (both number-average and weight-average) and degree of polymerization (DP) of cellulose are difficult to obtain, however evidence suggest that in higher plants the DP is 3500 to 7000 or higher based on the cellobiose repeat unit (Richmond, 1991). Higher plants are cellulose-producing organisms that have conducting tissues. This includes plants that flower or produce cones, ferns and others that are classified in the kingdom Plantae. The cellulose chains coalesce to form ordered, crystalline microfibrils that have high strength and stiffness in the longitudinal direction.

Lignin is considered an amorphous polymer that is based on the monomers shown in Figure 2.1b, c and d. Due to the difficulty in isolating lignin from wood without degradation, the actual molecular weight of lignin is unknown. However, the weight average molecular weight $M_w$, of softwood milled wood lignin is estimated at 20,000 with lower values reported for hardwoods. The polydispersity (weight average molecular weight / number average molecular weight, $M_w / M_n$) of lignin is also high, 2.5, compared with cellulose. A high polydispersity means that the sample is a mixture of long and short molecules. The number-average molecular weight and weight-average molecular weight are defined as:

$$\overline{M}_n = \frac{\sum N_i M_i}{\sum N_i}$$  \hspace{1cm} (2.1) \\
$$\overline{M}_w = \frac{\sum N_i M_i^2}{\sum N_i M_i}$$  \hspace{1cm} (2.2)

respectively where $N_i$ is the number of molecules of molecular weight $M_i$ in a polymer samples. These definitions arise because almost all polymers have a distribution of molecular weights.
(Sperling, 1992). The lower molecular weight and greater polydispersity of lignin lead to a lower fracture toughness than for cellulose.

### 2.1.2 Cellular Scale

At the cellular level, wood can be considered a fibre reinforced polymer composite of cellulose microfibrils in a lignin matrix. The cell wall consists of four discrete layers (Nikitin, 1966) in which the orientation of the cellulose microfibrils varies from 0° to 90° to the longitudinal axis of the fibre. The primary wall, P, is an irregular network of microfibrils with a lignin content of less than 50 percent. As well, the cellulose chains within the primary wall microfibrils have a low DP, approximately 250, as compared with a DP of 3500 to 7000 for the cellulose found in the secondary wall. The microfibrils of the outer secondary wall, S₁, form two overlapping spirals resulting in a structure similar to a ±45 layer in a polymer composite. The middle secondary wall, S₂, forms the majority of the cell wall and consists of concentric layers of microfibrils oriented almost parallel to the fibre axis. Finally, the inner secondary wall, S₃, consists of microfibrils that lie nearly perpendicular to the fibre orientation. In total, the secondary wall of the cell contains 10 to 12 percent lignin. The cells are held together in a honeycomb fashion by the middle lamella, which consists of approximately 70 percent lignin. The distribution of the lignin within the middle lamella is isotropic, i.e. there is no long-range order. The organization and microfibril orientation of the cell wall and middle lamella are shown in Figure 2.2.

Since the majority of the cell wall material is found in the S₂ layer, the strength of wood is greater in the longitudinal direction compared with the radial and tangential directions. As well, the secondary wall consists of approximately 80 percent of the total cellulose and it has greater fracture toughness than the primary wall and middle lamella.

Moving towards the macroscopic scale, solid wood can be viewed as a honeycomb network of cells cemented together by the middle lamella. This leads to the two major fracture paths found in wood (Gibson and Ashby, 1988; Boatright and Garrett, 1983), cell fracture and cell separation (DeBaise, 1972). The first is through cell fracture or intracellular fracture, where the cell wall itself fractures, as shown in Figure 2.3a. The type of fracture that occurs is determined to a large extent by the wood density. For convenience, the density of a wood is often given as the ratio of wood density, ρ, to that of the cell wall material, ρₛ, which is typically
1500 kg/m$^3$. Cell fracture is generally found in low-density ($\rho / \rho_s < 0.2$) woods and the earlywood of higher density woods. Since the density of the cell wall material is constant at 1500 kg/m$^3$ any change in wood density is due to the relative proportion of cell wall material to voids. Higher density woods will have relatively greater amounts of cell wall material, i.e. thicker cell walls and thus a greater amount of cellulose increasing the difficulty with which cell fracture occurs. This causes the shift in fracture type from cell fracture to cell separation described below. Cell fracture is the typical failure mode in the RT, RL, LR and LT directions.

Cell separation or intercellular fracture is shown in Figure 2.3b. During cell separation the crack propagates through the middle lamella and primary cell wall leaving the secondary wall intact. This occurs in higher density woods ($\rho / \rho_s > 0.2$) along with some cell fracture. Gibson and Ashby (1988) found that propagation in the TR direction is through cell separation. In addition, a crack running at an angle between the RT and TR directions will tend to deviate toward the TR direction adopting the cell separation mode of fracture.

Cell fracture has higher fracture toughness than cell separation. This is shown by the greater fracture toughness of the RT, RL, LR and LT directions over the TR and TL directions and the preference of the TR direction for a crack running at an angle between the RT and TR directions. The difference can be explained by the relative proportions of cellulose and lignin as well as the type of cellulose found in the various cellular layers. Since cellulose has higher fracture toughness than lignin and is predominantly found in the secondary cell wall it is reasonable to assume that cell fracture, which occurs through fracture of the secondary cell wall, will have greater fracture toughness than cell separation. As well, the cellulose in the primary cell wall has a significantly lower DP as compared with the cellulose in the secondary cell wall and exhibits lower fracture toughness.

Vessels affect the fracture path by acting as crack arrestors as shown in Figure 2.3c. The crack path tends to deviate towards a vessel and either enter it or run partly around the edge of the channel and stop (Gibson and Ashby, 1988). These are examples of extrinsic toughening mechanisms: crack-tip blunting and crack deflection respectively.

With increasing temperature and moisture content, more viscoelastic deformation occurs and there is a shift from cell fracture to cell separation. As well, this reduces the extent and
frequency of unstable crack extension that typically occurs in the case of cell fracture as compared with slow crack growth that occurs in the case of cell separation (DeBaise, 1972).

2.1.3 Growth Ring Scale

During the year, cells are added to the tree in two distinct layers. Earlywood, grown during the spring consists of large diameter, thin-walled cells. Latewood, deposited later in the growing season are smaller diameter cells with thicker cell walls. The repeated layering of the early and latewood produces the pattern known as growth rings. The difference in cellular structure of the earlywood and latewood lead to variations in density and stiffness which further explain the order of fracture toughness for the various directions. Thuvander et al. (2000a, 2000b; Thuvander and Berglund, 2000) demonstrate the increased fracture toughness of the TR direction over the TL direction. The crack propagates in the cell separation mode at the level of individual cells and a stick-slip mechanism of crack growth was found at the growth ring scale due to the alternating stiff (latewood) and flexible (earlywood) layers. As well, crack propagation typically occurs through the formation of secondary cracks that are displaced tangentially from the primary crack where the latewood acts as a crack arrestor, shown schematically in Figure 2.4. Further inactive secondary cracks are also found well away from the primary crack. Since energy is required to form the new surface area of the inactive secondary cracks, they contribute to the fracture toughness of wood.

The difference in stiffness between early and latewood also causes inclined cracks to deviate to the TR direction (Thuvander and Berglund, 2000). Inclined cracks propagated by both cell fracture and cell separation with the latter mechanism being dominant. Upon reaching the latewood, the crack is arrested and a secondary crack is initiated in the radial direction at a weak point in the latewood. As above, this secondary crack is displaced tangentially from the original, i.e. horizontally in Figure 2.4.

2.1.4 Macroscopic Scale

While the knowledge gained in examining the fracture of wood at the molecular and cellular level is important, the majority of applications are dependent on the behaviour of the macroscopic scale. Thus, the majority of researchers concentrate on this level. However, many of the effects seen at the macroscopic scale can be explained by phenomena at the molecular and cellular level. The literature on the macroscopic fracture toughness of wood can be divided into
the three principle modes of crack propagation: Mode I, Mode II and Mode III as well as mixed-mode fracture and studies on fracture strength.

2.1.4.1 Mode I Fracture Toughness

Mode I, or the opening mode of fracture, is considered the most important mode in engineering failures. However, wood often exhibits a significant component of Mode II fracture. The Mode I fracture toughness is the most easily measured of the three modes, thus the majority of the literature concentrates on this property. A summary of the work to date on the fracture of solid wood is listed in Table 2.1 by fracture specimen geometry, direction of crack growth, species and effect examined.

The possible directions of a Mode I fracture toughness test can be grouped into those where the crack is propagating perpendicular to the grain (LR and LT) and those parallel to the grain (RL, TL, RT, TR). The Mode I fracture toughness of wood perpendicular to the grain is approximately one order of magnitude greater than the fracture toughness in the other directions as shown in Table 2.2. This is due to the crack propagating through cell fracture rather than cell separation. Cell fracture also leads to the slightly higher value of fracture toughness for the RL direction over the TL direction. It should be noted that cracks tend to propagate parallel to the grain even if the starter crack is originally in the LR or LT direction.

A variety of fracture toughness specimens have been used to characterize the Mode I fracture toughness of wood. Wood properties are inherently variable and no major difference in Mode I fracture toughness due to specimen geometry has been reported. The ASTM Standard E399-90: Standard Test Methods for Plane-Strain Fracture Toughness of Metallic Materials (1994) is typically applied to wood. However, Bostrom (1990) recommends against using the CT specimen for measuring the Mode I fracture toughness of solid wood due to difficulties in determining when the crack begins propagating.

The Mode I fracture toughness of solid wood is affected by strain rate, moisture content, density, defects and thickness. The effect of strain rate has been examined by various authors (Blicblau and Cook, 1986; Ewing and Williams, 1979a; Johnson, 1973; Mai, 1975; Mindess et al., 1975a, 1975b; Nadeau et al., 1982; Schniewind and Pozniak, 1971); with the exception of
Mai (1975), they concluded that the fracture toughness of wood increases with strain rate. This is due to subcritical crack growth that causes delayed failure of wood (also known as the duration-of-load effect where the fracture toughness of wood specimens decreases as a function of time). At lower strain rates, there is sufficient time for cracks to reach the critical size at which point the test specimen fails catastrophically. At higher strain rates, less time is available for crack growth and the fracture toughness of these samples is higher than for specimens strained more slowly.

Studies on moisture content have examined wood from oven-dry to green, (Ewing and Williams, 1979b; Johnson, 1973; Kretschmann et al., 1990; Mai, 1975; Mindess, 1977; Petterson and Bodig, 1983; Sobue et al., 1985) although the moisture content is typically between 10 to 15 percent (Boatright and Garrett, 1979; Ewing and Williams, 1979a; Schniewind and Centeno, 1973; Triboulot et al., 1984). Ewing and Williams (1979a) found that the fracture toughness of wood initially increases from an oven-dry condition up to 6 to 8 percent, and then decreases monotonically thereafter. This increase in Mode I fracture toughness with moisture content is due to increased viscoelastic behaviour of the wood (Mindess, 1977), leading to greater energy absorption through viscous deformation and an increased toughness. However, above the level of 6 to 8 percent it appears the Mode I fracture toughness becomes constant or decreases slightly (Kretschmann et al., 1990). The slight decrease in Mode I fracture toughness at approximately 8 percent may be due to the ingress of water into the crystal structure of the cellulose microfibrils. This leads to a reduction in crystallinity and a subsequent reduction in fracture toughness (Nikitin, 1966). For higher moisture content values, the validity of linear elastic fracture mechanics becomes questionable due to increased plasticity during crack propagation. Evidence of plastic work during the fracture event was observed by Atack et al. (1961).

The reduction of Mode I fracture toughness at low moisture contents is due to the difference in shrinkage rate of the radial, tangential and longitudinal directions. This leads to residual drying stresses (Ewing and Williams, 1979b; Sobue et al., 1985) that produce radial cracks and a subsequent reduction in fracture toughness. Schniewind and Centeno (1973) varied the relative humidity between 35 and 87 percent with the humidity held at each value for 12 hours and showed that cyclic changes in humidity can significantly decrease the time to failure of solid wood.
The fracture toughness of wood increases with increasing density (Ashby et al., 1985; Gibson and Ashby, 1988; Kretschmann et al., 1990; Leicester, 1974a, 1985; Petterson and Bodig, 1983), as shown in Figure 2.5. It should also be noted that this is true for green wood (Petterson and Bodig, 1983) where the validity of fracture mechanics is questionable. The following relationships between density and $K_{lc}$ have been postulated:

$$K_{lc}^n = 20 \left( \frac{\rho}{\rho_s} \right)^{3/2} \text{ (MPa} \cdot \text{m}^{1/2}) \quad \text{Ashby et al. (1985), (2.3)}$$

$$K_{lc}^a = 1.81 \left( \frac{\rho}{\rho_s} \right)^{3/2} \text{ (MPa} \cdot \text{m}^{1/2}) \quad \text{Ashby et al. (1985), (2.4)}$$

$$K_{lc} = 0.15 \rho \text{ (N} \cdot \text{mm}^{-3/2}) \quad \text{Leicester (1985), (2.5)}$$

$$K_{lc}^g = 0.02 \rho \text{ (N} \cdot \text{mm}^{-3/2}) \quad \text{Leicester (1985), (2.6)}$$

$$K_{lcg} = 0.5657 (\rho)^{0.952} \text{ (psi} \cdot \text{in}^{1/2}) \quad \text{Petterson and Bodig (1983), (2.7)}$$

where $K_{lc}^n$ is the Mode I fracture toughness normal to the grain (LR, LT direction), $K_{lc}^a$, the Mode I fracture toughness along the grain (RL, TL, RT, TR direction), $K_{lcg}$, the Mode I fracture toughness along the grain for green wood, $\rho$, the wood density (kg/m$^3$), and $\rho_s$, the density of cell wall material (1500 kg/m$^3$).

The model put forth by Leicester underestimates the effect of density on Mode I fracture toughness normal to the grain, whereas the model by Ashby agrees better with experimental data. However, the Ashby, Leicester, and Petterson and Bodig models fit well for fracture toughness along the grain. The increase in fracture toughness with density is due to the increase in the amount of wood as compared to voids in a given cross-section and is comparable with the effect of latewood on the crack path as reported by Thuvander and Berglund (2000).

Ewing and Williams (1979b), Wright and Fonselius (1986) and Triboulot et al. (1984) examined the effect of thickness on the Mode I fracture toughness. Ewing and Williams demonstrate the effect of moving from the plane stress regime to the plane strain regime as the fracture toughness decreases to a plateau when the thickness is increased. Wright and Fonselius found that for Pine, Spruce, and Spruce laminated veneer lumber (LVL), a specimen thickness of
20 mm was sufficient to achieve plane strain conditions. Triboulot et al. (1984) also show that the necessary thickness for plane strain is on the order of 20 mm where 65 percent of the specimen is in a plane strain condition. This conclusion was based on experiments where the strain of the specimen was measured across the thickness near the crack-tip during loading.

The defects studied include notches (Ewing and Williams, 1979b; Mindess, 1977; Schniewind and Pozniak, 1971), checks (Schniewind and Lyon, 1973; Schniewind and Pozniak, 1971), and knots (Boatright and Garrett, 1979; Pearson, 1974). Ewing (1979b) states that the effect of changing notch depth (5 to 15 mm) and notch radius (from less than 5 to 300 μm) was inconclusive in the ranges studied. Schniewind and Pozniak (1971) found that Douglas Fir specimens with a notch length of either 3.6 to 4.1 mm (0.14" or 0.16") failed away from the pre-existing notch tip. This confirms the existence of inherent flaws in Douglas Fir on the order of 3.8 mm (0.15"). Checks were found to reduce the fracture strength and toughness of solid wood to a greater degree than other defects when the wood is tested perpendicular to the grain (Schniewind and Lyon, 1973). Checks are radial cracks formed due to differences in radial and longitudinal shrinkage upon drying and terminate in a sharp crack tip, whereas other defects studied such as resin streaks, pitch pockets and pith are blunt. This difference in crack tip radius causes the greater reduction in fracture toughness by checks over other wood defects.

Knots in wood significantly disturb the regular grain structure of wood, which in turn leads to changes in the fracture toughness. The effect, however, is unclear since the knot may increase $K_c$ in some cases and decrease it in others. Boatright and Garrett (1979) found that the knot ratio, which is the ratio of the amount of specimen perimeter that is covered by knots to the entire specimen perimeter as shown in Equation 2.8 and Figure 2.6, was the best indicator of the effect of knots on the fracture toughness of solid wood. The knot ratio, $KR$, depends on the position of the knot within the board and takes into account the increased effect of edge knots over central knots as well as its size. The strength of the specimen decreases with increasing knot ratio.

$$KR = \frac{\sum (x_i + y_i)}{2(w + t)},$$

(2.8)

where $x_i$ is the length of the knot exposed to the surface of the board, $y_i$, the length of the vertical axis of the knot, $w$, the width of the board, and $t$, the thickness of the board.
Pearson (1974) modelled knots with equivalent cracks and found that if the crack length was taken to be the same as the knot dimensions, the fracture toughness was underestimated. This suggests that a knot cannot be modelled by a crack. Fracture mechanics confirms this as the maximum stress at the tip of an elliptical crack in an isotropic, elastic, infinite body in a plane stress condition is given by:

\[
\frac{\sigma_{\text{max}}}{\sigma_a} = 1 + \frac{2a}{b},
\]

where \(\sigma_{\text{max}}\) is the maximum applied stress at the end of the major axis, \(\sigma_a\) the applied stress applied normal to the major axis, \(a\) the half major axis, and \(b\) the half minor axis, and shows that as the minor axis is reduced, i.e. the crack tip becomes sharper, the maximum stress at the end of the major axis increases. Since knots typically have a larger minor axis than a crack, an equivalent crack will underestimate the fracture toughness of a knot.

### 2.1.4.2 Mode II Fracture Toughness

While Mode I fracture is typically considered the driving force behind engineering failures in most materials, experience has shown that wood failure often has a significant Mode II component. Table 2.3 summarizes the literature on Mode II fracture toughness for the specimen geometries, directions, species and effects that have been examined. The orthotropic directions that have been studied are RL and TL. No literature on the other directions has been found; likely due to the difficulty of obtaining specimens of significant length where the length is not parallel to the longitudinal direction.

The majority of the researchers (Barrett and Foschi, 1977a, 1977b; Cramer and Pugel, 1987; Murphy, 1989) have found that it is difficult to obtain values for pure Mode II failure due to the low Mode I fracture toughness parallel to the grain. Very little stress is required to precipitate failure in this direction and therefore, Mode I failure may occur away from the desired location (Cramer and Pugel, 1987) or the specimen may fail earlier than predicted (Barrett and Foschi, 1977a) for pure Mode II loading. As well, closing friction and minor changes in specimen geometry may affect the calculated values for \(K_{\text{II}}\) and \(K_{\text{IIc}}\) leading to greater variability in these values compared with \(K_f\) and \(K_{fc}\). However, the absolute values for \(K_{\text{IIc}}\) are
consistently greater than $K_{lc}$ for the corresponding direction. For example, Douglas Fir in the TL direction has a $K_{lc}$ of 0.36 MPa·m$^{1/2}$ (Schniewind and Centeno, 1973) and a $K_{IIc}$ of 1.56 MPa·m$^{1/2}$ (Cramer and Pugel, 1987).

Varying the moisture content, from 10 to 20 percent (Fonselius and Riipola, 1988) appears to have little effect on Mode II fracture toughness, which agrees with the results for Mode I fracture toughness, although the range studied was small. Cramer and Pugel (1987) found that a harsher drying history, which produces more and larger flaws within the sample, led to a decrease in $K_{IIc}$.

A similar relationship to that for Mode I exists between density and Mode II fracture toughness, as $K_{IIc}$ also increases with increasing density (Fonselius and Riipola, 1988; Leicester, 1974a, 1985; Murphy, 1989). This similarity between Modes I and II is also seen for varying crack lengths. An increase in crack length leads to an increase in the Mode II fracture toughness for end-notched flexure and centre-slit beam specimens (Barrett and Foschi, 1977a, 1977b; Murphy, 1979a, 1989).

2.1.4.3 Mixed Mode Fracture Toughness

Since pure Mode I or pure Mode II are rarely encountered in practice, a number of researchers have examined mixed mode fracture. Studies in mixed mode failure have given empirical mixed-mode failure criteria, based on knowledge of pure Mode I and II values, for use in design. The literature on mixed mode fracture toughness is summarized in Table 2.4.

The following empirical failure criterion (Equations 2.10 to 2.15) have been proposed for the onset of mixed-mode fracture:

$$\frac{K_I}{K_{lc}} = 1 \quad (K_{II} \geq 0) \quad \text{Mall et al. (1983),} \quad (2.10)$$

$$\frac{K_I}{K_{lc}} + \frac{K_{II}}{K_{IIc}} = 1 \quad \text{Leicester (1974b),} \quad (2.11)$$
EL + a = 1

Hunt and Croager (1982), where \( a = 1.005 \) and \( \beta = 3.4 \),

\[ \frac{K_I}{K_{lc}} + \alpha \left( \frac{K_{II}}{K_{IIc}} \right)^\beta = 1 \]

(2.12)

Leicester (1985) and Wu (1967),

\[ \frac{K_I}{K_{lc}} + \left( \frac{K_{II}}{K_{IIc}} \right)^2 = 1 \]

Mall et al. (1983), and

\[ \frac{K_I}{K_{lc}}^2 + \left( \frac{K_{II}}{K_{IIc}} \right)^2 = 1 \]

Mall et al. (1983).

The criteria which best predicts the mixed-mode failure is that postulated by Wu (1967), who studied Balsa and confirmed by Mall et al. (1983) who examined Spruce. The fact that a single mixed-mode failure criteria adequately describes the behaviour of Balsa and Spruce may be coincidental, however, this failure criteria may describe the behaviour of a wide variety of species. Further work is necessary to determine this.

Lum and Foschi (1988) have shown that for a rectangular end-notched flexure (rENF) specimen the Mode I stress intensity factor increases with notch depth and notch length. It should be noted that this is not pure Mode I loading and therefore, must be categorized as mixed-mode.

2.1.4.4 Mode III Fracture Toughness

The final mode of fracture is Mode III: the tearing or antiplane shear mode. This has been examined by Murphy (1980) using a side cracked cantilever beam of Sitka Spruce in the RT direction obtaining a \( K_{IIIc} \) of 0.66 MPa·m\(^{1/2}\).

This is comparable to Mode I or Mode II failure in the RL direction. However, any comparison must be tempered by the difference in species between Mode I and Mode III and therefore, must be made with caution. For Mode II, the fracture toughness for Spruce in the RL direction is approximately twice that of Mode III in the RT direction. Murphy found that for crack ratios (crack length / specimen width) below 0.15 the beam behaves as if it was composed
of clear wood, i.e. wood with no defects such as knots. This is further evidence that the intrinsic flaw size in wood is on the order of 3.8 mm (0.15"). The shear span also affects the Mode III fracture toughness. As the shear span (length vs. width) and hence the crack width increases, $K_{IIIc}$ is reduced.

2.1.4.5 Fracture, Strength

Experiments on the fracture strength or failure load of wood, summarized in Table 2.5 demonstrate the applicability of fracture mechanics to the study of wood failure. Fracture mechanics predicts both the effect of defects such as notches and knots as well as the effect of moisture content. While the majority of researchers do not state the orthotropic direction studied it is presumably either the RL or TL direction. As with Mode II fracture toughness, it is difficult to obtain sufficiently large specimens where the length is not parallel to the longitudinal direction.

Several researchers (Leicester, 1973; Leicester and Poynter, 1979; Murphy, 1979b; Stieda, 1966) have studied the effect of notches on failure load and modulus of rupture (MOR). Leicester (Leicester, 1973; Leicester and Poynter, 1979) found that as the sharpness of the notch tip increases the MOR and failure load of the specimen is reduced. This agreed with the findings of Stieda (1966) who also reported a similar effect for notch depth, which is an equivalent concept to crack length.

As with Mode I fracture, knots affect the failure load. Using finite element analysis, Cramer and Goodman (1983) shows that edge knots lead to a greater stress concentration, defined as $\sigma_{\text{max}}/\sigma_u$, than centre knots. This confirms the effect found by Pearson (1974) where the fracture strength of edge knots, modelled by an equivalent crack, was between 30 to 62 percent of the fracture strength for a similarly sized centre knot.

Stieda (1966) examined the effect of moisture content on the failure mechanism. As the moisture content is increased to fibre saturation, the material behaviour shifts from brittle to tough. This is similar to results for Mode I and Mode II fracture. Initial failure at the notch for dry material typically resulted in complete failure of the beam, as is the case for brittle materials. In contrast, initial failure in green material resulted in a small load drop followed by a
subsequent increase in load, as the green specimens failed slowly by shear at the notch tip rather than sudden cross-grain failure as is the case for dry beams.

The effect of strain rate was shown by Spencer (1979) who found that bending strength increased with strain rate. The effect was more pronounced for samples with a greater initial strength, which likely have a smaller intrinsic flaw size. The mechanism for the increased bending strength at higher strain rates is likely the same as that for Mode I fracture toughness.

2.1.5 Summary

A review of the fracture literature on solid wood finds that to fully understand the fracture behaviour of solid wood it should be studied in all modes – Mode I, Mode II and Mode III as well as mixed-modes and in all directions – RL, TL, RT, TR, LR and LT. It should also be examined at all scales – molecular, cellular, growth ring and macroscopic. Each level will provide insight into the behaviour of the next level with the end result being that the macroscopic fracture behaviour of wood can be related to the chemical composition and micro/meso-structure.

Thus the major conclusions found on the macroscopic fracture of solid wood can be related to the behaviour of wood at the lower levels. These major conclusions are that the fracture toughness perpendicular to the grain is approximately one order of magnitude greater than that parallel to the grain. Fracture perpendicular to the grain occurs by destruction of the cellulose microfibrils rather than simple cleavage leading to increased fracture toughness. Increasing fracture toughness with increasing density can also be related to the cellular level, as the crack must pass through a greater amount of cellulose, which has a high fracture toughness, to propagate. Finally, fracture toughness increases with moisture content to a maximum at 6 to 8 percent, decreasing thereafter. This is due to a shift in fracture behaviour from brittle to ductile. At higher moisture contents the crystal structure of the microfibrils is disturbed by the increased water and hence, fracture toughness is reduced.

Other conclusions are based on an understanding of fracture mechanics. Mode II fracture toughness is greater than Mode I for a given species as is the case with the majority of materials. Fracture toughness increases with increasing strain rate since the internal flaws do not have sufficient time to reach the critical length. Checks and other defects reduce the strength and
fracture toughness of a specimen as they introduce flaws greater than those inherently found in
the material. However, in the case of knots there is the possibility of an increase in fracture
toughness. Also, as with most materials, edge defects have a greater effect than centre defects.

2.2 FRACTURE OF WOOD COMPOSITES

Over the past decades the use of wood composites has increased. While the literature on
fracture of solid wood is extensive, limited research has been undertaken on fracture of wood
composites. The fracture behaviour of solid wood is important when studying wood composites
as wood composites are identical to solid wood up to the cellular level, with the only difference
being that at larger scales, the wood-adhesive bonds add complexity to the wood composite not
present in solid wood.

Wood composites can be considered an amalgamation of wood-adhesive joints. As one
moves to larger and larger furnish, from particles to strands to veneer, only the length to
thickness ratio of the furnish changes, but the composite remains a series of adhesive joints.
Therefore, an examination of the structural adhesive joint literature, which is related to the
furnish-resin (meso) scale of wood composites, will allow greater understanding of the behaviour
of wood composites at the macroscopic scale.

The literature can be divided into wood-adhesive research and wood-composite research
with the majority of studies concentrating on either the geometry of the joint or composite,
effects associated with the adherend, or effects associated with the adhesive. Table 2.6 lists the
wood-adhesive joint fracture toughness geometries, adhesives, species and effects examined in
the literature. Table 2.7 lists the relevant literature for wood composites. The resin system used
in wood composites is typically phenol-formaldehyde (Barnes, 2000, 2001) but isocyanate resins
have also been studied (Furuno et al., 1983; Kamke et al., 1996a; Lei and Wilson, 1979, 1980;
Youngquist et al., 1987).
2.2.1 Specimen Geometry

The strength and fracture toughness of wood composites can be more easily interpreted by drawing on the large body of research on structural adhesive joint behaviour. Examples of common structural adhesive joints are shown in Figure 2.7. Geometric parameters examined in the literature include the effect of lamination or veneer thickness and lap length.

An examination of the adhesive lap and strap joint literature reveals that a reduction in lamination thickness reduces both the shear and peel stresses at the end of the bonded overlap (Tong et al., 1999). Lamination thickness is defined as the thickness of the adherend in an adhesive joint, e.g. the thickness of each individual ply in a plywood board. For wood-adhesive joints it has similarly been found that a decrease in lamination thickness leads to an increase in fracture toughness and failure stress (Jung and Murphy, 1983; Komatsu et al., 1976; Leicester, 1973; Leicester and Bunker, 1969; River et al., 1989; Scott et al., 1992; Walsh et al., 1973). The effect of lamination thickness on failure stress is shown in Figure 2.8, with sufficiently thin laminations having fracture loads similar to that of defect free wood.

At the macroscopic scale similar observations are made. Lei and Wilson (1979) show that there is a reduction in fracture toughness with increasing veneer thickness, a result also corroborated by Jung and Murphy (1983). Lei and Wilson also found that for LVL constructed with very thin veneers, 0.8 mm (1/32"), the directionality of solid wood disappears for the RL, TL, TR or RT crack directions and therefore, crack direction has no effect on the fracture toughness. This finding should be applicable to other wood composites since the thickness of that veneer is similar to that of OSB strands. Thus, the fracture toughness of OSB should not be affected by the orthotropic nature of solid wood.

Of the two remaining dimensions for adhesive joints, width and length, the one typically studied is lap length. The results of Leicester (1974b), Komatsu et al. (1976) and Walsh et al. (1973) indicate that the failure load of lap and strap joints increases with lap length. However, as shown in Figure 2.9, increasing the overlap length leads to diminishing returns and the normalized failure load plateaus above a characteristic length, for a given system. The plot of normalized failure load, $P\alpha/2Y$, versus normalized lap length, $0.5\alpha L$, in Figure 2.9 allows a dimensionless comparison of load and lap length.
Komatsu (1984) examined Mode III fracture toughness by bonding lap shear samples at varying angles and loading the samples in compression. The ratio of Mode II to Mode III is a function of the angle between the adherends with a significant Mode II component at large angles (pure Mode II at 180°). The $G_{IIIc}$ value decreased as the angle increased from 90° to 180°. The fracture pattern also changed with lap angle. At low lap angles (90° and 120°), the fracture was simple and brittle, occurring through fracture in rolling shear at the glue lines. At angles of 150° and above, the fracture was more complex with some samples failing within the wooden members themselves.

For modelling purposes, it is often easier to describe the properties of wood composites in terms of the ratio of furnish length to thickness. Furnish is the collective term used to describe the wood component of wood composites, e.g. strands in oriented strand board. The ratio of furnish length to thickness is referred to as the slenderness ratio. Barnes (2001), Equations 2.16 and 2.17, and Simpson (1977), Equations 2.18 to 2.20, developed models based on the inherent properties of wood and the slenderness ratio. Barnes uses a modified Hankinson equation whereas Simpson's model accounts for the shear strength of the adhesive used. (It should be noted that the symbols for the variables within these and subsequent equations have been changed for ease of comparison.)

**Barnes Model:**

$$\sigma_R = \frac{\sigma_\parallel \times \sigma_\perp}{\sigma_\parallel \sin^n(\arctan(2t_b/l)) + \sigma_\perp \cos^n(\arctan(2t_b/l))},$$

$$t_b = t_o \times \left(\frac{\rho_o}{\rho_b}\right),$$

where $\sigma_R$ is the resultant structural property, modulus of elasticity (MOE), or modulus of rupture (MOR), $\sigma_\parallel$, the strength parallel to the grain, $\sigma_\perp$, the strength perpendicular to the grain, $n$, the experimentally determined coefficient (0.9 to 1.5), $l$, the length of strand, $t_o$, the initial strand thickness, $t_b$, the in situ strand thickness, $\rho_o$, the initial wood density, and $\rho_b$, the product wood density.
Simpson Model:
\[ \sigma_R = \frac{\sigma_w (r + k)}{r + ku}, \quad (2.18) \]
\[ r = \frac{l}{t}, \quad (2.19) \]
\[ u = \frac{\sigma_w}{\tau}, \quad (2.20) \]

where \( \sigma_R \), is the tensile strength of the OSB, \( \sigma_w \), the tensile strength of the strand in the direction of orientation, \( r \), the shear strength of the adhesive bond between flakes, \( r \), the slenderness ratio, \( l \), the flake length, \( t \), the flake thickness, and \( k \), the proportionality constant relating the forces for tensile and shear failure of the flakes to the number of strands that fail by each method (assumed to be 1).

Both models predict that at higher slenderness ratios the structural properties: modulus of elasticity (MOE) and modulus of rupture (MOR) in the Barnes model, and tensile strength in the Simpson model, increase. Increasing the slenderness ratio of the furnish is analogous to moving from OSB to Parallam™. This supports the concept of a wood composite being an amalgamation of wood-adhesive joints, as the trends of increasing failure load and stiffness with decreasing thickness and increasing length are also seen at the wood-adhesive scale. However, the Simpson model has only been compared qualitatively to data in the literature as the author states that it would be difficult to test quantitatively.

As has been shown, models based on wood-adhesive joint geometry have been used to predict the strength of wood composites. Models based on fracture mechanics and intrinsic flaws have also been proposed. Ilcewicz and Wilson (1981) studied the fracture mechanics of particleboard using a nonlocal theory. Unlike theories based on continuum mechanics, which only considers the behaviour at a point (locally), nonlocal theories take into account the behaviour of a region when predicting failure. The results from Ilcewicz and Wilson (1981) show that the fracture toughness of particleboard can be predicted by Equations 2.21 and 2.22, which includes the intrinsic strength, intrinsic flaw size and a characteristic dimension of the material. For the specific example modelled by Ilcewicz and Wilson, particleboards with a
specific gravity of 0.70, this characteristic dimension is equal to the particle thickness. Increased
particle thickness at this specific gravity gives increased fracture toughness.

\[ K_{ic} = \sigma_{IB} \sqrt{a_o} Y(a_o/W), \]  
\[ \sigma_{IB} a_o = \frac{\lambda}{2k^2} \sigma_c^2, \]  

where \( K_{ic} \) is the Mode I fracture toughness (psi-in\(^{1/2}\)), \( \sigma_{IB} \), the internal bond strength (psi), \( a_o \), the intrinsic flaw size (in), \( Y(a_o/W) \), the fracture toughness geometry factor, \( W \), the width of the specimen (in), \( \lambda \), the characteristic dimension of the material, particle thickness (in), \( \sigma_c \), the intrinsic strength (psi), and \( k \), the stress concentration factor equal to 0.73.

The applicability of the intrinsic flaw concept for wood composites is confirmed by the work of Lei and Wilson (1980, 1981) who studied the fracture toughness of oriented flakeboard in the TL, RT, RL and TR directions. A TL flakeboard is equivalent to solid wood with the crack propagating in the TL direction that has been cut into flakes and re-assembled. Lei and Wilson found that the fracture toughness is affected by interflake void size and board density and is unaffected by direction and the amount of resin applied to the flake. The frequency of interflake voids reflects the uniformity of the bond-line with the voids themselves acting as sites for crack initiation. It is assumed that the size of interflake voids will decrease with increasing board density and that LVL represents perfectly bonded OSB, e.g., OSB with no interflake voids as the bond line is continuous rather than formed of discrete spots. Therefore, the fracture toughness of LVL can be taken as an upper bound to that of OSB. Thus, one would expect fracture toughness to decrease as the number of interflake voids increases in agreement with work by Lei and Wilson (1980), as shown in Figure 2.10.

Lei and Wilson (1981) proposed a model for the prediction of fracture toughness of oriented flakeboard that includes a term to account for the influence of interflake voids, Equations 2.23 and 2.24. This model assumes that an induced crack will be extended by an existing void or nonbonded region, and thus predicts the propagation fracture toughness of an established crack rather than the critical stress intensity factor required for crack initiation. Lei and Wilson have shown that the fracture toughness of the panel is affected by the average size of the inherent flaws in the wood, \( \Omega \), which is a material property, and the average void length, \( 1/\mu \),
which is affected by resination and other processing parameters. They combined these in a proportionality constant, \( e^{(\Omega - 1/\mu)} \). This constant accounts for the possibility of producing a composite that has fracture toughness greater than that of clear, solid wood. One feature of the model is that it predicts fracture toughness identical to that of the solid wood when the average void length is set equal to the average inherent flaw size of solid wood. However, note that as the average inherent flaw size increases, an increase in board fracture toughness over that of solid wood is predicted given a constant void length.

\[
\frac{K'_{lc}}{K_{lc}} = \left[ e^{(\Omega - 1/\mu)} \right] \frac{a^{1/2} Y\left(\frac{a}{W}\right)}{(a + \Delta a)^{1/2} Y\left(\frac{a + \Delta a}{W}\right)},
\]  

(2.23)

\[
\Delta a = \frac{1}{\mu} \left( \frac{\lambda}{\mu + \lambda} \right),
\]  

(2.24)

where \( K'_{lc} \) is the fracture toughness of oriented flakeboard, \( K_{lc} \) the fracture toughness of wood used to make strands, \( \Omega \) the average size of inherent flaws, \( a \) the initial crack length for an edge-notched specimen, \( \Delta a \) the expected increase in crack length due to interflake voids, \( Y(a/W) \) the geometry factor for edge-notched specimen, \( 1/\mu \) the average void length, and \( 1/\lambda \) the average distance between voids.

A comparison of model predictions and experimental data are shown in Figure 2.11. The model predicts a \( K_{lc} \) value equivalent to that of solid Douglas Fir at an expected crack length of 2.5 mm (0.1"), in agreement with the measured flaw size for Douglas Fir of 2.5 to 3.8 mm (0.1" to 0.15") reported by Schniewind and Lyon (1973).

Mihashi and Hoshino (1989) found that fracture mechanics, provided one used the non-linear J-integral method, accurately predicted the results for experiment on LVL. Linear fracture mechanics, \( K_{lc} \) and \( G_{lc} \), underestimated the fracture toughness. Two fracture geometries were studied: one where the crack lays across (A) the veneers, and one where the crack runs parallel (P) to the veneers, both shown schematically in Figure 2.12. They found that the tensile strength of type A specimens was less than that of type P. However, the fracture toughness of type P specimens was less than type A. The type A specimen is an example of crack divider geometry where the specimen acts as a series of thin plane-stress samples rather than one thick plane-strain.
sample (Hertzberg, 1996). Since fracture toughness is higher in plane stress, the type A specimen will exhibit greater fracture toughness than type P.

However, the applicability of fracture mechanics to wood composites is not universal. Sato (1988a, 1988b) examined the Mode I fracture toughness using a compact tension (CT) specimen and the mixed mode fracture of medium density fibreboard (MDF) and found that the fracture toughness decreased for deeper, sharper notches. Fracture toughness, $K_c$, was also found to increase with specimen width. Since fracture toughness is a material property, there should be no size effect associated with notch depth. Therefore, based on this, the use of linear elastic fracture mechanics is suspect in the case of MDF.

A better understanding of fracture at the macroscopic level can be obtained by examining the underlying micromechanisms of fracture. Laufenberg (1984) studied the fracture surface of OSB tested in tension, and found that the strands failed in four distinctive patterns, listed in Table 2.8. Examination of the table reveals that the type of failure is related to the angle of strand orientation with respect to the loading axis. This was confirmed by Barnes (2000) who found that strength decreased with increasing angle of the strands to the applied load for both parallel (unidirectional) and cross-angled (cross-ply) composites. The effect was less for cross-angled products due to a change in failure mode.

Laufenberg also confirmed the effect of disbond length, which includes all bond-line failures, delaminations, and any area that may not have been in contact with other flakes, interflake voids. The tensile strength is seen to decrease with longer disbond lengths. Since adhesive failure at the bond-line between the resin and the wood typically occurs at a lower strength than cohesive failure in either the wood or resin and longer disbond lengths correspond to an increase in the amount of bond-line failure, the tensile strength decreases. Longer disbond lengths can also represent an increase in the size of interflake voids, which must decrease the tensile strength since these are zero strength regions. Laufenberg found that the orthotropic failure criteria: the maximum stress criterion, the Tsai-Hill criterion and Hankinson’s Formula, provide reasonable upper-bound estimates of the panel strength. Laufenberg attributed the deviation between predicted and measured strength to bond-line failures and recommends that the influence of bond quality should be examined using a fracture mechanics approach.
The work on specimen geometry for wood composites leads to the following conclusions. Furnish should have a high slenderness ratio, i.e., be as thin and as long as possible for a composite product with high strength. As well, fracture mechanics predicts the behaviour of wood composites with reasonable accuracy since the models for wood composites are based on intrinsic flaws within the product. Note that an increase in slenderness ratio leads to a higher surface area to volume ratio for the furnish, which will require higher resin consumption. Thus, there is a trade-off between mechanical properties and cost for the wood composite.

2.2.2 Adherend

The fracture behaviour of wood-adhesive joints and wood composites is dependent on the fracture behaviour of the solid wood as well as the surface preparation of the wood. The relevance of the fracture behaviour of solid wood is most evident for glulam. Since the size of the wood component is relatively large, it behaves similarly to solid wood. In a series of studies, Murphy (1986) compared the strength reduction of notched beams to beams containing a narrow slit parallel to the long axis of the beam. It was found that slits gave lower bending strengths than a notch of the same length. The magnitude of the strength reduction led Murphy to recommend that large, notched beams should be replaced with clear beams equal to the net section of unnotched material in design.

The moisture content of the wood used to form the joint also has an effect on adhesive joint performance as the fracture energy increases with decreasing moisture content, down to approximately 10 percent (Ebewele et al., 1986a). This is similar to solid wood where the fracture toughness passes through a maximum at approximately 6 to 8 percent and decreases thereafter. The difference is likely due to the range examined. Ebewele et al. did not study moisture contents below 10 percent, however it is likely that maximum would be found below this value. In addition, thermal effects, such as harsh drying, can compound moisture effects, and reduce the overall fracture toughness of the wood itself by introducing more and larger internal flaws. As well, in the production of oriented strand board, the strands are dried before placement within the blender causing them to absorb water from the resin reducing the overall volume of resin and increasing its viscosity interfering with bond formation (Meinecke and Klauditz, 1968). This effect is found in liquid PF and water dispersed pMDI resins. Contrary to this, Christensen and Robitschek (1974) found that increased moisture content will lead to a greater amount of resin per strand for both liquid and powder resins.
The increase of fracture toughness and strength with increasing density for solid wood is well known. This trend has also been observed in the strength of wood composites, specifically OSB. Barnes (2000) found an increase in board properties with increased board density, and developed the following model, to predict the structural properties (MOE and MOR) of OSB:

\[
F_R = F_i \left( \frac{\rho_b}{\rho_a} \right)^x, \tag{2.25}
\]

where \( F_R \) is the resultant property, MOE or MOR, \( F_i \), the initial property, MOE or MOR, \( \rho_a \), the initial dry wood density, \( \rho_b \), the dry wood density in product, and \( x \), the exponent for the desired property (1.0 for MOE, 1.2 for MOR).

The processing of wood-adhesive joints and wood composites affects the behaviour of the final product. Surface preparation of the adherends is important in the formation of an adhesive bond. Surface damage increases the discontinuity of the bond-line thus increasing the number of internal flaws (White and Green, 1980). Sasaki et al. (1973) shows that the fracture strength of a wood-adhesive bond is greatest for microtomed and planed surfaces followed by fine-sawn, rough-sawn, disk cut (planed with a disk planer) and sanded. Each of these preparation techniques lead to increased damage of the wood fibres.

The effect of the OSB strand surface characteristics on the bond-line formed is similar to the effect of surface roughness for wood-adhesive bonds. Strands having a rough surface or containing large surface voids are more likely to trap resin and form discontinuous bonds after pressing, Figure 2.13. These voids and the natural variability of the wood will also affect the uniformity of the resin penetration into the wood strand (Brady and Kamke, 1988). Strand size homogeneity is also important since; increased homogeneity should, theoretically, lead to a better resin distribution (Maloney, 1993). Therefore, improved strand generation, i.e., reduction of the size variability between strands and the surface roughness, should improve the fracture toughness of OSB. Damage of the wood fibres can also occur through thermal degradation, e.g., heating that results from machining with dull blades (Ebewele et al., 1986a). As above, the increased surface damage will reduce joint performance. Jung and Murphy (1983) have also
shown that there is a reduction in fracture toughness with increasing veneer thickness. They speculate that as the thickness increases there is increased damage of wood fibres.

The adherend surface will also affect the crack path in a wood-adhesive joint. There are three possible fracture paths: the crack can propagate in the wood alone, the adhesive alone, or at the wood-adhesive interface. The highest fracture energies occur in the first two cases (Ebewele et al., 1979) with a high percentage of wood failure indicating a strong adhesive bond (Ebewele et al., 1986b). The percentage of wood failure can be estimated by ASTM Practice D5266-99: Standard Practice for Estimating the Percentage of Wood Failure in Adhesive Bonded Joints (2000).

In contrast, Ebewele et al. (1980) who compared hand sanding to machine sanding, found that the hand-sanded surface gave higher fracture toughness than a comparable machine-sanded specimen. This was despite the fact that hand sanding led to greater surface roughness. However, hand sanding is done in a back-and-forth motion whereas machine sanding is in one direction, the direction of the grain. The back-and-forth motion of hand sanding leads to the creation of pre-failed interfaces between fibres. These planes of weakness allow the crack to deviate from the adhesive layer and arrest the crack thus increasing the fracture toughness.

A similar effect is seen when examining the angle the grain makes with the bond-line of the wood-adhesive specimen. Mijovic and Koutsky (1979) found that minimum fracture toughness occurs at approximately 30°. Below 30° the crack deviates into the wood, similar to the effect found by Ebewele et al. (1980) with hand-sanded substrates, giving a measurement of solid wood cohesive fracture toughness. As the grain angle is reduced from 90° to 30° there is less surface area available for resin penetration.

The studies discussed above show that the behaviour of solid wood is an important factor in determining the overall behaviour of wood-adhesive joints and wood composites. The effect of notches, moisture content, and density on the fracture toughness of wood composites is similar to that for solid wood. In the formation of wood-adhesive joints, surface preparation is vital to ensure that appropriate conclusions are drawn from experiments. Improper preparation may lead to deviations of the crack from the desired location: in the wood, in the resin, or at the wood-resin interface.
2.2.3 Resin

The adhesive is the component used to bond the joint or composite together. Numerous authors (Ebewele et al., 1979, 1982, 1986b; Gagliano and Frazier, 2001; Sasaki et al., 1973; Shimizu and Okuma, 1981; White, 1976; White and Green, 1980) have studied parameters associated with the bond-line including bond-line thickness and resin penetration (Shimizu and Okuma, 1981). Both Sasaki et al. (1973) and Ebewele et al. (1979) have shown that there is an optimum bond-line thickness for maximum fracture toughness. This is evident in the work by Ebewele et al. (1979) that compared the fracture toughness for crack initiation with that for arrest of the same crack over a range of bond-line thicknesses for the Hard Maple/phenol-resorcinol system, shown in Figure 2.14. It should be noted that the optimums were found for two very different systems. Sasaki found an optimum of 500 μm for Kauri/epoxy while Ebewele et al. (1979) found an optimum thickness of 85 μm for Maple/phenol-resorcinol. Since the optimum thickness is specific to each wood/resin system, each combination of wood species and resin type should be studied independently. The uniformity of the bond-line thickness and the area coverage are also key parameters as Shimizu and Okuma (1981) found that increased uniformity leads to higher strength bond lines. For a bond line to be considered perfectly uniform, a sample from any location along that bond line will be identical to any other sample taken at a different location. For example, continuous bond-lines will have the same thickness; discontinuous bond-lines will have the same pattern, e.g. dot-pitch and size. In both cases, the bond-line is self-similar along the entire length.

At the wood composites scale, fracture toughness of oriented strand board is directly affected by the resin dispersion and distribution. This in turn affects the uniformity of the bond-line and the probability of finding sections that are not bonded. Due to the small amount of adhesive applied in OSB manufacture, on the order of 2 to 3 weight percent, the bond-line formed is discontinuous. This leads to stress concentrations at each resin droplet. The resin dispersion is the size distribution of the resin droplets produced by the atomizer and resin distribution is the spatial distribution of the resin on the strand. Meinecke and Klauditz (1968) found that a decrease in droplet size (from an average droplet diameter of 35 μm to 8 μm) results in enhanced bond properties at a lower specific resin mass (i.e., the mass of solid resin available to coat 1 m² of strands). The increase in internal bond strength with a smaller droplet size was also reported by Burrows (1961), Youngquist et al. (1987), and Kamke et al. (1996a) However,
neither Burrows nor Youngquist et al. report the absolute droplet size. Both based their conclusions on a qualitative comparison between "fine" atomization to "coarse" atomization. Kamke et al. (1996a), who examined droplet sizes from 49 μm to 86 μm, and Youngquist et al. (1987) show a similar effect on modulus of rupture (MOR) which increases with an increased uniformity of resin coverage. Kamke et al. (1996a) have also stated that the optimum dispersion and distribution are unknown.

If a resin droplet is too small, it may be completely absorbed into the strands, leaving no resin at the interface and available for bonding. Kamke et al. (1996a) noted the phenomena in their studies of OSB. This also leads to the sharp decrease in toughness at thin bond-lines as seen in Figure 2.14. Joint starvation due to over penetration of resin into the wood will result in reduced mechanical properties. However, some penetration is necessary to repair the adherend surface from damage caused during preparation. This is shown by White (1976) who found that the fracture toughness increased as the penetration depth of the resin into the wood increased. In contrast, Ebewele et al. (1986b) state that a shallow penetration depth can produce weak bonds, but that a definitive statement on the correlation between penetration depth and mechanical properties cannot be made at this time.

The resin distribution and dispersion of a random sampling of strands can be directly measured using available imaging techniques. Methods have been developed to quantify both the PF and pMDI resins typically used in OSB manufacturing as well as the waxes. Roll and Roll (1994) and Kamke et al. (1996b) report methods to quantify the degree of pMDI resin coverage through digital imaging. Kamke et al. (1996b) and Saunders and Kamke (1996) also report a method for PF resins and lignosulfate-based emulsion wax. These methods are based on colour and uses fluorescence to enhance the contrast between the wood and resin. To measure the different resins and waxes these methods stain the strand with a variety of dyes and vary the wavelength of the excitation light and the filter, demonstrated in Figure 2.15. Groves (1998) demonstrates an alternative method for the measurement of PF resins. This method relies on recognition of the colour contrast between the resin and the wood base.

The effect of resin amount was also studied by Higgins (1990) who used a modified Hankinson equation, similar to Barnes (2001), Equations 2.16 and 2.17, to model the strength (MOR) of oriented strand composites. In this case, the modification is the use of a von Mises
probability distribution function (pdf), \( g(\theta, m, k) \), to account for the orientation of the strands. The inputs to the model are the concentration parameter, \( k \), which defines the degree of orientation of the board; the specific longitudinal, \( \sigma_l \); and transverse, \( \sigma_\perp \), tensile strengths of the strands, Equations 2.26 to 2.28. As the concentration parameter increases, the von Mises pdf narrows and the maximum value increases meaning that a higher proportion of the strands are oriented parallel to the board axis.

\[
g(\theta, m, k) = \frac{1}{\pi I_0(k)} e^{k \cos(\theta - m)}, \tag{2.26}
\]

\[
s(\theta) = \frac{\sigma_l}{1 + \left[ \frac{\sigma_l}{\sigma_\perp} \right] - 1} \sin^2 \theta, \tag{2.27}
\]

\[
S_m(\theta) = \int_{-\pi/2}^{\pi/2} g(m, k, \theta) \cdot s(\theta) d\theta, \tag{2.28}
\]

where \( g(\theta, m, k) \) is the grain angle pdf, \( \theta \), the individual strand grain angle, with respect to the board axis, \( m \), the angle between the principal orientation axis and the axis of load, \( k \), the orientation parameter of strand angular spread, \( I_0(k) \), a modified Bessel function of order zero, \( S_m(\theta) \), a mathematical expectation of composite strength, \( s(\theta) \), a Hankinson expression for the specific strength of a strand loaded at angle \( \theta \) with respect to its grain, \( \sigma_l \), the mean specific tensile strength of strands tested parallel to grain, and \( \sigma_\perp \), the mean specific tensile strength of strands tested perpendicular to grain.

Higgins (1990) concluded that the model is accurate when sufficient resin is available to provide adequate stress transfer, and underlines the importance of the bond line in achieving the maximum strength of oriented wood composites. Bonding was increasingly critical as the orientation level, the percentage of strands oriented in the direction of applied load, increased. Experimentally, 5 to 7 times the amount of resin typically used in industry was required to achieve the maximum specific tensile strength predicted by the model. As well, Higgins (1990) also found that the internal bond strength of randomly oriented board was reduced by 25 to 30 percent when as little as 10 percent of the surface was inadequately covered with resin. In addition, the model put forth by Barnes (2001), Equations 2.16 and 2.17, also predicts increasing
stiffness and strength with increasing strength perpendicular to the bond line, and emphasizes the significance of the bond-line.

Wood-adhesive joints and wood composites are used for long periods of time and thus, time effects associated with the effectiveness of the bond-line are of importance. River et al. (1989) found that the fracture toughness of UF resins changed with time. The material may initially exhibit stable fracture and have a constant fracture toughness, and at longer times, up to 2 weeks after bonding, the fracture toughness of the adhesive joint can continue to increase. River et al. speculate that this is due to continued cross-linking and physical aging of the resin. This concept of optimum time can also be applied to the curing of a bond-line. Both Gagliano and Frazier (2001) and Ebewele et al. (1982) found that there is an optimum cure time for maximum fracture toughness. Again, each system has its unique optimum, dependent on the parameters used to form the joint including the substrate, the resin, and the formation temperature. Gagliano and Frazier report that the maximum fracture toughness for the Yellow Poplar/phenol-formaldehyde combination was achieved at a temperature of 175°C after 20 minutes whereas Ebewele et al. (1982) reports maximums reached after 30 minutes to 4 hours as the bonding temperature decreases from 150°C to 50°C for the Hard Maple/phenol-resorcinol system. Both also show a reduction in fracture toughness beyond this optimum time, which they speculated is the result of embrittlement due to excessive cross-linking.

The effect of the bond-line on wood-adhesive joints and wood composites can be summarized as follows. The wood-adhesive joints with the greatest fracture toughness are theoretically those with a uniform thickness of resin: the optimum thickness depends on the wood-adhesive combination used. Care should be taken so that over-penetration of the resin into the wood does not occur. This is a concern both for continuous and discontinuous bond-lines. In the case of discontinuous bond-lines, the resin distribution should be uniform and composed of small resin droplets. However, the optimum distribution has not been determined. As well, while work by Barnes (2001) and Higgins (1990) demonstrates the importance of the bond line in achieving the desired macroscopic board properties, none of the proposed models addresses how the adhesive bond is affected by resin dispersion and distribution. Therefore, an examination of the effect of resin dispersion and distribution (size variability and inter-droplet spacing) on the fracture toughness of wood-adhesive joints and wood composites would be beneficial.
2.2.4 Summary

The literature on the fracture of wood composites can be related to fracture of wood-adhesive joints and solid wood. Based purely on geometrical considerations, the literature shows that the basics of structural bonded joint behaviour are readily applicable to wood-adhesive joints and wood composites. The strength of both increases with increasing lap length and increasing slenderness ratio, i.e., decreasing lamination or veneer thickness. Fracture mechanics is also applicable to wood composites as the models proposed are based on the concept of intrinsic flaws within the product.

The behaviour of the adherends is important. All wood component sizes: lumber, veneer, strands, particles, and fibres have been studied and the majority of researchers have concluded that fracture of wood-adhesive joints should occur in wood alone and not at the wood-resin interface for optimum performance. Thus, an understanding of the fracture of solid wood at all levels is also essential to better comprehend the behaviour of wood composites. The location of fracture in a wood composite is dependent upon the surface preparation and grain angle of the wood substrates. For OSB in particular, fracture toughness can be improved by improving strand generation. Improved strand generation leads to decreased surface roughness and therefore, as is the case with wood-adhesive joints, increased fracture toughness.

The fracture toughness of OSB also increases with increasing uniformity of resin distribution (spatial variability) and decreasing resin dispersion (size variability). Both will lead to greater bond-line uniformity and will increase the fracture toughness, as found in wood-adhesive joints. Despite this knowledge on resin dispersion and distribution, the optimum conditions will depend on the wood species and resin system used. As well, there are currently no proposed models that address the effect of resin dispersion and distribution on the adhesive bond, such models are necessary to optimize the desired macroscopic board properties.
Table 2.1: Mode I fracture toughness specimen geometries, directions, species, and effect examined.

<table>
<thead>
<tr>
<th>Specimen Geometry</th>
<th>Direction</th>
<th>Specimen Geometry</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>RL</td>
<td>TL</td>
</tr>
<tr>
<td>Compact Tension (CT)</td>
<td>P: (1)†</td>
<td>P: (2,3,4)</td>
</tr>
<tr>
<td></td>
<td>S: (3)</td>
<td>P: (4,5,6)</td>
</tr>
<tr>
<td></td>
<td>MC**: (2)</td>
<td>P, S, T:</td>
</tr>
<tr>
<td>Notched Four Point Bend</td>
<td>DF, SR: (8)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>HF, D,</td>
<td>HF, MC:</td>
</tr>
<tr>
<td></td>
<td>AT, MC:</td>
<td>U, D:</td>
</tr>
<tr>
<td>Double Torsion</td>
<td>DF, SR: (13)</td>
<td></td>
</tr>
<tr>
<td>Single Edge Notch (SEN)</td>
<td>DF, MC:</td>
<td>DF, D: (14,15)</td>
</tr>
<tr>
<td></td>
<td>B: (16)</td>
<td>B: (20)</td>
</tr>
<tr>
<td></td>
<td>P: (17)</td>
<td>P, D:</td>
</tr>
<tr>
<td></td>
<td>S: (18)</td>
<td>P: (24)</td>
</tr>
<tr>
<td>Tapered Cleavage</td>
<td>D: (15,17)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>T: (16)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>SR: (19)</td>
<td></td>
</tr>
<tr>
<td>Double Cantilever Beam (DCB)</td>
<td>S, SR, MC:</td>
<td></td>
</tr>
<tr>
<td></td>
<td>DF: (25)</td>
<td></td>
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<td></td>
<td>B: (16,26)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>P: (24)</td>
<td></td>
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<tr>
<td></td>
<td>C, S, H, L:</td>
<td>(26)</td>
</tr>
<tr>
<td></td>
<td>MC: (26)</td>
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<tr>
<td>Double Edge Notch (DEN)</td>
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<td></td>
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<td></td>
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<td></td>
</tr>
<tr>
<td></td>
<td>P: (19)</td>
<td></td>
</tr>
<tr>
<td>Centre Notch (CN)</td>
<td>P, DF, C, M, Bi, O, La, SR, MC:</td>
<td>(27)</td>
</tr>
<tr>
<td></td>
<td>DF: (25,27)</td>
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<tr>
<td></td>
<td>P, C, M, Bi, O, La:</td>
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<td></td>
<td>SR: (25,27)</td>
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<td>MC: (27)</td>
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<tr>
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<td>Unknown</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>


**Effects:** D = defects, MC = moisture content, ρ = density, SR = strain rate, T = thickness.

Table 2.2: Mode I fracture toughness of air-dry Douglas Fir. (Schniewind and Centeno, 1973)

<table>
<thead>
<tr>
<th>Direction</th>
<th>( K_{fc} ) (MPa·m(^{1/2}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>RL</td>
<td>0.41</td>
</tr>
<tr>
<td>TL</td>
<td>0.31</td>
</tr>
<tr>
<td>RT</td>
<td>0.35</td>
</tr>
<tr>
<td>TR</td>
<td>0.35</td>
</tr>
<tr>
<td>LR</td>
<td>2.69</td>
</tr>
<tr>
<td>LT</td>
<td>2.42</td>
</tr>
</tbody>
</table>

Table 2.3: Mode II fracture toughness specimen geometries, directions, species and effect examined.

<table>
<thead>
<tr>
<th>Specimen Geometry</th>
<th>Direction</th>
<th>Specimen Geometry</th>
<th>Species</th>
<th>Effects</th>
</tr>
</thead>
<tbody>
<tr>
<td>End Notched Flexure (ENF)</td>
<td>RL</td>
<td>DF, CL (^{**})</td>
<td>Ba, DF, H</td>
<td>CL, MC, p</td>
</tr>
<tr>
<td></td>
<td>TL</td>
<td>DF, CL (^{**})</td>
<td>P, S</td>
<td></td>
</tr>
<tr>
<td></td>
<td>RT</td>
<td>H, CL (^{**})</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>TR</td>
<td>H, CL (^{**})</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>LR</td>
<td>P, DF, MC (^{**})</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>LT</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>N/A</td>
<td>P, DF, MC (^{**})</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Centre-Slit Beam (CSB)</td>
<td>RL</td>
<td>DF, CL (^{**})</td>
<td>Ba, DF, H</td>
<td>CL, MC, p</td>
</tr>
<tr>
<td></td>
<td>TL</td>
<td>DF, CL (^{**})</td>
<td>P, S</td>
<td></td>
</tr>
<tr>
<td></td>
<td>RT</td>
<td>H, CL (^{**})</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>TR</td>
<td>H, CL (^{**})</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>LR</td>
<td>P, DF, MC (^{**})</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Centre Notch Under Shear</td>
<td>RL</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>TL</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>RT</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>TR</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>LR</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>LT</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>N/A</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Effects: CL = crack length, MC = moisture content, \( \rho \) = density.

\(^{**}\)Authors: 1 = Murphy, 1979a; 2 = Barrett and Foschi, 1977a; 3 = Barrett and Foschi, 1977b; 4 = Wright and Fonselius, 1986; 5 = Fonselius and Riipola, 1988; 6 = Murphy, 1989; 7 = Cramer and Pugel, 1987; 8 = Wu, 1967.
Table 2.4: Mixed Mode fracture toughness specimen geometries, directions and species.

<table>
<thead>
<tr>
<th>Specimen Geometry</th>
<th>Direction</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>RL</td>
</tr>
<tr>
<td>Modified End Notched Flexure (mENF)</td>
<td>BR*: (1)**</td>
</tr>
<tr>
<td>Compact Shear (CS)</td>
<td>P: (3)</td>
</tr>
<tr>
<td></td>
<td>S: (4)</td>
</tr>
<tr>
<td>Compact Tension Shear (CTS)</td>
<td>P: (3)</td>
</tr>
<tr>
<td>End Notched Flexure (ENF)</td>
<td></td>
</tr>
<tr>
<td>Single Edge Notch (SEN)</td>
<td>S: (4)</td>
</tr>
<tr>
<td>Centre Notch (CN)</td>
<td>S: (4)</td>
</tr>
</tbody>
</table>


**Authors: 1 = Hunt and Croager, 1982; 2 = Lum and Foschi, 1988; 3 = Valentin and Caumes, 1989; 4 = Mall et al., 1983; 5 = Barrett and Foschi, 1977b; 6 = Wu, 1967.

Table 2.5: Specimen geometries, directions, species and effect examined in the study of fracture strength.

<table>
<thead>
<tr>
<th>Specimen Geometry</th>
<th>Direction</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>TL</td>
</tr>
<tr>
<td>Four Point Bend</td>
<td>DF*: (1,2)†</td>
</tr>
<tr>
<td></td>
<td>E: (3)</td>
</tr>
<tr>
<td></td>
<td>H: (4,5)</td>
</tr>
<tr>
<td></td>
<td>Bm, C: (5)</td>
</tr>
<tr>
<td></td>
<td>SR**: (2)</td>
</tr>
<tr>
<td></td>
<td>D: (1,2,3)</td>
</tr>
<tr>
<td></td>
<td>MC: (5)</td>
</tr>
<tr>
<td>Three Point Bend</td>
<td>DF: (1)</td>
</tr>
<tr>
<td>Tension</td>
<td>DF, D: (6)</td>
</tr>
</tbody>
</table>

Species: Bm = Balsam, C = Cedar, DF = Douglas Fir, E = Eucalyptus.

**Effects: D = defects, MC = moisture content, SR = strain rate.

†Authors: 1 = Murphy, 1979b; 2 = Spencer, 1979; 3 = Leicester, 1973; 4 = Lum and Foschi, 1988; 5 = Steida, 1966; 6 = Cramer and Goodman, 1983.
Table 2.6: *Wood-adhesive fracture specimen geometries, adhesives, species, and effect examined in the literature.*

<table>
<thead>
<tr>
<th>Specimen Geometry</th>
<th>C</th>
<th>E</th>
<th>P</th>
<th>PR</th>
<th>R</th>
<th>RF</th>
<th>UF</th>
<th>N/A</th>
</tr>
</thead>
<tbody>
<tr>
<td>Single Lap</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Sh</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>BL</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>(1)</td>
</tr>
<tr>
<td>Double Lap</td>
<td>LC, LT, LC: (2)</td>
<td>P, LC: (3)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Butt Double Strap</td>
<td>P, Bi, Q, Bk, LT, LC: (4)</td>
<td>LC, LT, LC: (2)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Butt</td>
<td>Elk, Cp, Sla, BT, D, T, BL, SC: (7)</td>
<td>DF, LT, SC: (8)</td>
<td>Elk, Cp, Sla, BT, D, T, BL, SC: (7)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>(9)</td>
</tr>
<tr>
<td>Compact Tension</td>
<td>P, BL: (10)</td>
<td>C, M, BL: (11)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>(CT)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Double Cantilever</td>
<td>YP, BL: (12)</td>
<td>Bi: (13)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Beam (DCB)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Contoured Double</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Cantilever Beam</td>
<td>A: (14)</td>
<td>M: (14 – 20)</td>
<td>Bi, LT: (21)</td>
<td>BL: (15,17)</td>
<td>SC: (16,18,20)</td>
<td></td>
<td></td>
<td>(9)</td>
</tr>
<tr>
<td>(CDCB)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Four Point Bend</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Resins:** C = casein, E = epoxy, P = phenolic, PR = phenol-resorcinol, R = resorcinol, RF = resorcinol-formaldehyde, UF = urea-formaldehyde.

**Species:** A = Aspen, Bi = Birch, Bk = Blackwood, BT = Brown Terminalia, C = Cedar, Cp = Campnosperma, D = Dillenia, DF = Douglas Fir, Elk = East Indian Kauri, LC = Lawson Cypress, M = Maple, P = Pine, Q = Quandong, Sh = Shinanoki, Sla = Solomon Island Albizia, T = Taun, YP = Yellow Poplar.

**Effects:** BL = bond line, LC = lap characteristics, LT = lamination thickness, SC = surface characteristics.

Table 2.7: Wood composite fracture toughness specimen geometries, species, and effect examined in the literature.

<table>
<thead>
<tr>
<th>Specimen Geometry</th>
<th>Glued Laminated Timbers (Glulam)</th>
<th>Laminated Veneer Lumber (LVL)</th>
<th>Oriented Strand Board (OSB)</th>
<th>Particleboard (PB)</th>
<th>Medium Density Fibreboard (MDF)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Three Point Bend</td>
<td>DF, D**: (1)</td>
<td>C, BL, p: (2)</td>
<td></td>
<td>U, D: (4)</td>
<td></td>
</tr>
<tr>
<td>Compact Tension (CT)</td>
<td>DF, C, S, L, D: (3)</td>
<td>DF: (5)</td>
<td>A: (6,7)</td>
<td>Ad, Bi, C, P: (7)</td>
<td>DF: (10)</td>
</tr>
<tr>
<td>Internal Bond (IB)</td>
<td>DF, BL, p: (5)</td>
<td>DF: (10)</td>
<td>A: (6)</td>
<td>Ad, Bi, C, P: (7)</td>
<td>DF: (10)</td>
</tr>
<tr>
<td>Compact Tension Shear (CTS)</td>
<td>DF: (12)</td>
<td>DF: (5,13)</td>
<td>A: (6)</td>
<td>Ad, C, P: (7)</td>
<td>DF: (10)</td>
</tr>
<tr>
<td>Single Edge Notch (SEN)</td>
<td>DF: (5,12)</td>
<td>DF: (10)</td>
<td>A: (6)</td>
<td>Ad, C, P: (7)</td>
<td>DF: (10)</td>
</tr>
<tr>
<td>Contoured Double Cantilever Beam (CDCB)</td>
<td>U: (14)</td>
<td>P: (14)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Tension</td>
<td>A: (7,15)</td>
<td>Bi: (7,16)</td>
<td>DF: (16)</td>
<td>Ad, C, P: (7)</td>
<td>BL: (15)</td>
</tr>
<tr>
<td>Unknown</td>
<td>U, TS: (17)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>


**Effects: BL = bond line, D = defects, LC = lap characteristics, LT = lamination thickness, p = density, TS = tensile strength.


Table 2.8: Failure mechanisms in randomly oriented strand board (Laufenberg, 1984)

<table>
<thead>
<tr>
<th>Failure Pattern</th>
<th>Characteristics</th>
<th>Failure Type (%)</th>
<th>Orientation of Strand with respect to Loading Axis</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transverse/Shear</td>
<td>Failure along wood fibre.</td>
<td>81</td>
<td>10° to 90°</td>
</tr>
<tr>
<td>Rolling Shear</td>
<td>Fracture perpendicular to principal stress, rotation of prismatic cross section of wood strand.</td>
<td>6</td>
<td>70° to 90°</td>
</tr>
<tr>
<td>Tensile</td>
<td>Long splintered strand end, wood fibre nearly parallel to load direction.</td>
<td>5</td>
<td>&lt; 12°</td>
</tr>
<tr>
<td>Disbonding</td>
<td>Low strand-to-strand bond strength or high strand strength.</td>
<td>8</td>
<td>&lt; 45°</td>
</tr>
</tbody>
</table>
Figure 2.1: Molecular components of wood: repeat unit of cellulose – (a) cellobiose molecule (Nikitin, 1966); monomers of lignin – (b) coniferyl alcohol, (c) sinapyl alcohol and (d) p-coumaryl alcohol (Lin and Lebo, 1995).

Figure 2.2: Organization of the cell wall layers and middle lamella. The cell diameter is approximately 40 μm. Adapted from Nikitin (1966).

Figure 2.3: Schematic of various crack paths in wood: (a) crack advance by cell fracture (intracellular fracture), (b) crack advance by cell separation (intercellular fracture) and (c) crack arrest at a vessel showing a crack splitting the wall of a vessel (crack deflection). Adapted from Gibson and Ashby (1988).
Figure 2.4: Schematic of crack propagating in the TR direction. E = earlywood, L = latewood. Adapted from Thuvander and Berglund (2000).

Figure 2.5: Effect of density on Mode I fracture toughness. Data obtained from Ashby et al., 1985; Bostrom, 1990; Johnson, 1973; Kretschmann et al., 1990; Lei and Wilson, 1980; Mindess et al., 1975b; Nadeau et al., 1982; Patton-Mallory and Cramer, 1987; Pearson, 1974; Schniewind and Centeno, 1973; Schniewind and Lyon, 1973; Schniewind and Pozniak, 1971; White and Green, 1980.
Figure 2.6: Diagram showing the values used in calculating the knot ratio, KR. Adapted from Boatright and Garrett (1979).

Figure 2.7: Some common structural bonded lap joints (a-i) and butt joints (j-l). (Tong and Steven, 1999)
Figure 2.8: Effect of lamination thickness on failure load for laminated pine containing butt joints. 
○ = stress at ultimate load (psi), ● = stress at fracture (psi). Adapted from Leicester (1973).

Figure 2.9: Effect of lap length on failure load for a double strap butt joint where 
\( P = \) failure load, \( \alpha = f(\text{geometry and material properties}) \), \( Y = \) maximum allowable shear stress, \( L = \) lap length. Adapted from Tong and Steven (1999).

Figure 2.10: Effect of board density on Mode I fracture toughness (resin spread rate = 1.350 lb/1000 ft²). Adapted from Lei and Wilson (1980).
Figure 2.11: Predicted and experimental retention of Mode I fracture toughness. Calculations for expected crack length are based on interflake void length plus nonbonded length. Adapted from Lei and Wilson (1981).

Figure 2.12: Schematic of Type A, crack lies across the veneers, and Type P, crack lies parallel to the veneers, fracture geometries. Adapted from Mihashi and Hoshino (1989).
adhesive not used in bonding

adhesive used in bonding

(a)

vessels
adhesive not used in bonding

adhesive used in bonding

(b)

Figure 2.13: *Formation of an adhesive joint:* (a) variations due to strand surface roughness, (b) variations due to wood morphology.

---

Figure 2.14: *Effect of bond-line thickness on Mode I fracture toughness for Hard Maple/phenol-resorcinol system.* $G_{ic}$ is the critical fracture energy and $G_{ia}$ is the arrest load energy. If $G_{ic}$ is greater than $G_{ia}$, unstable crack growth occurs. (Ebewele et al., 1979)
Figure 2.15: Various photomicrographs of Kamke resin distribution measurement method: (a) white excitation light and long-pass UV filter; (b) UV excitation light and long-pass UV filter; (c) blue excitation light and green long-pass filter; (d) green excitation light and red long-pass filter; (e) and (f) green excitation light and red emission filter; (g) blue excitation light and green emission filter (Kamke, 2001).
3 – FRACTURE: SPECIMEN PREPARATION

Having established that there are currently no proposed models that address the effect of resin dispersion (size variability) and distribution (spatial variability) on the adhesive bond, this work attempts to determine the effect of droplet diameter and dot-pitch (the centre-to-centre spacing of these droplets) on fracture toughness. To do so it is necessary to vary the dot-pitch and diameter in a controlled manner. This control was achieved using a modified flexographic printing technique, which will be described in this chapter. The variability associated with specimen preparation is also examined and the criteria for the selection of the fracture geometry presented. Furthermore, the determination and selection of the cure cycle for the specimens will be outlined.

3.1 MATERIALS

The adhesive bonds were constructed with Borden Chemical liquid phenol-formaldehyde (PF) resin FT57F on planed Douglas Fir substrates. A planed surface was chosen since this should give the highest fracture toughness value as demonstrated by Sasaki et al. (1973). All of the Douglas Fir specimens (6.35mm thick by 25.4mm wide by 356mm long) were cut such that the crack propagated in the TL direction. The TL direction was chosen for two reasons. First, the TL direction has the lowest Mode I fracture toughness for Douglas Fir and it is unlikely that a crack will deviate from the TL direction once initiated. Second, in the manufacture of oriented strand board the strands have either a TL or RL orientation.

3.2 CONDITIONING

All specimens were kept in a constant climate room at 65% relative humidity and 20°C, giving an equilibrium moisture content of 12 percent, between the various stages of preparation.

3.3 PRINTING PROCEDURE

This section presents a printing procedure that was initially developed by Smith (2002) and refined in this study. The placement of resin upon the substrate in a known location and amount is of utmost importance. To accomplish this, flexographic printing techniques typically used to print ink onto paper were modified such that the resin was substituted for the ink and the wood substrate for the paper.
Prior to printing the resin viscosity, the room temperature and relative humidity, the mass of the beam to be printed and the mass of a control beam, which was not printed, were recorded. To print a specimen, 15 mL of resin was poured onto a clean glass plate. A wire wound rod, which consists of a 12.7 mm stainless steel rod wrapped by wire, was drawn down through the resin pool leaving a film of resin behind. The thickness of the resin film depends upon the diameter of the wire wrapped around the rod. For bars purchased in the United States the relationship between rod number, where the number denotes the diameter of the wound wire in thousandths of an inch, and film thickness is the following:

\[ \lambda = 2.725d \]  

(3.1)

where \( \lambda \) is resin film thickness (\( \mu m \)), and \( d \) is the wire wound rod number.

A printing plate was laid on top of the resin film and lightly pressed to ensure a full coverage of resin and to remove any air pockets formed. Once coated, the plate was flipped over and laid flat. The wood specimen was pressed onto the plate for 60s with a 6.8 kg weight. The mass of the printed beam and the control specimen were recorded again. This process was repeated for a second beam with the same printing plate to form one complete specimen, i.e., resin was applied to both sides of the adhesive joint formed.

### 3.3.1 Selection of the Printing Plate Patterns

The printing plates consist of a square array of raised dots formed by truncated cones, Figure 3.1, with the first number denoting the dot-pitch or number of lines per mm (lpmm) and the second number, the area coverage (%). The dot-pitch determines the spacing of the resin droplets and the area coverage determines the droplet diameter with increasing dot-pitch and decreasing area coverage giving smaller droplets. The theoretical droplet diameter for each printing plate is calculated based on dot-pitch and area coverage, Equation 3.2,

\[
D = 2 \times \sqrt{\left(\frac{1}{W}\right)^2 \frac{A_{bond}}{100}} = \frac{1}{5} \sqrt{\frac{A_{bond}}{\pi W^2}}
\]

(3.2)

where, \( D \), is the theoretical droplet diameter, \( W \), the dot-pitch, and \( A_{bond} \), the area coverage expressed as a percent. The theoretical droplet diameters are shown in Table 3.1. This can be
compared to results obtained by Kamke et al. (1996b) who examined two industrial oriented strandboard mills. They found that the resin droplets were between 56 µm and 203 µm in diameter. However, a considerable portion of the strand was coated with droplets with a diameter greater than 252 µm.

Before determining the optimum droplet diameter and dot-pitch, an examination of the typical dispersion and distribution of strands pressed in industry was necessary. A random sample of industrial strands from Weyerhauser – Drayton Valley and blended at Forintek Canada Corp. (with a laboratory scale blender, 2.44 m diameter and 1.22 m deep, equipped with a Coil spinning disk atomizer) were used. The strands were coated with 2.5wt% PF resin containing a charcoal additive. The resin droplet size was measured ($n = 110$, 10 measurements on 11 strands) and a histogram obtained, Figure 3.2. The figure also shows the range of printing plates chosen for this study, which adequately cover the droplet diameter range found in industry.

### 3.3.2 Selection of the Wire Wound Rod

A study was undertaken to determine the rod most suitable for this work as resin film thickness increases with wire wound rod number. An increase in film thickness may affect the pattern of resin droplets applied to the substrate. The selection criteria were that the printed droplet size should mimic that found in industry and should closely resemble the theoretical droplet size of the printing plate. Based on previous work, (Smith, 2002) rods with the following numbers: 4, 5, 6, 8, and 18 were selected. The printed droplet size distributions obtained are shown in Figure 3.3.

Based on these results, the #6 wire wound rod was chosen. While the distribution does not necessarily resemble that of industry it was deemed that mimicking the pattern found on the printing plate was the more important of the two criteria. The high frequency of droplets in the 0-50 µm range for bar #4 was due to a large number of areas where no resin was applied to the specimen. It should be noted that these results are based on Borden Chemical PF-resin GP-45. However, the results are applicable to the PF-resin FT57F as confirmed by the printing pattern formed using FT57F with a #6 bar, Figure 3.4. The droplets formed resemble the original printing pattern. The printed droplet size reduces slightly in comparison with the printing plates as seen by comparing Figure 3.1 and Figure 3.4 and the theoretical droplet size for specimen 225 (287µm), Table 3.1 and the printed size obtained (200 to 250µm), Figure 3.3. As well, the
printed droplets may smear during pressing, which will significantly change the resin pattern on the substrate. Therefore, all relationships presented in the following will be based on the theoretical droplet size as calculated from the printing plate.

3.3.3 Mass of Resin Deposited on Substrate

To determine the mass of resin applied to the substrate, the mass of the printed beam and the mass of a control beam were recorded before and after printing. The mass of resin deposited was calculated as follows:

\[ m_r = (m_{fb} - m_{ia}) - (m_{fc} - m_{ic}) \]  

(3.3)

where \( m_r \) is the mass of resin deposited, \( m_{ia} \) is the initial mass of the substrate, \( m_{fb} \) is the final mass of substrate, and \( m_{ic} \) and \( m_{fc} \) are the initial and final mass of the control respectively. The mass recorded was the maximum measured upon weighing as fluctuations in mass (± 2 mg) due to changes in moisture content of the specimens were encountered. Therefore, the mass of resin added to the substrate may be less than that reported but not greater. The use of a control reduces some of the variability associated with changes in moisture content during the printing process. An examination of Figure 3.5 reveals that the variability of the printing process is quite high. However, for a given area coverage, none of the masses are significantly different from each other at a 95% confidence level. Therefore, any subsequent change in fracture toughness should be independent of the resin mass applied. It should be noted that these and subsequent confidence levels throughout the thesis were calculated based on a two-tailed Student's t probabilities with \( 2\alpha = 0.05 \).

To determine the cause of the variability in the mass of resin applied, the effect of wire-wound-rod number, environmental conditions and resin viscosity were examined. The mass of resin deposited on a glass slide (25 x 75 mm) increases with increasing rod number and relative humidity, Figure 3.6. The effect of resin viscosity can also be seen by comparing GP-45 (\( \eta = 14 \)) to FT57F (\( \eta \sim 250 \)) with increasing viscosity resulting in a greater resin mass being applied. As stated previously in Equation 3.1, increasing rod number gives a thicker resin film. Therefore, these results suggest that increasing resin viscosity and relative humidity also result in a thicker film, which corresponds to more resin deposited on the substrate. A subsequent study on Douglas Fir showed that for minor variations in resin viscosity and relative humidity the
effect becomes less pronounced as shown in Figure 3.7. However, the trend of a lower mass of resin deposited at lower relative humidity is still evident. This is likely due to increased evaporation of water from the liquid resin to the atmosphere at lower relative humidity.

3.4 FRACTURE GEOMETRY SELECTION

At present it is not feasible to produce the thousands of strands necessary for the manufacture of an oriented strand board with a controlled droplet diameter and dot-pitch. Therefore, a less complicated fracture geometry was chosen, the Double Cantilever Beam (DCB). This fracture geometry was also chosen for ease of preparation and the ability to obtain multiple measurements of $G_{ic}$ for each sample. Additionally, the use of an ideal fracture toughness sample should allow the prediction of oriented strand board properties such as internal bond strength. The dimensions were based on ASTM D3433-99: Standard Test Method for Fracture Strength in Cleavage of Adhesives in Bonded Metal Joints (2000). For solid wood specimens, a starter notch was made with a fine band saw blade, 0.75 mm thick, to a depth of 50.8 mm. To obtain a starter notch for the adhesively bonded specimens, no resin was applied to the first 50.8 mm. Specimen dimensions are shown in Figure 3.8.

A modification to ASTM D3433-99 was necessary; the thickness of the specimens was reduced from 25.4 mm to 12.7 mm. This was required because the mass of resin applied was insufficient to maintain bond integrity in the thicker specimens. It is speculated that larger specimen thickness results in high loads on the bond-line due to thermal gradients and specimen warpage during cure. By reducing the thickness the effect of process-induced stresses is reduced and the bond integrity retained.

The specimen width was selected as 25.4 mm based on ASTM D3433-99, which coincidently is the typical strand width found in oriented strand board. This width also places the specimens safely within the plane strain regime as shown by Wright and Fonselius (1986) and Triboulot et al. (1984). A minimum of four replicates were manufactured for each type of specimen.
3.5 CURE CYCLE

The criteria for the cure cycle were that it provides sufficient heat and compaction such that a bond is formed for all printing plate patterns. A typical cure cycle used in this study is shown in Figure 3.9. This cycle was chosen based on a literature review and experimentation. The literature reports a wide range of cure pressures, temperatures and times for phenol-formaldehyde resins – 700 to 1750 kPa, 85 to 175°C and 4 to 60 minutes (Ebewele et al., 1986, 1993; Gagliano and Frazier, 2001; Lei and Wilson, 1979). Thus, a series of experiments was necessary to select a cure cycle for this particular resin-substrate combination. All experiments and subsequent specimen preparation were performed on a 76.2 cm x 76.2 cm (5800 cm²) Pathex 338 Ton Hydraulic press.

3.5.1 Platen Temperature and Press Time

In industry, the core temperature of the board must typically reach 120°C to ensure full cure of phenol-formaldehyde resins. However, this value is for a board composed of a multitude of strands rather than a DCB specimen. Therefore, the platen temperature must be selected such that the centre of the DCB specimen is held at 120°C for the required cure time. Three experiments at various platen temperatures: 150, 175 and 200°C were conducted with the centre line temperature of 25.4 mm specimens (each specimen is composed of two 12.7 mm thick beams) measured with a thermocouple, Figure 3.10. Since the centre-line temperature should reach a minimum of 120°C a platen temperature of greater than 175°C is necessary. To ensure that the resin was fully cured, a cure cycle of 200°C for 25 minutes was selected. However, these results are for 25.4 mm thick specimens rather than the 12.7 mm thick specimens eventually used. Therefore, the cure cycle was reduced to 15 minutes at 200°C. This was calculated based on the lumped capacitance method, Equation 3.4 (Incropera and De Witt, 1990). A reduction in thickness, \( B \), by two \((B \rightarrow B/2)\) gives a similar reduction in volume \((V \rightarrow V/2)\). For \( \theta / \theta_i \) to remain constant, i.e. the same core temperature is achieved this requires a reduction in time, \( t \), of two as the remaining variables \((h, \text{the convection heat transfer coefficient between aluminum and wood}, A_s, \text{the surface area}, \rho, \text{the density of wood}, \text{and } c, \text{the specific heat of wood})\) are constant. To ensure fully cured resin the reduction was kept to a factor of 1.67.

\[
\frac{\theta}{\theta_i} = \frac{T - T_\infty}{T_i - T_\infty} = \exp \left[- \left( \frac{hA_s}{\rho Vc} \right) t \right] = \exp \left[- \left( \frac{2hA_s}{\rho Vc} \right) \frac{t}{2} \right] \quad (3.4)
\]
3.5.2 Cure Pressure

The cure pressure must be sufficient to provide intimate contact between the bonding surfaces yet not crush or deform the specimens excessively. To engage the automatic function of the press, a pressure of approximately 22 MPa, significantly above the compression strength of Douglas Fir (6 MPa) must be reached. Therefore, the press was operated in manual mode. Pressing under displacement control provided sufficient compaction when the distance between the platens was the sum of the caul sheet thickness (6.6 mm) and the specimen thickness (nominally 12.9 mm) less 0.25 mm for a total of 19.3 mm as seen in Figure 3.9. This displacement typically gives a maximum sample pressure of 2 MPa, well below the compressive strength of Douglas fir. Therefore, there should be no excessive deformation of the wood cellular structure and subsequently, no effect of such a deformation on the fracture toughness values.

3.6 SUMMARY

The modification of the flexographic printing process to applying PF-resin onto wood substrates is possible. However, the selection of the printing plate patterns and rod number is critical if a comparison with industrially blended strands is to be made. For Borden Chemical PF-resins GP-45 and FT57F printing plates with a dot-pitch of 1 or 2 lpmm and area coverages ranging from 6 to 50 percent allow this comparison when a #6 wire wound rod is used.

The variability of the printing process is quite high with room conditions and resin viscosity having major effects. Therefore, further research into the printing process in an effort to reduce variations in the mass of resin deposited on the substrate is necessary. Further research into the cure cycle can also be undertaken to ensure that the maximum value of $G_{lc}$ for this resin/wood species combination is obtained. As well, subsequent research on other species, specifically Aspen, is recommended as Aspen is used in the production of oriented strand board.
Table 3.1: Theoretical droplet diameter based on dot-pitch and area coverage.

<table>
<thead>
<tr>
<th>Dot-pitch (lpm)</th>
<th>Area coverage (%)</th>
<th>Droplet Diameter (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>50</td>
<td>811</td>
</tr>
<tr>
<td>1</td>
<td>25</td>
<td>573</td>
</tr>
<tr>
<td>1</td>
<td>12</td>
<td>397</td>
</tr>
<tr>
<td>1</td>
<td>6</td>
<td>281</td>
</tr>
<tr>
<td>2</td>
<td>50</td>
<td>405</td>
</tr>
<tr>
<td>2</td>
<td>25</td>
<td>287</td>
</tr>
<tr>
<td>2</td>
<td>12</td>
<td>199</td>
</tr>
<tr>
<td>2</td>
<td>6</td>
<td>140</td>
</tr>
</tbody>
</table>

Figure 3.1: Flexographic printing plates used for resin application. The first number denotes the dot-pitch and the second, the area coverage.
Figure 3.2: Frequency of droplet diameter for industrially blended strands. The bars represent the corresponding theoretical droplet diameter for the flexographic printing plates and are not frequencies. The first number denotes dot-pitch and the second, the area coverage. 
Ipm = lines per mm.
Figure 3.3: Comparison of droplet diameter for strands from Weyerhauser – Drayton Valley and blended at Forintek Canada Corp., 2/25 printing plate and various wire wound rod numbers.

Figure 3.4: Droplet pattern formed when printing on Douglas fir with PF resin FT57F (η = 260 cps) and wire wound rod #6 at a room relative humidity of 31.8%. The first number denotes the dot-pitch and the second, the area coverage.
Figure 3.5: Mean mass of resin deposited. The values at 6 and 50% area coverage are calculated from four samples. Those at 12 and 25% area coverage are calculated from three. The error bars denote 95% confidence intervals. Note: The value is the sum of the mass added to each substrate.

Figure 3.6: Effect of wire-wound-rod number, relative humidity, and resin viscosity on the mass of resin deposited on a glass slide (25 x 75 mm). The error bars denote 95% confidence intervals.
Figure 3.7: Variability of resin deposited on Douglas fir substrate (25 x 305 mm): (a) effect of resin viscosity, and (b) effect of relative humidity. The error bars denote 95% confidence intervals.
Figure 3.8: Schematic of Double Cantilever Beam specimen.

Figure 3.9: Typical cure cycle.
Figure 3.10: Effect of platen temperature on centre line temperature for 25.4 mm Douglas fir specimens. The centre-line temperatures above are the mean of six measurements along the length of one DCB specimen.
This chapter outlines the experimental procedure and data analysis used in the fracture toughness tests. Furthermore, it presents the results of these tests and an examination of the resultant fracture morphology. A relationship between fracture toughness and droplet diameter is outlined and industrial implications given.

4.1 TESTING PROCEDURE

The testing procedure is based on a combination of techniques used by Gagliano and Frazier (2001), by Blackman and Kinloch (2001), and recommendations by ASTM D3433-99: Standard Test Method for Fracture Strength in Cleavage of Adhesives in Bonded Metal Joints (2000). The testing was performed on an Instron Model 8500 PLUS Dynamic Testing System with WaveRunner™ software for data acquisition and test control. Crack growth was monitored using a travelling microscope (10x) with diluted liquid paper painted over the bond-line to increase contrast and make identification of the crack-front easier. Prior to loading, the free end of the specimen was supported to ensure the specimen was horizontal. Tests were completed under displacement control, which results in stable crack growth, with initial loading at 1 mm/min. Displacements were measured using a clip gauge with a maximum displacement of 10.5 mm. Once the crack propagated, the load was held constant for 45 seconds allowing the crack to reach a quasi-stable state. The point of arrest was marked and the sample unloaded until the clip gauge returned to zero displacement. This loading procedure was repeated on the specimen until it failed catastrophically or the maximum displacement of the clip gauge was reached. To ensure that fracture occurred within one minute from the onset of loading it was necessary to change the displacement rate between loading cycles. Subsequent crosshead speeds were obtained by dividing the previous crack mouth opening displacement (CMOD) obtained from the clip-gauge by one minute with maximum crosshead speeds on the load frame typically in the 7 to 8 mm/min range. The testing apparatus and test set-up is shown in Figure 4.1.
4.2 DATA ANALYSIS

Data analysis was performed using the linear elastic fracture mechanics compliance method. Three versions of this technique were evaluated:

1. Simple Beam Theory (SBT),
2. Corrected Beam Theory (CBT), and
3. Experimental Compliance Method (ECM).

4.2.1 Simple Beam Theory

All of the methods are based on the general relationship between \( G_{lc} \) and compliance,

\[
G_{lc} = \frac{P^2}{2B} \frac{dC}{da} \bigg|_{\text{at} \ \text{crack end}}
\]

(4.1)

where \( G_{lc} \) is the Mode I critical energy release rate, \( P \), the load, \( B \), the specimen width, \( C \), the specimen compliance, and \( a \), the crack length. For thin adhesive layers, simple beam theory shows that the rate of change of compliance with crack length can be expressed in terms of the specimen geometry and Young’s modulus,

\[
\frac{dC}{da} = \frac{8}{E_s B} \left( \frac{3a^2}{h^3} + \frac{1}{h} \right)
\]

(4.2)

where \( h \) is the thickness of one of the cantilevered arms and \( E_s \) is the Young’s modulus of the substrate obtained from a separate source such as literature or a tensile test. Combining the above equations yields:

\[
G_{lc} = \frac{4P^2}{E_s B^2} \left( \frac{3a^2}{h^3} + \frac{1}{h} \right)
\]

(4.3)
4.2.2 Corrected Beam Theory

Simple beam theory will underestimate the compliance of the DCB specimen. This is because the specimen is not perfectly built-in; some rotation of the loaded arms occurs at the crack tip since wood is not a perfectly stiff material. This rotation would not occur in a perfectly cantilevered beam where the beam is cantilevered to a material that does not deform under loading at the far end of the beam. By treating the beam as if it contained a slightly longer crack \((a + \Delta)\) this can be corrected. The additional length can be found by plotting the cube root of compliance, \(C^{1/3}\), as a function of crack length, \(a\). A linear fit for the data is found and \(\Delta\) is the negative x-intercept. Experiments showed that the additional length, \(\Delta\), which is dependent on the material and specimen geometry, is on average, \(27.8 \pm 6.3\) mm, at a 95% confidence level. Using this method, \(G_{lc}\) is calculated as:

\[
G_{lc} = \frac{3P \delta}{2B(a + |\Delta|)} \tag{4.4}
\]

where \(\delta\) is the crack mouth opening displacement (CMOD).

To determine the compliance, two methods were used: deviation from linearity (NL) and 5% increase of compliance or maximum load point (5% or MAX). The first method, NL, is based on the concept that a region of non-linearity typically precedes the maximum load. The point of deviation was determined by discounting any values below 5% of the maximum load, which removes any slack found in the system. Secants are then drawn from the first point on the load-displacement curve to each subsequent point and the slope calculated. The point of deviation from linearity was determined using the following equation and confirmed visually. The load and CMOD used in subsequent calculations were taken from that point.

\[
Deviation = \left| \frac{S_{i+1} - S_i}{S_{i+1} + S_i} \right| > \text{error} \tag{4.5}
\]

where \(S_{i+1}\) = slope of \(i^{th} + 1\) secant \(S_i\) = slope of \(i^{th}\) secant
Using the 5% or MAX method, the compliance was calculated at each point and compared to the previous compliance. The average of the compliance values where the deviation:

\[
\text{Deviation} = \left| \frac{(C_{i+1} - C_i)}{\frac{C_{i+1} + C_i}{2}} \right| < \text{error}
\]  

(4.6)

where \( C_{i+1} \) = compliance at point \( i+1 \)
\( C_i \) = compliance at point \( i \)

was taken as \( C_0 \). A new value, \( C_0 + (0.05)C_0 \) is then calculated and the intersection of a line with this slope and the load-displacement curve is determined. The values of CMOD and load at whichever point occurs first, the maximum load or the intersection point, are used in subsequent fracture toughness calculations. The error used ranged from 1 to 2% in the slope for NL and in the compliance for 5% or MAX. These methods are shown graphically in Figure 4.2. The \( C_0 \) and \( C_0 + 5\% \) lines do not pass through the origin due to slack within the system.

4.2.3 Experimental Compliance Method

The experimental compliance method is similar to CBT, however, the logarithm of compliance is plotted versus the logarithm of crack length with the compliance determined from either the NL or 5% or MAX method. \( G_{lc} \) is calculated as:

\[
G_{lc} = \frac{nPD}{2Ba}
\]  

(4.7)

where \( n \) is the slope of the log(C) versus log(\( a \)) plot with zero intercept. This method allows for the possibility that the relationship between the compliance and crack length is not necessarily cubic with \( n \) being on average 1.98 ± 0.30, at a 95% confidence level.

4.2.4 Selection of Method Used for Comparison

By combining the NL and 5% or MAX methods with the three alternative compliance techniques, there are six possible values for \( G_{lc} \) for each point, Figure 4.3. For ease of comparison, only one of these was chosen. Simple beam theory was discounted as it ignores
both the variability in the stiffness of the Douglas fir samples and that the DCB specimens are not perfectly built-in. The 5% or MAX method was not selected since more manipulation of the data is necessary, i.e. an initial compliance must be chosen from a curve that is not necessarily linear, Figure 4.2, as compared with the NL method. As well, in the 5% or MAX method, the crack mouth opening displacement, $\delta$, load, $P$, and slope, $n$, are based on the initial compliance whereas only the additional crack length, $\Delta$, is for the NL method. An examination of Figure 4.3 gives that the variability associated with the CBT technique is less than the ECM technique. Thus, all subsequent comparisons where one value of $G^{*}$ is reported are based on values obtained using the Corrected Beam Theory technique coupled with the selection of the load and displacement using the point of Non-Linearity method. The mean $G^{*}$ values reported in subsequent figures are based on 4 replicates with multiple measurements of fracture toughness on each replicate. The sample sizes range from 6 to 33 and are summarized in Table 4.1

An examination of Figure 4.3 also shows that the variability between the data analysis methods decreases with increased crack length. At long crack lengths the difference due to the basic assumptions of each method are reduced. For the SBT method the shear term, $1/h$ is reduced and the fracture toughness is based purely on bending of the load arm. For the CBT method, the percentage of the effective crack length due to the term, $\Delta$, is reduced and Equation 4.4 reduces to Equation 4.7 with $n = 3$.  

4.3 FRACTURE OF BONDED DCB SPECIMENS

As mentioned in Chapter 3, printing plates were used to deposit various droplet diameters and dot-pitches on the wood substrate with four replicates at each plate designation. Appendix A contains the design of experiments for both the bonded DCB specimens and the solid wood tests.

4.3.1 Validity of Data

It should be noted that test specimens DFTLAD-112-3, 125-3, 212-3, and 225-3, are not used in subsequent comparisons due to excessive resin, between 11 to 32 times typical values. In these specimens, the crack path quickly deviated from the centre-line (Figure 4.4) thus the fracture toughness measurements from these specimens are not that of an adhesive bond but rather solid Douglas fir after being subjected to the aforementioned cure cycle. This result
demonstrates that forcing the crack into the solid wood is quite simple provided enough resin is applied to the specimen.

An examination of the load-displacement curves reveals a change in fracture behaviour during certain tests, Figure 4.5a. Each load-unload sequence (one loop) on a load-CMOD (crack mouth opening displacement) curve gives a measure of the fracture toughness. In all cases, a minimum of three load-unload sequences and thus a minimum of three measures of the fracture toughness are used to determine the mean fracture toughness for a given specimen. Four replicates for each specimen give the sample sizes shown in Table 4.1. After a large growth in crack length, the compliance is too low to obtain accurate compliance data, and subsequently points associated with long crack lengths were discounted. This is supported by the behaviour of the load-displacement curve following the change in behaviour, which typically occurred after a substantial load drop and crack growth. As well, the R-curve, which is a plot of fracture toughness versus crack length, for the same test shows a considerable loss of fracture toughness, Figure 4.5b. The load-displacement and R-curves for all bonded DCB samples can be found in Appendix B with the data summarized in Figure 4.6.

4.3.2 Variability

Figure 4.6 to Figure 4.8 show that the variability of the fracture toughness increases as the area coverage and droplet size increases. There are three main factors that may cause this. The first is the variability associated with printing larger droplets. A misprint, i.e. a portion of the droplet or an entire droplet is not printed, at a larger droplet size will lead to a greater variation in the area coverage than a similar misprint at a smaller size. For example, if a misprint consisted of a half droplet then a misprint at 50% area coverage would lead to a reduction in area coverage of 25% as compared with a reduction from 6% to 3% at the lowest area coverage examined. Since fracture toughness increases with area coverage, this variation will lead to increased variability in fracture toughness at larger droplet sizes. The effect is compounded by the second factor, the surface morphology of the substrate. A larger droplet will cover more of the substrate surface and subsequently encounter a greater variation in surface morphology leading to increased variability in fracture toughness. The final factor is a direct result of increased wood failure at larger droplet sizes, which will be discussed in Section 4.5 Fracture Morphology. Since, the variability in the fracture toughness of solid wood is high (coefficients of variation on the order of 20 to 25 percent), an increased amount of wood failure at larger droplet sizes gives
increased variability in the reported fracture toughness value. The percentage error is relatively constant at 16 percent, although it is higher for the smallest droplet diameters of each dot-pitch and specimens with a plate designation of 2/25.

It can also be seen that the variability of specimens with a plate designation of 2/25 is high compared with the other specimens, Figure 4.6 to Figure 4.8. This demonstrates the effect of growth rings on the fracture toughness in the TL direction. Upon planing the specimens, a distinct earlywood/latewood pattern is seen with ridges of latewood protruding from the bonded surface of the substrate, Figure 4.9. As the difference between the earlywood and latewood becomes more pronounced, the fracture toughness increases. Therefore, any set of specimens that exhibit a wide range of growth ring patterns will exhibit a wide range of fracture toughness. While the effect of growth ring pattern on fracture toughness is not unique to the 2/25 series it is the most pronounced and explains the high variability of this series as compared with the others. Since the specimens are pressed under displacement control, it is speculated that the increased thickness of the latewood leads to a greater local pressure and subsequently more intimate contact between adherends, creating a stronger bond.

4.3.3 Effect of Droplet Diameter and Spacing

The fracture toughness as a function of theoretical droplet diameter is shown in Figure 4.7. Least-square straight-line fits to the means were made to both data sets. To determine the least-square fits, the data point for a dot-pitch of 1-lpmm and 6% area coverage ($D = 281 \mu m$) was discounted since it is statistically zero based on a 95% confidence interval. As well, the fracture toughness could diminish to zero at any point below 12% area coverage ($D = 397 \mu m$). To determine the minimum droplet diameter for the 1-lpmm dot-pitch printing plates in this range (from 12% to 6% area coverage) must be examined. Without knowledge of the absolute minimum droplet diameter the data point for a dot-pitch of 1-lpmm and 6% area coverage was discounted in all subsequent least-square fits. Figure 4.7 shows that $G_{fc}$ appears to be proportional to the theoretical droplet diameter for a given dot-pitch. A minimum droplet diameter for each dot-pitch below which an adhesive bond cannot be formed ($G_{fc} = 0$) is shown and summarized in Table 4.2. Comparing the $G_{fc}$ values for the two dot-pitches at a droplet diameter of approximately 400 $\mu m$ shows that dot-pitch significantly affects fracture toughness.
4.3.4 Effect of Area Coverage

The effect of area coverage on fracture toughness is shown in Figure 4.8. The curves were calculated using a least-square fit to the following equation:

\[ G_{lc} = m\sqrt{A - b} \]  

(4.8)

where \( G_{lc} \) is the fracture toughness, \( A \), the area coverage, and \( m \) and \( b \) are fitting parameters. The parameter \( b \) corresponds to the minimum area coverage required for bond formation. The square-root relationship in Equation 4.8 is justified based on the linear relationship with droplet diameter, \( D \), shown in Figure 4.7. It is clear that an increase in area coverage leads to a corresponding increase in fracture toughness with the values at a dot-pitch 2 and 1-lpmm being significantly different. This shows a strong correlation between fracture toughness and the area of the substrate covered by resin regardless of droplet diameter or spacing. However, Figure 4.8 shows that droplets should be closer together for the same area coverage to achieve a high fracture toughness. The minimum area coverage required to form an adhesive bond and the corresponding droplet diameters are given in Table 4.2.

4.3.5 Resin Efficiency

While the increase in fracture toughness with increasing droplet diameter and decreasing dot-pitch is significant, the figures can be misleading since this appears to continue without the penalty of increased resin consumption until a continuous bond-line is achieved. However, in industry, a continuous bond-line is not feasible due to the large area to be bonded in OSB. Therefore, a measure of resin efficiency is required. Resin efficiency is defined in this work as the fracture toughness, \( G_{lc} \), divided by the mass of resin deposited on the substrate, \( m_r \), and has the units J/m\(^2\)/mg. The plots of resin efficiency versus area coverage and droplet diameter are shown in Figure 4.10. The resin efficiency follows similar trends to that of fracture toughness, where resin efficiency increases with area coverage, droplet diameter and decreasing dot-pitch. Again, a minimum droplet size can be determined, Table 4.2. There are two key features that differ between the plots of resin efficiency and fracture toughness. First, at the 50 percent area coverage, the droplets spaced farther apart have a higher efficiency. However, the variability of the 1-lpmm specimen is quite high so this result is not conclusive. Second, at 2-lpmm the curve is significantly different than the curves in Figure 4.7 and Figure 4.8. In Section 3.3.3 Mass of Resin Applied, it was stated that the mass of resin should have no effect on subsequent fracture
toughness values, as the mass of resin was statistically equivalent for all area coverages. If fracture toughness was independent of the resin mass deposited then the resin efficiency curves, Figure 4.10, should be nearly identical to that for fracture toughness, Figure 4.7 and Figure 4.8. However, this is not the case. Therefore, the resin mass deposited on the substrate may have a minor effect on the fracture toughness, Figure 4.11, although no conclusive trends can be observed. From Figure 4.11 it appears that fracture toughness decreases with increasing resin mass deposited in the range studied. One would typically expect the opposite to be true; increased resin mass deposited gives increased fracture toughness. For the discontinuous wood/adhesive bonds in this study, increasing resin mass does not correlate to increased fracture toughness which suggests that the mechanical properties of oriented strand board could possibly be improved simply by modifying the droplet diameter and dot-pitch while maintaining or even decreasing the current resin consumption.

4.4 FRACTURE OF SOLID WOOD

Before the performance of an adhesive bond can be determined, a baseline must be established against which it can be measured. In this case, the baseline chosen is solid Douglas Fir with the crack propagating in TL-direction. A typical load-displacement curve and the corresponding R-curve are shown in Figure 4.12. The solid wood load-displacement and R-curves can be found in Appendix C with the results of all samples summarized in Figure 4.13. The variability in the fracture toughness results is derived from two components: the inherent variability of wood and deviation of the crack from the centre-line of the specimen. The first is a natural source of variation although care was taken to reduce it as much as possible. To remove the second source, only specimens DFTLSW – 9 and 13 are used in comparison with the bonded DCB specimens since the cracks remained along the centre for these specimens. Specimen 9 is compared to one where the crack deviates (DFTLSW – 12) in Figure 4.14. Once the crack deviates from the centre-line of a DCB specimen the data analysis techniques used here become invalid.

4.4.1 Solid Wood – Bonded Joint Comparison

One of the goals of a bonded joint is to obtain a fracture toughness equal to or greater than the substrate. Thus, a comparison of the bonded joint DCB results and those for solid Douglas fir is shown in Figure 4.15. As the droplet size increases for a given dot-pitch the
retention of solid wood fracture toughness, which is defined as the fracture toughness of an adhesively bonded specimen divided by the fracture toughness of solid wood, increases to a maximum of 75 ± 10%. This comparison is against solid wood tested at room temperature, i.e. not subjected to the cure cycle used for the adhesive joints. Since the wood composite industry is moving toward the production of lumber-like products, this comparison is justified as the wood composites are in direct competition with solid sawn lumber.

4.5 FRACTURE MORPHOLOGY

An examination of the fracture morphology, Figure 4.16, gives insight into the increase in fracture toughness with droplet size. As the droplet size increases the amount of solid wood failure increases as measured by ASTM D5266-99: Standard Practice for Estimating the Percentage of Wood Failure in Adhesive Bonded Joints (2000) and shown in Figure 4.17 and Figure 4.18. The depth of wood fracture also increases. In Figure 4.18, a pattern emerges in the location of solid wood failure – it tends to occur in the latewood. This is likely due to the difference in thickness between the early and latewood, with the latewood being thicker, as previously shown in Figure 4.9. During pressing, the latewood ridges will be pressed together more tightly and subsequently produce stronger bonds. Comparing these results to the retention of solid wood fracture toughness, Figure 4.19, it is evident that the area of solid wood failure is directly related to the amount of fracture toughness retained. This relationship between the area of solid wood failure and the retention of solid wood fracture toughness is similar to that for area coverage and fracture toughness. Therefore, a least-square square-root relationship should adequately describe the data as shown in Figure 4.19. Retention of approximately 100% of the solid wood fracture toughness is obtained at 100% solid wood failure at 2-lpmm. This is not the case for 1-lpmm, where 100% solid wood failure gives solid wood fracture toughness retention of only 75%. It is hypothesized that this may be due to the depth of wood failure mentioned previously. At 2-lpmm wood failure occurred away from the bond-line and thus the crack behaved in a manner similar to that of solid wood. However, at 1-lpmm, the wood failure was shallow, typically returning to the bond-line and subsequently did not behave in a manner similar to solid wood. Further research at the microscopic level is needed to confirm this hypothesis.
4.6 DISCUSSION

Examination of Figure 4.7 shows a linear relationship between fracture toughness and droplet diameter, \( D \), for a given dot-pitch. Since, the bonded area is a function of \( D^2 \), the relationship between fracture toughness and area coverage should be described by a square root relationship. This fit was shown in Figure 4.8. Subsequently, a plot of \( G_{lc}^2 \) versus area coverage should be linear and examination of such a plot, Figure 4.20, shows that this is indeed the case. The same relationship also holds when the fracture morphology is examined. Figure 4.19 shows the correlation between the retention of solid wood fracture toughness and the percentage of solid wood failure. Again, the correlation is described by a square root relationship and a plot of the retention of solid wood fracture squared versus the area of solid wood failed, Figure 4.21, is approximately linear. These experimentally determined relationships are summarized in Equations 4.9 to 4.12,

\[
G_{lc} \propto D, \tag{4.9}
\]

\[
A_{bond} = \frac{\pi D^2}{4}, \tag{4.10}
\]

\[
G_{lc}^2 \propto A_{bond}, \text{ and} \tag{4.11}
\]

\[
\left( \frac{G_{lc}}{G_{lc,sw}} \right)^2 \propto A_{sw}, \tag{4.12}
\]

where \( G_{lc} \) is the bonded specimen fracture toughness, \( D \), the droplet diameter, \( A_{bond} \), the bonded area or area coverage, \( G_{lc,sw} \), the solid wood fracture toughness, and \( A_{sw} \), the area of solid wood failure.

The above argument relies on the fact that \( G_{lc} \) is proportional to the droplet diameter. The fracture toughness, \( G_{lc} \), can be described as the energy required to increase the crack surface and subsequently has the units J/m\(^2\). Since the bonded area is proportional to \( D^2 \), it is expected that \( G_{lc} \) should also be proportional to \( D^2 \). However, assume that on initiation the crack front is always within a droplet, which appears reasonable since it is unlikely the crack will arrest in non-bonded region. Consider a slice of bonded area of thickness \( A_{\Delta} \), Figure 4.22. The \( G_{lc} \) of the system should be proportional to the \( G_{lc} \) of the bond and the percentage of the area covered by
that bond. Based on Figure 4.22, the subsequent equations, 4.13 to 4.18, show how the linear relationship between the $G_Ic$ of the system and diameter arises.

$$G_{lc\ (system)} \propto G_{lc\ (bond)} \frac{A_{bond}}{A_{total}}$$ (4.13)

$$A_{bond} = \frac{1}{2} r^2 (\theta_1 - \sin \theta_1) - \frac{1}{2} r^2 (\theta_2 - \sin \theta_2)$$ (4.14)

Rearranging gives,

$$A_{bond} = \frac{1}{2} r^2 (\theta_1 - \theta_2 - \sin \theta_1 + \sin \theta_2)$$ (4.15)

$$A_{total} = rW \left( \cos \frac{\theta_2}{2} - \cos \frac{\theta_1}{2} \right)$$ (4.16)

$$\frac{A_{bond}}{A_{total}} = \frac{1}{2} \frac{r^2}{rW} \left( \theta_1 - \theta_2 - \sin \theta_1 + \sin \theta_2 \right)$$ (4.17)

Substituting $D/2$ for $r$,

$$\frac{A_{bond}}{A_{total}} = \frac{1}{4} \frac{D}{W} \left( \theta_1 - \theta_2 - \sin \theta_1 + \sin \theta_2 \right)$$ (4.18)

Equation 4.18 gives that a limit of $A_{bond}/A_{total} = 0.785$ is obtained when $D/W$ is set to 1, $\theta_1$, to $180^\circ$, and $\theta_2$ to $0^\circ$, i.e., the full half-circle is described. This is equivalent to:

$$\frac{A_{circle}}{A_{square}} = \frac{\pi D^2}{4W^2} \Rightarrow \frac{\pi}{4} \left( \frac{D}{W} \right)^2 = 0.785$$ (4.19)

Setting $D/W$ to 1, also allows the bond fracture toughness to be estimated from the system fracture toughness, Figure 4.23. If the experimental data are extrapolated to $D/W = 1$ a bond fracture toughness of 250 to 330 J/m$^2$ is obtained and is similar to the measured solid wood fracture toughness of 324 J/m$^2$. Therefore, once the droplets form a continuous line, i.e., their edges touch; the fracture toughness of solid wood is achieved. However, this continuous line does not correspond to complete coverage of the substrate with resin but rather the maximum
78.5% area coverage as described in Equations 4.18 and 4.19. Provided the resin has sufficient fracture toughness this is also the maximum fracture toughness that can be obtained by the system, assuming the resin does not reinforce the wood, which supports Equation 4.12. As well, those specimens where excess resin was applied failed in the solid wood. Therefore, the maximum fracture toughness of the adhesive bond must be equal to the fracture toughness of the substrate. Since the maximum fracture toughness of the discontinuous bond is similar to that of solid wood, the relationship between the bond fracture toughness and the area bonded should be similar to that for the area of solid wood failed and the retention of solid wood fracture toughness. For the minimum droplet sizes shown in Table 4.2, the ratio of the bonded area to the total area is 0.11 to 0.27 for a dot-pitch of 2-lpmm and 0.33 to 0.41 for a dot-pitch of 1-lpmm, Figure 4.24. This suggests that for any dot-pitch, when the ratio of bonded area to the total area is less than approximately 0.3 there is insufficient bonded area to form a bond. If this is true then Equation 4.18 gives that the minimum droplet diameter for a dot-pitch 1-lpmm should be twice that for 2-lpmm \((W\rightarrow2W)\). The data in Table 4.2 do not clearly support this. Also, increasing the space between droplets \((W\rightarrow2W)\) will also lead to the slope of the linear fit for a dot-pitch of 2-lpmm being twice that of 1-lpmm. Again, the data does not clearly support this although the relationship is close, e.g., \(m = 0.841\) for 2-lpmm and \(m = 0.371\) for 1-lpmm. The discrepancy between theory and experimental data is likely due to the variability associated with the specimens as discussed in Section 4.3.2 Variability. To determine if \(A_{\text{bond}}/A_{\text{total}}\) is constant for a given wood/resin system, more dot-pitches need to be studied.

The implications of these results are the following. If the ratio of bonded area to total area below which an adhesive bond will not form is constant then the minimum droplet diameter for any dot-pitch can be calculated by setting \(W\) to 1/dot-pitch, \(\theta_1\), to 180°, and \(\theta_2\) to 0°. Secondly, if \(G_{lc}\) is proportional to internal bond (IB) strength then the internal bond strength could possibly be obtained simply by measuring the area of solid wood failed when a bond is opened. Since the IB test is used for quality control this would simplify the process as a specimen need only be fractured and the area of solid wood failure determined. However, the variability associated with such a qualitative test will likely be greater than the quantitative load measurement currently in practice. Finally, the area of solid wood failure is related to the durability of a bond (Feihl et al., 1977). Therefore, the life span of the board can be inferred by fracturing a sample a measuring the area of solid wood failure.
4.7 SUMMARY

Based on the above results it appears that fracture toughness is independent of, or decreases with, resin mass deposited. Additionally, the experimental results suggest a linear relationship between $G_{lc}$ and the theoretical droplet diameter. The physical basis for this is that for a given increase in crack length, $\Delta a$, the fracture toughness of the system is proportional to the fracture toughness of the bond and the ratio of the area bonded to the total fracture area. This ratio is proportional to the droplet diameter which gives rise to the linearity between $G_{lc}$ and droplet diameter. The linearity leads to a square root relationship between $G_{lc}$ and bonded area. A similar relationship was found between the area of solid wood failure and the retention of solid wood fracture toughness. A minimum droplet size for each dot-pitch was also found. A comparison of droplet diameter with the ratio of bonded area to total area shows that a minimum ratio is required for bond integrity. Therefore, the minimum droplet size for any spacing can be calculated. Overall, the results suggest that the mechanical properties of oriented strand board can be improved while maintaining or perhaps decreasing resin consumption by modifying the droplet diameter and dot-pitch.
### Table 4.1: Sample size for calculation of mean fracture toughness values.

<table>
<thead>
<tr>
<th>Specimen (Dot-pitch / Area Coverage)</th>
<th>Sample Size (n)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2/6</td>
<td>10</td>
</tr>
<tr>
<td>2/12</td>
<td>26</td>
</tr>
<tr>
<td>2/25</td>
<td>11</td>
</tr>
<tr>
<td>2/50</td>
<td>23</td>
</tr>
<tr>
<td>1/6</td>
<td>6</td>
</tr>
<tr>
<td>1/12</td>
<td>13</td>
</tr>
<tr>
<td>1/25</td>
<td>22</td>
</tr>
<tr>
<td>1/50</td>
<td>33</td>
</tr>
</tbody>
</table>

### Table 4.2: Minimum droplet diameter estimates from various plots.

<table>
<thead>
<tr>
<th>Figure</th>
<th>Dot-pitch (lpmm)</th>
<th>Minimum Droplet Diameter (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Droplet Diameter vs G_{ic} (Figure 4.7)</td>
<td>2</td>
<td>112</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>339</td>
</tr>
<tr>
<td>Area coverage vs G_{ic} (Figure 4.8)</td>
<td>2</td>
<td>139 (5.3% area coverage)</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>388 (11.4% area coverage)</td>
</tr>
<tr>
<td>Droplet Diameter vs Resin Efficiency (Figure 4.10a)</td>
<td>2</td>
<td>56</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>384</td>
</tr>
<tr>
<td>Area coverage vs Resin Efficiency (Figure 4.10b)</td>
<td>2</td>
<td>136 (5.6% area coverage)</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>395 (11.9% area coverage)</td>
</tr>
<tr>
<td>Area coverage vs G_{ic}² (Figure 4.20)</td>
<td>2</td>
<td>132 (5.3% area coverage)</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>418 (13.3% area coverage)</td>
</tr>
</tbody>
</table>
Figure 4.1: Double cantilever beam test set-up: (a) full set-up, and (b) close-up of loading jig.
Figure 4.2: Sample load-displacement curve for an adhesive DCB test (DFTLAD-250-4a).

Figure 4.3: Sample R-curve (DFTLAD-212-2).
Figure 4.4: Crack path of DCB sample (DFTLAD-112-3) with excess resin.

Figure 4.5: Load-CMOD (crack mouth opening displacement) and R-curves for DFTLAD-225-4 showing change in specimen behaviour.

Figure 4.6: Summary of bonded DCB results. The error bars denote 95% confidence intervals. The first number denotes the dot-pitch and the second, the area coverage.
Figure 4.7: Effect of theoretical droplet diameter on $G_{tc}$. The theoretical droplet diameter is based on dot pitch and area coverage. Least-square straight-line fits were made to each data set. The error bars denote 95% confidence intervals.
Figure 4.8: Effect of area coverage on $G_{lc}$. The curves are least-square fits of a square root relationship. The error bars denote 95% confidence intervals.

Figure 4.9: End view of bonded DCB specimens: (a) DFTLAD-225-1, (b) DFTLAD-225-2, and (c) DFTLAD-225-4. Fracture toughness increases from (a) to (c). Note: the top of the specimen is the bonded surface.
Figure 4.10: Resin efficiency, fracture toughness per mg of resin used in the bond, versus: (a) droplet diameter, and (b) area coverage. The curves are described by least-square linear and square root fits respectively. The error bars denote 95% confidence intervals.
Figure 4.11: Effect of resin mass deposited on $G_{ic}$. The error bars denote 95% confidence intervals.
Figure 4.12: Typical results for solid Douglas fir (Specimen DFTLSW-9): (a) Load-CMOD (crack mouth opening displacement) curve, and (b) R-curve.
Figure 4.13: **Summary of solid wood fracture tests.** The error bars denote 95% confidence intervals for $G_{ic}$ (8 to 12 replicates each).

Figure 4.14: **Crack path for solid Douglas fir specimens:** (a) DFTLSW - 9: crack remains along centre line, and (b) DFTLSW - 12: crack deviates from centre.
Figure 4.15: Retention of solid wood fracture toughness ($G_{ic}/G_{ic \text{ solid wood}}$): fracture toughness of bonded specimens relative to fracture toughness of solid wood. The first number for each specimen denotes the dot-pitch and the second, the area coverage.

Figure 4.16: Fracture surface of bonded DCB specimens. The first number denotes the dot-pitch and the second, the area coverage.
Figure 4.17: Percentage of wood failure for adhesive bonded DCB specimens. The first number for each specimen denotes the dot-pitch and the second, the area coverage. The error bars denote 95% confidence intervals.
Figure 4.18: Fracture patterns of bonded DCB specimens. The grey area represents solid wood failure as determined by ASTM D5266 (2000). The upper side of each square represents the end of the starter notch. Each square is 25.4 x 25.4 mm. The first number denotes the dot-pitch and the second, the area coverage.
Figure 4.19: *Retention of solid wood fracture toughness as compared with percentage of solid wood failure in a bonded DCB.* The curves are least-square square root fits. The error bars denote 95% confidence intervals.

Figure 4.20: *Effect of area coverage on $G_{lc}^2$.* The curve is a least-square linear fit. The error bars denote 95% confidence intervals.
Figure 4.21: Correlation between the retention of solid wood fracture toughness and the area of solid wood failure. The data is described by a least-square straight-line fit.

Figure 4.22: Schematic of bonded area for one droplet (unit cell).
Figure 4.23: Effect of D/W on $G_{ic}$. Least-square straight-line fits were applied to the data. The error bars denote 95% confidence intervals.

Figure 4.24: Effect of droplet diameter on the ratio of bonded area to total area. The minimum ratio of bonded area to total area can be seen for each dot-pitch.
5 – BLENDING DYNAMICS OF OSB

The effect of droplet diameter and dot-pitch on fracture toughness has provided a relationship between the area fractured and the fracture toughness of a discontinuous bond. However, the droplet diameter and dot-pitch were controlled through a flexographic printing technique. This is not the manufacturing method used in industry. Therefore, the physics of industrial resin application must be understood. Knowledge of this will allow operation of the rotary drum blender such that the desired droplet diameter and dot-pitch based on fracture results can be obtained. The limited research done on blending focuses on the effect of resin dispersion, the variation of resin droplet size produced by the atomizer, and distribution, the spatial distribution of the resin on the strand, on the mechanical properties with little work for the method of obtaining the desired resin coat. This chapter presents a literature review of blending dynamics of strands for oriented strand board and identifies key parameters that affect the dispersion and distribution of resin and additives within a rotary drum blender. This chapter outlines preliminary experimentation in this area and includes a comparison of the experimental results with modelling work.

5.1 LITERATURE REVIEW

The objective of blending during the manufacture of OSB is to coat the strands with a resin and wax dispersion and distribution such that the intrinsic properties of the wood are realized in the composite board. This is accomplished using a rotary drum blender. Typical blenders, schematically shown in Figure 5.1, consist of a rotating drum and a distribution system for resin, wax, and other additives. The rotating drum can be divided into three sections: the feed chute, the drum, and the exit. The feed chute is designed to provide a consistent flow of wood strands to the rotating drum portion of the blender. The interior of the steel drum is circular with evenly spaced flights and is lined with high-density polyethylene. The drum is inclined 1° to 4° to the horizontal plane to move the strands down its length and typically rotates between 12 to 25 rpm. The flights, and the centripetal force created through the rotation of the drum, lift the strands to the top of the drum during a revolution. Upon reaching the zenith of the drum, gravity causes the strands to fall through the drum center and the mist of resin and wax. The randomly tumbling strands are coated and the process is repeated several times along the length of the drum. Similar to the feed chute, the exit is designed to provide a consistent flow of material
from the drum, preventing a build-up of strands. A modification to the feed chute and exit that prevents this is the use of two or three long flights that protrude into these sections and scrape strands off the interior wall.

The resin, wax and additive distribution system contains a header and several spinning disc atomizers or spray nozzles positioned along the length of the drum. The header allows the selection of different flow rates and hence different spray characteristics for each applicator. The atomizers and nozzles partially control the dispersion and distribution of the resin and wax droplets, forming the mist through which the strands tumble.

5.1.1 Blender Operation

As mentioned, the objective of blending is to coat the wood strands such that a composite board product with the optimal properties for a given application is achieved. Several researchers (Carroll and McVey, 1962; Christensen and Robitschek, 1974; Kamke et al., 1996a; Lehmann, 1965) have reported that the properties of the board are enhanced by an evenly distributed (both on the strand and between strands) and finely dispersed resin coating. The combination of distribution and dispersion is known as the resin efficiency, not to be confused with the term outlined in Chapter 4, and a greater efficiency leads to increased continuity of the resin film formed between strands. A discontinuous film composed of resin droplets will have stress concentrations associated with each resin spot. Therefore, the local stress at each wood/adhesive interface would be greater than that experienced in a board with a continuous glue-line, giving reduced board properties, particularly internal bond strength (Kamke et al., 1996a). The distribution and dispersion of resin are controlled by the operation of the blender; hence, it is desirable to relate the operating parameters of the blender to their effect on the resin coating.

Examination of a rotary drum blender, Figure 5.1, suggests that the following parameters will be significant in controlling the resin coating on the strands:

1. the angle of the drum to the horizontal plane or tilt,
2. the revolution speed of the drum,
3. the number, spacing, shape and size of the blender flights,
4. the quantity of strands within or “loading” of the drum,
5. the ability of the interior of the drum to resist strand and resin build-up.
Both the angle of the drum and its rotation speed will affect the residence time of the strands in the drum. Increased retention time will increase the amount of resin applied to each strand, which in turn will enhance the mechanical properties of the finished board. This is due to increased continuity of the glue line between strands (Furuno et al., 1983). As the resin content increases, the glue lines shift from a small, widely spaced distribution to spotty, eventually covering the entire strand at approximately 8 to 10 weight percent resin. Both the modulus of rupture and internal bond strength increase with increasing resin content (Avramidis and Smith, 1989; Burrows, 1961; Generalla et al., 1989; Lehmann, 1970). However, at higher levels of resin, diminishing returns are experienced (Avramidis and Smith, 1989; Shimizu and Okuma, 1981). Increased retention time will reduce the variability in the resin coverage (Kamke et al., 1996a). Furuno et al. (1983) found that a more uniform resin distribution is also provided by increasing blender speed. However, this has the detrimental effect of reducing retention time. As well, the rotation speed will be a factor in determining the location of strand tumble initiation, as the greater the speed, the higher the point at which strands will begin falling.

Increased agitation within the drum will increase the contact between the strands and therefore give improved board characteristics (Youngquist et al., 1987). The increased rubbing will predominantly affect the efficiency of powdered resin distribution. The effect is lessened for liquid resins and it is virtually non-existent for polymeric methylene diisocyanate (pMDI) based resins. A drawback of greater agitation is the increased breakage of the strands. However, this increased breakage can be reduced by using strands with higher moisture content due to their tendency to bend rather than break (Phillips et al., 1991).

The blender flight properties will partially determine the point of tumble initiation. Higher flights will retain more wood strands for a longer period on the climb up the drum wall. The shape of the flight will likely influence the speed with which the drum interior becomes blocked with strands and resin; bull-nosed flights will become congested more slowly. No research has been done on the effect of blender flights on resin dispersion and distribution.

The quantity of strands within the drum will affect the overall tumbling characteristics and hence the resin distribution. Furuno et al. (1983) varied the loading of the drum, from 1 to 3 kg, while drum speed (14 rpm), resin content (4.5wt% of ovendry flakes) and flake moisture content (2 to 3%) were kept constant. They found that the 2 kg load had the greatest amount of
resin, while the 3kg load of strands had the least. As the dimensions of the drum are not given, the percentage capacity cannot be calculated and therefore, these results require confirmation and further examination.

The drum wall material is related to the efficiency of the flights within the drum. As the strands, fines and resin adhere to and build-up on the flights, their effective size is reduced and operating procedures for the blender should be adjusted to accommodate this. The friction characteristics of the drum material will affect the tumbling action and the rate at which the flights become clogged.

5.1.1.1 Atomization

The interaction of the tumbling strands within a blender and the resin curtain affects the resin distribution and subsequently, board properties. This can be modified, for example, by reducing the resin flow to the first and last applicator to reduce the amount of resin sprayed onto the interior wall of the drum thereby reducing build-up and preventing blockage of the feed chute and drum exit. The performance of the atomizer within a blender is linked to its ability to consistently form droplets of a uniform size. Some parameters related to the resin applicators are:

1. the type of applicator (spray nozzle or spinning disc atomizer),
2. the location, spacing and number of the applicators,
3. the size of the resin droplet, and
4. the order and type of additive application.

Each type of applicator will have distinctive distribution and dispersion properties and solutions that optimize the operation of one applicator will not necessarily be applicable to the other. For example, at equal resin addition levels, spinning disk atomizers will give a greater resin coverage than air atomizers (Kamke et al., 1996a).

For spray nozzles, droplet formation is dependant on the flow of liquid past the orifice. Atomization is accomplished by turbulent flow which is promoted by (Lefebvre, 1989):

1. large passage diameters,
2. changes in flow velocity and direction,
3. abrupt changes in cross-sectional area,
4. surface roughness,
5. imperfections in atomizer geometry,
6. mechanical vibrations,
7. low liquid viscosity, and
8. high liquid velocity.

The location, spacing and number of the applicators will determine the overall resin pattern along the drum length. Figure 5.2 compares the relative amount of adhesive, variation of flow rate and form of the spray zone against the drum wall for three nozzle configurations.

In contrast, spinning disc atomizers have three distinct modes of atomization: direct drop formation, ligament formation and film formation (Lefebvre, 1989). Direct drop formation corresponds to low flow rates where the liquid spreads across the disc surface and is thrown off in drops of approximately the same size. On increasing the flow rate, to approximately 28 times that for direct drop formation, unstable ligaments are formed at the edge of the disc. These disintegrate into drops at some distance from the periphery as illustrated by Figure 5.3. Atomization by ligament formation also leads to a relatively uniform droplet size. Further increasing the flow rate increases both the number and thickness of the ligaments to a saturation value. At this point, a film that extends past the rim of the disc is created. When this film collapses, it forms randomly sized ligaments, leading to significant variations in droplet size (Lefebvre, 1989). The critical flow rates depend on the size and geometry of the cup, cup rotational speed, liquid flow rate, and the physical properties of the liquid. The governing equations for each mode are given in Equations 5.1 to 5.4.

1. Direct drop formation:

\[ q \leq 2.8 \left( \frac{D}{n} \right)^{2/3} \left( \frac{\sigma}{\rho L} \right) \left[ 1 + 10 \left( \frac{\mu L}{(\rho L \sigma D)^{0.5}} \right)^{1/3} \right]^{-1} \]  

(5.1)

where \( q \) is the volume flow rate (cm\(^3\)/s), \( D \), the disc diameter (cm), \( n \), the rotational speed (rpm), \( \mu L \), the dynamic viscosity of liquid (dyn/cm\(^2\)), \( \rho L \), the liquid density (g/cm\(^3\)), and \( \sigma \), the surface tension (dyn/cm).
2. Ligament formation:

\[ q = 80 \left( \frac{D}{n} \right)^{2/3} \left( \frac{\sigma}{\rho_L} \right) \left[ 1 + 10 \left( \frac{\mu_L}{(\rho_L \sigma)^{1/5}} \right)^{1/3} \right]^{-1} \]  

(5.2)

3. Film formation:

\[ q \geq 5.3 \left( \frac{D}{n} \right)^{2/3} \left( \frac{\sigma}{\rho_L} \right) (\mu_L)^{1/3} \]  

for \( \frac{D \rho_L}{\mu_L} < 30 \text{ cm/s} \)  

(5.3)

\[ q = 20(D)^{1/2} \left( \frac{1}{n} \right)^{2/3} \left( \frac{\sigma}{\rho_L} \right)^{5/6} \]  

for \( \frac{D \rho_L}{\mu_L} > 30 \text{ cm/s} \)  

(5.4)

5.1.1.2 Forces Acting on the Strands during Blending

To obtain an insight into the forces acting on strands during blending, classical rotational physics is used. Assuming that the strands move as coherent block rather than individual pieces, and are in contact with the drum wall or in motion, only three forces are required to account for the motion of the strands within a blender, as shown in Figure 5.4. These are:

1. Gravitational:  
   \[ F_g = mg \]  
   (5.5)

2. Centripetal:  
   \[ F_\omega = m \omega^2 r \]  
   (5.6)

3. Frictional:  
   \[ F_f = \mu F_n \]  
   (5.7)

where \( m \) is the mass of the object (kg), \( \omega \) the angular velocity (rad/s), \( r \), the radius (m), \( \mu \), the coefficient of friction, and \( F_n \), the normal force.

Two scenarios for the calculation of forces on the strands within a blender are possible: with and without flights. These forces will determine the point of tumble initiation. As can be seen from Figure 5.4, tumble initiation will occur when either the force due to gravity overcomes the vertical elements of the frictional and centripetal forces or horizontal force due to friction exceeds the horizontal component of the centripetal force.
Examination of the force balance in the vertical direction, given in Equation 5.8, and the horizontal direction, Equation 5.9, shows that the mass of the strands cancels out, and that for a blender of given radius and material, the only variables are the angular velocity, \( \omega \), and the tumble angle, \( \theta \).

\[
F_g = F_a \sin \theta + F_f \sin(90 - \theta)
\]

\[
\Rightarrow mg = m\omega^2 r \sin \theta + \mu m\omega^2 r (90 - \theta), \quad \text{and} \quad (5.8)
\]

\[
F_f \cos(90 - \theta) = F_a \cos \theta
\]

\[
\Rightarrow \mu m\omega^2 r \cos(90 - \theta) = m\omega^2 r \cos \theta. \quad (5.9)
\]

Since, the angular velocity can be controlled the tumble angle can be predicted. Adding flights will modify the forces acting on a strand in the blender as shown in Figure 5.5. The assumptions are also modified such that the drum wall is replaced by the flight.

5.1.1.3 Tumbling Regimes

To describe the motion of a charge of strands within the blender, terms for the various tumbling regimes are taken from literature (Henein, 1981). These are slipping, rolling, cascading, cataracting and centrifuging, shown in Figure 5.6, and occur at increasing rotational speeds. Slipping occurs at low rotational speeds and the strands will remain in the lower half of the drum either oscillating back and forth or appearing to remain motionless against the rotating drum wall. No mixing of the strands occurs and no resin will be applied with the current industrial blender design. Rolling arises when motion is seen in the strands. The top layer of strands will pass over the lower layers and mixing will occur. However, the strands remain in the lower half of the drum and subsequently no resin is applied. Cascading is an advanced stage of rolling where the mass of strands assumes a kidney shape and the mixing occurs by periodic slumping of the upper half rather than continuous mixing as takes place in rolling. Increasing the rotational speed further, the boundary between cascading and cataracting is reached. This point is described as the rotational speed where a particle projected from the top of the charge will land at the midpoint of the main body of the charge. Below this point, the strands are in the cascading regime, above this, the cataracting regime. The cataracting regime shows erratic behaviour with
the majority of strands in free-fall during one revolution. Based on current industrial practice, this is the most desirable tumbling regime for oriented strand board blending. The final tumbling regime is centrifuging where the strands do not lose contact with the drum wall for the entire revolution. The critical speed for centrifuging is given by Equation 5.10.

\[
\omega_{cr} = \frac{42.3}{\sqrt{D}}
\]  

(5.10)

where \( \omega_{cr} \) is the critical speed for centrifuging (rpm) and \( D \) is the diameter of the blender (m).

### 5.1.2 Modelling

Currently, no models describe the tumbling of strands within a rotary drum blender. Therefore, preliminary models must be created from first principles or based on models for similar phenomena in other fields. A model based on ball-milling (Davis, 1919) can be used as a basis. Contrary to the above discussion, the model only considers the centripetal and gravitational forces, ignoring friction. However, the assumptions that the strands move as a block, and that the strands are either in contact with the drum wall or in motion are still valid. The Davis model divides the blender into two sections, Figure 5.7. In the first, the strands move along a circular path and there is no relative motion between the strands and the drum wall. In the second, the strands are in free fall following a parabolic path. The boundary between the circular and parabolic sections is described by the following curves:

**Curve aO**

\[
\cos \alpha = \frac{1.266n^2 r}{3600}
\]

(5.11)

**Curve Od**

\[
x = 4r_1 \sin \alpha_1 \cos^2 \alpha_1
\]

(5.12)

\[
y = -4r_1 \sin^2 \alpha_1 \cos \alpha_1
\]

(5.13)
Curve $bc$

\[ 4\cos^4 \alpha_2 - \frac{7}{2}\cos^3 \alpha_2 + \frac{1}{4} = 0 \quad (5.14) \]

\[ \beta_2 = 3\alpha_2 - \frac{\pi}{2} \quad (5.15) \]

\[ r_2 = \frac{\cos \alpha_2}{1.266n^2} \quad (5.16) \]

where $n$ is the revolution speed of the drum in seconds (rps), $r$, the radius to any particle, $r_1$, the radius of the blender, $r_2$, the inner radius of the strands, $\alpha$, the angle from the vertical to $r$, $\alpha_i$, the angle from the vertical to $r_1$, $\beta_2$, the angle from the vertical to $r_2$, $\beta_2$, the angle from the horizontal to $r_2$, and $x$ and $y$ represent the relative movement from a given point on the $aO$ curve to find the corresponding point on the $Od$ curve. For clarification, see Figure 5.7. The path a particular block of strands follows at a given radius is shown in Equations 5.17 and 5.19.

**Parabolic Path**

\[ y = x \tan \alpha_1 - \frac{gx^2}{2V_1^2 \cos^2 \alpha_i} \quad (5.17) \]

\[ V_1^2 = r_1g \cos \alpha_i \quad (5.18) \]

**Circular Path**

\[ r_1^2 = x^2 + y^2 \quad (5.19) \]

where $V_1$ is the initial velocity of the block, $g$ is the acceleration due to gravity (32.2 ft/s$^2$), and $x$ and $y$ represent the co-ordinates of a parabola with an origin on the $aO$ curve. For the full development of Equations 5.11 to 5.19, the reader is directed to Davis (1919). Possible improvements to the model include the addition of a friction term and a derivation that describes a blender with flights.
5.1.3 Summary

To optimize the production of OSB with respect to the blending of resin and wood strands, an understanding of blending dynamics and their effect on the final board properties is required. Some of the key parameters affecting the distribution and dispersion of resin of the strands are:

1. the angle of the drum to the horizontal plane,
2. the revolution speed of the drum,
3. the number, spacing, shape and size of the blender flights,
4. the quantity of strands or loading of the drum,
5. the ability of the interior of the drum to resist strand and resin build-up,
6. the type of applicator (spray nozzle or spinning disc atomizer),
7. the location, spacing and number of the applicators.

Limited research has been done to quantify the importance and effect of the above parameters. Similarly, no models have been found in the literature that describes the tumbling of strands within a blender. However, models based on similar phenomena appear useful. Resin coverage can be measured directly through imaging techniques and indirectly by mechanical characteristics. The optimization of blender operation will result in reduced resin consumption and costs.

5.2 EXPERIMENTAL RESULTS

Of the variables listed above, revolution speed is speculated to have the greatest effect on the tumbling dynamics. In addition, an understanding of tumbling without flights is necessary to form a basis for subsequent experiments with flights and atomization.

5.2.1 Effect of Revolution Speed

As the revolution speed of the blender increases, the strand charge passes from the slipping regime through cascading and cataracting to centrifuging, shown in Figure 5.8. The blender surface was resin-coated plywood. There is a strong correlation between the revolution speed of the drum and the path a strand will follow within the blender, directly affecting the resination of the strands.
5.2.2 Effect of Drum Interior

The blender was modified such that the drum interior was lined with high-density polyethylene (HDPE) in contrast to the plywood above. Using the same charge as above, the blender was operated at 52.7 rpm. The strands no longer centrifuged due to the low coefficient of kinetic friction between the strands and the HDPE. Rather they were in the regime of sliding behaviour; the strands remained stationary with respect to the blender wall. Therefore, future experiments must include flights so that the strands will exhibit catartacting behaviour.

Since the effect of friction is strong, the coefficient of static friction ($\mu_s$) between the aspen strands and the wall liner was measured. The inclined plane method was chosen based on work by Henein (1981) and ASTM G115-98: Standard Guide for Measuring and Reporting Friction Coefficients (2000). Figure 5.9 and Figure 5.10 show schematics of the apparatus used and experimental set-up. A total of 40 runs were completed: 20 with the long axis of the strand perpendicular to the sliding direction and 20 with the long axis parallel. The results are given in Table 5.1. The static coefficient of friction ($\mu_s$) was calculated as follows:

1. The dimension $c$ is known (0.9m).
2. The dimension $b$ is measured as the distance the rope moves before the strand slides. The mark was taken at the top of the pulley as shown by the arrow in Figure 5.9.
3. The dimension $a$ and the static coefficient of friction are calculated:

$$a = \sqrt{c^2 - b^2}$$

(5.20)

$$\mu_s = \tan \theta = \frac{b}{a}$$

(5.21)

The test was considered invalid unless the strand slid at least 25.4 mm down the width of the HDPE sheet. In approximately half of the tests the strands slid the entire length of the sheet whereas for the remainder the strands slid between 25.4 to 76.2 mm. Finally, in those experiments where the long axis of the strand was perpendicular to the sliding direction as in Figure 5.10a, the strand tended to rotate before sliding down the sheet.
5.3 MODELLING

Two models were used to examine the behaviour of the rotary drum blender. The first is based purely on force balances, Figure 5.4 and Figure 5.5. It should be noted that friction is ignored in the case where no flights are present. However, it is taken into account in the model with flights. The second is the Davis model, which also ignores the effect of friction on tumbling behaviour.

5.3.1 Tumble Initiation

Tumble initiation will occur when the following force balances are true.

No Flights

\[ F_a = F_g \sin \theta \Rightarrow \frac{\omega^2 r}{g} = \sin \theta \]  \hspace{1cm} (5.22)

Flights

\[ F_w + F_f = F_g \sin \theta \Rightarrow \frac{\omega^2 r}{g} = \sin \theta - \mu \cos \theta \]  \hspace{1cm} (5.23)

where \( F_a \) is the centripetal force, \( F_g \), the force due to gravity, \( F_f \), the force due to friction, \( \omega \), the revolution speed, \( r \), the blender radius, \( g \), the acceleration due to gravity, and \( \theta \) is the angle with the horizontal. It should be noted that the subsequent calculations are for a blender rotating clockwise and are for the upper-left quadrant of the blender. Thus, \( \theta \) is always positive. An examination of Equations 5.22 and 5.23 show that for a given \( \theta \), between 0° and 90°, a reduction in revolution speed is necessary for the case with flights to achieve the same point of tumble initiation. This is shown in Figure 5.11.

Friction is not fully represented by these force balance models as demonstrated by the flightless experiment. According to Equation 5.10 and Figure 5.11, the strands should centrifuge at 34.26 rpm. However, as seen in Figure 5.8c, this is not the case as strands are still cataracting at 34.6 rpm. As well, with the addition of a high-density polyethylene liner, the strands are slipping at 52.7 rpm. Therefore, one or more of the assumptions that the strands move as a coherent block rather than individual pieces and that the strands are in contact with the drum wall or in motion are either invalid or another assumption is required. Both are true. The first assumption is invalid as demonstrated by Figure 5.8c. The second still appears reasonable and
hence, further assumptions and analysis are needed to model the tumbling of strands within a rotary drum blender.

5.3.2 Davis Model

A preliminary model of a rotary drum blender based on ball-milling (Davis, 1919) has been implemented. The inputs are the blender radius and revolution speed. Figure 5.12 shows the results for a 1.5m diameter blender. These were compared to the experimental results in Figure 5.8 and it is obvious that the current model, which does not include friction or the effects of air resistance, is insufficient to describe tumbling within a blender. This is demonstrated by the boundary conditions predicted by the model, particularly the high rpm case. The model states that above 34.25 rpm the strands should centrifuge but experimentally this is not the case as evidenced by the run at 34.6 rpm.

5.4 SUMMARY

Preliminary work done on the blending dynamics of oriented strand board show that revolution speed and the friction coefficient between the strands and the drum interior play a significant role. Modelling can demonstrate the effect of flights, however it is currently insufficient to fully describe the tumbling of strands within a rotary drum blender.
Table 5.1: Coefficient of static friction for HDPE sheet – Aspen strand tribological system.

<table>
<thead>
<tr>
<th>Long Axis of Strand</th>
<th>Average $\mu_s$</th>
<th>95% Confidence Interval</th>
</tr>
</thead>
<tbody>
<tr>
<td>Perpendicular</td>
<td>0.449</td>
<td>0.456</td>
</tr>
<tr>
<td>Parallel</td>
<td>0.456</td>
<td>0.453</td>
</tr>
<tr>
<td>Combined</td>
<td>0.453</td>
<td>0.449</td>
</tr>
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</table>

Figure 5.1: Schematic of a rotary drum blender. Adapted from Turner (1989).
Figure 5.2: Comparison of the relative amount of adhesive deposited, the variations in flow rate, and the shape of the spray pattern for three nozzle configurations. Adapted from Meinecke and Klauditz (1968).

Figure 5.3: Illustration of ligament formation and subsequent disintegration into droplets (Lefebvre, 1989).
Figure 5.4: Schematic of forces acting on the strands in a blender with no flights. Note: The blender is rotating clockwise.

Figure 5.5: Schematic of the forces on the strands in a blender equipped with flights. Note: The blender is rotating clockwise.
Figure 5.6: Schematic of tumbling regimes.

Figure 5.7: Davis model (1919). The grey area denotes the section in which the strands are traveling in a circular motion. Note: The blender is rotating clockwise.
Figure 5.8: Variation of blender revolution speed for a 1.5m diameter blender: (a) 14.2 rpm – slipping regime where the strands remain in the lower right hand quadrant and oscillate back and forth as indicated by the arrows, (b) 18.8 rpm – cascading regime where the strands fall such that they contact the main mass of strands upon reaching the end of their free fall, (c) 34.6 rpm – cataracting regime where the strands travel across the blender and contact the wall at the end of their path, (d) 52.7 rpm – centrifuging regime where the strands remain in contact with the blender wall for the entire revolution. Note: the blender is rotating clockwise.

Figure 5.9: Schematic of inclined plane apparatus.
Figure 5.10: Top view of HDPE sheet during friction experiments: (a) long axis of strand perpendicular to sliding direction, (b) long axis of strand parallel to sliding direction.

Figure 5.11: Point of tumble initiation for a 1.5m diameter rotary drum blender. Note: The blender is rotating clockwise.
Figure 5.12: Theoretical model bounds for a 1.5m diameter blender without flights; the grey area represents the area where the strands are in circular motion with no relative motion between the strand and the blender wall: (a) 18.25 rpm — Theoretical minimum rpm for cascading behaviour. Below this rpm the strands will roll, slump or slide, (b) 32.25 rpm — Theoretical maximum rpm for cataracting behaviour. Above this rpm the strands will centrifuge. Note: the blender is rotating clockwise.
6 – CONCLUSIONS AND FUTURE WORK

6.1 FLEXOGRAPHIC PRINTING

The modification of a flexographic printing technique allowed the deposition of known droplet sizes onto wood substrates. However, the variability of the resin mass deposited is relatively high. The mass of resin applied was found to vary with changes in resin physical characteristics – viscosity; room conditions – relative humidity, and substrate moisture content, with the mass of resin applied increasing with increasing viscosity and decreasing relative humidity. An increase in wire wound rod number, and therefore resin film thickness, also gives an increase in the mass of resin applied. Therefore, it is speculated that increased resin viscosity and decreasing relative humidity also result in an increase in the thickness of the resin film.

6.2 FRACTURE

The major conclusions of this work are that $G_{fc}$ is independent of the amount of resin applied for the range examined in this thesis and there appears to be a linear relationship between system fracture toughness, $G_{fc}$, and droplet diameter, $D$. This correlation is shown experimentally, confirmed analytically, and leads to a linear relationship between $G_{fc}^2$ and area coverage of the resin, $A_{bond}$. The experiments also indicate that there is a minimum droplet diameter of approximately 50 to 140 μm for a dot-pitch of 1-lpmm and 340 to 420 μm for a dot-pitch of 2-lpmm required to achieve a structural bond between the substrates. The minimum droplet diameter ranges suggest that a minimum ratio of bonded area to total area of approximately 0.3 is required to achieve bond integrity. Therefore, the calculation of minimum droplet diameter for any given dot-pitch is possible. The fracture toughness of the bond, $G_{fc(bond)}$, was also determined and was similar to that for solid wood. This is consistent with studies of the fracture surface, which showed that failure occurred mainly in the substrate beneath the bonded areas. Therefore, a linear relationship between the retention of solid wood fracture toughness squared and the area of solid wood failure was postulated and confirmed.

The industrial relevance of these findings is that if the fracture toughness is proportional to internal bond strength then the area of solid wood failure is also proportional to internal bond strength. Therefore, a simpler quality control test could possibly be implemented, which measures the area of solid wood failure instead of measuring the failure load. However, the
variability of such a qualitative measure would likely be greater than the current practice. Furthermore, since the fracture toughness is independent of resin mass applied, within the ranges studied, and directly affected by droplet diameter and dot-pitch, then an improvement of oriented strand mechanical properties should be achievable by modifying the droplet diameter and dot-pitch while keeping the resin consumption constant. Conversely, by modifying the droplet diameter and dot-pitch the current mechanical properties should be maintainable while decreasing in resin consumption.

There are two minor conclusions from the work. First, the variability of the measured fracture values increases with droplet diameter. This is due to the greater effect of substrate surface morphology, the increased difficulty in printing consistent droplets, and increased wood failure at larger droplet diameters. Second, the resin efficiency, which is defined as the fracture toughness divided by resin mass deposited, showed that resin mass deposited has only a minor effect on fracture toughness.

6.3 BLENDING

Preliminary work on the effect of revolution speed of a rotary drum blender on the tumbling of strands has been performed. By increasing the speed of rotation, the various tumbling regimes from sliding through rolling, cascading and catracting to centrifuging can be achieved. Based on current industrial practice the cataracting regime is the most desirable. The use of high-density polyethylene to line the blender necessitates the use of flights since the strands do not cataract without them. A model based on ball-milling represents a basis for further model development on the tumbling of strands within the blender.

6.4 FUTURE WORK

In printing, reduction of the variability associated with the process is necessary. This may include stricter environmental controls or reduced specimen size. Confirmation of the effect of resin viscosity and room conditions on the mass of resin deposited is also needed.

The future work in the area of fracture concerns the confirmation and generalization of the proposed model. The independence of droplet diameter and the ratio of bonded area to total area should be confirmed for other dot-pitches. This will also result in the verification of minimum droplet diameters. Verification of the effect of dot-pitch on the fracture morphology
is also necessary. To generalize the model, other wood/resin systems should be examined for the same effect, specifically Aspen/phenol-formaldehyde and Aspen/polymeric methylene diphenol diisocyanate since these are the two resin systems currently used in the manufacture of oriented strand board. Random droplet patterns should be examined and the relationship between fracture toughness and internal bond strength needs to be elucidated to use these results in an industrial setting. Finally, confirmation of the relationship of resin mass applied and fracture toughness is necessary, with the aim of maintaining current fracture toughness values with reduced resin amounts.

The model for blending should be updated to include the effect of friction. Blending experiments including one flight should be undertaken and once understood expanded to multiple flights and the addition of an atomizer. The ultimate goal being to understand the mechanics of tumbling such that the droplet diameter and dot-pitch identified by the fracture testing can be achieved in industry.
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This appendix contains a summary of all fracture tests including the substrate material, direction of crack growth, type of test (solid wood vs adhesive), dot-pitch, and area coverage. Any significant observations for a particular test are also noted. An invalid test is one that broke during preparation and subsequently no load-displacement or R-curves are available. Excess resin denotes those specimens were excess resin, as compared with the majority of specimens, was applied.

Table A.1: Design of experiments.

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<th>Substrate</th>
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<th>Dot-Pitch (lpmm)</th>
<th>Area Coverage (%)</th>
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APPENDIX B: BONDED DCB LOAD-DISPLACEMENT AND R-CURVES

This appendix contains the load-CMOD (crack mouth opening displacement) and R-curves for the adhesive-bonded DCB fracture tests. All of the specimens are denoted DFTLAD – abb – x where DF means Douglas Fir, TL means tested in the TL direction and AD means adhesively bonded; the a denotes the dot-pitch, the b denotes the grey scale, and the x denotes the test number. Tests 106-2, 106-3, 112-4, and 206-1 broke during preparation and no load-displacement or R-curves are available. Tests 112-3, 125-3, 212-3, and 225-3 had excess resin applied (182.8, 553.7, 440.9, and 599.2 mg respectively). All of the curves are plotted with the same scale for ease of comparison. The legend for the R-curves is as follows:

- Non-Linearity – Simple Beam Theory
- Non-Linearity – Corrected Beam Theory
- Non-Linearity – Experimental Compliance Method
- 5% Increase in Compliance or Maximum Load – Simple Beam Theory
- 5% Increase in Compliance or Maximum Load – Corrected Beam Theory
- 5% Increase in Compliance or Maximum Load – Experimental Compliance Method

Figure B.1: Test DFTLAD-106-1: (a) load-displacement curve, and (b) R-curve.
Figure B.2: Test DFTLAD-106-4: (a) load-displacement curve, and (b) R-curve.

Figure B.3: Test DFTLAD-112-1: (a) load-displacement curve, and (b) R-curve.

Figure B.4: Test DFTLAD-112-2: (a) load-displacement curve, and (b) R-curve.
Figure B.5: Test DFTLAD-112-3: (a) load-displacement curve, and (b) R-curve.

Figure B.6: Test DFTLAD-125-1: (a) load-displacement curve, and (b) R-curve.

Figure B.7: Test DFTLAD-125-2: (a) load-displacement curve, and (b) R-curve.
Figure B.8: Test DFTLAD-125-3: (a) load-displacement curve, and (b) R-curve.

Figure B.9: Test DFTLAD-125-4: (a) load-displacement curve, and (b) R-curve.

Figure B.10: Test DFTLAD-150-1: (a) load-displacement curve, and (b) R-curve.
Figure B.11: Test DFTLAD-150-2: (a) load-displacement curve, and (b) R-curve.

Figure B.12: Test DFTLAD-150-3: (a) load-displacement curve, and (b) R-curve.

Figure B.13: Test DFTLAD-150-4: (a) load-displacement curve, and (b) R-curve.
Figure B.14: Test DFTLAD-206-2: (a) load-displacement curve, and (b) R-curve.

Figure B.15: Test DFTLAD-206-3: (a) load-displacement curve, and (b) R-curve.

Figure B.16: Test DFTLAD-206-4: (a) load-displacement curve, and (b) R-curve.
Figure B.17: Test DFTLAD-212-1: (a) load-displacement curve, and (b) R-curve.

Figure B.18: Test DFTLAD-212-2: (a) load-displacement curve, and (b) R-curve.

Figure B.19: Test DFTLAD-212-3: (a) load-displacement curve, and (b) R-curve.
Figure B.20: Test DFTLAD-212-4: (a) load-displacement curve, and (b) R-curve.

Figure B.21: Test DFTLAD-225-1: (a) load-displacement curve, and (b) R-curve.

Figure B.22: Test DFTLAD-225-2: (a) load-displacement curve, and (b) R-curve.
Figure B.23: Test DFTLAD-225-3: (a) load-displacement curve, and (b) R-curve.

Figure B.24: Test DFTLAD-225-4: (a) load-displacement curve, and (b) R-curve.

Figure B.25: Test DFTLAD-250-1: (a) load-displacement curve, and (b) R-curve.
Figure B.26: Test DFTLAD-250-2: (a) load-displacement curve, and (b) R-curve.

Figure B.27: Test DFTLAD-250-3: (a) load-displacement curve, and (b) R-curve.

Figure B.28: Test DFTLAD-250-4: (a) load-displacement curve, and (b) R-curve.
APPENDIX C: SOLID WOOD LOAD-DISPLACEMENT AND R-CURVES

This appendix contains the load-CMOD (crack mouth opening displacement) and R-curves for the solid wood DCB fracture tests. All of the specimens are denoted DFTLSW – x where DF means Douglas Fir, TL means tested in the TL direction and SW means solid wood. The x denotes the test number. Only specimens 8 through 13 are shown as tests 1 through 7 are for specimens with a thickness of 25.4 mm rather than 12.7 mm and subsequently cannot be compared to any adhesive-bonded DCB specimens. All of the curves are plotted with the same scale for ease of comparison. The legend for the R-curves is as follows:

- • Non-Linearity – Simple Beam Theory
- ■ Non-Linearity – Corrected Beam Theory
- ▲ Non-Linearity – Experimental Compliance Method
- ○ 5% Increase in Compliance or Maximum Load – Simple Beam Theory
- □ 5% Increase in Compliance or Maximum Load – Corrected Beam Theory
- △ 5% Increase in Compliance or Maximum Load – Experimental Compliance Method

Figure C.1: Test DFTLSW - 8: (a) load-displacement curve, and (b) R-curve.
Figure C.2: Test DFTLSW - 9: (a) load-displacement curve, and (b) R-curve.

Figure C.3: Test DFTLSW - 10: (a) load-displacement curve, and (b) R-curve.

Figure C.4: Test DFTLSW - 11: (a) load-displacement curve, and (b) R-curve.
Figure C.5: Test DFTLSW - 12: (a) load-displacement curve, and (b) R-curve.

Figure C.6: Test DFTLSW - 13: (a) load-displacement curve, and (b) R-curve.