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The effective mass-air-mass resonance frequency of cavity stud walls

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ABSTRACT

The traditional formula for the mass-air-mass resonance frequency of a cavity wall works well for walls without a structural connection between their two wall leaves or for walls whose wall leaves are connected by highly resilient structural connections such as steel studs manufactured from thin 25 gauge steel sheet. However, for cavity walls whose wall leaves are connected by steel studs manufactured from thicker steel sheet or by wood studs, the effective mass-air-mass resonance frequency is significantly higher than that predicted by the traditional formula. This is because the air spring of the cavity connects, not the masses per unit area of the two wall leaves, but the drum modes of the two wall leaves between two adjacent studs. The frequencies of the drum modes depend on the boundary conditions imposed by the studs. Because the motion of wall leaves at the effective mass-air-mass resonance is 180 degrees out of phase and each wall leaf is symmetrical about the line connection to a stud, the boundary conditions are expected to be close to clamped for rigid studs. As the stud stiffness decreases, the drum mode resonant frequencies of both wall leaves decrease, and this decreases the effective mass-air-mass resonance frequency.

Keywords: Sound insulation, Sound transmission loss, Mass-air-mass resonance
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1. INTRODUCTION

Theories for calculating the sound insulation of cavity stud walls predict that there will be a minimum or a change of slope at the normal incidence mass-air-mass resonance frequency. However figure 6 in Davy[1] with one experimental measurement for 13 mm gypsum plaster board on each side of the studs, and figure 6 in Davy[2] with three experimental measurements for 16 mm gypsum plaster board on each side of the studs, both show that the dip in the measured sound insulation occurs at a higher frequency than the theoretically predicted normal incidence mass-air-mass resonance frequency for the case of 90 mm rigid wood stud walls with porous sound absorbing material in the cavity.

Davy[2] comments that "Note that the predicted mass-air-mass resonance frequency of about 80 Hz is significantly less than the measured mass-air-mass resonance frequencies of 125 or 160 Hz. This may be due to a structural resonance, which is not included in the theory described in this paper. Bradley and Birta[3] showed that the sound insulation of wood stud exterior walls can be significantly degraded by a structural resonance if the two wall leaves are rigidly coupled by the wooden studs. They explained this structural resonance in terms of the analysis conducted by Lin and Garrelick[4]. The effects of this resonance can be reduced by structurally isolating the two wall leaves with resilient mounts. The frequency of the resonance is about double the calculated mass-air-mass resonance, and it reduces in frequency as the rigid stud spacing is increased and as the depth of the rigid studs is increased."

"Bradley and Birta[5] reported the results of laboratory sound insulation measurements on typical Canadian building facades. These measurements showed the structural resonance at 125 Hz. However, field measurements by Bradley *et al.*[6] and Bradley[7] with actual aircraft noise showed little effect due to this structural resonance."

Recently, Davy *et al.*[8] also observed that the dip in the measured sound insulation occurs at a higher frequency than the theoretically predicted normal incidence mass-air-mass resonance frequency for cavity walls with one or two layers of 16 mm gypsum plaster board screwed to both sides of steel studs made from sheet steel thicker than 25 gauge. This difference in resonance frequency led to differences between the measured and predicted sound insulation of up to 17.5 dB at 160 Hz. The differences between measured and predicted sound insulation in the region of 160 Hz are much greater for a stud spacing of 406 mm than for a stud spacing of 610 mm. These observations prompted the research described in this paper.

The first objective of this paper is to offer a physical explanation of why the experimentally observed effective mass-air-mass resonance frequency for cavity stud walls with stiffer studs is significantly higher than the theoretically predicted normal incidence mass-air-mass resonance frequency. The second objective is to point out that that the isothermal speed of sound should be used for wall cavities which are filled with porous sound absorbing material.

Lin and Garrelick[4] is the only paper that the authors are aware of which has theoretically predicted the significant increase in the effective mass-air-mass resonance frequency which occurs with stiffer studs and they only considered wooden studs. Unfortunately, their dimensionless variables appear to disagree with the properties of the wall whose sound insulation they claimed to be calculating. Their use of the Fourier series method means that the actual physical reason for the increase in effective mass-air-mass resonance frequency is not obvious and they are unable to model the effects of the finite size of the wall.

Narang[9] and Davy *et al.*[10] have provided experimental evidence for the use of the isothermal speed of sound in a wall cavity which is filled with sound absorbing material.

2. THEORY

The first bending mode between two adjacent studs of each wall leaf of a cavity stud wall is modelled as a linear harmonic oscillator. These two linear harmonic oscillators are coupled by the spring of the air cavity. The position, mass and stiffness of each linear harmonic oscillator are x_i, m_i and K_i respectively where i = 1, 2. The stiffness of the spring coupling the two linear harmonic oscillators is K_{12}. The system comprising the two coupled linear harmonic oscillators has kinetic energy T and potential energy V. Its Lagrangian is

$$L = T - V = m_1\dot{x}_1^2/2 + m_2\dot{x}_2^2/2 - K_1x_1^2/2 - K_{12}\left(x_2 - x_1\right)^2/2 - K_2x_2^2/2\,. \tag{1}$$

The Lagrangian equations of motion are

$$\frac{d}{dt}\left(\frac{\partial L}{\partial \dot{x}_i}\right) - \frac{\partial L}{\partial x_i} = 0 \text{ for } i = 1,2\,, \tag{2}$$

where t is the time. Applying equations (2) to equation (1) gives

$$\begin{aligned} m_1\ddot{x}_1 + \left(K_1 + K_{12}\right)x_1 - K_{12}x_2 &= 0 \\ m_2\ddot{x}_2 - K_{12}x_1 + \left(K_2 + K_{12}\right)x_2 &= 0 \end{aligned}\,. \tag{3}$$

To find the resonance angular frequencies of the two coupled linear harmonic oscillators, assume that

$$x_i = a_i \exp(j\omega t) \text{ for } i = 1,2\,, \tag{4}$$

where a_i, i = 1,2, are the complex amplitudes of the two coupled linear harmonic oscillators and ω is the angular frequency. This assumption gives

$$\begin{pmatrix} K_1 + K_{12} - \omega^2 m_1 & -K_{12} \\ -K_{12} & K_2 + K_{12} - \omega^2 m_2 \end{pmatrix}\begin{pmatrix} a_1 \\ a_2 \end{pmatrix} = \begin{pmatrix} 0 \\ 0 \end{pmatrix}. \tag{5}$$

Equation (5) can only be true for non-zero a_i, i = 1, 2, if the determinant of the matrix in equation (5) is zero. Thus

$$\left(K_1 + K_{12} - \omega^2 m_1\right)\left(K_2 + K_{12} - \omega^2 m_2\right) - K_{12}^2 = 0\,. \tag{6}$$

Dividing equation (6) by m_1m_2 gives

$$\left(K_1/m_1 + K_{12}/m_1 - \omega^2\right)\left(K_2/m_2 + K_{12}/m_2 - \omega^2\right) - \left(K_{12}/m_1\right)\left(K_{12}/m_2\right) = 0\,. \tag{7}$$

Putting

$$f = \frac{\omega}{2\pi},\ f_1 = \frac{1}{2\pi}\sqrt{\frac{K_1}{m_1}},\ f_2 = \frac{1}{2\pi}\sqrt{\frac{K_2}{m_2}},\ f_{a1} = \frac{1}{2\pi}\sqrt{\frac{K_{12}}{m_1}} \text{ and } f_{a2} = \frac{1}{2\pi}\sqrt{\frac{K_{12}}{m_2}}\,, \tag{8}$$

gives

$$\left(f^2 - f_1^2 - f_{a1}^2\right)\left(f^2 - f_2^2 - f_{a2}^2\right) - f_{a1}^2 f_{a2}^2 = 0. \tag{9}$$

Expanding this equation gives

$$f^4 + pf^2 + q = 0, \tag{10}$$

where

$$p = -\left(f_1^2 + f_2^2 + f_{a1}^2 + f_{a2}^2\right) \text{ and } q = f_1^2 f_2^2 + f_1^2 f_{a2}^2 + f_2^2 f_{a1}^2. \tag{11}$$

Thus, the resonance frequencies of the system comprising two coupled linear harmonic oscillators are

$$f_{\pm} = \sqrt{\left(-p \pm \sqrt{p^2 - 4q}\right)\big/2}. \tag{12}$$

If $f_1 = f_2 = f_0$ and $f_{a1} = f_{a2} = f_a$ then equation (9) becomes

$$\left(f^2 - f_0^2\right)\left(f^2 - \left[f_0^2 + 2f_a^2\right]\right) = 0, \tag{13}$$

and its positive solutions give the two resonance frequencies of the coupled linear harmonic oscillators as

$$f_- = f_0 \text{ and } f_+ = \sqrt{f_0^2 + 2f_a^2}. \tag{14}$$

In the situation considered in this paper, the frequency f_i is the resonance frequency of the first bending mode of the ith wall leaf between two adjacent studs and f_{ai} is the normal incidence mass-air resonance frequency of the ith wall leaf and the air in the wall cavity.

The normal incidence mass-air resonance frequency f_{ai} of the ith wall leaf and the air in the wall cavity is

$$f_{ai} = \frac{1}{2\pi}\sqrt{\frac{\rho_0 c^2}{dm_i}}, \tag{15}$$

where ρ_0 is the density of air, m_i is the mass per unit area of the ith wall leaf, d is the width of the wall cavity and c is the speed of sound in air. This means that if $m = m_1 = m_2$ is the mass per unit area of each wall leaf, the second term under the square root in equation (14) is the square of the normal incidence mass-air-mass resonance frequency f_{mam}.

$$\sqrt{2f_a^2} = \frac{1}{2\pi}\sqrt{\frac{\rho_0 c^2}{d}\frac{(m+m)}{mm}} = \frac{1}{2\pi}\sqrt{\frac{\rho_0 c^2}{d}\frac{(m_1+m_2)}{m_1 m_2}} = f_{mam}, \tag{16}$$

Thus, when the wall leaves are the same, the lower resonance frequency f_- is the resonance frequency of the first bending mode of a wall leaf between two adjacent studs and the higher resonance frequency f_+ is the root mean square sum of f_0 and f_{mam}. The situation is more complicated when the two wall leaves are different, and the resonance frequencies are given by equation (12).

The frequency f_i is the resonance frequency of the first bending mode of the ith wall leaf between two adjacent studs. The problem is that the exact boundary conditions at the studs are not known. If the boundary conditions were simply supported at each stud or guided at each stud, the resonance frequency f_i of the first bending mode of the ith wall leaf between two adjacent studs is

$$f_i = \frac{\pi}{2L^2}\sqrt{\frac{E_i h_i^2}{12\rho_i\left(1-\nu_i^2\right)}}, \tag{17}$$

where L is the spacing between the studs and E_i, υ_i, ρ_i and h_i are respectively the Young's modulus, the Poisson ratio, the density and the thickness of the ith wall leaf. Note that

$$m_i = \rho_i h_i \tag{18}$$

On the other hand, if the boundary conditions were clamped at each stud or free at each stud

$$f_i = \frac{3.56}{L^2}\sqrt{\frac{E_i h_i^2}{12\rho_i\left(1-\nu_i^2\right)}} \tag{19}$$

Equation (19) produces resonance frequency values which are 2.27 times greater than those given by equation (17). Because the wall leaves are vibrating out of phase in the effective mass-air-mass resonance mode, a rigid stud line connection will stop the wall leaves from moving at the line connection. Because the vibration of a wall leaf is symmetrical about the stud line connection in the effective mass-air-mass resonance mode, the part of the wall leaf on one side of the line connection will stop the part of the same wall leaf on the other side rotating at the line connection. Thus, the boundary conditions are likely to be close to clamped. As the studs become less rigid, the boundary conditions, imposed by the studs and the wall leaves on the other sides of the studs, are expected to depart further from clamped boundary conditions. Nightingale and Bosmans[11] have shown experimentally that point connections of a building leaf to a stud behave like line connections when their spacing is less than half the bending wave length of the building leaf. Thus, the above conclusions for line connections also apply to point connections in the low frequency region where the effective mass-air-mass resonance occurs. As the spacing between the point connections becomes greater than half the bending wavelength of the building leaf with increasing frequency, the behaviour of point connections gradually starts to differ from the behaviour of a line connection.

In this paper, the resonance frequency of the first bending mode between the studs is calculated by multiplying equation (17) for the simply supported resonance frequency by an empirical correction factor r. Japanese researchers[12] use a similar approach to calculate the resonance frequencies of concrete floor slabs by multiplying the approximate formula for the resonance frequencies of a clamped panel by a frequency multiplier. The empirical correction factor r is determined by choosing the value which gives the best agreement between theory and experiment. It will be greater than zero and is expected to be less than 2.27.

Because the vibration of the two wall leaves in the mass-air-mass resonance mode is out of phase there will be a surface through the studs where the studs are stationary.

This means that the studs will not transmit any translational energy. Because the vibration of a wall leaf in the mass-air-mass resonance mode is symmetrical about the effective line connection between the stud and the wall leaf, the wall leaf will not rotate at the connection to the stud and hence will not transmit rotational energy. This conclusion applies regardless of the stiffness of the studs. This means that the leaves are effectively not coupled by the studs when vibrating in the mass-air-mass resonance mode. Of course, the studs will transmit power for other types of leaf motion by coupling the motion of the wall leaves.

The critical frequency f_{ci} of the ith building element leaf is

$$f_{ci} = \frac{c^2}{2\pi}\sqrt{\frac{12\rho_i\left(1-\nu_i^2\right)}{E_i h_i^2}} \tag{20}$$

The experimental observation is that a building leaf consisting of two layers, which individually have same sheet material properties and thickness, and which is screwed, or spot glued, to the studs, has the same critical frequency as a single layer with the same sheet material properties and thickness. The reason is that the spot fastening enables the two layers to slide relative to each other when bent dynamically, provided the bending wave length is shorter than the screw spacing. In the sound insulation prediction method used in this paper, this behaviour is modelled by treating the double layers as a single layer with twice the thickness and one quarter of the Young's modulus of the actual single layer sheets. This means that the product $E_i h_i^2$ is the same for both the double layer and single layer building element leaves. Thus, these double and single layer leaves have the same critical frequencies and the same bending wave resonances between studs with the same spacing.

In Davy *et al.*[13], the calculated adiabatic mass-air-mass resonance frequency was set to the upper resonance frequency f_+ by modifying the cavity depth used in the theoretical calculations. The adiabatic mass-air-mass resonance frequency equation (the last two expressions in equation (16)) was inverted and used to calculate the equivalent cavity width which would make the calculated adiabatic mass-air-mass resonance frequency equal to the upper resonance frequency f_+. This equivalent cavity width was used instead of the actual cavity width when applying the existing theory of Davy[1,2,14] in order to avoid reprogramming the existing theory.

Also, in Davy *et al.*[13], because the theory could not predict some of the very deep dips in the sound insulation spectrum at the upper resonance frequency f_+, in some cases the sound absorption coefficient of the wall cavity was multiplied by an empirical factor at and below an empirical frequency. These empirical values were determined by making the theory agree with experiment as well as possible. The sound transmission between the wall leaves via the studs was included below the upper resonance frequency f_+ in Davy *et al.*[13]. In this paper, the sound transmission between the wall leaves via the studs was only included above the upper resonance frequency f_+.

The effective mass-air-mass resonance is included in the theory used in this paper by modifying Davy's[1,2] theory for the transmitted sound due to airborne sound transmission across the cavity. The sound transmission coefficient of the wall is initially modelled as though it is a single leaf wall with the same total mass per unit area[1,15]. Then, following an approach like that of Bradley *et al.*[16], this sound transmission coefficient is multiplied by the following mass-spring resonance power transmissibility function T.

$$T = \frac{1}{\left[1-\left(\frac{f}{f_+}\right)^2\right]^2 + 4D^2\left(\frac{f}{f_+}\right)^2}, \tag{21}$$

where D is the ratio of the actual damping to the critical damping and f_+ is the resonance frequency of the effective mass-air-mass resonance. At higher frequencies, this approach would predict too low a sound transmission coefficient due to sound transmission across the air cavity via the air in the cavity. This problem is solved by taking the maximum of the sound transmission coefficients above the effective mass-air-mass resonance frequency predicted by this approach and Davy's[1,2] existing theory for the transmitted sound due to airborne sound transmission across the cavity.

Because the power transmissibility function T already includes the rapid decrease in airborne sound transmission across the wall cavity that occurs above the effective mass-air-mass resonance frequency f_+, the frequency dependent restriction imposed by equation (35) of Davy[1] is removed. Once the sound transmission via the air in the wall cavity is calculated, it is combined with the sound transmission via the studs[14], but as stated above, the stud borne sound transmission is only included above the effective mass-air-mass resonance frequency f_+.

All the cavity stud walls considered in this paper had porous sound absorbing material in their wall cavities. Based on the observations of Narang[9] and Davy *et al.*[10] that adding porous sound absorbing material to a wall cavity changes the speed of sound from the adiabatic value to the isothermal value, the isothermal speed of sound was used in equation (15). For 25-gauge studs, it appears experimentally that the decrease due to the isothermal speed of sound in wall cavities filled with sound absorbing material counteracts the smaller increase in the mass-air-mass resonance frequency due to the drum mode.

3. EXPERIMENTAL RESULTS

The empirically determined values of the bending wave resonance frequency multiplier r, the effective mass-air-mass resonance frequency f_+, the ratio D of the actual damping to the critical damping of the effective mass-air-mass resonance, the Young's modulus E and the damping loss factor η of the gypsum plaster board (GPB) layers are given in Table 1 for 8 cavity stud walls measuring 3.05 m wide by 2.44 m high, with 39 x 89 mm wood studs and layers of 13 or 16 mm gypsum plaster board on each side,. The numbers in the GPB Layers columns denote the thicknesses of the GPB layers in mm. The letter X denotes type X fire rated GPB. All the walls had porous sound absorbing material in their wall cavities. The wood stud wall data is taken from Halliwell *et al.*[17] and experimentally determined values of the surface density of the GPB layers were used. In Davy *et al.*[13], experimentally determined values of the Young's modulus and a fixed value of 0.3 for the damping loss factor were used. However, when comparing the theory with the experimental results, it was noticed that there was obviously variability in the Young's modulus and the damping loss factor for the different samples of GPB used to construct the 8 different cavity stud walls. Thus in this paper, empirical values of the Young's modulus E and the damping loss factor η were determined by making the theoretical predictions agree with the experimental sound insulation values as well as possible in the region of the critical frequency dip. Empirical values of the bending wave resonance frequency multiplier *r*, the effective mass-air-mass resonance frequency f_+ and the ratio *D* of the actual damping to the critical damping of the effective mass-air-mass resonance were determined by making the theoretical predictions agree with the experimental sound insulation values as well as possible in the region of the effective mass-air-mass resonance.

Quirt *et al.*[18] determined the Young's modulus *E* and damping loss factor η by supporting beams of gypsum plaster board horizontally on pipe supports with a 2.5 cm overhang at both ends. The beams were tapped with an impact hammer or a finger and the impulse response at the centre of the beam was measured with an accelerometer. The impulse response was Fourier transformed to obtain the frequency response. The frequency of the first beam mode was determined from the first resonance frequency peak in the frequency response and the Young's modulus *E* was calculated by assuming that the beam was simply supported. The damping loss factor η was calculated by dividing the half power bandwidth of the first resonance peak by the frequency of the first resonance peak. Note that Quirt *et al.*[18] give flexural stiffness as the product *EI* of the Young's modulus *E* and the second moment of area *I*. They give the damping in terms of the damping ratio ζ which is half of the damping loss factor η which is used in this paper and express the damping ratio ζ as a percentage. The product *EI* is expressed in the slightly unusual units of N mm^2/mm. The Young's modulus *E* was measured using beams cut perpendicularly and parallel to the long axis of the gypsum plasterboard sheets and the average value obtained across the two directions is quoted in this paper. The damping loss factor η was determined only from measurements on beams cut perpendicularly to the long axis of the gypsum plasterboard sheets.

Table 1. Empirically determined values of the bending wave resonance frequency multiplier *r*, the effective mass-air-mass resonance frequency f_+, the ratio *D* of the actual damping to the critical damping of the effective mass-air-mass resonance, the Young's modulus *E* and the damping loss factor η.

Wall No.	GPB Layers	GPB Layers	*r*	f_+ (Hz)	*D*	*E* (GPa)	η
1	12.7X	12.7X	1.98	148	0.075	2.47	0.042
2	12.7	12.7	1.72	136	0.106	1.93	0.034
3	12.7X	12.7X	1.80	136	0.095	2.53	0.029
4	15.9X	15.9X	1.58	146	0.088	2.15	0.030
5	12.7X	12.7X+12.7X	1.91	141	0.000	2.55	0.040
6	12.7	12.7+12.7	1.76	133	0.100	1.99	0.034
7	15.9X	15.9X+15.9X	1.70	151	0.074	2.17	0.031
8	12.7X+12.7X	12.7X+12.7X	2.01	144	0.000	2.60	0.040

The 12.7 mm X fire rated gypsum plaster board had the highest empirical effective Young's moduli of 2.60, 2.55, 2.53 and 2.47 GPa. The average measured Young's modulus was 2.26 GPa. The 15.9 mm X fire rated gypsum plaster board had empirical effective Young's moduli of 2.19 and 2.17 GPa compared to an average measured value of 2.41 GPa. The 12.7 non-fire rated gypsum plaster board had empirical effective Young's moduli of 1.99 and 1.77 compared to an average measured value of 2.18 GPa. It is interesting to note that the relative order of the different gypsum plaster boards is different for the empirical values and the average values measured on an isolated beam.

The 12.7 mm X fire rated gypsum plaster board had the three highest damping loss factors of 0.042, 0.040 and 0.040. Strangely it also had the lowest empirical damping loss factor of 0.029. Its damping loss factor was not measured. The 12.7 mm non-fire rated gypsum plaster board had empirical damping loss factors of 0.034 and 0.034. The averaged measured damping loss factor of isolated beams was 0.0230 with a standard deviation of 0.0152. The 15.9 mm X fire rated gypsum plaster board had empirical damping loss factors of 0.031 and 0.030. The averaged measured damping loss factor of isolated beams was 0.0136 with a standard deviation of 0.0016. As expected, the in-situ damping loss factors were higher than those measured on isolated beams through the relative order is the same. Further investigation revealed that the three walls with the highest damping loss factors had 90 mm of blown cellulose fibre insulation in their wall cavities while the five walls with the lower damping loss factors had 90 mm of mineral fibre insulation in their wall cavities.

The bending wave resonance frequency multipliers *r* of the 12.7 mm X fire rated gypsum plaster board were 2.01, 1.98, 1.91 and 1.80. The frequency multipliers for the 12.7 mm non-fire rated gypsum plaster board were 1.76 and 1.72, while the 15.9 mm X fire rated gypsum plaster board had frequency multipliers of 1.70 and 1.58. These frequency multipliers are different from those determined by Davy *et al*.[13] because the empirically determined Young's moduli, which also effect the effective mass-air-mass resonance frequency, are different from the measured values used by Davy *et al*.[13] and because the method of determining the frequency multipliers by fitting the transmissibility function of equation (21) is more sensitive and accurate.

It is interesting to note that, for the resonance frequencies of concrete floor slabs, Japanese researchers[12] use the approximate formula for the resonance frequencies for a clamped panel with a frequency multiplier of 0.8. This is the same as a frequency multiplier *r* of 1.82 times the simply supported panel resonance frequencies.

The ratios *D* of the actual damping to the critical damping of the effective mass-air-mass resonance for the 12.7 mm non-fire rated gypsum plaster board were 0.106 and 0.100. The 12.7 mm X fire rated gypsum plaster board had damping ratios of 0.095, 0.075 0.000 and 0.000. The damping ratios of the 15.9 mm X fire rated gypsum plaster board were 0.088 and 0.074. The two damping ratio values of 0.000 cannot be correct and only occur because the two effective mass-air-mass resonance frequencies of 141 and 144 Hz are the two frequencies which are closest to the frequency of 141 Hz which is the mid-point frequency on a logarithmic scale between the third octave band centre frequencies of 125 and 160 Hz. This means that the resonance transmission function *T* values at 125 and 160 Hz are not strongly controlled by the damping ratio *D* values in these cases.

The fact that the empirical values were usually similar for the same type of gypsum plaster board gives confidence in the empirical values.

Figure 1 shows the difference between theory and experiment in decibels for the sound insulation of the eight 39 x 89 mm wood stud cavity walls listed in Table 1, with layers of 13 or 16 mm gypsum plaster board (GPB) on each side, measuring 3.05 m wide by 2.44 m high. The differences are small in the 100 to 125 Hz range, where the effective mass-air-mass resonance occurs, and at 3150 Hz, in the critical frequency region, because the empirical values were determined by minimising the differences in these two frequency ranges. The theory systematically under predicts below 100 Hz and above the critical frequency. The theory also under predicts at 200 or 250 Hz because it struggles to predict the very rapid increase in the sound insulation above the effective mass-air-mass resonance frequency. Above 250 Hz and below the critical frequency, the theory both over predicts and under predicts.

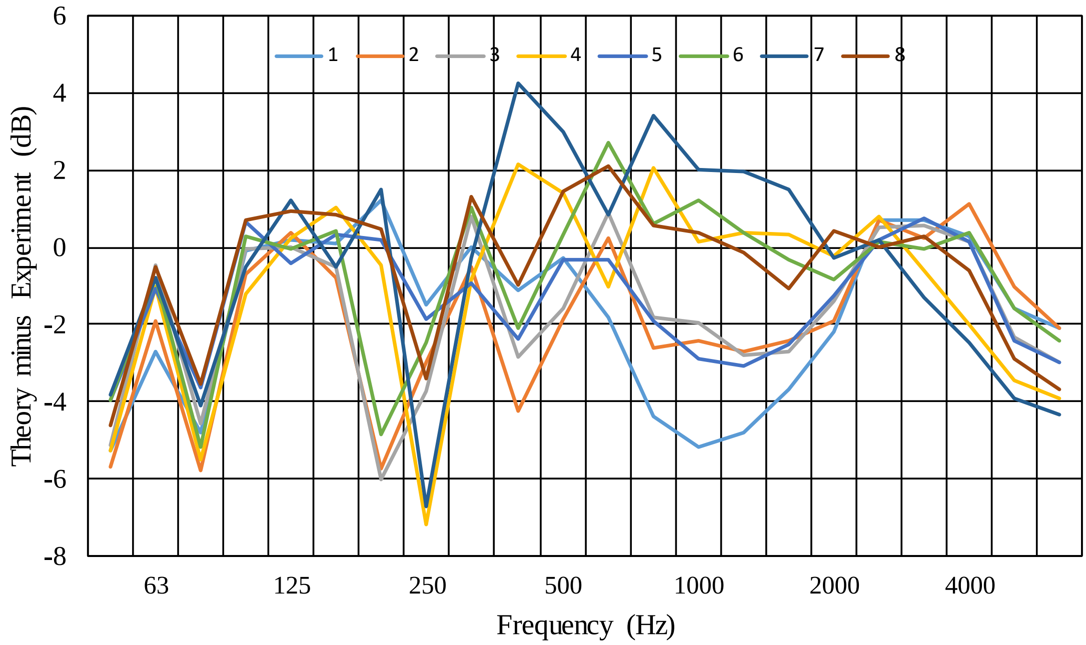

Figure 1. The difference between theory and experiment in decibels for the sound insulation of the walls listed in Table 1.

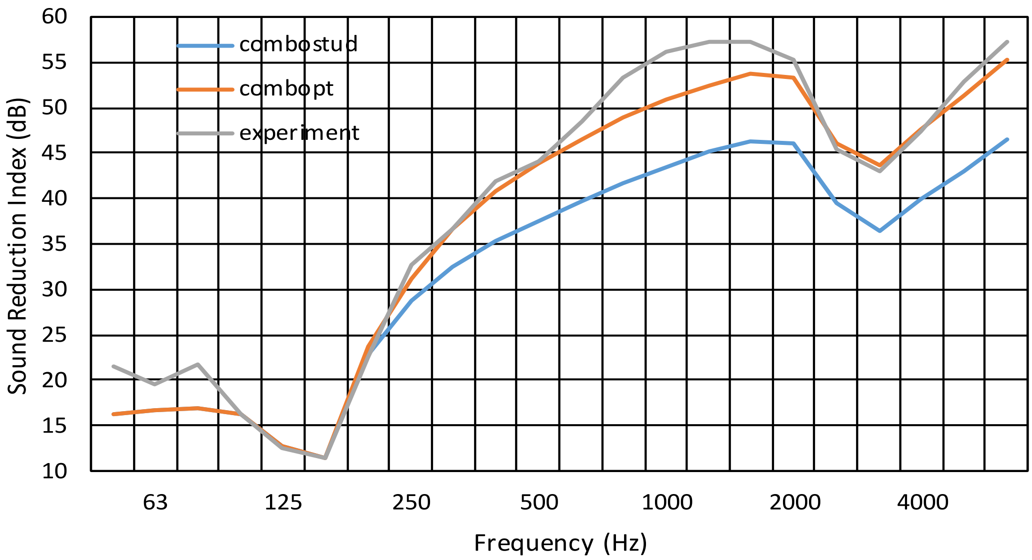

Figure 2. Comparison of the measured sound insulation for wall No. 1. with predictions.

Figure 2 compares the experimental sound insulation for wall No. 1. with predictions using the point connection model (combopt) and the line connection model (combostud) for modelling the sound transmission across the cavity via the studs. This wall has single sheets of 12.7 mm fire rated GPB on each side of 39 x 89 mm wood studs. The point connection model predicts the sound insulation much better than the line prediction model. This shows the importance of the screw spacing.

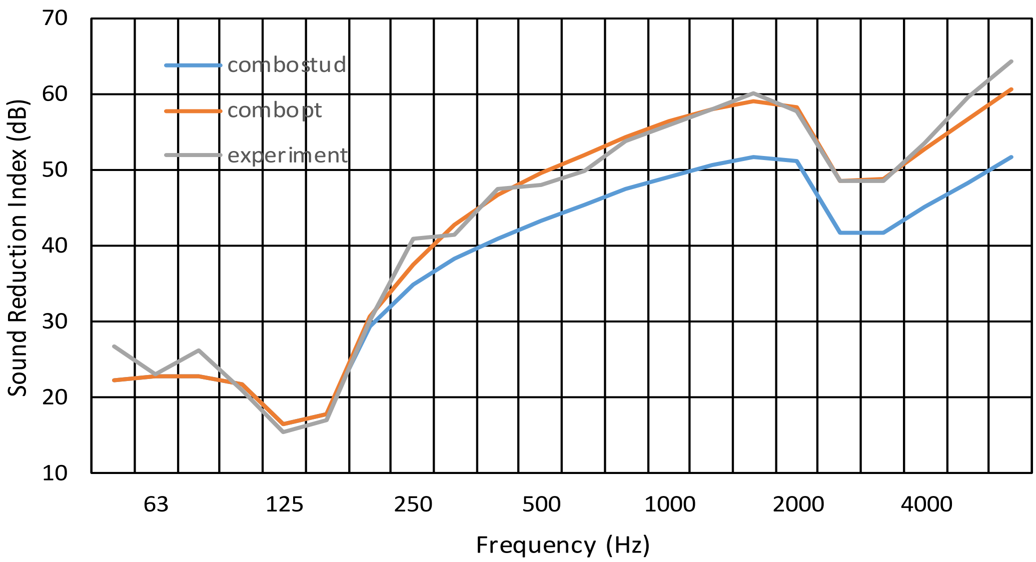

Figure 3. Comparison of the measured sound insulation for wall No. 8. with predictions.

Figure 3 compares the experimental sound insulation for wall No. 8. with predictions using the point connection model (combopt) and the line connection model (combostud) for modelling the sound transmission across the cavity via the studs. This wall has two sheets of 12.7 mm fire rated GPB on each side of 39 x 89 mm wood studs. Again, the point connection model predicts the sound insulation much better than the line prediction model.

4. CONCLUSIONS

This paper presents the theory for calculating the effective normal incident mass-air-mass resonance frequency for a double leaf cavity stud building element. If the two building element leaves are similar, this frequency is the root mean square of the first bending wave mode resonance frequency of the building element leaf between adjacent studs and the normal incident mass-air-mass resonance frequency of the version of the building element without studs. If the building element cavity contains porous sound absorbing material, the isothermal normal incident mass-air-mass resonance frequency should be used. Although not shown in this paper, for a building element cavity without porous sound absorbing material, it is expected that the adiabatic normal incident mass-air-mass resonance frequency should be used.

Because the exact boundary conditions of the building element leaves at the studs are not known, and because these boundary conditions will depend on the compliance of the studs, this paper gives empirically determined factors by which to multiply the first bending wave mode resonance frequency of the building element leaf between adjacent studs with simply supported boundary conditions in order to obtain this resonance frequency with the actual boundary conditions.

In order to calculate the correct sound insulation of a double leaf cavity stud building element with porous sound absorbing material in its cavity in the vicinity of the effective normal incident mass-air-mass resonance frequency, this paper gives empirically determined values of the bending wave resonance frequency multiplier r, the effective mass-air-mass resonance frequency f_+, the ratio D of the actual damping to the critical damping of the effective mass-air-mass resonance, the Young's modulus E and the damping loss factor η of the gypsum plaster board (GPB) layers for 8 wooden stud walls with porous sound absorbing material in their wall cavities.

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