Human-Induced Vibration of Cross-Laminated Timber (CLT) Floor under Different Boundary Conditions

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Abstract
Vibration of a cross-laminated timber (CLT) floor is strongly related to its boundary conditions. In this study, the effect of beam spacing, beam size and supporting conditions on the dynamic behaviours of CLT floors were investigated. To this end, the open-source software framework Open Software for Earthquake Engineering Simulation (OPENSEES) was used to simulate the dynamic performance of CLT floors and the simulated results were validated against the results of onsite experiments. OPENSEES is under continual development, and is mainly used to develop applications for simulating the behaviour of structures and geotechnical systems under seismic excitations. In this study, a novel model was developed to enable OPENSEES to carry out foot-fall analyses. Moreover, an analytical model was established to enable engineers to quickly estimate the relevant dynamic properties of CLT floors with different boundary conditions. The simulated results agreed well with the experimental data. According to those simulated results, increasing the spacing between the beams would reduce the natural frequency and increase the vibration acceleration significantly. Moreover, the results indicate that increasing the beam stiffness up to a certain level would increase the natural frequency of the CLT floors, and consequently enhance their serviceability performance. The one-way and two-way CLT floors show little difference in vibration performance when the beams can provide sufficient support.

Keywords: CLT floor; boundary condition; human-induced vibration; OPENSEES simulation

1. Introduction
In mass timber structures, floors, walls and roofs are the most important structural components. A timber floor in such a structure is especially important because it is the only component in constant contact with the occupants. The floor system is subjected to incessant excitations due to human activities, for instance walking, running, jumping, dancing and doing sports. The occupants can perceive the response from the floor,

and its performance influences the comfort level of the occupants to a certain degree at all times. Therefore, serviceability of the floor system is being focused on increasingly, while human-induced vibration is a key influencing factor. Given the low bending strength of timber relative to those of traditional construction materials, human-induced vibration is more prominent on timber floor systems, and the serviceability requirements of such floor systems are more restrictive [1]. In recent years, the demand for timber floors has increased considerably. For instance, more than 300,000 timber floors are built each year in the UK [2]. Additionally, the spans of timber floors in modern buildings have grown owing to the availability of larger architectural spaces and engineered timber products. In this context, vibration-related problems of timber floors warrant more attention from researchers, and the dynamic performance of timber floors should be evaluated precisely.

The vibration of a timber floor is mainly governed by its mass, stiffness and damping. In the early stages, the design of timber floors is commonly based on the static stiffness properties. For instance, the Federal Housing Administration (FHA) standard specifies the uniform load deflection method (ULD) [3], and the National Building Code of Canada (NBCC) standard specifies a concentrated load deflection method [4]. Undeniably, these methods are very easy to use in practical applications. However, even if the static stiffness of a timber floor satisfies the requirements outlined in the standards, unsatisfactory floor vibration could occur. This is because vibration of a timber floor is a complex dynamic problem, and floor response is governed by multiple dynamic factors such as resonance, excitation factors, energy dissipation and boundary conditions. The approach outlined in Eurocode 5, the criteria of which specify an 8 Hz frequency limitation, is commonly employed by engineers [5]. In Eurocode 5, static deflection and unit impulse velocity response checks are carried out for frequencies higher than 8 Hz. Furthermore, the code specifies that a special investigation should be conducted when the vibration frequency of a floor is less than 8 Hz. However, there have been some concerns about the approach in Eurocode 5 as follows. It can be applied only to residential floors with spans of up to 6 m [2]; the proposed value of 1% for the damping ratio is rather low [6] and variations in the design equations and design limits remain large among European countries, which means further harmonization is needed [7]. Alternative standards have been proposed with consideration of external excitations such as human activity [1,2,8], for instance, BS 6472-1:2008 [9] and ISO 10137 [10], in which root mean square (RMS) and vibration dose value (VDV) are used to evaluate the vibration of timber floors, respectively.

These standards are usually based on simplified models for timber floors accompanied by simplified calculations of the dynamic properties of such floors [1]. For accurate evaluation, advanced methods, for example, the finite element method (FEM), should be used, and more boundary conditions of floor systems should be considered.

With the development of new timber products and construction technologies, more types of timber floors have been developed in recent years. Chang et al. [2] measured the vibrations of various timber floor systems, for example, joist floors, cross-laminated timber (CLT) floors and Profi Deck floors in Europe, and found differences in dynamic behaviours among these floors. For the traditional joist timber floors, Zhang et al. [11] observed that spacing, strongback bracings and ceiling do not affect the frequency and damping ratio of the floor, and the measured damping ratio is lower than the 1% threshold specified in Eurocode 5. Jarnero et al. [6] investigated the effect of the boundary conditions of a CLT floor on the vibration behaviours excited by a shaker and found that the damping ratio increased as the boundary condition was changed from simply supported to being placed on a polyurethane interlayer, and that the natural frequency changed considerably when the floor element was coupled with adjacent elements. Bernard [12] studied the effect of fasteners and the inclusion of blocking on the vibration performance of a timber floor. Glisovic and Stevanovic [13] conducted a comprehensive FEM study of human-induced vibration in a joist timber floor and advised a design of the joist timber floors.

The literature indicates that the boundary conditions of a timber floor influence its vibration response [6,12]. Very few studies have investigated the vibration performance of CLT floors because they are a newly developed structural form. Casagrande et al. [1] performed analytical, numerical, and experimental assessments of the vibration performance of both CLT and timber–concrete composite floors. Their results indicated that internal partitions and non-structural elements have substantial effects on the dynamic responses of these floors, especially on their mode shapes, frequencies and damping characteristics. Koyama et al. [14] found negligible influence of the connections between a floor and walls on floor vibration when using L-shaped angles, vertical screws or diagonal screws. In the aforementioned studies, the effects of boundary conditions on the vibration of a CLT floor were not studied comprehensively. In the calculations related to CLT floors specified in various engineering standards, the boundary conditions are simplified as simply supported [15], and in numerical modelling for research, they are simplified as simply restrained [1].

Thus, a refined evaluation of timber floor vibration should involve accurate modelling of the boundary conditions. Moreover, the effect of the boundary conditions on the vibration of CLT floors should be investigated.

In the present study, numerical simulations are performed using the open-source Open Software for Earthquake Engineering Simulation (OPENSEES) framework to model a CLT floor from a case study and validate the model against on-site measured data. In the model, various beam-supporting plans are taken into consideration, and the dynamic performance of the CLT floor is tested under human-induced vibration. The objective is to investigate the effect of boundary conditions on the vibration behaviours of the CLT floor and reduce vibration of the CLT floor system.

2. Methods

2.1 Numerical modelling

A finite element (FE) model of the CLT floor is created using OPENSEES 2.5.0. The OPENSEES framework allows users to create FE applications for simulating the response of structural systems subjected to earthquakes. Although OPENSEES is mainly used for earthquake engineering simulations, it can be used to analyse the dynamic responses of systems subjected to human-induced vibration. More importantly, OPENSEES is a free-to-use and instalment-free software, and its size is only 19.1MB, which is convenient for structural designers, researchers and engineers. According to the literature review, no study has employed OPENSEES for simulating timber floors, and this study is the first attempt to develop an open-source FE model of CLT floor systems.

Floor vibration was modelled based on a real CLT floor case. In this study, a three-layer CLT floor with a total thickness of 120 mm (layup: 40L-40T-40L) was investigated at University Centre Farnborough in the UK. This building is a two-storey hybrid structure consisting of CLT panels and steel frames. As shown in the plan in Figure 1, the floor selected for this analysis is circled with red lines, and it is located on the second floor of this structure. The longitudinal direction of the CLT floor is marked in the drawing, with the lengths of the longitudinal and transverse spans of the floor as 9.0 m and 6.6 m, respectively. The CLT panel is supported by three UB 406×140×46 steel I-beams in the longitudinal direction and four UB 203×133×30 steel I-beams in the transverse direction. Thus, the CLT floor spans continuously over a central support beam (A2-B2) in the longitudinal direction. As presented in the drawing, the steel beams are supported by six steel columns.

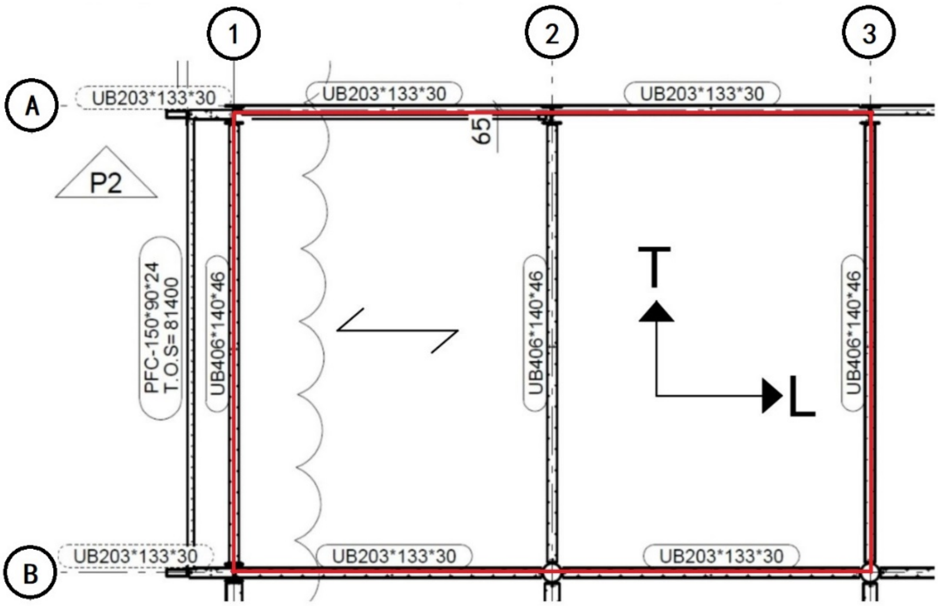
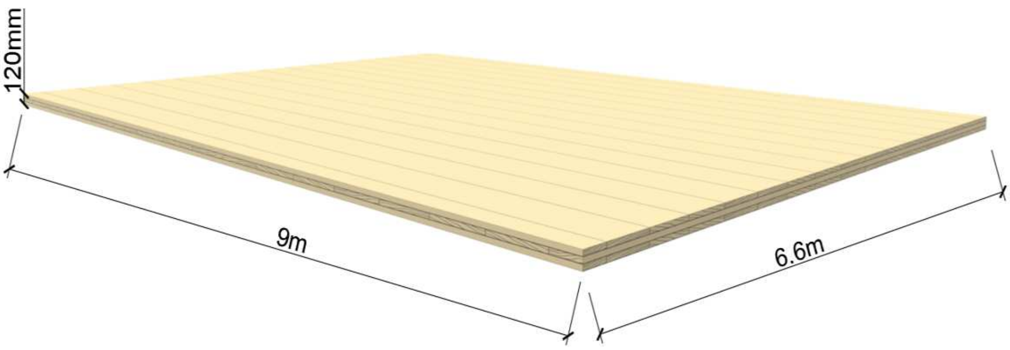
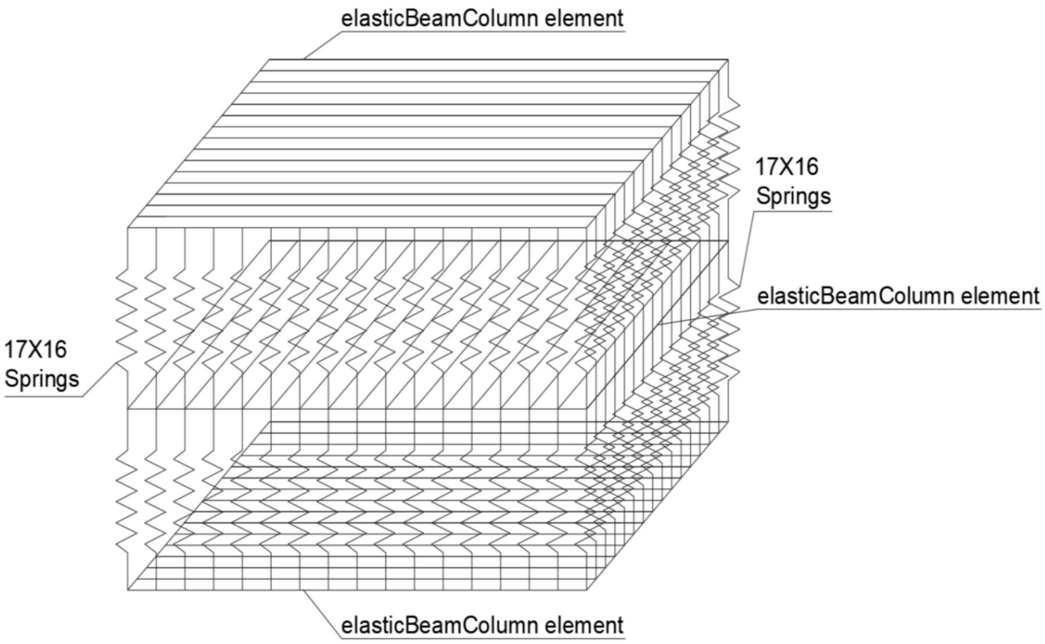


Figure 1 Plan of the CLT floor in University Centre Farnborough.

In OPENSEES, the three-layer CLT panel is modelled using three-layer orthogonal beams. As can be seen in Figure 2, each layer of the CLT panel is modelled using a row of elasticBeamColumn elements with the mechanical properties of C24 timber, and the sum of the beam widths is equal to the width of the layer. The use of elasticBeamColumn elements helped to model the CLT floor anisotropically in OPENSEES. Between each layer, the beams are placed in an orientation of 90° to form the structure of the CLT panel, as shown in Figure 2(a). Notably, the contact between each layer is modelled using springs. As shown in Figure 2(b), a total of 17 × 16 = 272 springs connect each layer, and the stiffness of these springs is set to a super large value to prevent any slip between layers. Unlike the modelling of CLT floors by using shell elements, the modelling method developed in this study reduces computing time, and the resulting model is structurally similar to actual CLT panels. The non-structural elements on the floor are not modelled in the FE simulation, but their masses are considered. The total mass of the floor system is 14 t. The damping ratio of the floor system is set to 3.04% based on on-site measurement.



(a)



(b)

Figure 2 (a) CLT panel on the selected case; (b) Numerical modelling of CLT panel in OPENSEES.

In terms of the boundary conditions, Figure 3(a) shows one of the supporting beams, and Figure 3(b) demonstrates that the connection between the CLT floor and the supporting steel beam is modelled using springs in OPENSEES. In practice, the CLT floor is connected with the steel beams by using self-tapping screws. The spacings and sizes of the screws could affect the vibration performance of the CLT floor. To avoid the influence of the screws in the simulations, the self-tapping screws were modelled using springs with super large stiffness values, and the floor was assumed to rigidly connect to the supporting beams. The steel beam shown in Figure 3(b) was modelled using elasticBeamColumn elements with a Young's modulus of 200 GPa and shear modulus of 79.3 GPa. Notably, the two ends of the beam were restrained in three dimensions, and this boundary condition was the same as those applied to the other beams in the system. The reason for modelling the beam boundary condition in this way is that the beam ends are commonly restrained by columns, and the deformation of the column–beam connections due to floor vibration is ignored in this study.

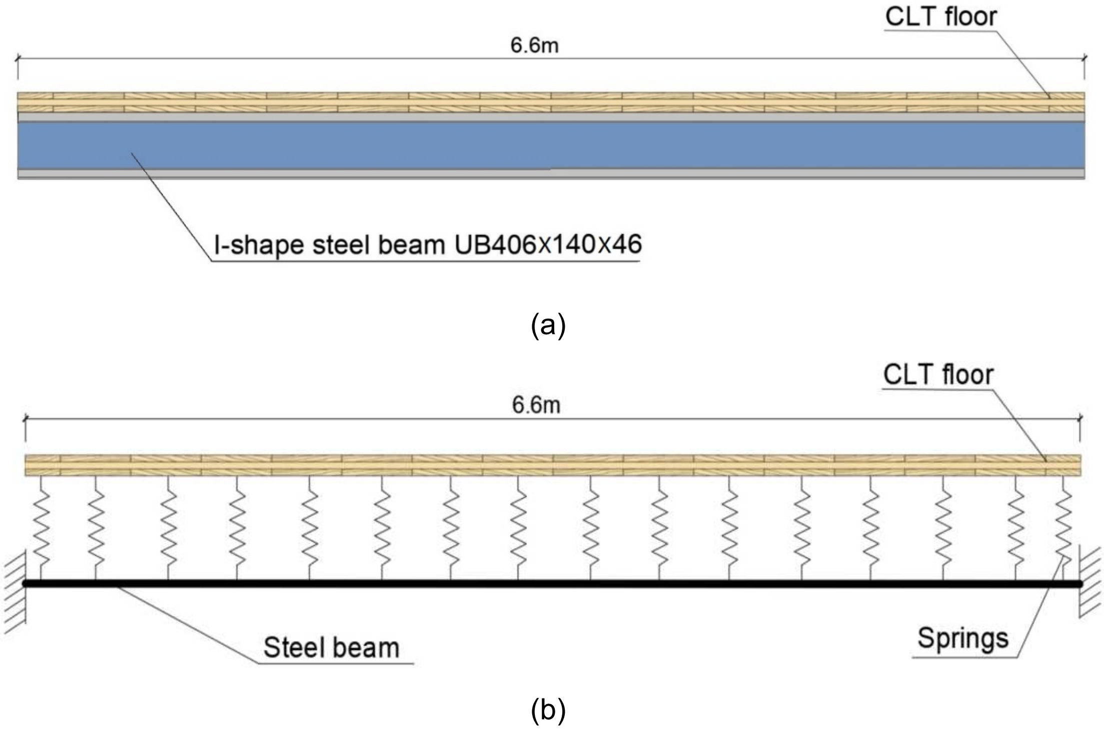
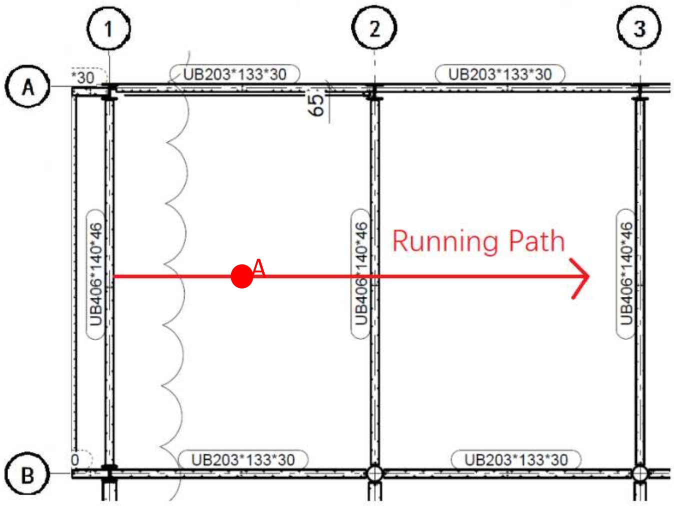


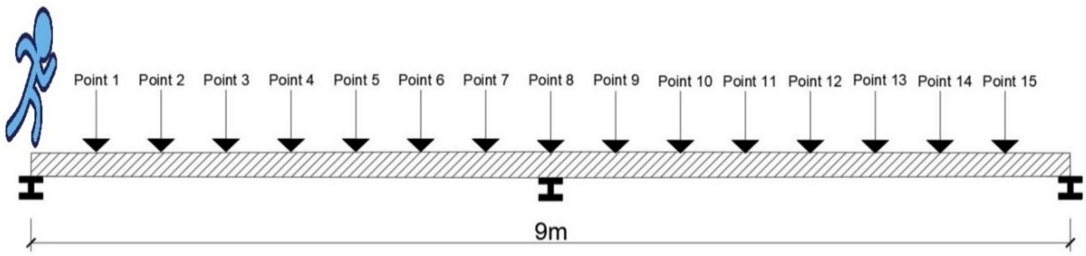
Figure 3 (a) A steel beam supporting the CLT floor; (b) Numerical modelling of the boundary conditions of the steel beam.

2.2 Loading protocol

In this study, footfalls during running are considered as the excitation. Figure 4 shows the loading protocol of one person running on the floor. As shown in Figure 4(a), the running path is marked on the floor, which shows that a person is running from one end to the other end in the longitudinal direction. Figure 4(b) indicates that this person takes a total of 15 steps each time to traverse the floor. In Figure 4(c), the force-time history of each running step is modelled as two peaks by referencing the measurement and characterisation of footsteps proposed by Galbraith and Barton [16], Ohlsson [17] and Thelandersson and Larsen [18]. The first peak corresponds to heel strike, and the second peak corresponds to toe-lift-off contact. The amplitude of the second peak is about 2.1 kN, which models the running characteristics of a person under a gravitational force of approximately 1 kN.



(a)



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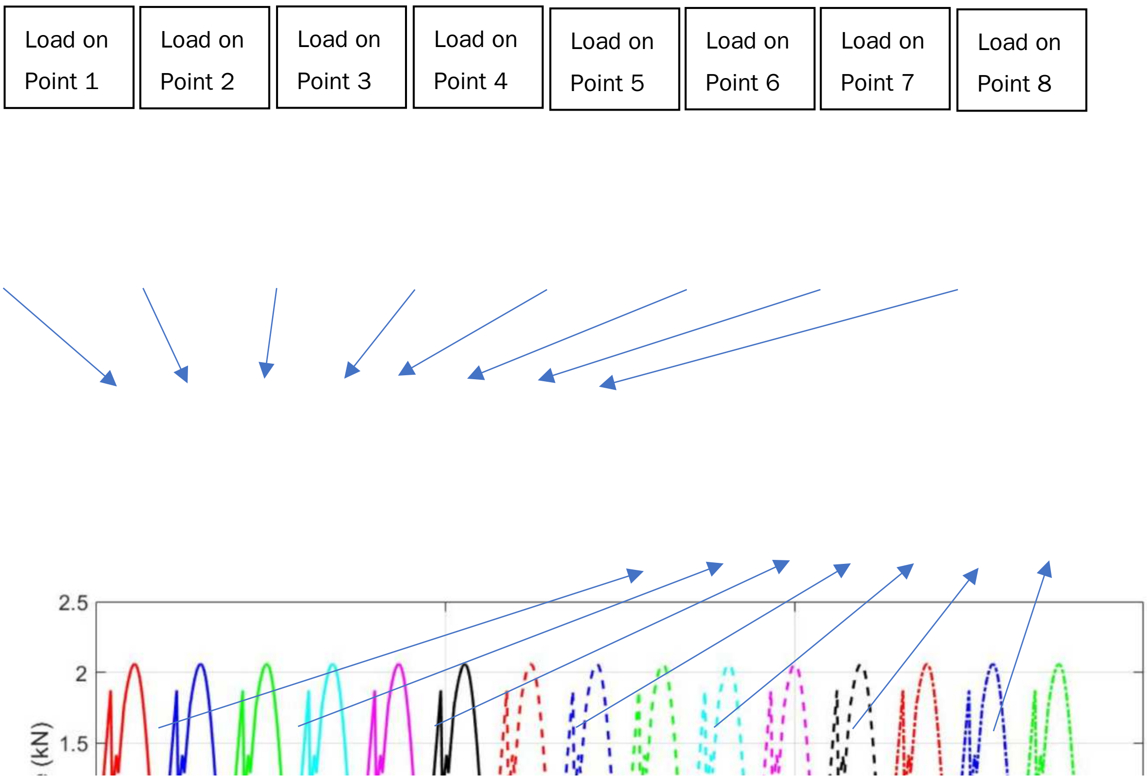
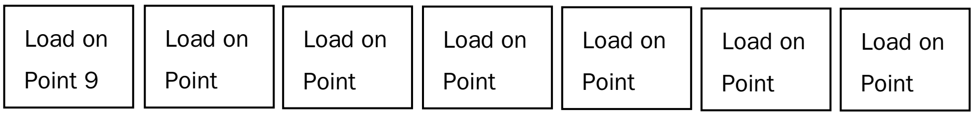




(c)

Figure 4 (a) Running path on the floor; (b) Running footfall loading positions; (c) Running footfall loading protocol.

The loading protocol shown in Figure 4(c) was input step-by-step into OPENSEES for each point, and the vertical acceleration of Point A on the timber floor was measured for 15 seconds. As presented in Figure 4(a), Point A is located on the centroid of the left part of the CLT floor.

2.3 Model validation

The same loading protocol was employed on-site at University Centre Farnborough in the UK. In the associated excitation event, a single tester weighing 100 kg ran along the same path as that indicated in Figure 4(a). The running pace was approximately 0.945 s/step. An accelerometer was placed at Point A, as shown in Figure 4(a), to record the floor response with a sampling rate of 200 Hz. Modal analysis was performed on the measured free vibration data, and the fundamental natural frequency of the CLT floor was found to be 7.5 Hz. According to the modal analysis performed on the results obtained with the proposed FEM model, the fundamental natural frequency was found to be 7.6 Hz, which is similar to that measured on-site. A comparison of the numerically modelled vertical acceleration – time history at Point A with the one measured on-site is shown in Figure 5. In general, the modelled acceleration response of the floor agreed reasonably well with the measured response. The peak acceleration computed using OPENSEES was lower than the measured acceleration. One of the reasons for this could be that the high-frequency noise during measurement may have heightened the response. Another reason is that human excitation is more random in experiments. By contrast, in the simulations, the running footfall model was constant. Human excitations employed in experiments can occasionally differ from the human running models employed in simulations. Such a difference in excitation could lead to a difference between the measured and simulated floor vibration performances. In future studies, load cells should be installed on the soles of the shoes worn by the human to record the excitation loads, which can be used to calibrate the numerical model.

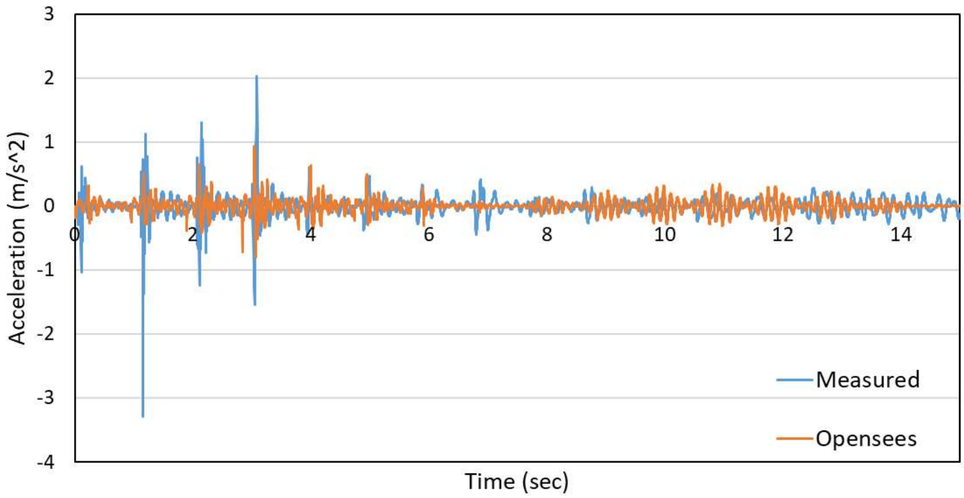
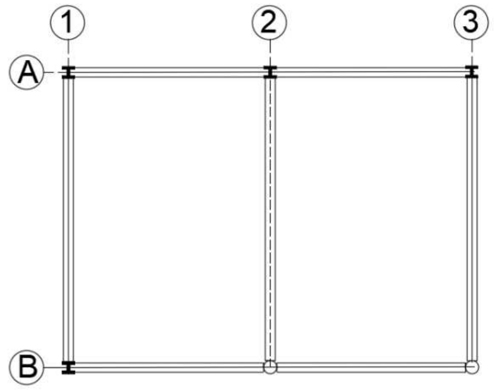
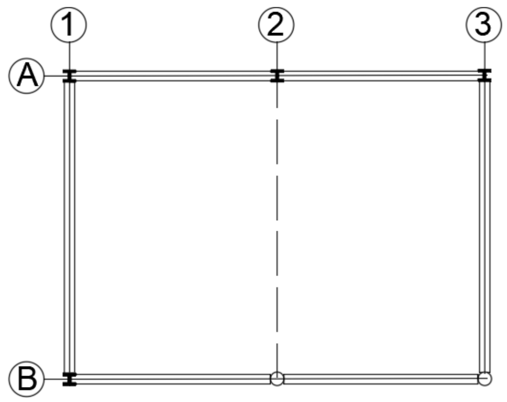
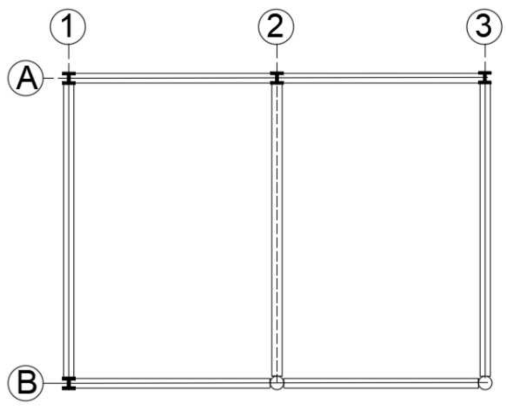


Figure 5 Comparison of numerically modelled and measured vertical acceleration – time history curves of Point A.

2.4 Parametric study

Numerical modelling tests were performed on the aforementioned CLT floor under excitations produced by the footfalls of a human running on the floor at a rate of 3.5 Hz. The footfalls were modelled using the method described in Section 2.2. In this study, the effect of boundary conditions on the vibration performance of a CLT floor was investigated. The test plan is summarized in Table 1. In Test No. 1, as a reference, the boundary condition was the same as that in the real case. In Test No. 2, the beam sizes were the same in two directions to provide a two-way supporting condition. In Test No. 3, the beam A2-B2 in the middle was removed, and the floor was supported by other six beams. In Test Nos. 4, 5, 6 and 7, the beam size was increased, and the beam supporting condition is in one way in Test Nos. 4 and 6 while the beam supporting condition is in two ways in Test Nos. 5 and 7. The beam size was decreased in Test Nos. 8, 9, 10 and 11. As can be inferred from Table 1, the floor was one-way supported in Test Nos. 8 and 10 and two-way supported in Test Nos. 9 and 11. During the loading, the vertical acceleration at Point A was measured for 6 s with a sampling rate of 200 Hz.

Table 1 Testing plan.

Test No.	Size of beam parallel to the longitudinal direction	Size of beam parallel to the transverse direction	Explanatory drawings of boundary conditions
1	UB203×133×30	UB 406×140×46	
2	UB406×140×46	UB 406×140×46	
3	UB203×133×30	UB 406×140×46	
4	UB305×165×54	UB533×165×85	
5	UB533×165×85	UB533×165×85	
6	UB254×146×43	UB457×152×82	
7	UB457×152×82	UB457×152×82	
8	UB178×102×19	UB305×127×48	
9	UB305×127×48	UB305×127×48	
10	UB127×76×13	UB254×102×28	
11	UB254×102×28	UB254×102×28	

3. Results and Discussion

3.1 Natural frequency

Table 2 shows the first three natural frequencies of the CLT floor under different testing conditions, as analysed using the OPENSEES simulated time-history data. In Test No. 3, the beam A2-B2 in the middle was removed, and the first natural frequency decreased considerably to 2.0 Hz. This is because the beam spacing increased, and the area of the floor panel was doubled. Consequently, the bending stiffness of the floor system decreased. When the beam size was increased in Test Nos. 4, 5, 6 and 7, the natural frequencies increased to more than 9 Hz. The natural frequencies decreased to a little more than 7 Hz in Test Nos. 8, 9, 10 and 11 because the beam size was decreased.

Table 2 First three natural frequencies of the floor system under different testing conditions, as analysed using OPENSEES simulated time-history data.

Test No.	1	2	3	4	5	6	7	8	9	10	11
f_1 (Hz)	7.6	7.9	2.0	9.7	9.8	9.8	9.7	7.2	7.7	7.1	7.2
f_2 (Hz)	10.0	9.2	3.2	15.0	15.5	15.3	15.1	10.4	14.6	12.3	13.9
f_3 (Hz)	14.4	14.6	7.6	32.0	32.3	32.3	32.0	28.2	29.0	27.6	27.6

Theoretically, the fundamental natural frequency f_{th} of the CLT floor system can be estimated using Equation (1) as:

$$f_{th} = \frac{1}{2\pi}\sqrt{\frac{k_{eff}}{m}} \quad (1)$$

Because the floor is axial-symmetric about the axis A2-B2, its natural frequency can be analysed from its left half part A1-A2-B2-B1. In Equation (1), m and k_{eff} are the mass and effective bending stiffness of the CLT floor system, respectively. k_{eff} can be calculated using Equation (2) as:

$$k_{eff} = k_{CLT,panel,\ \ eff} + k_{beam,l,eff} + k_{beam,t,eff} \quad (2)$$

where $k_{CLT,panel,\ \ eff}$ is the effective bending stiffness of the CLT panel, $k_{beam,l,eff}$ is the effective stiffness of the two beams parallel to longitudinal direction (A1-A2 and B1-B2) and $k_{beam,t,eff}$ is the effective stiffness of the beams parallel to transverse direction (A1-B1 and A2-B2).

$k_{CLT,panel,\ \ eff}$ can be calculated as follows:

$$k_{CLT,panel,\ \ eff} = \frac{A_{panel} \cdot EI_{CLT,panel,\ \ eff} \cdot \left(\frac{\pi}{0.8 l_l}\right)^4}{l_t} \quad (3)$$

where l_l and l_t denote the longitudinal and transverse side lengths of the CLT floor panel, respectively, 0.8 is a coefficient of the floor span because the floor is continuously spanned, and A_{panel} is the area of the CLT panel. $EI_{CLT,panel,\ \ eff}$ can be calculated using the shear analogy theory derived from the Timoshenko Beam Theory [19]. $EI_{CLT,panel,\ \ eff}$ was calculated using Equation (4) as:

$$EI_{CLT,panel,\ \ eff} = \sum_{i=1}^{3} E_i \frac{b_i h_i^3}{12} + \sum_{i=1}^{3} E_i A_i z_i^2 \quad (4)$$

where E_i is the modulus of elasticity of layer *i*, b_i, h_i and A_i are the width, height and area of layer *i*, and z_i is the distance from the centroid of layer *i* to the centroid of the cross section.

When the floor is one-way supported, $k_{beam,l,\,eff,one-way}$ represents the effective bending stiffness of the two beams parallel to the longitudinal direction (A1-A2 and B1-B2). Given that the beams parallel to the transverse direction (A1-B1 and A2-B2) are in torsion, $k_{beam,t,\,eff,one-way}$ denotes the torsional stiffness of the beams parallel to the transverse direction. These two stiffness values can be calculated using Equations (5) and (6), respectively.

$k_{beam,l,\,eff,one-way}$ can be calculated using the simply supported beam bending equation as follows:

$$k_{beam,l,\,eff,one-way} = \frac{48\ \ E_{beam,l} I_{beam,l}}{l_l^3} \times 2 \quad (5)$$

where $E_{beam,l}$ denotes the modulus of elasticity, $I_{beam,l}$ is the second moment of area of the beam with I-shaped cross section parallel to the longitudinal direction.

$k_{beam,t,\,eff,one-way}$ can be calculated as:

$$k_{beam,t,\,eff,one-way} = \frac{8 \cdot G_{beam,t} \cdot I_{beam,t}}{l_t \cdot l_l \cdot B_{beam,\,t}} \times 2 \quad (6)$$

where $G_{beam,t}$ denotes the shear modulus, and $I_{beam,t}$ and $B_{beam,t}$ are the torsional moment of inertia and the width of section of the beam with I-shaped cross section parallel to the transverse direction, respectively.

When the floor is two-way supported, the floor is bent in two directions, and all beams are in bending. Therefore, both $k_{beam,l,\,eff,two-way}$ and $k_{beam,t,\,eff,two-way}$ denote the effective bending stiffnesses of the beams in two-way bending condition, and they can be calculated using Equations (8) and (9), respectively. Because floor is two-way supported, the load on the floor is distributed in two directions, and the load distribution ratio (r) can be calculated as follows:

$$r = (\frac{l_t}{l_l})^4 \tag{7}$$

Thus, $k_{beam,l,\,eff,two-way}$ should be multiplied with a coefficient of $\frac{1}{r+1}$, and $k_{beam,t,\,eff,two-way}$ should be multiplied with $\frac{r}{r+1}$.

Moreover, $k_{beam,l,\,eff,two-way}$ can be calculated as:

$$k_{beam,l,\,eff,two-way} = \frac{1}{r+1} \cdot \frac{48\, E_{beam,l} I_{beam,l}}{l_l^3} \times 2 \tag{8}$$

$k_{beam,t,\,eff,two-way}$ can be calculated as:

$$k_{beam,t,\,eff,two-way} = \frac{r}{r+1} \cdot \frac{48\, E_{beam,t} I_{beam,t}}{l_l^3} \times 2 \tag{9}$$

where $E_{beam,t}$ and $I_{beam,t}$ denote the modulus of elasticity and second moment of area of the beam with I-shaped cross section parallel to the transverse direction, respectively.

Table 3 presents the f_{th} calculated using Equation (1) under different test conditions. Compared with the first natural frequencies analysed by OPENSEES simulations in Table 2, the theoretically calculated fundamental natural frequencies match well with those calculated from OPENSEES. Test No. 3 was a special case because the fundamental natural frequency was extremely low, which influenced the estimation accuracy. In most test conditions, the errors can be controlled within 20%, which indicates that Equations (1)-(9) can be used to estimate the fundamental frequencies under various boundary conditions.

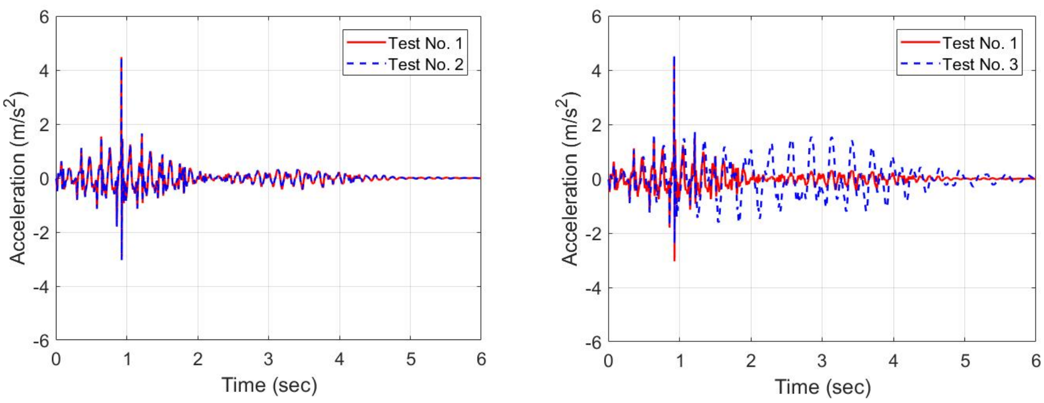
Table 3 Theoretically calculated natural frequencies of floor system for different testing conditions compared with the values obtained using OPENSEES.

Test	Theoretically calculated fundamental natural frequency, f_{th} (Hz)	Fundamental natural frequency calculated using OPENSEES, f_1 (Hz)	Error ($(f_{th} - f_1)/f_1 \times 100\%$)
No.1	7.7	7.6	1.3%
No.2	8.7	7.9	10.1%
No.3	2.8	2.0	40.0%
No.4	9.7	9.7	0.0%
No.5	11.7	9.8	19.4%
No.6	8.7	9.8	-11.2%
No.7	10.7	9.7	10.3%
No.8	7.3	7.2	1.4%
No.9	8.1	7.7	5.2%
No.10	7.0	7.1	-1.4%
No.11	7.4	7.2	2.8%

When the floor was two-way supported, the first natural frequency increased marginally, indicating that the two-way supported CLT floor was stiffer than the one-way supported floor. This is because all beams were in bending under the two-way support condition, and the bending stiffness was greater than the beam's torsional stiffness. The natural frequencies in Test Nos. 4, 5, 6 and 7 were higher than that in Test No. 1 because the larger supporting beams in Test Nos. 4, 5, 6 and 7 increased the beam stiffness. As presented in Table 1, the natural frequencies of the two-way supported CLT floors in Test Nos. 5 and 7 did not differ considerably from those of the one-way supported floors. This can possibly be ascribed to the fact that the larger beams parallel to the longitudinal direction provided adequate bending stiffness to the floor system, which weakened the influence of the boundary condition parallel to the transverse direction. In Test Nos. 8, 9, 10 and 11, the natural frequencies decreased compared with that in Test No. 1 because of smaller beam sizes. The two-way supported floors had higher natural frequencies than those of the one-way supported floors. According to Table 1, between Test Nos. 8 and 9, the first natural frequency increased from 7.2 Hz to 7.7 Hz because the beams parallel to the transverse direction provide a larger bending stiffness than its torsional stiffness.

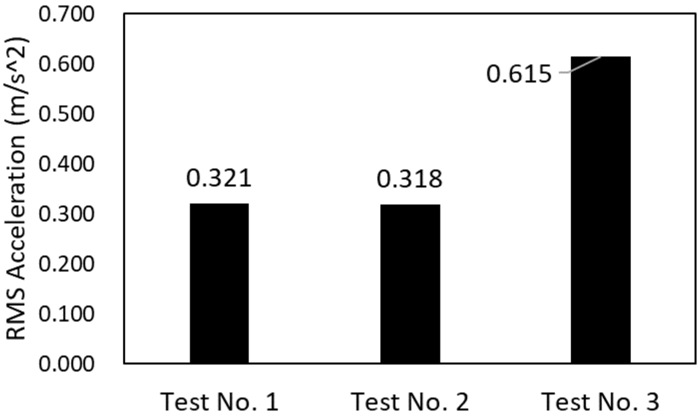
3.2 Time-history analysis

Figure 6 presents the vertical acceleration – time history of the floor as recorded at Point A in Test Nos. 1–3. Figure 7 shows the RMS accelerations in the first 6 s of individual tests. From Figure 6(a), the difference between Test Nos. 1 and 2 is small. However, the floor responses in Test No. 3 are visually large compared with those in Test No. 1, as shown in Figure 6(b). According to Figure 7, the RMS acceleration in Test No. 1 was 0.321 m/s^2, and it increased to 0.615 m/s^2 in Test No. 3. Moreover, the vibration amplitude remained large after the first 2 seconds in Test No. 3, which increased the RMS value of acceleration. Therefore, placing the supporting beam in the middle to reduce the floor span can increase the bending stiffness and damp the human-induced vibration effectively. In Figures 8(a) and (b), the floor responses show little difference when the beam size is increased, regardless of whether the floor is one- or two-way supported. In Figure 7(b), the RMS accelerations in Test Nos. 4, 5, 6 and 7 are approximately equal to that in Test No. 1. This result can possibly be ascribed to the fact that the first natural frequencies of the floor with larger beams in Test Nos. 4, 5, 6 and 7 are higher than 9 Hz, which makes them considerably higher than the running rate of 3.5 Hz. Because resonance with running could rarely occur, the vibration response of a floor was barely affected by the human running on it. Therefore, the vibration performance of the CLT floor remained constant even when larger beams were used. Moreover, there was little difference in floor responses between the one- and two-way supported floors because the floor boundary support was adequately stiff, and the effect of variations in the beams on the floor vibration was relatively weak.

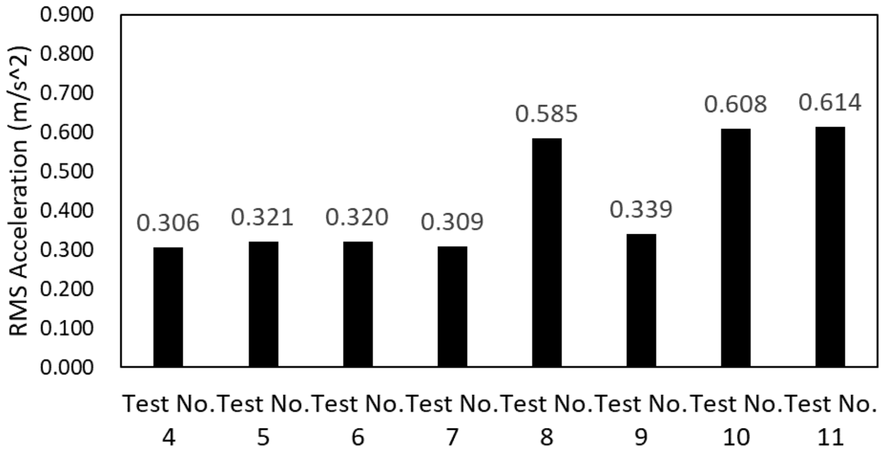


(a) (b)

Figure 6 Comparison of time histories of accelerations in Test Nos. 1, 2 and 3.



(a)



(b)

Figure 7 (a) Comparison of RMS accelerations in Test Nos. 1, 2 and 3; (b) Comparison of RMS accelerations in Test Nos. 4, 5, 6, 7, 8, 9, 10 and 11.

Unlike the case of increasing beam size, the vibration response of the CLT floor was significantly affected when the beam size was decreased, as shown in Figures 8(c) and (d). The time-history acceleration in Test No. 8 increased dramatically compared with that in Test No. 1 when smaller supporting beams were used. As the natural frequencies decreased and approached the running rate of 3.5 Hz, the resonance effect would lead to a larger floor vibration response. When the floor was two-way supported, as in Test No. 9, the RMS acceleration decreased from 0.585 m/s^2 to 0.339 m/s^2 which is approximately at the same level as that in Test No. 1. The response was attenuated in Test No. 9 because the beams parallel to the longitudinal direction (UB305×12×48) had a considerably higher stiffness, whereas the beam stiffness (UB178×102×19) in Test No. 8 was considerably lower. Moreover, the stiffness provided by the beams in torsion is less than that when the beams are in bending for Test Nos. 8 and 9. Therefore, a large difference was observed between the one- and two-way supported floors when the supporting beam had a smaller size. As can be seen in Figures 8(d) and 7(b), the CLT floor response obtained in Test No. 11 is similar to that obtained in Test No. 10 because their first natural frequencies are approximately the same.

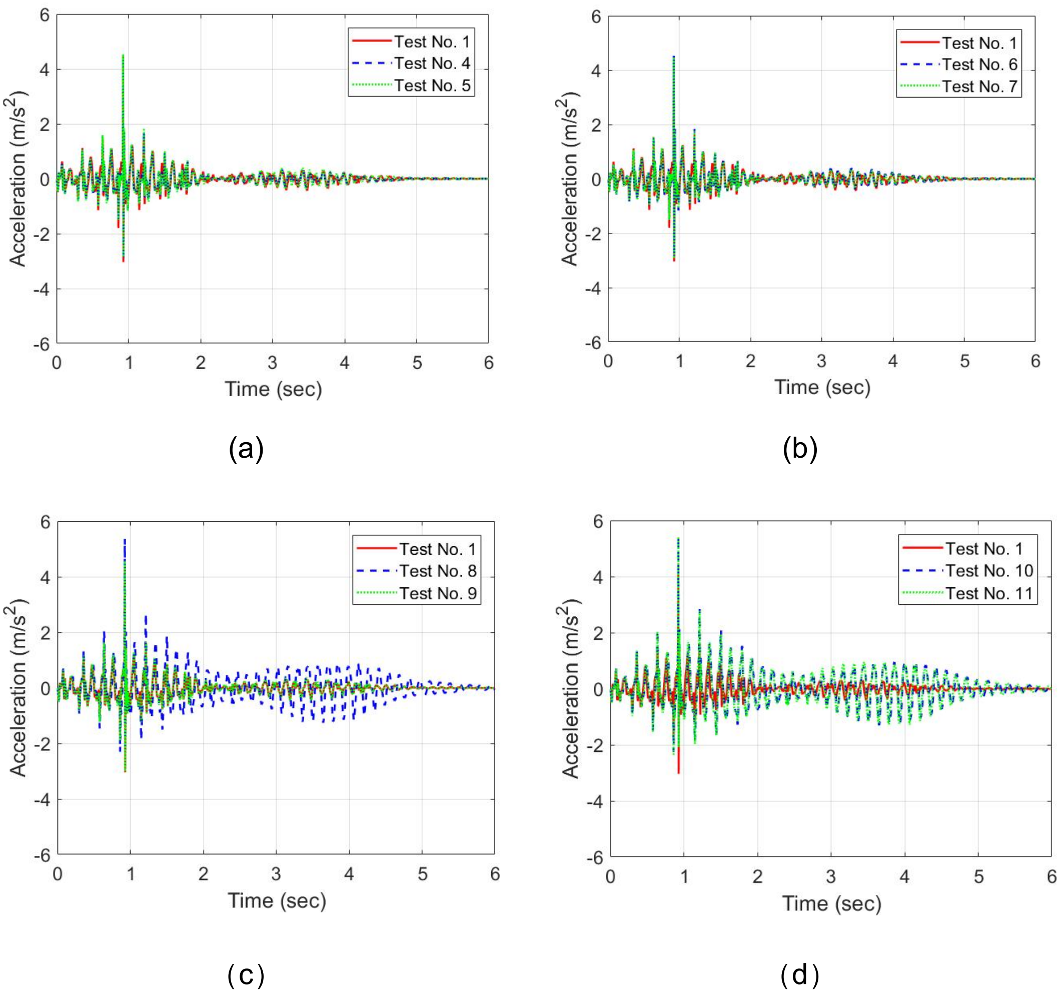


Figure 8 Comparison of time histories of accelerations in Test Nos. 1, 4, 5, 6, 7, 8, 9, 10 and 11.

3.3 VDV analysis

The VDV method is currently applied mostly to assess the response of existing floors, and it is used in the standards BS 6472-1:2008 [9] and ISO 10137 [10]. The VDV values can be calculated from the frequency-weighted floor acceleration-time response as follows:

$$VDV = \left[\int_0^T a_w^4(t)dt\right]^{\frac{1}{4}} \tag{10}$$

where $a_w(t)$ is the weighted acceleration and can be calculated with frequency weighting by using the curve proposed in BS 6472-1:2008 [9], which is shown in Figure 9(a). The weightings demonstrate the maximum sensitivity to vertical acceleration in the frequency range of 4–12.5 Hz. For example, the weighted time-history data in Test No. 1 are presented in Figure 9(b).

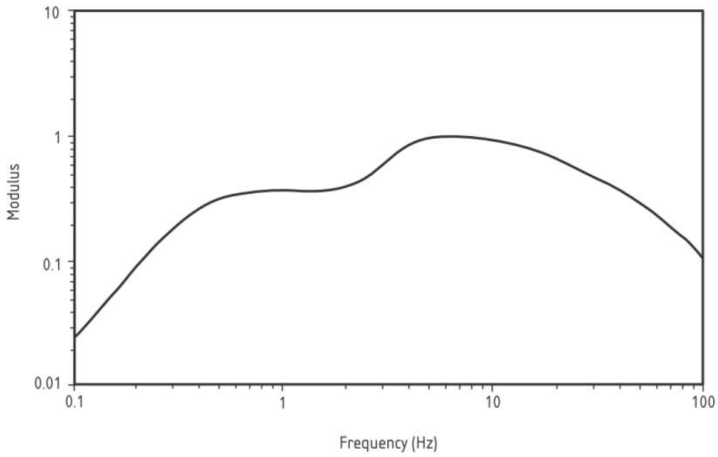
In Equation (10), T is the total time during which the floor is excited. According to the British standard BS 6472-1:2008 [9], the exposure time T can be set to 16 h for the daytime and 8 h for the night time in the case of residential buildings. The ranges of the VDV values might result in various probabilities of adverse comments for people, as summarized in Table 4. As indicated by Ellis [20], over an exposure period of 16 h or 8 h, we can assume the occurrence of 32 running events. Thus, Equation (10) can be expressed as follows:

$$VDV_{total} = 32^{\frac{1}{4}}\left[\int_0^{T_{single}} a_w^4(t)dt\right]^{\frac{1}{4}} = 2.38\, VDV_{single} \tag{11}$$

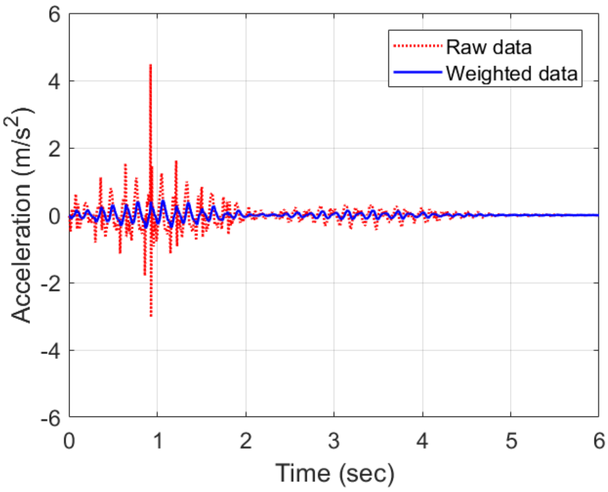
where T_{single} is the duration of a single running event, and VDV_{single} is the VDV of a single running event. VDV_{total} in each test was calculated, and the results are presented in Figure 10. Moreover, the corresponding weighted peak values obtained in all tests are shown adjacent to VDV_{total} in Figure 10.

Table 4 The ranges of the VDV values which might result in various probabilities of adverse comment for people.

Place and time	Low probability of adverse comment ($m/s^{-1.75}$)	Adverse comment possible ($m/s^{-1.75}$)	Adverse comment probable ($m/s^{-1.75}$)
Residential buildings (16h daytime)	0.2 to 0.4	0.4 to 0.8	0.8 to 1.6
Residential buildings (8h night time)	0.1 to 0.2	0.2 to 0.4	0.4 to 0.8

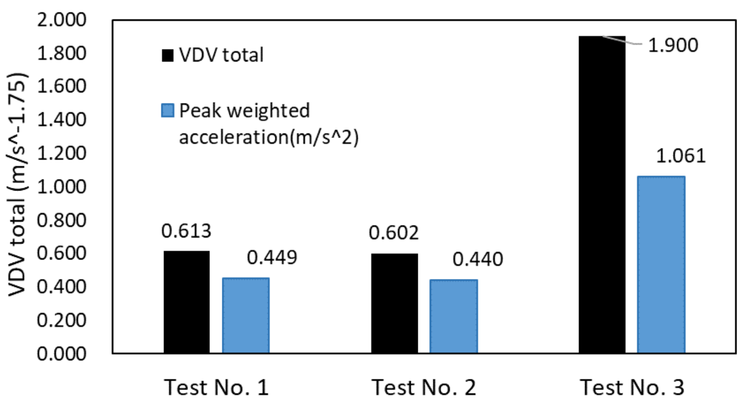


(a)

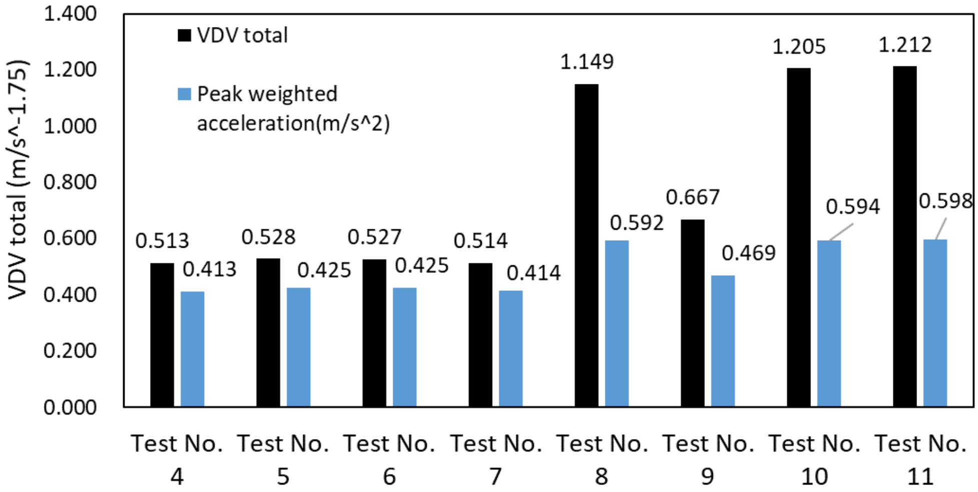


(b)

Figure 9 (a) Frequency-weighting curve for vertical vibration proposed in BS 6472 [9]; (b) Weighted acceleration-time history data from Test No. 1.



(a)



(b)

Figure 10 (a) Comparison of VDV and weighted peak accelerations in Test Nos. 1, 2 and 3; (b) Comparison of VDV and weighted peak accelerations in Test Nos. 4, 5, 6, 7, 8, 9, 10 and 11.

In Figure 10(a), it can be seen that the floor response falls under 'adverse comment possible' in Test Nos. 1 and 2 in the daytime. However, in Test No. 3, the VDV is higher than the upper limit of 'adverse comment probable' in the daytime. Thus, the floor response could cause significant discomfort to residents. This highlights the importance of designing supporting beams with proper spacing to provide adequate bending stiffness. The peak weighted acceleration, as shown in Figure 10, exhibits a trend similar to the VDV acceleration. Figure 10(b) shows that the VDV values in Test Nos. 4, 5, 6 and 7 range from 0.4 $m/s^{-1.75}$ to 0.6 $m/s^{-1.75}$, which indicates the floor response falls under 'adverse comment possible' in the daytime. The effect of enlarging the beam size on improving the comfort level of the floor is small. Other solutions, for example, increasing panel thickness and adding more supporting beams, can be studied in the future. In Test Nos. 8, 10 and 11, the VDV increased significantly to 0.8–1.6 $m/s^{-1.75}$ to fall under 'adverse comment probable' in the daytime. This shows that decreasing the size of the supporting beam can increase vibration considerably and influence the comfort level significantly. When the floor is two-way supported, as in Test No. 9, the comfort level can be improved.

4. Conclusions

In this study, the effect of boundary conditions on the vibration performance of a CLT floor was investigated by means of on-site measurement and numerical simulation. The on-site measurement was conducted in a building in the UK with a CLT floor and a steel frame. The CLT floor was modelled innovatively by using an open-source software OPENSEES. On-site measurement data were used to validate the capability of OPENSEES in terms of simulating the effects of human-induced vibration on a CLT floor.

The theoretical estimations of the first natural frequencies matched well with those obtained using OPENSEES. Moreover, the effect of boundary conditions on the fundamental natural frequency was estimated using the equations. The simulated results indicate that the spacing between the supporting beams is important for controlling the bending stiffness of the floor, and plays an important role in ensuring that floor serviceability remains within a comfortable level. When the size of the supporting beams was increased, the difference in vibration responses between the one- and two-way supported CLT floors decreased. By contrast, the difference between one-way and two-way supported CLT floors became significant when the beam size was reduced.

A noteworthy finding in this study is that increasing the beam size beyond a certain point would not improve floor serviceability. Therefore, other solutions such as increasing the panel thickness, adding more beams or installing dampers should be considered and studied in the future. However, reducing the beam size can cause significant vibration issues because it can lead to resonance between the floor vibration and human excitation. It is important to design the supporting beam size such that the natural frequencies of the floor system are sufficiently higher than the frequency range of human activities.

Acknowledgements

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