THE UNIVERSITY *of* EDINBURGH

Edinburgh Research Explorer

Thermo-mechanical behaviour of cross-laminated timber slabs under standard and natural fires

Citation for published version:

Mindeguia, J-C, Mohaine, S, Bisby, L, Robert, F, McNamee, R & Bartlett, A 2020, 'Thermo-mechanical behaviour of cross-laminated timber slabs under standard and natural fires', *Fire and Materials*.
<https://doi.org/10.1002/fam.2938>

Digital Object Identifier (DOI):

[10.1002/fam.2938](https://doi.org/10.1002/fam.2938)

Link:

[Link to publication record in Edinburgh Research Explorer](#)

Document Version:

Peer reviewed version

Published In:

Fire and Materials

General rights

Copyright for the publications made accessible via the Edinburgh Research Explorer is retained by the author(s) and / or other copyright owners and it is a condition of accessing these publications that users recognise and abide by the legal requirements associated with these rights.

Take down policy

The University of Edinburgh has made every reasonable effort to ensure that Edinburgh Research Explorer content complies with UK legislation. If you believe that the public display of this file breaches copyright please contact openaccess@ed.ac.uk providing details, and we will remove access to the work immediately and investigate your claim.

THERMO-MECHANICAL BEHAVIOUR OF CROSS-LAMINATED TIMBER SLABS UNDER STANDARD AND NATURAL FIRES

Jean-Christophe Mindeguia¹, Siyimane Mohaine², Luke Bisby³, Fabienne Robert², Robert
McNamee⁴, Alastair Bartlett³

¹I2M, University of Bordeaux, France

²CERIB, France

³School of Engineering, the University of Edinburgh, UK

⁴Brandskyddslaget, Sweden

ABSTRACT

The results presented in this paper are part of a multi-partner collaborative project named ‘The Épernon Fire Tests Programme’, the purpose of which is to analyse the different behaviours of combustible and non-combustible loaded structures when they are tested under standard and ‘natural’ fires. The project has several scientific objectives, such as quantification of the energy participation of combustible materials in fire tests, the influence of combustible surfaces and ventilation factors on the dynamics of compartment fires, and the thermomechanical behaviour of concrete and timber elements under natural fires. This paper focuses on the study of the thermomechanical behaviour of cross-laminated timber (CLT) slabs under standard and natural fires. The objective is to bring new experimental data to the complex study of the behaviour of unprotected CLT slabs exposed to fire. In addition, we propose a detailed analysis of the thermomechanical behaviour of the slabs, especially in order to better understand the failure of some of the slabs during the fire.

INTRODUCTION

Cross-Laminated Timber (CLT) products are massive elements of wood, made in the form of boards by gluing wood lamellas with alternating fibres directions (90°). These products allow the realization of floors and load-bearing walls with mechanical, thermal and acoustic characteristics deemed satisfactory to be used in high-rise buildings projects ¹. For such constructions, fire safety is obviously a major challenge ², and the fire behaviour of CLT walls and floors must be well known, especially when the structures can potentially be directly exposed to fire (unprotected structures).

The main manufacturers of CLT products have already carried out standard fire tests (see ³ for instance), which allow obtaining the fire grading of the product (on the basis of thermal insulation, integrity and load-bearing capacity according to ^{4,5,6}). CLTs generally obtain satisfactory fire ratings ⁷, allowing them to be implemented in many projects. However, beyond the mandatory aspect of these tests, it is important to keep in mind that the standard fire curve can be quite different from a real fire, called ‘natural fire’, because it does not involve a development phase, nor cooling ⁸. The standard fire does actually describes a defined growth phase with very low oxygen content that can happen in a real fire, but this is only representative of one possible fire scenario, i.e. for a particular compartment (geometry, linings, and ventilation) and without cooling phase. It then clearly cannot be considered representative of all possible scenarios that might warrant consideration in a CLT building (or in any type of building)

In addition, the fact is that under natural fire, particular behaviours has already been observed for CLTs: smouldering, delamination, fire recovery^{9,10,11}. In addition, the standard fire curve does not take into account the ventilation conditions of the compartment, which is essential for the way the fire develops¹² and spreads¹³, especially when the structure itself is combustible, as for unprotected CLT. Moreover, concerning the justification by calculation of the fire mechanical resistance, it is important to note that the current version of Eurocode 5¹⁴ does not provide precise requirements or methods for CLTs, especially under natural fire. It is worth noting however that current Eurocode 5 was written around 2000-2002, when CLT was still a young product. The new Eurocode 5 will give design methods for CLT and design methods for natural fire exposure.

There is therefore a real need for experimental results, both under standard and natural fires, in order to better understand the fire behaviour of CLT elements. In the last years, several research actions have been conducted on compartment fires with CLT elements. A series of compartment tests have been performed in Carleton University to study the contribution of CLT to fire, depending on its ratio of exposition and orientation of elements^{15,16,17}. The CLT was found to have no contribution to the fire when fully encapsulated. The presence of two exposed CLT surfaces led to delamination and a second flashover. In 2016, Brandon and Östman have reviewed the literature on performed compartment tests with combustible structures¹⁸. The authors have concluded that fully encapsulated timber elements have little to no contribution to the development of fire inside a compartment. However, the loss of protection during the fire led to higher charring rates depending on the test parameters. In the case of under-ventilated fires, it was observed that the presence of exposed timber could lead to more important external flaming. In some of these cases, no auto-extinction of fire was observed during the tests.

The US Department of Agriculture (USDA) has conducted five full-scale tests representative of a two-story apartment building made of mass timber – including CLT¹⁹. The performance of the building was assessed by varying the ratio of exposed timber (for both horizontal and vertical orientations) and delaying the time to activation of the sprinklers. The tests were run for four hours and in each case, the fire was contained in the unit of origin. The Fire Protection Research Foundation for the NFPA mission has conducted six large CLT compartment fire tests - including two baseline tests - in order to quantify the contribution of CLT building elements in compartment fires²⁰. It was found that the higher the exposed surface area of CLT, the bigger its contribution to the compartment fire. The peak temperatures with exposed CLT were found to be similar to the baseline, although with higher heat release rates and heat fluxes to the exterior façades. Exposed CLT experienced delamination during the tests which led either to a fire regrowth after decay or to no decay of the fire. Following these tests, a new series of compartment tests was performed by the National Research Council of Canada (NRCC) with more severe conditions (higher ratio of exposed timber, decrease of the total thickness of passive protection)²¹. The objective of this series was to observe the performance of a 2nd generation of CLT panels (with thermal resistant adhesive complying with ANSI/APA PRG-320:2018²²) in resisting char fall-off, which led to a second flash-over in the previous tests or to no decay of the fire. This new standard update requires full-scale testing with certain conditions to test the adhesive for delamination in fire. In the new series of compartment tests, no char fall-off was observed even after the char front passed the glue line bond. Except for two tests where the ratio of exposed surface was too high and where re-ignition of elements took place, auto-extinction of the fire was achieved.

The work presented in this paper fits directly into this context, presenting an experimental study on the fire behaviour of unprotected CLT slabs, both under standard and three natural fires.

This study is part of the multi-partner project “Épernon Fire Tests Programme” (test reports are free to download at ²³).

Five CLT slabs measuring 5.90 x 3.90 m in plan dimensions were manufactured in an industrial line with thermocouples installed in the slabs during the manufacturing process. Two slabs were tested in a fire resistance test furnace under exposure to a standard fire, and three slabs were tested in identical compartments aside from having three different ventilation conditions. The temperature measurements in the compartments and in the slabs are first presented. The temperature fields in the slabs allow estimation of the experimental charring rates and comparison with the behaviour of the first exposed timber lamella. The deflection measurements at mid-span of the slabs, as well as an analysis of their collapse times, is presented. A model based on a "zero-stiffness layer" is presented, which is complementary to more conventional approaches assuming a “zero-strength layer” ²⁴. The new model takes advantage of the measurement of the deflection to provide an analysis of the thermomechanical behaviour of the slabs and to help to explain why certain slabs have collapsed, and others not.

The aim of this study is not to propose new models for the fire design of CLT slabs, but to present different fire experiments results, some of them leading to the failure of slabs. The aim of the presented mechanical model is not to criticize or to compare it to standard calculation models. The aim of the model is to help analyzing the failure (or the absence of failure) of some of the slabs, thanks to the measurement of their deflection during fire.

The results presented in this paper can be useful for fire designers, or can be used as a case-study for the choice of a calculation framework.

EXPERIMENTAL STUDY

Manufacture and instrumentation of CLT slabs

The tested CLT slabs were factory-manufactured by a CLT supplier in France and represent products intended for use in the construction market. These were manufactured using polyurethane adhesive and vacuum pressing at room temperature for 24 hours. Their total thickness was 165 mm, with 5 spruce (C24 grade) lamellae of thickness 33 mm each. Specimens 12 x 2 m² in plan were fabricated before being cut and assembled to obtain 5.9 x 3.9 m² slabs (Figure 2). The CLT panel’s assembly (Figure 1) on the furnace followed the steps below:

- An intumescent seal (of width 20 mm and depth 2 mm) was bonded along the full-length at the level of the first lamella (exposed face) on the longitudinal side of the first CLT panel;
- The second CLT panel was bonded to the first;
- A layer of 13 mm thick soft ceramic wool was placed inside the cavity (of 154 x 33 mm² on full-length) at the two CLT panels’ upper junction;
- A wooden (spruce) strip of width 150 mm and thickness 25 mm was placed on top of the ceramic wool over the full length of the CLT panels (in two pieces of lengths 4.0 m and 1.9 m), inside the reservation at the junction between the two CLT panels; and
- The two CLT panels were fixed together by screwing the assembly strip to the fourth lamella of the two panels (by screws of 6 x 60 mm staggered every 250 mm starting from 75 mm of the slab end).

Figure 1. Detailing of the assembly of CLT panels above the furnace.

Slabs were tested with the odd layers (longitudinal layers) parallel to the mechanical span, which was 5.60 m.

Instrumentation was installed during the CLT fabrication, using shielded 1.5 mm diameter Type-K thermocouples. In a first attempt, the thermocouples instrumentation followed the steps below: a groove (parallel to the small dimensions of the slabs) was created in each of the first three layers to allow the thermocouples' passage and to not disrupt the gluing of the layers (Figure 3); several holes (of diameter 1 mm) at different depths were drilled in these layers; the thermocouples' ends were inserted inside the drilled holes, resulting in a few centimetres of TC that were perpendicular to the isotherms; and the thermocouples were stapled into the grooves and came out on the uncut longitudinal side of the CLT panels. Thermocouples were mounted in 4 locations in each slab (denoted A, B, C, and D). In each location 12 thermocouples were placed over the thickness, as shown in Figure 4. This protocol aimed to not disrupt the thermomechanical behaviour of slabs, and to minimize errors caused by inserting thermocouples in a low-conductivity material perpendicular to the isotherms²⁵. However, in order to check the reliability of the temperature measurement, additional thermocouples were also inserted perpendicular to the isotherms during one of the natural fire tests (scenario 3), at the interfaces of the plies. The Figure 5 presents the evolution of the temperature at the interface between the first and the second ply during natural fire scenario 3, for both techniques of thermocouple's installation. The figure also presents the difference between both types of thermocouples, showing an average temperature difference of almost 400 °C. It is clearly showed that the type of thermocouples installation has a great influence on the temperature measurement, and then on the resulting calculation of charring rates. As a consequence, only the thermocouples that were placed parallel to the isotherms, i.e. the ones placed at the interface of the plies (TCxe, TCxh and TCxi in Figure 4) are considered in this study for the calculation of the charring rates.

Figure 2. Top view of the CLT slab with positions of sensors.

Figure 3. Photos taken during the CLT fabrication and thermocouple installation.

Figure 4. Cross-section of the slab and positions of the thermocouples over the slab's thickness.

Figure 5. (a) Temperature at the interface between the 1st and 2nd lamellae using thermocouples installed parallel and perpendicular to the isotherms; (b) Temperature difference between the two thermocouples as a function of time (parallel – perpendicular). For both graphs: continuous lines: average values; dashed lines: minimum and maximum values).

Table 1 gathers measured moisture content of timber for each CLT experiment. The moisture content of the CLT slabs was determined on the day of each test. The three samples used for this measurement for each test (dimensions of 100 x 100 x 165 mm³, thickness composed of the 5 plies) were placed in an oven at 105 °C and removed from the oven when the mass decrease was less than 0.1% between two successive weightings spaced with 24 hours.

Table 1. Water content of timber used for the CLT fire experiments.

Moisture content at the day of the test (by mass)	
Standard test 1	12.60 % (± 0.15 %)
Standard test 2	11.60 % (± 0.2 %)
Natural fire test: Scenario 1	12.60 % (± 0.15 %)
Natural fire test: Scenario 2	11.05 % (± 0.05 %)
Natural fire test: Scenario 3	10.60 % (± 0.06 %)

Experimental program

The portion of the experimental programme reported in this paper consisted of two standard furnace tests (performed in accordance with EN 1363-1⁴ and EN 1365-2⁵ in a horizontal furnace), and three natural (compartment) fire tests. All tests were performed at CERIB, in Épernon France. Natural fire exposures used an outdoor experimental facility. The internal dimensions of the fire compartment were 6 x 4 x 2.52 m³. The basic material of the walls of the fire compartment was aerated concrete with a nominal density of 350 kg/m³ and a thickness of 30 cm. The floor was covered with calcium silicate boards (thickness 25 mm) on

top of mineral wool insulation (thickness 25 mm). Thermal properties at ambient temperatures of materials used for the compartment boundaries - as given in the technical sheets provided by the manufacturers - are gathered in Table 2. Thermal properties at ambient temperatures of wood and concrete according to the corresponding Eurocodes are also indicated in Table 2. These data are used as input for the parametric model calculation in the “Charring rate” section.

Table 2. Thermal properties of materials used for boundaries of the fire compartment

	Density [kg/m ³]	Specific heat [J/(kgK)]	Thermal Conductivity [W/(mK)]	Thermal inertia ("b" in Eurocode) [J/(m ² s ^{1/2} K)]
Aerated concrete blocks	350	1000	0.36	355
Calcium silicate board	900	1000	0.212	437
Mineral wool	96	1000	0.05	69
Wood	450	1530 (EC)	0.12 (EC)	287
Concrete	2400	900 (EC)	1.33 (EC)	1695

Ventilation conditions

Three different ventilation conditions were investigated during the natural fire tests. Each natural fire test had a different configuration of openings in one of the long faces of the compartment. The opening geometries are given in Table 3 and illustrated in Figure 6, along with the corresponding opening factors. These opening factors were calculated as defined in EN 1991-1-2 Annex A ²⁶, using the formula $A_v \sqrt{h_v} / A_t$ where A_v and h_v are the area and high of the openings respectively and A_t is the total area of enclosures (including floor and ceiling). These natural fires are denoted as scenarios 1, 2, and 3, respectively. The opening factors were chosen so as to induce a fuel-controlled fire (Scenario 1) as well as ventilation-controlled fires (Scenarios 2 and 3).

Table 3. Summary of opening geometries in the compartment fire tests.

Scenario #	Number of openings	Height of opening [m]	Width of opening [m]	Opening factor [m ^{1/2}]
1	2	2	2.5	0.144
2	3	1.2	1.25	0.050
3	1	2	1.1	0.032

Figure 6. Illustration of compartment configurations and corresponding opening factor.

Fire load

The wood cribs and fuel load were designed to achieve a representative fire load for dwellings. Six wood cribs with spruce wood sticks were used. Each crib was formed by 12 ranks of five wood sticks ($0.09 \times 0.09 \times 1 \text{ m}^3$ per stick) stacked and crossed at 90° , with a clear spacing of 0.1 m. The six wood cribs had a total mass of 1 191 kg, based on a mean timber density of 408.4 kg/m^3 (including an average moisture content of 11.4 %). The timber density was the average of nine measurements. Assuming a heat of combustion of 17.5 MJ/kg for wood ²⁶, the total fuel load provided by the six cribs was then 20 839 MJ. Three litres heptane per crib were used for ignition of the cribs (dimensions of the heptane container: $40.5 \times 40.5 \times 9 \text{ cm}$). Heptane has a heat of combustion of 44.6 MJ/kg ²⁷ and the density of 680 kg/m^3 , which leads to an additional fuel load of 91 MJ per crib, and a total of 546 MJ for the six cribs, assuming complete combustion. Thus, the total fire load delivered by the wood cribs and the fuel was 21 385 MJ, equivalent to a fire load density related to the surface floor area ($6\,000 \times 4\,000 \text{ mm}^2$) of 891 MJ/m^2 . This high fire load density is representative of the value prescribed by Eurocode 1, part 1-2, for dwellings ²⁶.

Mechanical load

For all fire exposures (i.e. furnace and compartment fire tests), the slabs were mechanically loaded with dead weights representative of an accidental load combination in accordance with EN 1991-1-1 ²⁸. Five steel beams were used as the dead weights on tops of the slabs, perpendicular to the mechanical span, with a total weight of 29.50 kN as illustrated in Figure 7. This corresponds to floor covering (0.10 kN/m^2), partitions (0.50 kN/m^2), variable actions (1.50 kN/m^2) and using a loading combination $G + 0.5 Q$. Deflection sensors were installed at mid-span on the unexposed face (referenced D1 and D2), as shown in Figure 2.

Figure 7. Mechanical load used for standard fire tests (*left*) and compartment fire tests (*right*).

RESULTS

Compartment temperatures

Figure 8 shows the evolution of the average, maximum and minimum temperatures measured by plate thermometers during the natural fire tests. These curves are compared to the standard fire curve. A much higher heating rate for natural fires is observed as compared to the standard fire. Scenario 1 had the highest heating rate, followed by Scenario 2, and Scenario 3. This correlates with the opening factors which gave lower availability of oxygen with lower opening factors (0.144, 0.050 and 0.032 m^{1/2} respectively). The maximum temperatures were similar for the three different scenarios, at about 1200 °C. Collapse of the slab tested under Scenario 3 occurred after 108 minutes, i.e. during the decay phase of the compartment fire. The slab tested under Scenario 2 collapsed 29 hours after the start of the fire, which was thought to be due to continued smouldering combustion. This last point may be important and should be investigated more in detail before giving clear conclusions. It is then not addressed in this paper. Analysis of the fire development in the presence of combustible structural elements is given elsewhere¹².

Figure 8. Temperatures in the compartments during natural fires and comparison to the standard fire. At right is a focus on the 20 first minutes of exposure.

Integrity of the 1st lamella

Many authors have observed delamination (i.e. fall-off) of the first lamella of CLT panels during fire tests ^{7,9,10}. Delamination of the 1st lamella is important for the fire behaviour of CLT because this can result in a sudden increased heating of the underlying lamella, and this increases the charring rate and accelerates loss of section and loadbearing capacity. In addition, delamination may occur during the decay phase of a fire, and under some conditions this can result in secondary flashover ¹¹. No large “global delamination” events, in which large parts of the outer lamella detached, were observed in the experiments presented herein. Rather, “local delamination” events, in which a progressive loss of small sections of the 1st lamella occurred, were repeatedly observed. This is illustrated in Figure 9 which shows the evolution of gas-phase temperatures (T_{ck}) and the temperatures at the interface of the first 2 lamellae (T_{ce}), measured at positions A, B, C, and D (see Figure 2 and Figure 4 for the thermocouples’ locations). Results are provided for Standard Fire 2 (a repeat of Standard Fire 1) and all three natural fire tests. The 1st lamella is considered totally lost when T_{ce} ≈ T_{ck}, i.e. when the interface temperature equals the gas-phase temperature.

Figure 9. Temperatures measured in gas-phase and at interface between the 1st and 2nd lamellae.

For both standard furnace tests, the loss of the 1st lamella occurred between 55 and 70 minutes. For natural fire Scenario 3, the loss occurred between 65 and 85 minutes. During these time intervals, the compartment gases are still very hot (around 900 °C for the standard fire and between 900 and 1100 °C for natural fire Scenario 3). As a result, the 2nd lamella underwent a rapid increase in exposure temperature, but without having a significant impact of its charring behaviour (see next section). This is generally attributed to the fact that the loss of the 1st ply occurs after the 300 °C isotherm. For natural fire Scenario 2, the loss of the 1st lamella occurred between 45 and 65 minutes, at which point the temperatures of the compartment were lower (between 600 and 1000 °C). For natural fire Scenario 1, the loss of the first lamella was less

obvious, or at least the resulting temperature increases were less noticeable. The loss appears to have occurred between the 40th and 60th minute of the fire, i.e. for gas temperatures below 600 °C. The faster development of natural fire Scenario 1, and the earlier decay phase therefore seem to minimize the influence of the loss of the first lamella on the thermo-mechanical behaviour of the slab as will be discussed in the following sections.

Charring

The charring rate is one of the most important parameters when dealing with the load bearing capacity of a CLT member – or more generally, of wood-based materials during a fire. This parameter is a vital input for the design models used for that matter (i.e. in EN 1995-1-2¹⁴, it is used in the Reduced Cross-Section Method). In this study, the char-line was monitored using temperature measurements at the interfaces between plies. The char-line was monitored using the 300 °C isotherm. Figure 10 illustrates the charring depths for all tests. The continuous line represents the mean value of the 300 °C isotherm for each depth. The single points represent the measurements at each of the four positions (A, B, C and D) to illustrate the scatter of the data. The charring rates are summarized in Table 4. In Figure 10 some of the charring depths obtained with the thermocouples inserted into the lamellas are also plotted (scenario 1 and 3). Although these points are not taken into account in our analysis, it shows that charring probably went through the next lamella.

Figure 10. Charring depth for CLT slabs under both standard fire tests (*left*) and natural fire exposures (*right*).

Table 4. Approximate charring rates at different stages for standard fire tests and natural fire tests using only thermocouples placed between two successive lamellas and considering start of charring at $t=0$.

Charring rate (mm/min)	In the 1 st lamella	In the 2 nd lamella	In the 3 rd lamella
Scenario 1 ($O = 0.14 \text{ m}^{1/2}$)	1.43 (min 1.29 – 1.65)	Charring through the lamella*	No charring recorded
Scenario 2 ($O = 0.05 \text{ m}^{1/2}$)	1.19 (min 1.13 – max 1.4)	0.80 (min 0.72 – max 0.86)	No charring recorded
Scenario 3 ($O = 0.03 \text{ m}^{1/2}$)	0.85 (min 0.76 – max 0.9)	0.92 (min 0.73 – max 1.17)	Charring through the lamella*
Standard furnace tests	0.71 – 0.75 mm/min		

* Temperature measurement not taken into account for TC put inside plies (perpendicular to isotherms)

The estimated char depths (i.e. based on the 300 °C isotherms) were found to be consistent with the remaining lamellae of the slabs at the end of the natural fire tests. For Scenario 1 (Figure 11), 3.5 layers of CLT remained. For Scenario 2, after the collapse of the slab at 29 hours, only 2.5 layers remained. No observation was possible at the end of Scenario 3 due to its full burnout after collapse.

Figure 11. Remaining CLT layers at the end of natural fire Scenario 1.

For both standard furnace tests, the evolution of charring depth with time was relatively linear, as is typically reported in the literature for furnace testing^{30,31}. The charring rate calculated on the basis of a linear relation for both tests was reasonably repeatable (at 0.71 and 0.75 mm/min), with rates slightly higher than the basic design charring rates β_0 assumed for example in the current version of EN 1995-1-2¹⁴ ($\beta_0 = 0.65 \text{ mm/min}$). It is worth noting that the loss of the 1st ply, as it occurred after having reached 300 °C, does not influence the charring behaviour of the subsequent plies. A more recent model³² was developed to determine the notional charring rate β_n for cross-laminated timber and for wood-based materials in general. This approach relates β_n with the basic design charring rate β_0 given in EN 1995-1-2¹⁴ with coefficients that can increase the charring rate by considering the peculiarities of the CLT element as represented in equation [1]:

$$\beta_n = \prod_{k_i} k_i \beta_0 \quad [1]$$

where k_i are the coefficients influencing the charring rate of timber, see³².

For a typical non-protected CLT product, only one coefficient can be considered according to the authors; this coefficient accounts for gaps between boards (k_g). The gap factor can be assumed as:

$$k_g = \begin{cases} 1.0 & \text{for gaps} \leq 2 \text{ mm} \\ 1.2 & \text{for gaps} > 2 \text{ mm and} \leq 5 \text{ mm} \end{cases} \quad [2]$$

For gaps with a width greater than 6 mm, a fire with exposure from three sides should be used in the calculation. The authors introduce two models to represent the evolution of charring depth with time when there is a significant falling-off of layers and for initially unprotected slabs that contain gaps between the layers: (1) The double charring rate model as illustrated in Figure 12 and (2) a mean notional charring rate $\beta_{n,mean} = 1.2 \text{ mm/min}$ for a fire resistance up to 90 minutes. This value is presumed to be a conservative value which implicitly takes into account falling off of the layers and double charring for layers $i \geq 2$ ³².

Figure 12. Charring rates for different CLT applications with significant falling off of layers according to ³².

Figure 13 shows a comparison between the evolution of CLT charring based on the average charring rate value obtained from the standard tests in the Épernon Fire Tests Programme and based on (i) the double charring rate and (ii) the simple charring rate $\beta_{n,mean}$ models proposed by ³². For this model, an individual notional charring rate $\beta_{n,i}$ was calculated for each layer. The transverse layers had gaps of width $2 \text{ mm} < w \leq 5 \text{ mm}$ which led to use $k_g = 1.2$ (only in the transverse plies). From the figure it can be seen that both charring models proposed by ³² are conservative compared to the experimental charring depth.

Figure 13. Comparison between CLT charring evolution based on experimental results (average value for both standard fire tests) and on double and simple charring rate models from ³².

For the natural fire tests, the evolution of charring depths is also shown in Figure 10 and the resulting charring rates are given in Table 4. As expected, the charring rate through the 1st lamella for the three slabs followed the same trend as the compartment temperature: the higher the compartment temperature, the higher the charring rate (Scenario 1 > Scenario 2 > Scenario 3). However, the compartment temperature for Scenario 1 was the earliest and the most rapidly decaying (after approximately 30 minutes). This fast decay effectively halted the charring front inside the 2nd lamella. For Scenarios 2 and 3, the charring front continued its progression through the 2nd lamella. More particularly for Scenario 3, for which the delayed and less rapid decay phase (after approximately 60 minutes), a continuous progression of the charring front through the third lamella was observed even during the decay phase, since the temperatures within in the compartment remained comparatively high. For Scenario 2, the charring rate of

the 2nd lamella (0.80 mm/min) decreases compared to charring through the 1st ply (1.19 mm/min) as the decay phase of the fire starts around 40 minutes. For Scenario 3, the overall average charring rate through the 2nd lamella is slightly higher than the charring rate through the 1st ply. When the char line was within the 2nd lamella (i.e. on average between 40 and 75 minutes), temperature within the fire compartment was still increasing and remained relatively high (more than 1000 °C).

In the following, the charring depth given by the Annex A of the Eurocode 5 ¹⁴ for parametrical fire is compared to the experimental results. This charring model is based on the parameters used for calculating the temperature in the gas phase according to the Annex A of Eurocode 1, part 1-2 ²⁶, i.e. the opening factor of the compartment (O), the thermal absorptivity of the total enclosure (b), the design fire load density related to the total area of floors, walls and ceiling which enclose the compartment ($q_{t,d}$), and the notional design charring rate (β_n). The Table 5 summarizes these different parameters depending on the scenario of natural fire. The charring model for parametrical fire is nonlinear, depending on time intervals given by a time t_0 (also given in Table 5). It is worth noting that the charring model given by Eurocode 5 is limited in time ($5.t_0$) and in depth (quarter of the thickness, i.e. 41.25 mm in this study).

Table 5. Parameters used to calculate both the gas temperature in the compartment (Annex A of Eurocode 1, part 1-2 ²⁶) and the induced charring depth in slabs (Annex A of Eurocode 5 ¹⁴).

	Design charring rate (mm/min) β_n	Opening factor ($m^{1/2}$) O	Absorptivity of the total enclosure ($J.m^{-2}.s^{-1/2}.K^{-1}$) b	Design fire load density (MJ/m ²) $q_{t,d}$	t_0 (min)
Scenario 1 (O = 0.14 m ^{1/2})	0.65	0.14	322	173.8	10.9
Scenario 2 (O = 0.05 m ^{1/2})		0.05	345		31.2
Scenario 3 (O = 0.03 m ^{1/2})		0.03	334		49.5

The Figure 14 presents for natural fires 1 and 2 the measured gas temperature (average, minimal and maximal values) and the calculated gas temperature by the parametrical model of Eurocode 1, part 1-2. The same figure also presents the charring depth, the one measured and the one calculated by the Eurocode 5 model. Due to the fact that t_0 is higher than 40 minutes, the Eurocode charring model is not applicable for scenario 3.

Figure 14. Gas temperature in the compartment (measured vs. calculated by the Eurocode 1 parametrical fire model) and charring depth (measured vs. the Eurocode 5 model for parametrical fires).

For both scenario, we note an underestimation of charring depth at the 1st ply interface. The gap is higher in the case of scenario 1, which is a fuel-controlled fire. It is worth noting that Eurocode 5 parametric model was not able to predict the charring in the 2nd ply for scenario 2 due to its depth limitation (quarter of the thickness). These results underline the fact that more data is needed to improve the charring model for CLT slabs exposed to natural fires. In parallel, the reliability of the Eurocode 1 parametrical model can be questioned in case of a combustible ceiling. A more accurate fire model is presented in ¹².

Deflection and slab failure

Due to an important deflection of the slabs during the standard fire tests (more than 30 cm at mid span), the steel beams that were used as dead loads came unintentionally in contact with the furnace frame long after reaching the limiting deflection of the slabs. It occurred at 140 and 136 minutes, respectively for standard fire tests 1 and 2. After this time the slabs were then totally unloaded. After that it was decided to let the burners on, but only until 154 and 169 minutes (test 1 and 2 respectively), due to safety reasons. After the stop of the burners, a total collapse of the slabs occurred at 203 and 179 minutes (test 1 and 2 respectively). In that context, it is difficult to evaluate the real fire resistance of the slabs. The fire resistance of slabs was then assessed based on their deflections (as given in EN 13501-2 ⁶, i.e. 475.1 mm of deflection or a deflection rate of 21.1 mm/min). The failure of the slabs was reached at 125 and 121 minutes (test 1 and 2 respectively). Of the three natural fire tests, only the slab tested under Natural Fire 3 experienced collapse during the primary fire, at 108 minutes. The slabs tested under natural fire scenarios 1 and 2 did not collapse during the fire development phase or during the initial decay phase. The slab tested under natural fire Scenario 2 collapsed 29 hours after the start of the fire, but this particular behaviour may need more analysis and is not addressed in this paper. The failure times are summarized in Table 6.

Table 6. Failure time for slabs tested under standard furnace tests and natural fires. For standard furnace tests, EN 13501-2 criteria for fire resistance ⁶ are also provided.

	Standard Fire 1	Standard Fire 2	Scenario 1 $O = 0.144 \text{ m}^{1/2}$	Scenario 2 $O = 0.050 \text{ m}^{1/2}$	Scenario 3 $O = 0.032 \text{ m}^{1/2}$
Collapse time	203 min (unloading at 140 min and burners stopped at 154 min)	179 min (unloading at 136 min and burners stopped at 169 min)	No collapse	No collapse during the heating and cooling phase (failure after 29 hours ^{**})	108 min
Time for limiting deflection (EN 13501-2)	125 min	121 min	Limiting deflection not reached*		107 min*
Time for limiting rate of deflection (EN 13501-2)	141 min	131 min	Limiting rate of deflection not reached*		107 min*

* Only for information, EN 13501-2 is not applicable for natural fire tests.

** Not addressed in this paper.

Figure 15 shows the evolution of the measured mid-span deflections (average of sensors D1 and D2, see Figure 2). The deflection underwent a significant acceleration about 20 minutes before the collapse of scenario 3 or reaching the limit criteria for both standard fires. A thermomechanical interpretation is proposed in the following section. During the first 30 minutes of testing, the rate of deflection correlates well with the rate of temperature development, i.e. Scenario 1 > Scenario 2 > Scenario 3 > Standard fires.

Figure 15. Evolution of the mean mid-span deflections for all fire tests. At right, first 80 minutes.

MECHANICAL ANALYSIS

The most commonly used method for calculating the fire resistance of timber structural elements is the so-called Reduced Cross-Section Method (RCSM), detailed in Part 1-2 of Eurocode 5 ¹⁴. This method is based on the assumption of a “zero-strength layer (ZSL)”, i.e. a layer of timber beneath the char line with total loss of mechanical properties due to the rise in

temperature of thermally affected timber. This zero-strength layer is added to the char thickness to give a thickness to be neglected performing mechanical calculations during fire exposure. In reality, because an in-depth temperature gradient exists within the member, there is also a gradient for deterioration of the mechanical properties, and the zero-strength layer is a fictional thickness accounting for this gradient. However, the ZSL approach significantly simplifies mechanical calculations at high temperatures. The thickness of the zero-strength layer is fixed at 7 mm by Eurocode 5. It should be noted that EN1995-1-2 in its current version is not strictly applicable to CLT, but it is used here regardless for illustrative purposes. The 7 mm ZSL value is strictly applicable only to structures subjected to standard furnace exposures. As the mechanical resistance profile is controlled by the thermal gradient in the first centimetres of the timber, the true zero-strength layer needed to account for in-depth heating beneath the char line will be affected by the heating rate (i.e. the type of fire and/or the presence of thermal protection) and by any mechanical stress. Some studies^{16,25} therefore have criticized the fixed ZSL value and recommended values adapted to the type of mechanical stress and/or adapted to be used specifically with CLT. To the authors' knowledge no data currently exist concerning the zero-strength layer development of CLT structures under natural fires exposures.

A recent study by Schmid et al.²⁶ proposes a zero-strength layer model adapted to CLT based on a study of the collapse of structural elements. A different (but complementary) approach is proposed here, based on the analysis of the deflection of the slab during heating. The method is inspired by²⁷ and²⁸. In the same way that the assumption of a zero-strength layer can be made, the existence of a “zero-stiffness layer” can also be imagined. Eurocode 5 predicts a temperature-dependant degradation curve for the modulus of elasticity of timber which is very similar to the tensile and compressive strength degradation curves. The knowledge of the zero-stiffness layer thus makes it possible to analyse the mechanical behaviour of the slabs exposed to fire and can thus constitute a useful tool for explaining the failure of some slabs of the study.

It is important to precise that during heating of the loaded slabs, a non-negligible part of the total deflection is due to the thermal bowing of the slab²⁹, especially at the beginning of heating. This is obviously directly linked to the coefficient of thermal expansion of wood. In this study, we decided to include the part of deflection due to thermal bowing in the definition of the zero-stiffness layer, in order to keep the model as simple as possible. It is worth noting that in this case, values obtained for zero-stiffness layer are certainly higher than the real layer in which the stiffness of wood become negligible. As a consequence, it is not relevant to compare the obtained values to classical zero-strength layer values.

The mechanical model for CLT slabs during fire tests carried out in this project is shown in the Figure 16. Using a Navier-Bernoulli beam model (assuming a 1 m wide beam) and the unit force method, an expression for the mid-span deflection of the slabs can be deduced (equation [3]).

Figure 16. Structural model of the CLT slabs.

$$\text{deflection (in mm)} = \frac{1.345 \times 10^7 \cdot F}{(EI)_{eff}} \text{ with } F \text{ in (kN) and } (EI)_{eff} \text{ in (N.m}^2\text{)} \quad [3]$$

In the tests described herein, $F = 5.9$ kN, distributed on 3.9 m width of the slab. As the calculations are performed on a 1 m wide beam, the force to be taken into account is 1.513 kN. The main parameter of the equation is the effective stiffness of the slab, which in the case of CLT can be determined by a composite elastic mechanical model. In this study, the shear analogy method (or Kreuzinger method ³⁰) is used. This method is commonly applied for Serviceability Limit State (expect for vibration) and Ultimate Limit State verification of CLT structural elements at room temperature ³¹. In this approach, the composite aspect of the CLT slab is taken into account by calculating the position of the centroid and the calculation of an effective stiffness, depending on the respective thicknesses of the lamellae and their mechanical properties, according to Figure 17 and equations [4], [5], and [6]. To take into account the anisotropy of the slab, the modulus of elasticity of the transverse layers is estimated as one thirtieth of the modulus of the longitudinal layers ^{28,31}. The measurement of the deflection at 20 °C (i.e. following the mechanical loading of the slab and before the start of the fire) allows calibration the modulus of elasticity of the material ($E_{1,3,5} = 13,500$ MPa).

Figure 17. Cross-sectional parameters for the calculation of effective stiffness.

$$(EI)_{eff} = \sum_{i=1}^n E_i \cdot b \cdot \frac{t_i^3}{12} + \sum_{i=1}^n E_i \cdot b \cdot t_i \cdot d_i^2 \quad [4]$$

i is for each CLT lamella, n = total number of lamellas (5 in this study)

$$\text{with } d_i = |Z_i - Z| \quad [5]$$

$$\text{and with } Z = \frac{\sum_{i=1}^n E_i \cdot t_i \cdot b \cdot Z_i}{\sum_{i=1}^n E_i \cdot t_i \cdot b} \quad [6]$$

When exposed to fire from below, a char front progresses into the slab. This follows the progression of the front corresponding to the zero-stiffness layer. Thus, the char layer (thickness d_{char}) and zero-stiffness layer (thickness $d_{0,s}$) is removed from the initial cross-section for the calculation of the effective stiffness. As said previously, $d_{0,s}$ includes the part of deflection due to thermal bowing of the slab. The total thickness of material removed from the calculations is then called the effective thickness d_{ef} ($d_{ef} = d_{char} + d_{0,s}$). During fire, several cases can then occur (see Figure 18):

- $d_{ef} < t_1$: in this case, the CLT is still composed of five lamellae and then the calculation is based on a 5-lamella CLT model. The thickness of the first lamella must be simply reduced by d_{ef} .

- $t_1 \leq d_{ef} < t_1 + t_2$: d_{ef} is located in the 2nd lamella, i.e. a transversal lamella of the CLT. In our model, as in other studies ^{34,39}, we assume that the heated transversal layers cannot contribute to the load-bearing of the slab. The transversal layers allow the longitudinal layers to work as a composite slab, as long as they stay cold. The stiffness of the cold transversal layers is taken into account for the calculation of the global stiffness of the slab. But in the case of d_{ef} being in the 2nd ply, the composite model is not able to calculate the deflection of the slab. This is then not possible to determine the exact position of d_{ef} . Thus, a time interval exists corresponding to the positioning of d_{ef} in the 2nd lamella, for which the method does not allow estimation of $d_{0,s}$. This interval is however not too long regarding the duration of the fire, and is far from the failure time of the slab (see Figure 19).

- $d_{ef} = t_1 + t_2$: at this moment of heating, d_{ef} is no longer in the 2nd lamella. The calculation is therefore based on a 3-lamella CLT section with lamellas not yet altered by the temperature.

- $d_{ef} > t_1 + t_2$: d_{ef} is in the 3rd lamella, the section is thus calculated as a 3-lamella CLT, whose first lamella is altered by temperature and then reduced by $(d_{ef} - t_1 - t_2)$.

Figure 18. Mechanical constitution of the CLT slab depending on time.

Thanks to the measurements made during testing, the mid-span deflection of the slab is known at all times of heating. By inverse analysis, and while considering the different configurations presented in Figure 18, it is possible to calculate, at each moment of the fire, the value of d_{ef} that fits the experimentally observed deflection. Since the position of the char front is also known (Figure 10), it is possible to determine the thickness, $d_{0,s}$, of the zero-stiffness layer. We precise here that the char front used in the model is an average model of experiments, based on a linear extrapolation between experimental measurements. The char depth is then an estimation based on the data, and not an absolute known value.

Figure 19 shows the deflection measured at mid-span and that calculated by the proposed model for the fire tests (Standard Fire 2 is similar to 1). The effective stiffness d_{ef} of the cross-section is calculated at each moment by accounting for the char thickness (300 °C isotherm) and the zero-stiffness layer thickness $d_{0,s}$ (from the inverse analysis). In the same figure the evolution of the char thickness d_{char} , the zero-stiffness thickness $d_{0,s}$ and the effective thickness d_{ef} ($d_{ef} = d_{char} + d_{0,s}$) are given. The graphs also show the time interval during which d_{ef} is in the 2nd lamella, without knowing exactly where. Since $d_{0,s}$ includes the contribution of thermal bowing, it is not relevant to compare it to classical values of zero-strength layer. Nevertheless, it appears that the thickness of this layer changes with time, with values that depend of the fire scenario.

Figure 19. On the left, plot of the measured mid-span and the one calculated by the composite model with variable effective stiffness (standard fire test 2 is similar to test 1). On the right, corresponding evolution of char thickness d_{char} , zero-stiffness layer thickness $d_{0,s}$ and effective thickness d_{ef} .

The Figure 20 shows the evolution of d_{ef} for the slabs that failed during the test, i.e. both standard fire tests (failure evaluated by deflection criteria, 125 and 121 minutes) and scenario

3 (real collapse of the slab at 108 minutes). For these tests a quick increase of deflection while d_{ef} is getting closer to the 4th lamella can be observed. After a certain time, d_{ef} reaches the 4th lamella and it can be noted that this time corresponds precisely to the time of slab failure. When d_{ef} reaches the 4th lamella, and since it is assumed that the temperature-altered transversal layer is not taken into account, the residual cross-section is no longer a composite (CLT) cross-section and only the 5th lamella remains. The last ply is then not able to sustain the load by itself, and the failure of the slab occurs.

Figure 20. Evolution of the effective thickness d_{ef} for testing configurations where failure of the slabs was observed.

On the other hand, Figure 21 shows the evolution of d_{ef} for the slabs that did not fail during the fires, i.e. scenarios 1 and 2. For these tests, d_{ef} remains in the 3rd lamella and the deflection does not experience a sudden increase. Moreover, no failure of the slabs is observed. It seems that the remaining thickness of virgin wood in the 3rd ply, even if small (9 mm for scenario 1 and 5 mm for scenario 2), was enough to prevent the slabs from failure.

The evolution of d_{ef} seems then a good indicator of the CLT slabs' failure. In our case, the failure of the slab only occurred if d_{ef} reached the penultimate ply. More experimental results are needed to confirm this result. In addition, a physical analysis of d_{ef} is needed in order to make it a potential tool for predicting the fire performance of CLT slabs, exposed to any type of fire exposure.

Figure 21. Evolution of the effective thickness d_{ef} for testing configurations where no failure of the slabs was observed.

CONCLUSIONS

The thermomechanical behaviour of CLT slabs subjected to fire is complex to predict. Indeed, many phenomena affect the progression of the charring front preceded by the existence of a layer of material with mechanical properties strongly degraded by temperature. In addition, the mechanical modelling of CLTs is complex because it implies the use of anisotropic composite models, whose geometric properties evolve during fire. In that context an experimental study has been carried out and the main results have been presented here.

The experiments allowed calculating the charring rate of the slabs, when exposed to standard fire tests (x2) and 3 natural fire with different ventilation conditions. The results showed that the charring of slabs exposed to standard fires was quite expectable and easy to predict, by using Eurocode 5, part 1-2 for example. On another hand, the charring behaviour of the slabs when exposed to natural fire was more difficult to predict. In particular, we showed that a charring parametrical model (the one given by Eurocode 5 in this paper) was not able to correctly predict the charring.

A thermomechanical model assuming a zero-stiffness layer has been used in order to explain the failure of certain slabs of the study. The mechanical analysis, based on deflection calculation, underlines the fact that the position of the charring front is not the best indicator for the structural performance of CLT slabs. The time of failure of slabs shows no correlation with the charring front alone. As quite expected, the global mechanical behaviour of the slabs is strongly dependent on the position of an effective depth d_{ef} , i.e. a sacrificial layer composed by charring plus an extra layer that includes the decay of mechanical properties (and the effect of thermal bowing). It has been showed that the failure of the slabs, for 3 different fire exposure of this study, occurred as soon as d_{ef} reached the penultimate ply of the slab. d_{ef} seems then a potential good indicator for CLT fire performance. The study also showed that the evolution of d_{ef} is dependant of the thermal exposure. Later investigations will then analyse, from a thermal point of view, the physical meaning of the zero-stiffness layer, before being able to correlate it with the parameters of the fire.

REFERENCES

- ¹ Schmidt J, Griffin CT. Barriers to the design and use of cross-laminated timber structures in high-rise multi-family housing in the United States. In: *Structures and Architecture: Concepts, Applications and Challenges*. Taylor & Francis Group. London: Cruz P.; 2013:2225-2231.
- ² Barber D. Tall Timber Buildings: What's Next in Fire Safety? *Fire Technology*. 2015;51(6):1279-1284. doi:[10.1007/s10694-015-0497-7](https://doi.org/10.1007/s10694-015-0497-7).
- ³ STORAENSO. CLT - Cross-laminated timber - Fire protection. Commercial document, Version 01/2016 AG. 2016.
- ⁴ EN 1363-1, Fire resistance tests –Part 1: General Requirements (European Norm), 2012.
- ⁵ EN 1365-2, Fire resistance tests for loadbearing elements - Part 2: Floors and roofs (European Norm), 2014.
- ⁶ EN 13501-2, Fire classification of construction products and building elements - Part 2: Classification using data from fire resistance tests, excluding ventilation services (European Norm), 2016.
- ⁷ Frangi, A., Fontana, M., Hugi, E., Jübstl, R., Experimental analysis of cross-laminated timber panels in fire. *Fire Safety Journal* 44, 1078–1087. <https://doi.org/10.1016/j.firesaf.2009.07.007>.
- ⁸ Bisby L, Gales J, Maluk C. A contemporary review of large-scale non-standard structural fire testing. *Fire Sci Rev*. 2013;2(1):1. doi:[10.1186/2193-0414-2-1](https://doi.org/10.1186/2193-0414-2-1).
- ⁹ Bartlett, A., Hadden, R., Bisby, L., Law, A., Analysis of cross-laminated timber upon exposure to non-standard heating conditions. Presented at the Fire and Materials, San Francisco, USA, 2015.
- ¹⁰ Brandon, D., Dagenais, C., Fire Safety Challenges of Tall Wood Buildings – Phase 2: Task 5 – Experimental Study of Delamination of Cross Laminated Timber (CLT) in Fire. Fire Protection Research Foundation, 2018.
- ¹¹ Su, J., Lafrance, P.-S., Hoehler, M., Bundy, M., Fire Safety Challenges of Tall Wood Buildings – Phase 2: Task 2 & 3 – Cross Laminated Timber Compartment Fire Tests. Fire Protection Research Foundation, 2018.
- ¹² McNamee, R., Zehfuss, J., Bartlett, A., Heidari, M., Robert, F., Bisby, L., 2020. Enclosure fire dynamics with a cross-laminated timber ceiling. *Fire and Materials*.
- ¹³ Bartlett, A., Bisby, L., Robert, F., Rottier, C., Jansson McNamee, R., Comparative Energy Analysis of Fire Resistance Tests on Combustible Versus Non Combustible Slabs. Presented at the Workshop on advancements in evaluating the fire resistance of structures, Washington, DC, USA, 2018.
- ¹⁴ EN 1995-1-2, Eurocode 5: Design of timber structures - Part 1-2: General - Structural fire design (European Norm), 2004.
- ¹⁵ McGregor, C., 2013. Contribution of cross laminated timber panels to room fires (Text). Carleton University.
- ¹⁶ Hevia, M., Ramon, A., 2015. Fire Resistance of Partially Protected Cross-Laminated Timber Rooms (Text). Carleton University.
- ¹⁷ Li, X., Zhang, X., Hadjisophocleous, G., McGregor, C., 2015. Experimental Study of Combustible and Non-combustible Construction in a Natural Fire. *Fire Technol* 51, 1447–1474. <https://doi.org/10.1007/s10694-014-0407-4>.
- ¹⁸ Brandon, D., Östman, B., 2016. Fire Safety Challenges of Tall Wood Buildings - Phase 2 : Task 1- Literature Review, Final Report, Fire Protection Research Foundation, NFPA.
- ¹⁹ Zelinka S. L., Hasburgh L. E., Bourne K. J., Tucholski D. R., Ouellette J. P., 2018. Compartment Fire Testing of a Two-Story Mass Timber Building - General Technical Report FPL-GTR-247, United States Department of Agriculture.

- ²⁰ Su J., Lafrance P.-S., Hoelher M., Bundy M., 2018. Fire Safety Challenges of Tall Wood Buildings - Phase 2: Task 2 & 3 - Cross Laminated Timber Compartment Fire Tests, Fire Protection Research Foundation, NFPA, NRCC.
- ²¹ Su, J., Leroux, P., Lafrance, P.-S., Berzins, R., Gratton, K., Gibbs, E., Weinfurter, M., 2018. Fire testing of rooms with exposed wood surfaces in encapsulated mass timber construction, Client Report (National Research Council of Canada. Construction); no. A1-012710.1. National Research Council of Canada. Construction. <https://doi.org/10.4224/23004642>.
- ²² ANSI/APA PRG 320-2018, 2018. Standard for Performance-Rated Cross-Laminated Timber, American National Standards Institute.
- ²³ <http://www.epernon-fire-tests.eu/>
- ²⁴ Schmid, J., König, J., Just, A., The Reduced Cross-Section Method for the Design of Timber Structures Exposed to Fire—Background, Limitations and New Developments. *Structural Engineering International* 22, 514–522. <https://doi.org/10.2749/101686612X13363929517578>, 2012.
- ²⁵ Fahrni, R., Schmid, J., Klippel, M., Frangi, A., Correct temperature measurements in fire exposed wood. Presented at the World Conference on Timber Engineering (WCTE 2018), ETH Zurich, Institute of Structural Engineering (IBK), p. MAT-09-02. <https://doi.org/10.3929/ethz-b-000289850>, 2018.
- ²⁶ EN 1991-1-2, Eurocode 1: Actions on structures - Part 1-2: General actions - Actions on structures exposed to fire (European Norm), 2002.
- ²⁷ Hurley MJ, Gottuk DT, Jr JRH, et al., eds. *SFPE Handbook of Fire Protection Engineering*. 5th ed. New York: Springer-Verlag; 2016.
- ²⁸ EN 1991-1-1, Eurocode 1: Actions on structures - Part 1-1: General actions - Densities, self-weight, imposed loads for buildings (European Norm), 2010.
- ²⁹ Schmid, J., Just, A., Klippel, M., Fragiaco, M., The Reduced Cross-Section Method for Evaluation of the Fire Resistance of Timber Members: Discussion and Determination of the Zero-Strength Layer. *Fire Technol* 51, 1285–1309. <https://doi.org/10.1007/s10694-014-0421-6>, 2015.
- ³⁰ König, J., Structural fire design according to Eurocode 5—design rules and their background. *Fire and Materials* 29, 147–163. <https://doi.org/10.1002/fam.873>, 2005.
- ³¹ Buchanan, A. H., *Structural Design for Fire Safety*, Wiley ed. ISBN: 978-0-471-89060-7, 2001
- ³² Klippel M, Schmid J., Design of Cross-Laminated Timber in Fire. *Structural Engineering International*. 2017;27(2):224-230. doi:[10.2749/101686617X14881932436096](https://doi.org/10.2749/101686617X14881932436096).
- ³³ Schmid, J., Klippel, M., Just, A., Frangi, A., Review and analysis of fire resistance tests of timber members in bending, tension and compression with respect to the Reduced Cross-Section Method. *Fire Safety Journal* 68, 81–99. <https://doi.org/10.1016/j.firesaf.2014.05.006>, 2014.
- ³⁴ Schmid, J., Klippel, M., Fahrni, R., Frangi, A., Tiso, M., Just, A., Werther, N., An improved model for the fire design of cross laminated timber in bending. Presented at the WCTE World Conference on Timber Engineering, Seoul, Republic of Korea, 2018.
- ³⁵ Bartlett, A., Hadden, R., Bisby, L., Lane, B., Sectional analysis of cross-laminated timber as a design for fire methodology. Presented at the 9th International Conference on Structures in Fire SIF, Princeton, USA, 2016.
- ³⁶ Doyle, N., Emberley, R., Torero, J.L., Fire Behavior of Cross-Laminated Timber (CLT) Slabs: Two-Way Action, in: Harada, K., Matsuyama, K., Himoto, K., Nakamura, Y., Wakatsuki, K. (Eds.), *Fire Science and Technology 2015*. Springer Singapore, pp. 281–288, 2017.
- ³⁷ Frangi, A., Fontana, M., Thermal Expansion Of Wood And Timber-concrete Composite

Members Under Iso-fire Exposure. Fire Safety Science 7, 1111–1122, 2003.

³⁸ Kreuzinger, H., Lecture B11: Mechanically jointed beams and columns, Timber Engineering Step 1, Basis of design, material properties, structural components and joints. Centrum Hout, The Netherlands, 1995.

³⁹ Karacabeyli, E., Douglas, B., CLT Handbook: Cross-Laminated timber, FP Innovations. ed., 2013.