The effect of modelling uncertainties on the vibration serviceability assessment of floors

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ABSTRACT: Slender structural designs result into long span floors prone to human induced vibrations. Current codes of practice provide methodologies to assess the vibration serviceability in design stage based on the predicted dynamic behaviour of the structure. This paper presents a typical example of an office floor with open plan layout and low inherent damping. An initial finite element model is developed based on the available technical drawings. At completion, the modal characteristics of the structure are identified experimentally and used to calibrate the numerical model. Subsequently, the vibration serviceability of the structure is assessed based on three pertinent design methodologies. A parametric study analyses the effect of the modelling uncertainties onto the vibration serviceability assessment. It is found that small variations in natural frequencies and assumed damping ratios can significantly influence the response prediction in case of low-frequency floors. Among the guides, small differences are observed in the applied load model as well as large differences in the vibration level that is considered to be acceptable.

KEY WORDS: Floor dynamics; Vibration serviceability assessment; Human induced vibrations; Slender structural designs.

1 INTRODUCTION

Slender structural designs and the shift from compartmentalised to open plan office spaces result into long span floors prone to human induced vibrations [1-2]. In design stage, dynamic behaviour of the structure is predicted based on a numerical model. The response under footfall excitation can be assessed based on the methodologies provided by current codes of practice, e.g. Willford et al. [3], Smith et al. [4] and HiVoSS [5].

The present contribution considers a typical example of an office floor with open plan layout and low inherent damping. A finite element model is developed to calculate natural frequencies and mode shapes but requires the estimation of a number of uncertain parameters as for example the support conditions of the office floor and contribution of the compression layer to the overall stiffness.

Operational modal data is available based on an extensive measurement campaign and is used to calibrate the initial finite element model of the structure. This updating procedure allows a more accurate estimation of the uncertain model parameters in order to obtain a better agreement between the numerical and experimental results.

The calibrated model is then applied to assess the vibration serviceability of the structure according to three pertinent design methodologies [3-5]. A parametric study analyses the effect of the modelling uncertainties onto the vibration serviceability assessment. The analysis reveals the differences and (in)sensitivities of the different design methodologies.

2 OFFICE BUILDING

The office floor considered in the analysis, is the first floor in a three-story building (datacenter KU Leuven, figure 1) for which the plan-view is presented in figure 2. The two office areas of 35mx15m are constructed with pre-stressed hollow-core concrete slabs with spans of 15m, a thickness of 400mm and a compression layer of 60mm. The remaining area (patio and gangway) is comprised of a respectively 200mm and 250mm thick solid concrete slab. The system of concrete walls, façades, columns and beams, enabled to realise a cantilever length of 6.8 m.
FINITE ELEMENT MODEL

The finite element (FE) model of the structure consists of a regular mesh of 4-node shell elements (Mindlin-Reissner theory) with six degrees of freedom at each node to model the floor areas and Timoshenko beam elements to represent the supporting beams at the borders of the cantilever area. The edges at the elevator shafts are considered perfectly clamped, all other support points are pinned with appropriate rotations restrained along the support lines corresponding to walls [7].

The floor in the office area, consisting of the hollow-core pre-stressed floor slabs and compression layer, is modelled as an equivalent solid slab with the same height as the actual floor but made of an orthotropic material. The mechanical parameters of the orthotropic slab are derived based on the method as described by Diaz et al. [6] and are listed in table 1.

The beams at the borders of the cantilever area (the \( c_x \)-beam and \( c_y \)-beams – see figure 2) are given an artificial high modulus of elasticity and density to simulate the behaviour of the concrete façades. Their stiffness will be tuned based on the experimentally identified modal characteristics. The initial results (figure 4) show two types of modes, vertical bending modes of the office areas and cantilever area respectively. The predicted natural frequencies will be discussed in the following section when the comparison is made with the experimental identified characteristics.

Table 1. Material properties of the initial FE-model.

<table>
<thead>
<tr>
<th>Material properties</th>
<th>Isotropic</th>
<th>Office area</th>
<th>Orthotropic derived according to [4]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Property</td>
<td>( E = 39.6 \text{ GPa} )</td>
<td>( E_x = 45.6 \text{ GPa} )</td>
<td>( E_Z = 45.6 \text{ GPa} )</td>
</tr>
<tr>
<td>( \rho = 2500 \text{ kg/m}^3 )</td>
<td>( G_{xy} = 14.7 \text{ GPa} )</td>
<td>( G_{yz} = 14.7 \text{ GPa} )</td>
<td>( G_{xz} = 14.7 \text{ GPa} )</td>
</tr>
<tr>
<td></td>
<td>( \rho = 1490 \text{ kg/m}^3 )</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

MODEL UPDATING

The FE-model simulates the physical behaviour of the structure and can be used to predict the response to service loads, in this case human induced loading. Predicting the dynamic behaviour of structures is difficult, even with refined models based on as-built plans because of the poor prior knowledge that is available regarding some parameters (for example the stiffness of the support conditions). Updating a numerical model consists of adapting model parameters such that an optimal correspondence is found between the experimentally identified and calculated characteristics. This will result into a better representation of the dynamic behaviour of the structure and, therefore, increase the accuracy of the numerical predictions of the response.

4.1 Operational modal analysis

An extensive measurement campaign was carried out to obtain accurate and reliable operational modal characteristics of the bare office floor. Output only identification is performed based on ambient vibrations. A total of 178 sensor locations are considered, among which 6 reference sensors for which the location was set applying an optimal sensor location algorithm [8]. In each point, vertical, lateral and longitudinal vibrations are measured using triaxial sensors. The measurement campaign consisted of 29 setups, each with a duration of 10 minutes. The output-only data have been processed using a reference-based data-driven stochastic subspace identification algorithm (SSI-data/cov [9-11]). In total six modes were identified. The natural frequencies and damping ratios are listed in table 2 and the corresponding mode shapes are presented in figure 5. Mode 1 and 6 are modes characterised by large modal displacements in the cantilever area. Mode 2 up to 5 are respectively the first and second vertical bending mode of the right and left office area. It can be observed that the identified bending modes of the two office areas are decoupled in contrast to the predicted
modes of the FE-model which assumed the structure to be perfectly symmetrical. Table 2 illustrates that the identified modal damping ratios range from 1% up to 2% with the exception of the first mode for which a high modal damping ratio of more than 12% was identified. Figure 5(1) shows that this mode is accompanied by a global movement of the first floor and it is assumed that the interaction with the entire building and foundation is the explanation for the relatively high damping ratio.

4.2 Model calibration

The main aim of the calibration, is to match the bending modes of the office areas as good as possible since these four modes are excitable by the third or fourth harmonic of normal walking, which is typically the governing excitation of office floors [7].

The objective of model calibration is to find the optimal parameters $\theta_M$ by minimising a cost function that measures the discrepancy between measured ($\tilde{d}$) and computed data:

$$\theta_M = \arg \min_{\theta_M} J(G_M(\theta_M), \tilde{d})$$

(1)

Applied to the available vibration data, the following least squares cost function is used (without regularisation):

$$J(\theta_M, \tilde{d}) = \sum_{r=1}^{N_m} \frac{\alpha_r (\lambda_r(\theta_M) - \lambda_r)^2}{\lambda_r^2} + \sum_{r=1}^{N_m} \beta_r \left\| L \phi_r(\theta_M) - y_r \right\|^2$$

in which $\theta_M$ is the vector with the updating variables, $r$ represents the mode number with $N_m$ the number of incorporated modes, $\alpha_r$ and $\beta_r$ are weighting factors, $\lambda_r$ and $\lambda_r$ are respectively the calculated and identified eigenvalues (squared natural frequencies), $\phi_r$ and $\tilde{\phi}_r$ are respectively the calculated and identified vectors of modal displacements, $L$ is a binary matrix that selects the measured DOFs, and $y_r$ is a scaling factor:

$$y_r = \frac{\phi_r^T L \phi_r(\theta_M)}{\left\| \phi_r \right\|^2}$$

(2)

The characteristics of 6 identified modes are applied in the updating procedure: the natural frequencies as well as 178 vertical modal displacements. The modal assurance criterion is subsequently used to identify matching modes.

When performing operational modal analysis, natural frequencies are generally identified more accurately than mode shapes. The frequency contributions in the residual vectors are therefore assigned a larger weight ($\alpha_r = 10$) than the mode shape contributions ($\beta_r = 0.1$).

4.2.1 Updating variables

The FE-model was developed according to the as-built plans. The updating variables are those parameters for which poor prior knowledge is available. A total of 6 variables are considered in this analysis, the moduli of elasticity and shear ($E_x, E_y$ and $G_{xy}, G_{yy}$) of the orthotropic floor and the stiffness of the $c_{xx}$- and $c_{xy}$-beam elements used at the edges of the cantilever floor area ($E_{xy}, E_{yy}$). Calibrating the stiffness parameters of the left and right floor separately, will allow for a decoupling of the vertical bending modes between the left and right office area.

Table 3 summarises the results of the updating procedure. It lists the values of the model parameters of the office floor before and after updating, as well as identified natural frequencies $\tilde{f}_j$, calculated natural frequencies $f_j$ and the relative error $\epsilon_j$ which is defined as:

$$\epsilon_j = \left( \frac{f_j - \tilde{f}_j}{\tilde{f}_j} \right) \times 100$$

(3)

The agreement between the numerical model and the measurements has significantly improved. The relative error in frequency of the first vertical bending modes has been reduced to less than 1%. These modes are expected to be dominant in the response under pedestrian loading. A difference of 10% in frequency for the second bending modes of the office areas remains and cannot be reduced further with the current set of updating variables.

The results show that the modulus of elasticity $E_y$ was underestimated in design stage. $E_x$ is slightly reduced for the right side, which resulted in the decoupling of the left and right bending modes. Relatively large changes for the moduli of shear are observed as these parameters are more difficult to estimate in design stage.
Table 3. Results model calibration

<table>
<thead>
<tr>
<th>Identified</th>
<th>Calibrated FEM</th>
</tr>
</thead>
<tbody>
<tr>
<td>No.</td>
<td>(f_j) [Hz]</td>
</tr>
<tr>
<td>1</td>
<td>4.43</td>
</tr>
<tr>
<td>2</td>
<td>8.01</td>
</tr>
<tr>
<td>3</td>
<td>8.43</td>
</tr>
<tr>
<td>4</td>
<td>10.50</td>
</tr>
<tr>
<td>5</td>
<td>11.26</td>
</tr>
<tr>
<td>6</td>
<td>12.30</td>
</tr>
</tbody>
</table>

Figure 6. First six modes of the calibrated FE-model.

5 VIBRATION SERVICEABILITY ASSESSMENT

In this section the vibration serviceability assessment is performed based on three pertinent design methodologies [3-5]. First, their methodology is briefly discussed. Secondly, the results are presented and discussed in section 5.5.

5.1 Walking load

In the methodologies described by Willford et al. [3] and Smith et al. [4], a different load model is described to assess the response to footfall excitation for respectively low- and high-frequency floors. For low-frequency floors (fundamental frequency < 10.5 Hz) resonance is possible with one of the lower harmonics of the walking load. In this case, the walking load is described as a Fourier series consisting of the four lowest harmonic components:

\[
F_c(t) = G + \sum_{h=1}^{n_h} G \alpha_{eh} \sin(2\pi f_c t - \theta_h)
\]

with \(F_c\) [N] the time series of the walking load in direction e, G [N] the weight of the pedestrian, \(n_h = 4\) [-] the number of harmonics, \(\alpha_{eh}\) [-] the dynamic load factor in direction e of harmonic h, \(f_c\) [Hz] the step frequency and \(\theta_h\) [rad] the phase angle of the harmonic h.

For high-frequency floors (fundamental frequency > 10.5 Hz) the response is characterised by an initial peak response such as produced by an single impulse. The non-resonant response is treated as repeating impulsive responses to individual foot impacts, with an effective impulse \(I_{\text{eff}}\) [Ns] for each mode calculated as:

\[
I_{\text{eff},j} = 54 \frac{f_{\text{max}}^{1.43}}{f_j^{1.30}}
\]

with \(f_{\text{max}}\) [Hz] the maximum step frequency and \(f_j\) the natural frequency of the considered mode j.

HiVoSS [5] describes the step-by-step walking load as a force-time history using a polynomial with eight components. A cumulative distribution is defined for each combination of step frequency and pedestrian weight.

The guides present slightly different ranges of step frequencies that have to be considered in relation to the type of environment of the floor. Generally a maximum step frequency of 2.5 Hz is specified.

5.2 Dynamic behaviour of the structure

The dynamic behaviour of the structure is characterised by the modal parameters, i.e. natural frequencies, modal damping ratios and mass-normalised modal displacements. For low-frequency floors, Willford et al. [3] and Smith et al. [4] suggest to account for all modes up to respectively 15 Hz and 12 Hz. HiVoSS [4] specifies no upper boundary for the number of modes to include in the modal superposition, and neither do Willford et al. [3] and Smith et al. [4] in case of high-frequency floors.

5.3 Response calculation

Willford et al. [3] and Smith et al. [4] provide two approaches to calculate the response: (1) resonant response in case of low-frequency floors and (2) an impulse (transient) response in case of high-frequency floors.

Resonant response

Modal superposition is used to calculate the acceleration levels due to each harmonic of the walking load:

\[
H_{h,j,\text{input,output}}^\theta = \frac{-\beta_{h,j}^2 \varphi_{j,\text{input}} \varphi_{j,\text{output}}}{\left(1 - \beta_{h,j}^2\right) + 2i\xi \beta_{h,j}}
\]

\[
\ddot{u}_{h,j,\text{input,output}} = \left|H_{h,j,\text{input,output}}^\theta\right| F_h
\]

with \(H_{h,j,\text{input,output}}^\theta\) the contribution of the harmonic h, at mode j for the transfer function of accelerations at a specific input and output location, \(\beta_{h,j}\) [-] the ratio of the harmonic loading frequency and the natural frequency of the considered mode j, \(\varphi_{j,\text{input}}\) [1/\(\sqrt{kg}\)] the mass-normalised modal displacement of mode j at the input location, \(\varphi_{j,\text{output}}\) [1/\(\sqrt{kg}\)] the mass-normalised modal displacement of mode j
at the output location and $\xi_j$ [-] the modal damping ratio of the considered mode $j$.

The contribution of the different harmonics in both guides is combined using the SRSS method. In Smith et al [4], this response is weighted according to the weighting curves of BS 6841 [12]. Willford et al. [3] will apply a comparable weighting procedure to the contributions of the different harmonics (according to ISO 2631-2 [13]) at the stage where the predicted response is evaluated (section 5.4).

**Impulse response**

Willford et al. [3] and Smith et al [4] calculate the velocity response for each mode based upon the effective impulse and the dynamic characteristics of the floor:

$$\ddot{u}_j(t) = \varphi_{j,input} \varphi_{j,output} I_{eff,j} e^{-2\pi \xi_j f_j t} \sin(2\pi f_j t)$$  \hspace{1cm} (8)

Its RMS-value is calculated with an integration period of $1/f_j$. A similar weighing procedure is applied as in the case of the resonant response.

**HiVoSS**

The weighted velocity response is calculated based on the given load, corresponding weights and the transfer function of velocity ($H_{input,output}^R$), for each combination of input and output location of the floor:

$$H_{input, output}^R(\Omega) = \sum_{j=1}^{n} \varphi_{j,input} \varphi_{j,output} (i\Omega/\omega_j^2) \left(1 - \beta_j^2\right) + 2i\xi_j \beta_j$$  \hspace{1cm} (9)

with $\omega_j$ [rad/s] the natural frequency of the considered mode $j$, $n$ [-] the number of contributing modes and $\Omega$ [rad/s] the loading frequency. The OS-RMS-value [mm/s] is calculated based on the peak to peak RMS-velocity of the inverse Fourier transformation of this weighted velocity response.

**5.4 Evaluation of the response**

Willford et al. [3] and Smith et al [4] calculate the response factor (R) by weighing the acceleration response as presented in ISO 2631 [13] (figure 7). This factor is a multiplier on the response for each mode based upon the effective impulse and the dynamic characteristics of the floor:

$$R_{input, output} = \frac{\ddot{u}_{input, output}}{\ddot{u}_{input}}$$  \hspace{1cm} (10)

HiVoSS [5] calculates the OS-RMS-value for each combination of pedestrian weight and step frequency with corresponding cumulative distribution. The OS-RMS$_{90}$ – value [mm/s] is the 90 percentile of the OS-RMS-values. This value times 10 can be compared to the response factor as predicted by Willford et al. [3] and Smith et al [4]. The acceptance criterion for office floors according to HiVoSS [5] is OS-RMS$_{90, max} = 3.2$ mm/s. The latter is equivalent to an R-factor of 32, and thus a lot higher than the level of acceptance according to Willford et al. [3] and Smith et al [4].

**Figure 7. Threshold of perception, defined in ISO2631 [13].**

![Figure 7. Threshold of perception, defined in ISO2631 [13].](Image)

**Table 4. Natural frequencies of the (left) calibrated FE-model with an additional mass of 300 kg/m$^2$, considering different (middle) boundary conditions and (right) magnitude of the additional mass.**

<table>
<thead>
<tr>
<th>No.</th>
<th>$f_1$ [Hz]</th>
<th>$f_2$ [Hz]</th>
<th>$f_3$ [Hz]</th>
<th>$f_4$ [Hz]</th>
<th>$f_5$ [Hz]</th>
<th>$f_6$ [Hz]</th>
<th>$f_7$ [Hz]</th>
<th>$f_8$ [Hz]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>4.62</td>
<td>4.62</td>
<td>5.03</td>
<td>4.65</td>
<td>4.60</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>6.65</td>
<td>6.64</td>
<td>8.94</td>
<td>7.03</td>
<td>6.33</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>6.92</td>
<td>6.84</td>
<td>9.18</td>
<td>7.31</td>
<td>6.59</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>9.83</td>
<td>9.51</td>
<td>11.86</td>
<td>10.39</td>
<td>9.35</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>5</td>
<td>10.24</td>
<td>9.92</td>
<td>12.26</td>
<td>10.82</td>
<td>9.75</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>11.69</td>
<td>11.68</td>
<td>12.65</td>
<td>11.81</td>
<td>11.58</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**5.5 Assessment of the calibrated model of the structure**

The vibration serviceability assessment is performed based on the calibrated FE-model. In the assessment, the mass of the floor should be equivalent to the self-weight and other permanent loads, plus a proportion of the imposed loads which might be reasonably expected to be permanent. Therefore, the effect of an additional distributed mass of 300 kg/m$^2$ is simulated (dead weight), thereby reducing the natural frequencies (cfr. table 2 with respect to table 4). The suggested modal damping ratio of 3% is applied for all modes, with exception of the first mode for which a high modal damping ratio of more than 12% was identified.

The results for the maximum predicted R-factors are presented in figure 8. This figure shows that all three guidelines predict an R-factor of about 2, which results into positive assessment for the entire floor. The vibration serviceability assessment of the entire first floor is visualised in figure 9. This figure illustrates that the first bending mode of each office area is dominant in the response under pedestrian excitation, as expected. The first mode, dominant in the cantilever area, is less critical due to the relatively small modal displacements and the high modal damping ratio.
6 PARAMETRIC STUDY

The objective of the parametric study is to analyse the effect of modelling uncertainties onto the vibration serviceability assessment. The influence of respectively the (1) boundary conditions, (2) magnitude of the additional mass on the floor due to permanent loading and (3) uncertainty with respect to the modal damping ratios, is investigated. The calibrated FE-model, including the effect of a permanent load of 300 kg/m² (dead weight) and the suggested modal damping ratios of 3%, (as presented in the previous section) is considered as the general starting point.

6.1 Boundary conditions

Three configurations of the FE-model are considered: (1) boundary conditions as assumed in design stage - based on the as-built plans (as discussed in section 3); (2) all borders simply supported; and (3) all borders perfectly clamped. The corresponding predicted natural frequencies are presented in table 4. The resulting predicted R-factors according to the guidelines are presented in figure 10.

Table 4 illustrates that considering all edges simply supported has a small impact on the natural frequencies, which was to be expected since only the edges at the elevator shafts were initially considered clamped. A comparable assessment of the vibration serviceability is expected.

Considering all supporting edges perfectly clamped, results into a significant increase in the predicted natural frequencies, especially with respect to the bending modes of the office areas (table 4). This indicates that the boundary conditions, although difficult to assess in the design stage, can have a significant influence on the predicted modal parameters [14]. It is expected that this increase in natural frequencies will result into a decrease in the resulting R-factor.

Figure 10 illustrates that the assessment of model 1 and 2 is highly similar, as was expected. This figure also illustrates that model 3 is assessed similarly in case of Willford et al. [3] and Smith et al. [4] but results into a significantly lower R-factor according to HiVoSS [5]. The explanation is found in the fact that for model 3, the natural frequencies have increased significantly with respect to model 1. In contrast to model 1, where it was the 3rd harmonic of the walking load
that coincided with the natural frequency of the dominant bending modes, it is now the 4th harmonic. The effect on the resulting R-factors can be explained by looking at the magnitude of the dynamic loading factors of the harmonics of the walking load, as applied by the different guidelines (figure 11). This figure illustrates that the amplitudes of the dynamic loading factors of harmonic 2 – 3 and 4 are comparable in case of Willford et al. [3] and Smith et al. [4]. However, a large decrease in amplitude is observed with increasing number of harmonics when considering the HiVoSS guideline [5]. As a result, the R-factor according to HiVoSS [5] is reduced by a factor of more than 2.

6.2 Magnitude of the additional weight on the floor

In this section, the effect of a lower (200 kg/m²) respectively higher (400 kg/m²) additional mass on the floor (dead weight) is considered in comparison to the suggested mass of 300 kg/m². The corresponding predicted natural frequencies are presented in table 4. The resulting predicted R-factors according to the guidelines are presented in figure 12.

Table 4 shows that the calculated natural frequencies decrease with increasing additional mass on the floor, as explained in section 5.5. This decrease is much more significant for the bending modes of the office areas. The increase in additional mass on the floor, and thus increase in modal mass, is accompanied by a decrease of the corresponding mass-normalised modal displacements.

Figure 12 illustrates that the resulting R-factor is in all three cases relatively comparable to the original assessment. This is explained by the fact that the small shift in natural frequencies does not affect the number of the harmonic of the walking load with which resonance is expected. Also, the small reduction in natural frequency (and thus the expected increase in dynamic loading factor), is compensated by the slight reduction in modal displacements.

When performing the assessment of a floor for which significant permanent loads are to be expected (compared to its self-weight), it is advised to take it into account when performing the vibration serviceability assessment.

6.3 Uncertainty with respect to the modal damping ratios

Uncertainty with respect to the modal damping ratios in the design stage is inevitable, as these parameters can only be estimated. This section investigates the effect of assuming a low damping ratio of 1% and a relatively high damping ratio of 5%, with respect to the suggested damping ratio of 3%. The resulting predicted R-factors according to the guidelines are presented in figure 14.

It is expected that an increase in modal damping ratios will decrease the predicted response and vice versa. This effect is also clearly illustrated by the predicted R-factors in figure 14. However, it can be observed that the influence of the damping ratio is greater in case of Willford et al. [3], Smith et al. [4] in comparison to HiVoSS [5], due to the different method applied to calculate the structural response.

The analysis shows that the influence of the modal damping ratios is significant – as is to be expected in case of the typical resonant response for low-frequency floors. These damping ratios can however only be estimated in design stage. Therefore it is advised to experimentally validate these assumptions in case the predictions reveal a high sensitivity of the floor to human-induced vibrations.

Additionally, these type of floors could benefit from targeted in situ control measurements, specifically designed to evaluate and validate the dynamic behaviour of the structure with respect to pedestrian loading. However, currently no clear guidelines exists for in situ experiments to validate the vibration serviceability at completion.
In figure 14, it can also be observed that considering the low damping ratio almost results into a negative assessment based on the comfort criteria presented in Willford et al. [3], Smith et al. [4], whereas according to the HiVoSS guideline [5], the predicted response is almost 10 times below the defined critical R-factor. This again illustrates that there is a big difference in the comfort criteria applied by the different guidelines.

7 CONCLUSIONS

Modelling uncertainties are inevitable in the design stage and should be taken into account when the vibration serviceability assessment is performed. It is found that small variations in assumed damping ratios and natural frequencies, due to the uncertainty with respect to the boundary conditions and material properties, can significantly influence the response prediction in case of low-frequency floors. Therefore it is advised to experimentally identify the modal characteristics of the structure in case the assessment reveals a strong sensitivity to human-induced vibrations.

The analysis shows that the methodology provided by Willford et al. and Smith et al. are highly similar and result into a comparable prediction of the structural response. A significant difference with the HiVoSS guideline lies in the applied load model. For the latter, this is characterised by dynamic loading factors that decrease significantly with increasing number of the harmonic of the walking load, whereas for Willford et al. and Smith et al., the dynamic loading factors of the second up to the fourth harmonic are comparable. It is observed that the predictions of Willford et al. and Smith et al. are more sensitive to small changes in the damping ratios.

There is also an ambiguity with respect to the level of vibrations that is considered acceptable and so the comfort criteria presented by the guides. According to Willford et al. and Smith et al., an R-factor of about 4 up to 8 is considered acceptable for office buildings, whereas the HiVoSS guideline allows the R-factor to be 4 times as high (R < 32).

Lively structures could benefit from targeted in situ control measurements, specifically designed to evaluate and validate the dynamic behaviour of the structure. However, currently no clear guidelines exists for in situ experiments in order to validate the vibration serviceability at completion.

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