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ABSTRACT

A set of novel structural fire tests on axially loaded cross-laminated timber (CLT) compression elements (walls), locally exposed to thermal radiation sufficient to cause sustained flaming combustion, are presented and discussed. Test specimens were subjected to a sustained compressive load, equivalent to 10 % or 20 % of their nominal ambient axial compressive capacity. The walls were then locally exposed to a nominal constant incident heat flux of 50 kW/m² over their mid height area until failure occurred. The axial and lateral deformations of the walls were measured and compared against predictions calculated using a finite Bernoulli beam element analysis, to shed light on the fundamental mechanics and needs for rational structural design of CLT compression elements in fire. For the walls tested herein, failure at both ambient and elevated temperature was due to global buckling. At high temperature failure results from excessive lateral deflections and second order flexural effects due to reductions the walls' effective cross-section and flexural rigidity, as well as a shift of the effective neutral axis in bending during fire. Measured average one-dimensional charring rates ranged between 0.82 and 1.0 mm/min in these tests. As expected, the lamellae configuration greatly influenced the walls' deformation responses and times to failure; with 3-ply walls failing earlier than those with 5-plies. The walls' deformation response during heating suggests that, if a conventional reduced cross section method (RCSM), zero strength layer analysis were undertaken, the required zero strength layer depths would range between 15.2 mm and 21.8 mm. Deflection paths further suggest that the concept of a zero strength layer is inadequate for properly capturing the mechanical response of fire-exposed CLT compression elements.

KEYWORDS: Structural response; structural design; cross-laminated timber; reduced cross-section method; instability; compression; zero strength layer.

NOMENCLATURE

d_0	zero strength layer	P	applied load
e	eccentricity due to deflection	**subscripts**	
h	residual cross-section	c	charring
K	stiffness matrix	s	deflections
M	bending moment		

INTRODUCTION

Engineered mass timber products are experiencing a rapid increase in popularity and utilisation, as structural elements for both residential and commercial developments globally. Cross-laminated timber (CLT), in which timber layers (lamellae) are built up in alternating orientations and bonded using polymer adhesives, offers multidirectional mechanical properties and is increasingly being used in load bearing floor and wall applications in multi-storey buildings. The use of engineered timber such as CLT offers many benefits in construction. Prefabricated off-site manufacturing enables rapid, accurate assembly on site, and timber's high strength-to-weight ratio enables lighter structures to be built, thus saving on site preparation and foundation costs [1] and permitting construction above pre-existing buildings or buried infrastructure. However, timber is combustible and its application for structural frames in tall buildings is heavily constrained by strict fire safety regulations and the approvals process in many jurisdictions. Building a knowledge-based, rational structural fire engineering approach for CLT is therefore a key hurdle for

advancing the engineered timber construction sector, particularly for multi-storey buildings in which there is an architectural aspiration for some of the CLT to be expressed within the finished building.

Fire compartment boundaries in buildings are typically required to fulfil three criteria to meet conventional 'fire resistance' design requirements. These are maintenance of: (1) sufficient insulation from the fire for the neighbouring spaces, (2) integrity to prevent the passage of hot gases or flames, and (3) load bearing capacity to prevent local or global collapse or the spread of fire. Each of these requirements must be maintained during exposure to a standard fire, the duration of which is based on local building code requirements. Required standard fire resistance times were originally derived based on an equivalency argument related to the equivalent duration of a real building fire that continued until burnout of the combustible contents within a fire compartment without intervention [2]. For a load-bearing CLT wall under sustained compression, structural failure during fire could compromise all three of the above criteria, with adverse consequences for both life safety *and* property protection. Thus, a proper physical understanding of CLT's mechanical response and failure modes in fire is needed to enable confident structural fire design and analysis of ever taller CLT buildings.

BACKGROUND

As early as 1967 Malhotra & Rogowski [3] proposed an empirical model for predicting the fire resistance of glued laminated timber columns of different species, adhesives, shapes, and load levels. This was based on full scale standard fire resistance tests undertaken in fire testing furnaces. Their model could be used to predict fire resistance based on assigning experimentally-derived input parameters for each of their investigated parameters; these were then multiplied in series, to extend their data and empirically predict fire resistance. However, the application space of this model is extremely restricted and it cannot be applied to CLT wall elements, which may make use of novel adhesive types, raw timber with varying mechanical properties, and with a crosswise (rather than unidirectional) lay-up of timber lamellae.

The Reduced Cross Section Method and Zero Strength Layer

The most common fire resistance design verification and analysis method currently used for mass timber structural elements is the reduced cross section method [4]. The RCSM assumes the formation of an insulating sacrificial char layer at the fire-exposed surfaces of timber structural elements; this provides an insulating layer and partially protects the underlying timber from fire, thus slowing the increase of internal temperatures and deterioration of the elements' load carrying capacity. In the RCSM method the timber is assumed to char at a nominal rate during exposure to standard fire conditions (or, more precisely, one of a number of prescribed nominal charring rates depending on the specific circumstances) [5]. The sacrificial char layer is assumed to have zero mechanical strength. In addition, a certain depth of 'thermally affected' timber beneath the char also has reduced mechanical properties due to heating and moisture transport effects. In the classical RCSM method, the mechanical consequences of the thermally affected timber beneath the char are treated by lumping a portion of the affected zone into a 'zero strength layer' (ZSL). The ZSL is typically assumed to be 7mm [4, 6], and this further reduces the size of the effective cross section; this reduced cross section is then used to predict the remaining load capacity in fire, assuming that the reduced cross section retains its full ambient temperature mechanical properties.

For fire safe design of common (i.e. low-rise) timber buildings the above approach is widely considered sufficiently accurate, since the temperature gradients in timber elements exposed in a standard fire resistance test are relatively steep and changes in the timber's strength and stiffness are concentrated close to the char [7]. However, the constant 7 mm ZSL depth currently suggested in design codes [4] is based largely on models calibrated from a relatively small number of flexural standard furnace tests on glulam beams undertaken in the 1980s [8]. The applicability of the current ZSL value to CLT in general [9], and to engineered timber compression elements more specifically [10, 11] is doubtful.

The reductions in mechanical properties experienced by heated timber are substantially different when considering tensile or compressive response, and these are also heavily grain dependent [12, 13]. The ZSL determined from computational models validated using flexural furnace tests is unlikely to apply to elements under uniform compression or combined compression and bending, as noted by König [6]. Furthermore, it has previously been shown – using both computer simulations and standard furnace tests – that the constant 7 mm ZSL used in design should actually vary for loading in bending, compression, or

tension. For flexural compressive loading (i.e. hogging moment with heating from below) Schmid et al. showed that the ZSL should be increased to between 12.5 and 18.9 mm with a mean of 14.8 mm [10].

Schmid et al. [11] performed tests on compressively loaded CLT wall elements exposed to fire from one side, and postulated that a minimum residual depth of 3 mm should be imposed when considering CLT, in which the cross layers have negligible strength and stiffness in the primary loading direction, particularly considering the propensity for buckling of compression elements and the comparatively fine margins of residual cross section depth that could result in instability failures. Schmid et al. also state that the precise depth of the ZSL is potentially irrelevant to the fire resistance time if the ZSL penetrates into a weak layer, since only the loadbearing function of the strong layers is critical. From analysis of a series of furnace tests on flexural CLT elements, Schmid et al. [9] have suggested that the 7 mm ZSL for CLT should be replaced with a ZSL depth that varies depending on the total depth of the remaining cross section. For example, for a CLT structural member in hogging with fire exposure on the compression face, Schmid et al. [9] propose a ZSL, d_0, from Eq. 1, where h is the reduced cross section depth (in mm).

$$d_0 = \frac{h}{20} + 11 \tag{1}$$

Goina [14] has used computation and experimental results to show that the above approach conservatively predicts fire resistances for compressively loaded CLT walls in standard furnace tests, by 45-47 %.

Fire-Induced Delamination

Notwithstanding the complexities of proposing a more conservative ZSL depth for use with either glued laminated or CLT compression (rather than flexural) elements, another potentially important issue in determining the structural fire response of laminated timber products is fire-induced delamination; sometimes alternatively called 'loss of stickability' or 'falling off'. Delamination is the detachment of charred lamellae at in-depth glue-lines, which can expose the underlying uncharred timber to direct heating and increase the effective rate of charring. It is noteworthy that delamination may also contribute additional fuel to a fire compartment, thus altering the resultant fire dynamics and further influencing the structural fire response (and fire resistance). While delamination has previously been reported as an important issue to consider for CLT floor slabs in fire [15, 16], it has been suggested that it is less likely in furnace tests of vertically oriented elements, presumably due to reduced separation forces from gravity [17, 18]. Delamination should not be confused with debonding, which describes loss of composite mechanical action between lamellae, with both plies still theoretically capable of performing a significant load bearing function [19].

Thermal Deformations in Fire

Unlike steel or concrete structural elements, which experience structurally significant thermal deformations during heating [20], thermal deformation of heated timber elements in standard fires is widely considered to be negligible [6]. This is apparently because the effects of thermal expansion and dehydration shrinkage counteract each other during heating, resulting in a net zero volume change. However, for compression elements secondary moments are likely to arise during fire due to a shift in the neutral axis of bending, since charring reduces the effective cross section from one side only. This could lead to instability (i.e. buckling) failures for elements, especially considering that their slenderness also increases during a fire, and that they are likely to have been designed as non-slender at ambient temperature.

In contrast to structural steel manufacturers, engineered timber manufacturers have less ability to control the production quality of their raw materials. The physical properties of timber vary between and within trees, and this causes unavoidable variability of mechanical and thermal properties that must be accounted for in design. The quality of timber is determined through grading before a CLT panel is manufactured [21], but one of the particularities arising from this variability is that, within each strength class, permissible design (i.e. minimum) values are given for bending, tensile, and compressive strength [22]. A considerable advantage of laminated timber products is their reduced variability of mechanical properties [23], and this effectively increases the presumed fire resistance of laminated timber columns as compared to solid timber columns [7]. This is already accounted for in design recommendations [24].

Instability Effects

The structural capacity of compression elements can be affected by progressive instability (i.e. secondary bending), which depends not only on the cross section's geometry, but also on its effective length and flexural rigidity. A series of 8 loaded CLT wall elements exposed to standard furnace testing by Suzuki et al. [25] all failed in global buckling, with runaway lateral deflections distinguishing the fire resistance times. Suzuki et al. [25] used a sectional temperature analysis to predict the reductions in elastic modulus, and thus the reduction in buckling resistance, using a secant formula to account for loading eccentricities. This approach enabled them to make slightly conservative predictions of the failure time for most of their experimental results. For some of their results the predictions were unconservative but in general the secant approach enabled improved predictions compared to assessment based on Euler buckling formulae. Suzuki et al. [25] also highlighted the importance of the outer layers in a fire-exposed wall to provide buckling resistance. A reduction in elastic modulus on heating (the rate of which with temperature is different than for strength) may reduce a CLT column or wall element's buckling capacity, and the ZSL approach, which lumps reductions in both strength *and* stiffness into a single ZSL depth based on tensile flexural strength reductions, is clearly unable to properly account for this.

Only limited guidance, and results from a small number of standard fire resistance tests, is available for structural fire design of CLT wall or column elements. This paper aims to investigate the thermal and mechanical response of CLT column elements (or perhaps more accurately, wall strips) when exposed to heating from one side, and to investigate the resulting mechanics and observed failure modes with a view to informing fire safe structural design of CLT elements in multi-storey buildings.

EXPERIMENTAL SET UP

To investigate the thermal and mechanical response of compressively loaded CLT wall strips, a total of eight tests was performed: four of these were ambient temperature control tests, and four were fire tests performed using sustained load and an imposed constant incident heat flux exposure. The CLT walls were cut from melamine formaldehyde adhesively bonded spruce/pine CLT slabs with an overall thickness of 100 mm. Two different lamination lay-ups were used: (1) a three lamella (3-ply) CLT build up with lamella thicknesses of 33 mm for the outer layers (with grain direction orientated parallel to the direction of loading) and 34 mm for the crosswise layer; and (2) a five lamella (5-ply) CLT build up with lamella thicknesses of 20 mm for all five layers (outer and central layers with grain direction orientated parallel to the direction of loading). The column samples were stored in humidity-controlled conditions, leading to an average moisture content of 10.3% $\pm 0.25\%$ at the time of testing (determined by mass loss dehydration in accordance with EN 332 [26]). The flexural strength of the CLT was determined using bending tests to failure, performed by the authors in accordance with BS EN 16351 [27]. For a failure mode consisting of tensile rupture in the outer lamella parallel to the grain, the average strength was found to be 35 N/mm^2 ± 2 N/mm^2 [28, 29]. Similarly, the elastic modulus orthogonal to the grain direction was found to be 335 N/mm^2, whereas it was determined to be 10,050 N/mm^2 parallel to the grain [28]. The nominal manufacturer specified and experimentally determined mechanical properties for the CLT materials used in the current study are given in Table 1.

Table 1. Mechanical properties of CLT used in the current study [28-30].

Property	Manufacturer specified (nominal) (N/mm^2)	Experimentally determined (N/mm^2)
Modulus of Elasticity, $E_{\parallel}$	11,600	10,050
Modulus of Elasticity, $E_{\perp}$	370	335
Compressive Strength, $f_{c,\parallel}$	21	--
Compressive Strength, $f_{c,\perp}$	2.5	--
Bending Strength	24.0	35.4

In practice, structural fire resistance is usually verified by subjecting a structural element to heating in a fire testing furnace, following a predefined standard gas temperature versus time curve [5]. However, 'standard fires' in furnaces differ from real fires in a number of potentially important ways, particularly as regards

testing of timber elements [31], and, because standard furnace tests are intended to be used as a comparative grading system (rather than an experiment), it is not possible to directly translate their results into real applications where more realistic design fires may be used. Van Zeeland et al. [32] have shown that the mechanical properties of timber at high temperature derived from standard furnace tests cannot be easily extrapolated to different heating conditions due to the influence of moisture movement within the timber, differing thermal gradients, and so on. Thus, the tests described herein used propane-fired radiant panels as the heating source, since this allows accurate control of the applied incident heat flux boundary condition, yields good repeatability between tests, and allows careful visual examination of the response during heating (e.g. deflection, cracking, splitting, and delamination). It must be noted here that the radiant panel test method used in the current work is potentially just as poor a representation of a 'real' fire as a standard fire resistance test performed in a fire testing furnace; additional research is needed to better understand the influence of various parameters on the response of CLT elements to fire, and the consequences of this for the fire safety of engineered timber buildings.

AMBIENT TEMPERATURE TESTS AND RESULTS

All tested CLT walls (which were intended to represent vertical strips of continuous CLT wall elements) had a width of 300 mm and an overall height of 1700 mm. Four walls were tested to failure at ambient temperature; two of these with three lamellae and two with five lamellae. All walls were concentrically loaded to failure using a 1000 kN mechanical testing frame, with idealised pinned-pinned end conditions (i.e. effective length equal to 1845 mm). The bottom pin support consisted of a steel cradle and roller, seated in a machined groove, and the top pin was the spherical seat of the testing frame crosshead (see Fig. 1). The vertical and lateral deflection responses of the wall were recorded using linear potentiometers (LPs) and a bespoke digital image correlation code (DIC) [33].
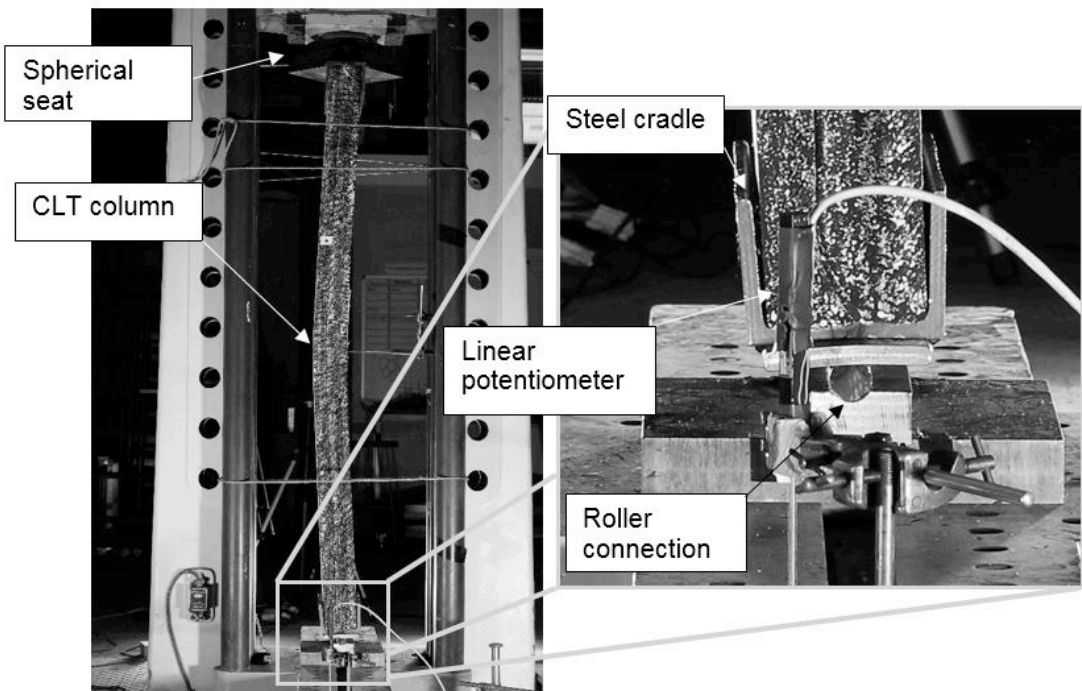

Fig. 1. Test setup for ambient temperature tests of CLT walls, also showing typical deflected shape at peak load (Specimen A5-2) and pinned connection detail at base.

The ambient temperature tests were performed under manual load control (using a pacer dial) at an intended linear rate of 11 to 14 kN/min. The slight variability in loading rate between samples is not expected to have significantly influenced the results. The actual loading rate for each sample is given in Table 2, along with the experimentally determined compressive load capacity. The capacities for identical walls differed by less than 7% (for the 5-ply specimens), demonstrating good repeatability.

Table 2. Results of ambient temperature compressive testing of CLT walls.

Test name[a]	Lamination build up [mm][b]	Approx. loading rate [kN/min]	Failure load [kN]	Failure mode[c]
A3-1	33-34-33	11.7	538	GB
A3-2	33-34-33	11.5	524	GB
A5-1	20-20-20-20-20	13.6	490	LS/GB
A5-2	20-20-20-20-20	12.5	456	GB

[a] A = ambient temperature, 5 or 3 = 5-ply or 3 ply CLT, 1 or 2 = repeat test numbers
[b] For all samples the outer lamellae were oriented with the grain in the direction of loading
[c] GB = global buckling failure at or near the column mid-height, LS/GB = local splitting near support followed by global buckling

The load versus axial and lateral deflection responses for the ambient temperature tests are shown in Fig. 2 (note that the deflection responses have been corrected for seating effects due to movement of the testing frame base). Lateral deflections display an initially linear elastic response, as expected for concentric axial compression of timber [24]. However, the lateral deflections increase at higher loads due to imperfections in the elements, as well as the presence of unavoidable inadvertent small eccentricities in the loading set up. This results in accumulation of secondary bending moments, eventually leading to instability (i.e. global buckling) failures combined with axial-flexural failure in the most compressed fibres of the outer lamella. Axial deflections are essentially linear-elastic initially, again as expected [24], until the point where lateral deflections and second-order effects become significant. While the compressive crushing strength given by the manufacturer (21 N/mm^2, neglecting bending effects) was exceeded for all walls tested at ambient temperature, the measured compressive elastic modulus ($\sim$7320 N/mm^2 $\pm$510 N/mm^2) approximated from the vertical displacement response (see Fig. 2) was 37 % less than expected based on manufacturer specified properties (Table 1) – probably due to the influence of bending moments, caused by inherent or accidental/unintended eccentricities – and also 27 % lower than the previously reported average elastic modulus of 10,050 N/mm^2 $\pm$1,200 N/mm^2 for these CLT materials determined from bending tests precisely of the same CLT materials and layups [28]. These results agreed with the manufacturer recommendations within 13.3% [28]. The expected maximum allowable ambient temperature crushing loads, according to EC5 [24] and based on the manufacturer supplied data in Table 1, were 272 and 299 kN for the five- and three-layer beams respectively; with CLT considered equivalent to glulam and crosswise layers ignored. This gives factors of safety of 1.7 and 1.8, respectively, as regards the capacities in Table 2.
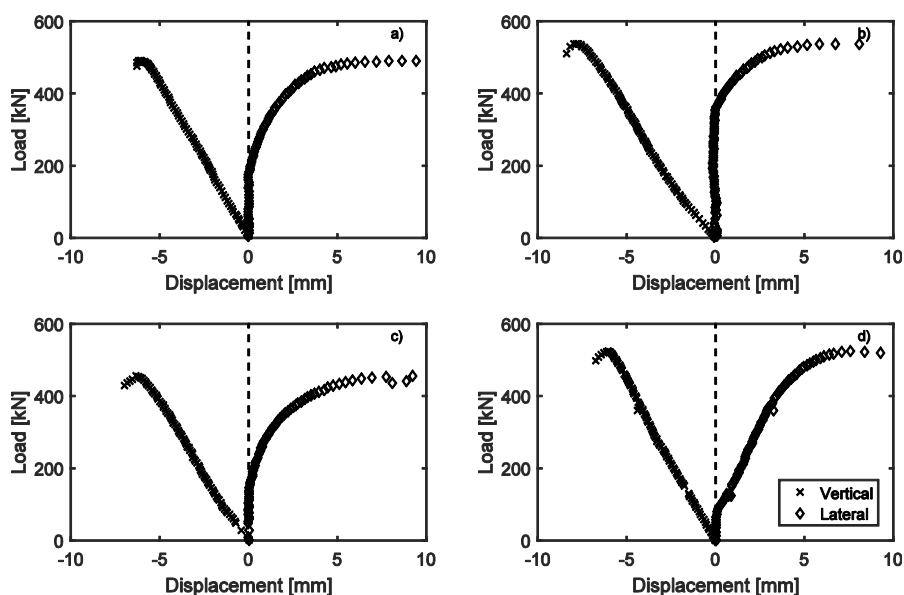

Fig. 2. Axial and lateral mid-height displacements as a function of applied load for CLT walls tested at ambient temperature: (a) A5-1, (b) A3-1, (c) A5-2, and (d) A3-2.

FIRE TESTS AND RESULTS

Four fire tests were performed with the specimens under sustained concentric axial compressive load, again with two tests for each lamella build up (i.e. 3-ply or 5-ply). A uniform one-dimensional heat transfer condition within the test samples was desired, so as to simulate the expected thermal gradients within a continuous CLT wall panel heated from one side. The column's sides and front faces were therefore insulated using mineral wool boards; this resulted in a thermally exposed area of 300 mm by 300 mm with an incident radiant heat flux from an array of propane fuelled radiant panels (see Figs 3 and 4).
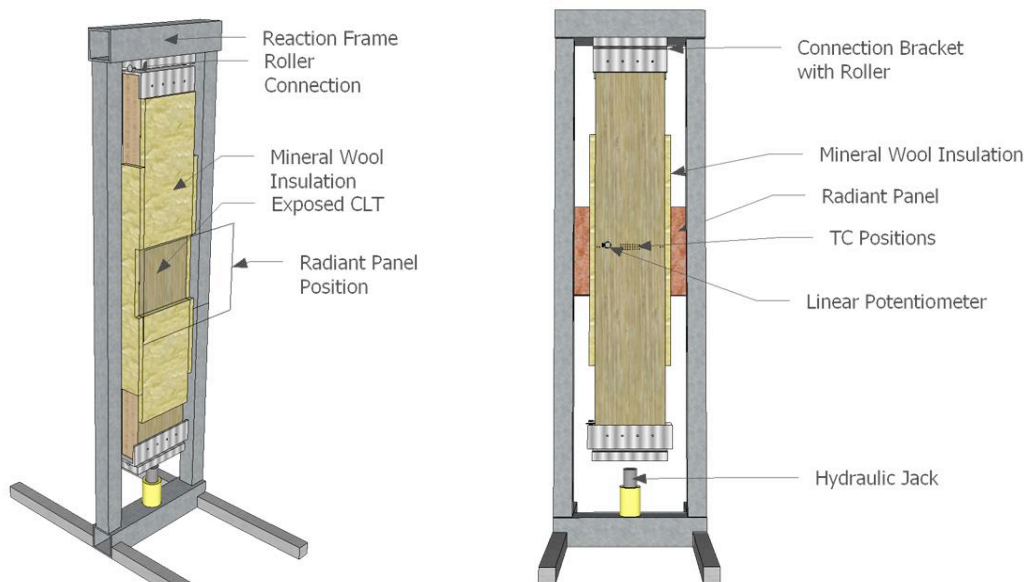

Fig. 3. Isometric and rear schematic views of the fire test set-up.

The incident radiant heat flux at the target surface of the CLT was mapped before the tests using a Gardon gauge heat flux meter, the results of which are given in Fig. 4. The distribution of measured incident heat fluxes was variable (between ~30 and ~50 kW/m^2 in the region of interest), which can be attributed to small view factor differences and to the influence of free convective flow. However, between the locations noted as -150 mm and 150 mm in Fig. 4 (the zone representing the extents of the exposed CLT), and considering that the received heat flux at any given location on the CLT surface will also be affected by the flaming process at the sample surface resulting from ignition and combustion of flammable pyrolysis gases (which occurred within 20 seconds of radiant panel ignition in all cases), the measured differences in initial incident radiant heat flux over the samples' fire-exposed surfaces are not considered significant for the purposes of the current study. This assumption was verified post-testing by studying the actual in-depth charring profiles in the respective samples.

Inconel clad Type K thermocouples (TCs) were inserted from the back of the CLT walls at chosen depths to monitor internal temperatures and the progression of the char front and thermally affected depth. The insertion depths for both of the respective lamellae configurations, and for each of the three rows of TCs placed in each sample, are shown in Fig. 5 a) and b). Thermocouples were also used to verify the uniformity and one-dimensionality of heat transfer within the samples. Care was taken to minimise errors in the TC locations, and to record their precise locations both before and after testing.

During the fire tests load was applied using a custom built, self-reacting frame that held the CLT walls in a vertical orientation under sustained load with coincident fire exposure, as described above. The loading frame was fabricated from steel box sections and fitted with a 100 kN hydraulic actuator. The tops and bottoms of each column were fitted with steel rollers attached to steel C-channels, as for the bottom pin support in Fig. 1 and shown schematically in Fig. 3.
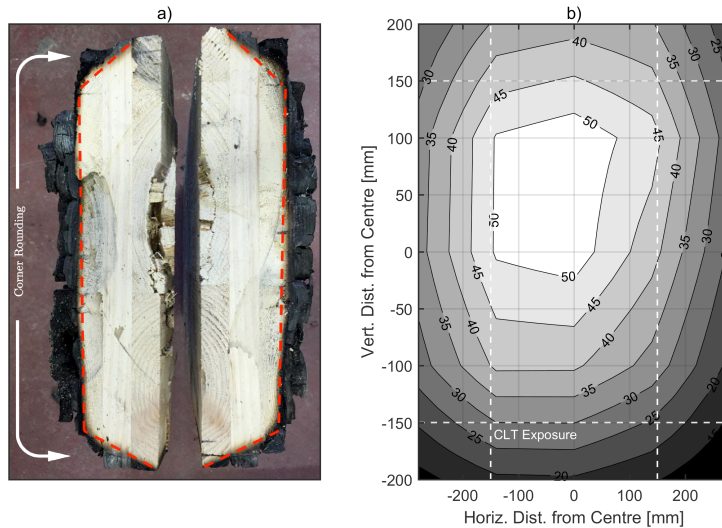
Fig. 4. (a) Post-test observed corner rounding (Test F3-10), and (b) incident heat flux distribution measured using a Gardon gauge at the location of the target CLT surface.

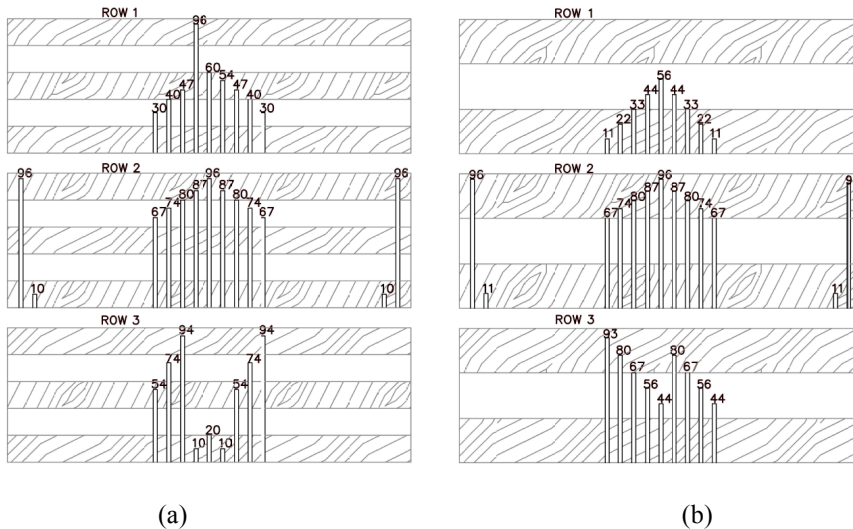(a)   (b)

Fig. 5. Thermocouple insertion depths in (a) 5-ply, and (b) 3-ply CLT samples; TC rows 1, 2, and 3 located at 10, 0, and -10 mm vertical distances from walls' mid-height; TCs inserted from unexposed face.

One of each type of column build-up (i.e. 3-ply or 5-ply) was subjected to a target sustained load of either 10 % or 20 % of the nominal crushing capacity for each type of column, as determined from the manufacturers specified material properties (refer to tables 1 and 3). These loads represented 16.6 or 8.3 % and 17.3 or 8.6 % of the average ambient capacities determined from ambient temperature tests for 3-ply and 5-ply specimens, respectively (for which failure was by global buckling). The sustained load was manually ramped prior to fire testing using a standalone hydraulic power pack with an on-board digital load holding feature. The load was held for 15 minutes prior to igniting the radiant panels, and was maintained ($\pm$6% of the target applied load) throughout the fire exposure. The axial and mid-height lateral deflections of the walls were again measured using linear potentiometers (LPs).

The radiant panels were switched off as soon as structural failure occurred (i.e. inability to support the load), at which point the timber self-extinguished, although with minimal smouldering combustion continuing beneath the mineral wool insulation which was manually extinguished using a water spray.

Charring Response

The progression of the charring front can be approximated from the measured thermocouple data by tracking the depth of the 300 ^{o}C isotherm in the timber; this is generally assumed to be the temperature at which pyrolysis of timber is complete [6, 15, 34]. The charring rate can then be calculated as the derivative of the char depth with time. The measured charring depths and rates are given in Fig. 6 for all four fire tests. The location of the 300 ^{o}C isotherm was approximated using a least-squares fit to the in-depth temperature readings with a cubic polynomial curve.

No charring was calculated for up to five minutes into the tests, since the radiant panels have to heat up before reaching steady-state radiative output and to heat the surface of the timber to 300 °C. As expected, all tests displayed an intial spike in charring rate, which was followed by a reduction in charring rate and stabilisation at a value close to the widely recommended 0.65 mm/min one-dimensional charring rate for softwood [4]. However the initial charring rate peak resulted in *average* charring rates somewhat greater than 0.65 mm/min (see Table 3). This may be due to higher oxygen content in the convective gases near the surface of the charred timber leading to accelerated char oxidation, or due to some other cause.

Whilst the sides of the walls were insulated with mineral wool during fire testing, post-test examinations revealed that minor corner rounding had occurred (Fig. 4a). This can be attributed to edge effects near the mineral wool boards, where increased turbulence and airflow, and reduced heat losses promote higher rates of char oxidation. The corner rounding may also be partly due to continued smouldering of the timber close to the mineral wool protection, once the radiant panels were turned off immediately following structural failure. Temperature measurements taken at equal depths within the cross section during testing, however, demonstrated that the internal temperature development in the timber was uniform over the exposed area, and the small influence of the corner rounding is not considered important. For example, Test F5-20 had three TCs placed at 4 mm depths, at its centreline and at the edges of the sample (Fig. 5a), and up to an assumed charring temperature of 300 ^{o}C the standard error for the temperatures at these locations was less than 14 %.
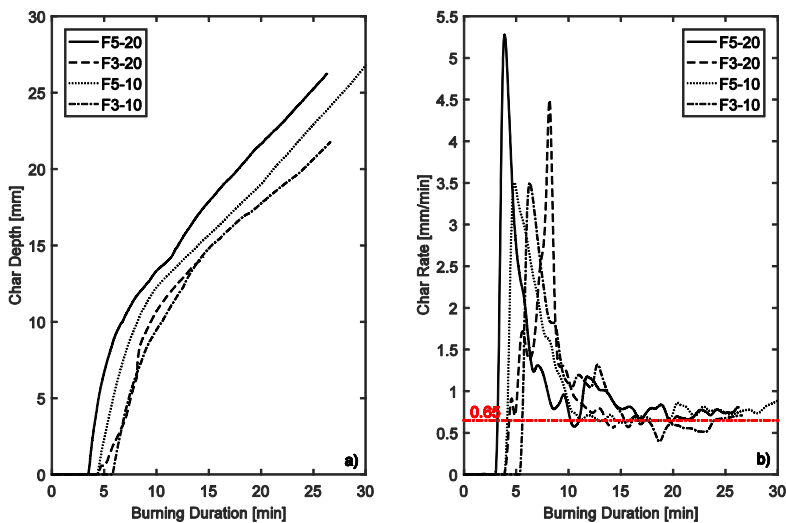

Fig. 6. Calculated variation in: (a) charring depths, and (b) charring rates for all four tests.

Deformation Response during Heating

Because CLT is made up of multiple timber lamellae bonded in an alternating crosswise lay-up, and because raw timber is anisotropic [13] with much greater strength and stiffness parallel to its grain, strong layers in CLT elements are typically placed in the outermost layers of a panel so as to maximise flexural strength and stiffness in the primary loading direction. In design of CLT elements either the strength of the crosswise layers is completely ignored [27], or a transformed section is used in which the effective width of the crosswise layers is reduced based the modular ratio between strong and weak layers [35]; the second option is used herein, thereby assigning an effective width to layers based on their respective stiffnesses

The experimental lateral deflection data are shown in Fig. 7 alongside predictions made using a simple, bespoke finite element analysis based on an RCSM model. Different predictions are compared incorporating various potential input parameter assumptions, including the assumed notional charring rate and the assumed ZSL depth, which is established in a linear manner for the first 20 minutes of the heating period. All of the predictions are based on a custom-coded finite element analysis model using simple Euler-Bernoulli beam elements that are assumed to be subjected to a combination of compressive load and bending moment. The analysis accounts for moments caused by the additional load eccentricity e_c resulting from the shift of the neutral axis, as well as that resulting from the secondary elastic lateral deflection of the column at each timestep (refer to Fig. 8). Within each timestep the applied bending moment is iteratively increased due to increasing mid-height deflection e_s, until the analysis converges to within 1.0 μm of lateral deflection. If no convergence occurs the secondary bending moments reach infinity and the wall is assumed to have buckled.
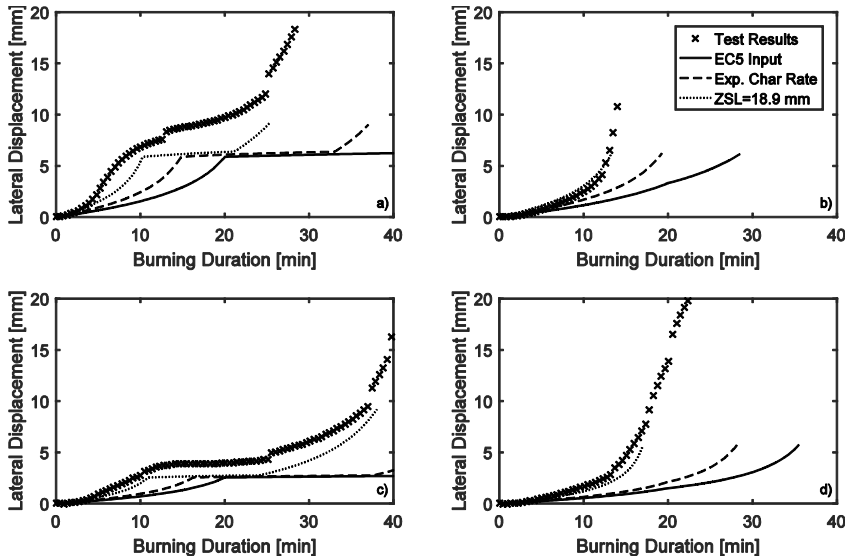

Fig. 7. Observed and predicted lateral mid-height displacement of the CLT walls with heating, for various assumed input parameters: (a) F5-20, (b) F3-20, (c) F5-10, and (d) F3-10.

The dimensions of the reduced cross-section for the fire-exposed portion of the column are recalculated at each timestep, according to the measured or calculated charring rate and the assumed ZSL depth. Using this reduced cross-section, the changes in flexural rigidity for each element along the column's length are written into a corresponding element stiffness matrix comprised of both elastic and geometric stiffness matrices. The strain and the resulting stress distributions over the column cross-section are subsequently determined, and failure is assumed to occur when the local stress exceeds the design strength of the timber material. The manufacturer specified material properties are assumed in the analyses (refer to Table 1). Despite the fact that 'fire resistance' predictions can be made with the model, it is actually more interesting for studying the walls' deflection during heating, and to shed light on the relevant mechanics under these conditions.

The 'EC5 input' curves (solid lines) in Fig. 7 assume a one-dimensional constant charring rate of 0.65 mm/min and a ZSL depth of 7 mm, as currently suggested by EC5 [4] (although EC5 does not specifically address CLT at present). It is noteworthy that, strictly speaking, these charring rate and ZSL values are only valid for standard tests performed within a fire testing furnace, and they are included here simply for illustrative purposes, rather than to criticise either EC5 [4] or the ZSL method. The 'Exp. Char Rate' curves (dashed lines) in Fig. 7 show the predicted lateral deflection with time response when constant average charring rates measured during the respective tests are assumed (see Table 3), together with a 7 mm ZSL. The 'ZSL = 18.9 mm' curves (dotted lines) assume the experimentally measured charring rates for each test (Table 3), along with a ZSL depth of 18.9 mm; this is the maximum value within the range suggested by Schmid et al. [10] (for situations with timber in flexural compression). Comparisons between the various predictions are made later in the Discussion.

$$M = M_c + M_s$$

$$M_c = P \cdot e_c$$

$$M_s = \lim_{n \to \infty} \sum_{i=1}^{n} P \cdot e_{s,n}$$

$$e_{s,n} = K^{-1} \cdot [P \cdot e_{s,n-1}]$$
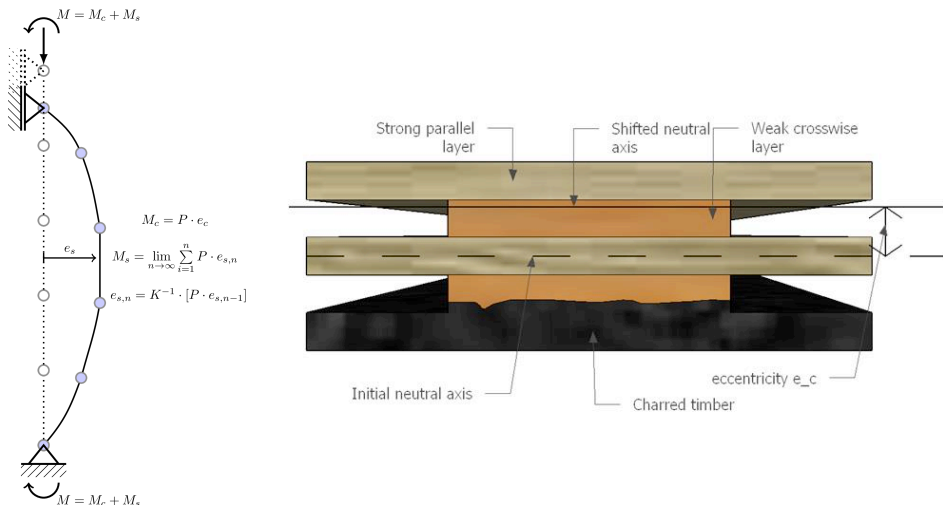
$$M = M_c + M_s$$

Fig. 8. Schematics of (a) deflection model and (b) charred transformed cross-section.

For the 5-ply CLT, the effect of the drastically different strengths and elastic moduli between the strong and weak layers is clearly evidenced by the plateaus in the lateral displacement curves. As the effects of charring and heating penetrate into the crosswise (weak) layer, additional reductions of effective cross section only slightly affect the deflection response. The computational predictions show sudden changes of deflection rate, whereas the experimental curves show more gradual transitions, although the plateauing behaviour is also apparent. This is expected since the ZSL is merely an idealisation used for design, and in reality a smooth temperature gradient exists within the timber, leading to a smooth deflection response.

For the 3-ply CLT, there is no noticeable influence of the weak layers in the deflection rates, either for the predicted or experimental curves. This is because thermally induced loss of the effectiveness of the first strong layer causes sufficient loss of strength that the walls fail, and therefore sacrificial burning of the cross-wise central lamella plays no obvious role in this case. This highlights an important consideration for the fire-safe design of CLT compression elements, in that CLT build ups with at least five lamellae should be preferred over those with three. It is noteworthy that the rate of lateral deflection decreases during charring of the first strong layer for both of the 5-ply test results. This is related to the observed sharp drop in initial charring rate (Fig. 6) as the protective char layer initially forms.

The measured final deflections exceed the computational predictions for all four fire tests, demonstrating runaway deflections that lead to failure. The computational results all predict failure at lower lateral displacements. This can partly be explained by the expected plasticity of heated timber in compression, and to the fact that in reality the timber's strength is somewhat higher than the nominal value quoted (and used to make the predictions), as shown in Table 1. Failure is therefore predicted by the model before it was achieved in practice (in terms of distance along the deflection path)

Table 3. Results of fire tests on loaded CLT compression elements.

Test name[a]	Average charring rate [mm/min]	Actual applied load [kN][b]	Test load ratio (w.r.t. to ambient capacity) [%]	Time to failure [min]	Failure mode[c]
F5-20	1.00	81.6	17.3	29.3	PGB
F5-10	0.98	40.8	8.6	41.3	PGB
F3-20	0.88	88.2	16.6	14.1	PGB
F3-10	0.82	44.1	8.3	28.4	PGB

[a] F = Fire test, 5 or 3 = 5-ply or 3 ply CLT, 20 or 10 = Nominal test loads as a percentage of ambient nominal crushing capacity (see Table 1)

[b] The loading arrangement used meant that precise control of initial loading was challenging

[c] PGB = Progressive global buckling, where large plastic deformations were observed to transition into hinging near mid-height and tensile rupture on the unexposed face

Additional Observations

All four fire test specimens failed due to accelerating (runaway) lateral deflections resulting from increasing secondary moments and plastic deformation of the heated timber in compression, causing high strains in the tension fibres on the unexposed face, and eventually leading to sudden tensile rupture near mid-height. In line with previous observations by Klippel [18] from fire tests of vertical CLT wall elements, no major delamination was observed in any of the tests discussed herein. However, small pieces of char were observed to come away from the heated surface periodicaly during testing. This localised loss of char was possibly caused by moisture induced spalling, which has previously been described by White and Schaffer [36], and which can be pronounced in compression due to a reduction of permeability and abrupt changes in vapour permability at adhesive interfaces [13].

DISCUSSION

Fire-Induced Failure Modes

For a CLT compression element exposed to one-sided heating, as can be expected for a wall in a typical multi-storey building, loss of timber cross section on charring will reduce the load bearing capacity due to (at least) three distinct effects: (1) reductions in the strength and stiffness of the timber materials from which the column is made; (2) increases in both the effective slenderness of the column and the effective eccentricity of the applied load, resulting from the reduced effective cross section and a shift of the neutral axis of bending, thus increasing both first- and second-order bending moments; and (3) reductions in the size of the effective cross-section available to resist compressive loads.

An important, but rarely discussed, underlying assumption of the ZSL concept is that reductions in both strength *and* stiffness of thermally-affected timber with temperature are the same, and can thus be combined into a single, finite depth of timber with assumed negligible mechanical properties. Whilst the ZSL value of 7 mm currently suggested for design [4] was originally calibrated from bending tests on glulam beams [8], for a sufficiently slender compression element the reduced material stiffness and increasing effective eccentricity may dominate both the deflection response and failure mode. The effects of charring and heating on instability failures cannot be properly captured in flexural tests such as those from which the ZSL concept emerged. Furthermore, the reduction in elastic modulus of timber with elevated temperature is known to be more severe in compression than in tension [37]. Thus, the ZSL method is not, in the opinion of the authors, in its current codified form as presented in the Eurocodes [4] and based on its derivation[10], applicable to structural elements dominated by compression.

In the tests reported herein heating was localised to within the central 300 mm of the walls' height, whereas in a real fire the consequences of the behaviours described above may be even more critical (i.e. if an entire element is subjected to heating from one side). In a multi-storey residential building for instance, a load bearing CLT compression element may be a wall rather than a column, with potentially differing mechanical boundary conditions to those tested herein, and therefore with different buckling modes. Nonetheless, the criticality of fire-exposed load bearing CLT walls for preventing structural collapse should not be overlooked and is worthy of additional research attention, particularly as design aspirations push CLT buildings ever taller.

Importance of CLT Lay-Up

From an ambient temperature structural design perspective, it is most efficient to place the majority of the strong timber layers in a CLT element distant from the neutral axis, so as to increase the effective transformed flexural rigidity and reduce the chances of instability failures for CLT elements loaded in compression (or bending). For a given overall thickness of CLT panel this can either be achieved through the use of thicker outer lamellae, or by the use of a 3-ply build up rather than a 5-ply (or even 7-ply) system. Three-ply systems also place 66% of the timber in a 'strong' orientation, whereas as the number of plies increases the strong timber tends towards 50% of the cross-section. Frangi et al. [15] have also suggested that using thicker external lamellae, and a 3-ply rather than 5-ply build up, can reduce the risk of delamination in fire and thereby reduce the effective charring rate for CLT products that are susceptible to delamination. These factors could result in designers concluding that 3-ply systems are preferred to 5-ply systems. However, from a structural fire engineering perspective 3-ply systems may be problematic and

could lead to rapid failure with very little reserve capacity after loss of effectiveness of the outermost ply. Three-ply CLT products should therefore be avoided where possible.

The measured charring rates shown in Fig. 6 display an initial peak that persists until a stable, uniform char layer forms, thus reducing the rate of heat transfer to the pyrolysis zone and slowing char progression. The initial elevated charring rate is potentially important for assessing the load bearing capacity of CLT walls (particularly for 3-ply products), because of the aforementioned criticality of the outer lamella. An initially high charring rate will rapidly reduce the effectiveness of the outer lamella, whereas most designers would employ a constant, average charring rate for the full burning duration. This is a reasonable approach for glued laminated timber beams – for which the ZSL method was originally developed – because the lamellae strengths are non-variant with depth. However, the assumption of a constant charring rate would be non-conservative for structural fire design of CLT in compression.

A better approach would be to include a bi-linear charring model by introducing a peak charring rate for timber that has not yet built up a protective char layer. A similar concept is already used in EC5 [4] for initially protected timber surfaces with protection that subsequently becomes ineffective after a period of heating. This approach has also been suggested by Frangi et al. [38] for predicting charring rates of exposed timber after delamination in standard furnace tests.

Comments on the Reduced Cross Section Method

Failure times calculated for the four tests described herein, when based on an assumed charring rate of 0.65 mm/min and a ZSL of 7mm, are un-conservative. Figure 7 and Table 4 show that the deflection responses are poorly predicted and the times to failure are over-predicted by more than 100% in some cases; this is partly because the experimentally observed charring rates were somewhat greater than those assumed, but also because the assumed ZSL depth of 7 mm is much too small to properly account for the loss of compressive strength and stiffness in the thermally-affected timber below the char. This hypothesis was confirmed both by repeating the calculations and assuming the experimentally measured charring rates and a ZSL of 7 mm, and furthermore by using the experimental charring rates in combination with an increased ZSL of 18.9 mm [10]. In both cases the computational results were closer to the experimental deflection curves, and the time to failure predictions became slightly conservative for all tests.

The predicted deflection responses in Fig. 7 show that the paths to failure for all modelling scenarios tend to underestimate deflections and predict longer times to failure than observed in the tests. The errors in deflection response prediction are particularly evident for the 5-ply samples for which increases in deflection were greatly reduced as the weak cross-wise layers were heated. A finite ZSL approach means that the strength of layers that already contribute very little to the overall load bearing behaviour are reduced to a strength of zero, while any temperature gradient induced effects behind the zero strength layer are not included. Thus, a few millimetres difference in the assumed ZSL can have a major impact on the predicted deflection, and hence on the predicted failure time. This issue has also been discussed by Schmid et al. [11], who showed that CLT wall elements exposed to standard fires failed when the charring had reduced the initial strong parallel layer to only a few millimetres. On this basis, Schmid et al. [11] subsequently proposed that a minimum residual depth (of 3 mm) of the strong layers should be imposed in design to account for uncertainties in the charring rates arising from changes in density, moisture content, or fire exposure.

It is possible to approximate the depth of a ZSL that would be needed to predict the walls' lateral deflection responses and paths to failure. This was done using a trial and error approach, and the resulting values are given in Table 4. The computed values range between 15.2 and 21.8 mm. Schmid et al. [10] have previously proposed an alternative design approach for CLT wherein the ZSL depth varies depending on the loading conditions. For timber elements in flexural compression, a back-calculated ZSL in the range of 12.5 to 18.9 mm was proposed, with a mean value of 14.8 mm. Except for Test F3.10, the back calculated ZSL values lie just beyond the upper limit of this range.

The RCSM and the 7 mm ZSL were developed based on standard furnace tests on beams. The current research confirms that they cannot be applied directly to compression elements. The highlighted issue of an initial peak charring rate has also been observed for simulated standard fires [39] and should be explored further. It is noteworthy that, while the incident radiant heat flux exposure used in the current study was within the notional range that would be expected within the early stages of a standard furnace test, the

observed in-depth time-temperature increases were more severe than should be expected on the basis of codified charring rates [4]. However, this has been accounted for in the amended charring rates used in our predictive analyses (Fig. 7). Quantifying the potential thermal exposures relevant to design of CLT elements in real building fires is an area of ongoing research, although the available literature suggests that fires in CLT compartments (particularly those with exposed timber surfaces) are likely to be more severe than those in conventional non-combustible buildings [40]. Standard fire exposures (and hence standard fire resistance ratings) are therefore not applicable to such buildings, and additional research in this area is warranted. It is also difficult to use data from standard furnace tests to advance fire safety engineering knowledge to a point where practising engineers are able to consider structural fire safety of CLT buildings from first principles; this will be especially important for designing CLT buildings in the future since these types of buildings fall outside current fire resistance design frameworks.

Table 4. Predicted and measured times to failure, along with prediction errors as compared to experiments, for various modelling input parameter choices.

Test Name[a]	EC5 input [4]		Exp. char rates and 7 mm ZSL		18.9 mm ZSL from [10]		Required ZSL for best fit to test response
	FT[b] [min]	D[c] [%]	FT[b] [min]	D[c] [%]	FT[b] [min]	D[c] [%]	ZSL [mm]
F5-20	57.5	98	37.3	27	25.5	-13	≈21.8
F5-10	70.0	69	51.8	25	38.3	-7	≈21.0
F3-20	28.8	106	19.5	38	13.5	-4	≈20.0
F3-10	35.8	27	28.5	0.3	17.3	-39	≈15.2

[a] F = Fire test, 5 or 3 = 5-ply or 3 ply CLT, 20 or 10 = Nominal test loads as a percentage of ambient nominal crushing capacity (see Table 1)

[b] FT = Failure time calculated based on predicted exceedance of nominal material strength

[c] D = Difference between calculated and observed failure time in %

CONCLUSIONS AND RECOMMENDATIONS

Eight CLT walls of two different lamellae configurations were tested to study their structural response to fire while under sustained concentric compressive load; four of these were tested at ambient temperature to act as controls and to confirm repeatability, and the remaining four were locally subjected to a constant heat flux whilst simultaneously loaded to nominally 10 or 20 % of their theoretical ambient capacity (using nominal mechanical properties). The deflection responses of the walls and their failure times were compared against predictions made using an RCSM analysis approach, with various input parameter assumptions, to elucidate the mechanical response of CLT walls during one-sided heating. The following conclusions can be drawn on the basis of the testing and analysis presented herein:

• Failure of the walls was dominated by secondary moments and instability effects linked to reductions in flexural rigidity and a shift of the neutral axis due to charring, rather than depending primarily on loss of material strength.

• The predicted failure times calculated using EC5 [4] input parameters were highly un-conservative for the CLT compression elements tested. This corroborates results from other researchers under different heating conditions [10, 11].

• Increasing the assumed ZSL from 7 mm to 18.9 mm, in line with the maximum value for compression elements suggested by Schmid et al. [10], yielded slightly conservative failure times in all cases. However, to properly predict the lateral deflection paths to failure, ZSL values between 15.2 and 21.8 mm were needed.

• The CLT ply configuration has an important effect on the structural fire response of CLT walls. Three-ply walls yield higher ultimate capacities at ambient temperatures as compared to 5-ply configurations, but, as a consequence of the critical importance of the outer layer for preventing instability failures, they failed considerably earlier when exposed to fire.

- The effects of changes in the elastic modulus of timber need to be carefully assessed, since these are also critical for preventing instability. The current design approach in Europe [4] lumps together losses in strength and elastic modulus; this approach should be carefully reconsidered

- Experimentally observed charring rates exhibited an early peak when the char layer was shallow. Available design guidance [4] currently suggests use of average charring rates; however, for CLT compression elements this is unconservative due to the increased importance of the outer layers in preventing instability failures (as noted above). It is recommended to develop a bi-linear charring rate approach for exposed CLT compression elements.

- In the authors' view, the current ZSL concept is too coarse an approximation to sufficiently account for the necessary physics in complex materials such as fire-exposed CLT. This method should be discarded and replaced with a more rational one, particularly for multi-storey buildings with exposed CLT structural elements.

Recommendations for Further Research

This paper has provided compelling corroborating evidence that instability failures, rather than material failures, are likely to be the defining structural fire failure mode for CLT compression elements in buildings. It is therefore recommended that this area receive further attention considering various thermal and structural boundary conditions. The reduction of elastic modulus in CLT when heated should carefully be investigated, since stiffness (rather than strength) is critical for maintaining structural stability. A novel design approach is needed that captures the underlying physics leading eventually to failure, including variable charring rates and their effects on material stiffness as well as strength. This research will need to focus on the heat transfer beneath the pyrolysis zone and its impacts on the mechanical response of the thermally affected timber.

ACKNOWLEDGEMENTS

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