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IMPACT OF STANDING WAVES IN THERMAL LAYERS ON THE ACOUSTIC PERFORMANCE OF FLOATING FLOORS

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ABSTRACT

The acoustic performance of a floating floor is often evaluated according to the Cremer-Vèr model. This model predicts an increase of 40 dB per decade of frequency above the resonance frequency for locally reacting floating floors. This approximation provides a reliable estimation only if the thickness of the underlay of the floating floor is negligible with respect to the wavelength of longitudinal waves present in this underlay. When the longitudinal wavelength is smaller than the thickness, standing waves appear in the underlay and the insulation effectiveness will be reduced. This paper highlights the effect of these standing waves for floating floors with thermal insulation layers by means of experiments and an adapted analytical model. For typical layer thicknesses (5-10 cm), standing waves can occur already from 500 Hz onwards.

Keywords: *Impact sound insulation, Thickness-resonance waves, floating floors, Harrison-Sykes-Martin model*

1. INTRODUCTION

To improve the impact sound insulation, a floating screed is often used. In practice, the resilient underlay is often laid on a thermal layer which levels and embeds the pipes on the base floor. This thermal layer therefore has the advantage of providing a flat surface necessary for the correct placement of the acoustic underlay. However, its presence is not without consequence on the acoustic performance of the floating screed [1, 2]. Indeed, the longitudinal wavelengths present in this layer can be of the same order of magnitude as its thickness at higher frequencies. This produces a standing wave field which can reduce the acoustic performance of the system. For typical thermal insulation layers used under floating floors, this phenomenon of thickness-resonances can already be observed from 500 Hz upwards. These thickness-resonances can also occur in the resilient underlayers themselves if they have a high density (e.g. rubber-based) . In order to better take into account the formation of these thickness-resonances in the thermal layers and to propose adequate solutions to better insulate against impact noise, a more suitable prediction model than the usual predictive calculations is needed. This paper presents the application of a new formula for predicting the improvement of impact sound insulation ΔL that takes into account this standing wave field [2]. This new formula is an adaptation of the Harrison-Sykes-Martin (H-S-M) model [3] and is currently only applicable to floating floor systems with a simple thick and/or heavy underlay. In the remainder of this paper, the floating systems studied will therefore consist of a finishing screed placed on a single resilient layer represented by the thermal layer.

2. ADAPTATION OF THE HARRISON-SYKES-MARTIN (H-S-M) MODEL FOR FLOATING FLOORS ON A SIMPLE THICK AND/OR HEAVY UNDERLAY

Several empirical models allow to estimate the improvement of impact sound insulation ΔL of a floating floor from the inertial and the elastic properties of the system [4, 5]. As a first approximation, a floating floor

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can be considered as a single-degree-of-freedom mass-spring mechanical system. The reduction of the vibrations amplitude, generated by the impacts on the floating screed (i.e., the input force, F_{in}) and transmitted to the base floor through the resilient layer, is quantified in terms of force transmissibility, according to Eqn. (1):

$$\Delta L = 20\log|F_{in}/F_{out}| = 20\log|1/\mathcal{T}| \qquad (1)$$

where F_{out} is the resulting amplitude of force transmitted on the base floor and $\mathcal{T}$ is the constitutive equation of the physical model adopted. This model is intended for floating floors built with highly damped (or "locally reacting") floating screeds.
The classical constitutive model usually used considers an acoustically thin resilient layer for which the system can be modelled as a single-degree-of-freedom mass-spring-damper system. If the damping is described as hysteretic, the improvement of impact sound insulation is expressed by the following well-known prediction formula:

$$\Delta L = 20\ \log\sqrt{\frac{\eta^2 + \left(1 - \frac{\omega^2}{\omega_0^2}\right)^2}{1+\eta^2}} \qquad (2)$$

where $\omega_0 = \sqrt{\frac{s}{M}}$ is the mass-spring-resonance frequency of the system and η is the loss factor. s (N m^{-3}) is the dynamic stiffness of the resilient layer, M (kg m^{-2}) is the actual mass per unit area of the floating screed.

But, for a floating floor system with a thick layer, complex transmissibility, named $\mathcal{T}_m$, must deal with traveling and standing waves in this layer which are a combination of compression, shear, bulk, torsion and surface waves for which the travelling motion of the longitudinal waves in the resilient layer is given by:

$$\underline{E}\frac{\partial^2 u_d}{\partial x^2} + \rho\omega^2 u_d = 0 \qquad (3)$$

in which ω is the angular frequency, ρ is the density of the resilient layer, $\underline{E} = E(1 + j\eta)$ is the complex elastic modulus, η is the loss factor and $u_d(x)$ is the amplitude of the harmonic one-direction displacement.

The solution for the square modulus of the force transmissibility $|\mathcal{T}_m|^2$ becomes [2]:

$$|\mathcal{T}_m|^2 = \frac{1+4n^2}{\left[\begin{array}{l}(1+4n^2).[sinh^2(n\beta L) + cos^2(\beta L)] \\ +\frac{\omega}{\omega_0}\sqrt{\frac{M}{m}}\left(\frac{1}{1+n^2}\right)[n\,sinh(2n\beta L) - (1+2n^2)\,sin(2\beta L)] \\ +\frac{\omega^2}{\omega_0^2}\frac{M}{m}\left(\frac{1}{1+n^2}\right)[sinh^2(n\beta L) + sin^2(\beta L)]\end{array}\right]} \qquad (4)$$

where $\omega_0 = \sqrt{\frac{E}{ML}}$ is the mass-spring-resonance frequency, m is the mass per unit area of the resilient layer (namely, $m = \rho L$) and $\beta L = \frac{\omega}{\omega_0}\sqrt{\frac{m}{M}}$ with L, the thickness of the resilient .

Eqn. (depends on well-defined mechanical quantities, such as the mass per unit area of the actual floating screed M, the mass per unit area of the resilient layer m, the fundamental resonance frequency of the floating floor ω_0, and the loss factor η of the resilient component.

In this way, once experimental values are accurately determined, it is possible to estimate the improvement of impact sound insulation of floating floors with an thick resilient layer, by applying $\Delta L_m = 20\log|1/\mathcal{T}_m|$, as shown in Fig. 1.

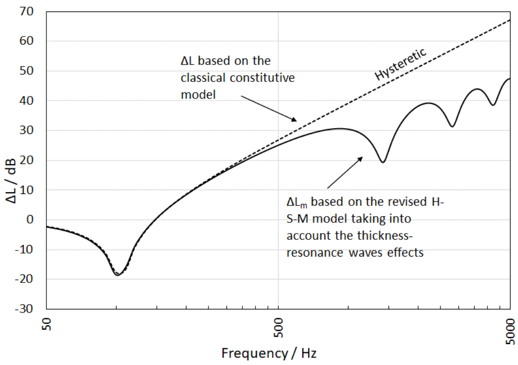


Figure 1. Example of an estimation of the improvement of impact sound insulation by applying the ordinary transmissibility theory with hysteretic damping (dotted line) and by applying the revised H-S-M model (full line).

The thickness-resonance wave effects involve an increase of the transmitted energy (therefore a decrease in the improvement of impact sound insulation), in correspondence with the resonance frequencies ω_i of the standing waves in the resilient component of the system. The resonance frequencies are determined by:

$$\omega_i = \omega_0 i\pi \sqrt{\frac{M}{m}} \tag{5}$$

(with $i = 1, 2, 3, \dots, \mathrm{n}$)

The corresponding values of ω_i minimize the denominator of Eqn. (. Relation (5) is very useful, since it allows to immediately identify, as a function of the inertial and elastic properties of the materials, the frequency range in which thickness-resonance wave effects occur.

3. EXPERIMENTAL VALIDATION

3.1 Experimental test procedure

To verify the effectiveness of the revised H-S-M model in predicting the effect of thickness-resonances on the acoustic performance of floating floors, four different thick thermal layers are investigated which consist of controlled expanded polystyrene granules mixed with cement, water and special additives (denoted as 'EPS'). The layers, considered as the resilient layers, are covered by a cementitious screed (Fig. 2).

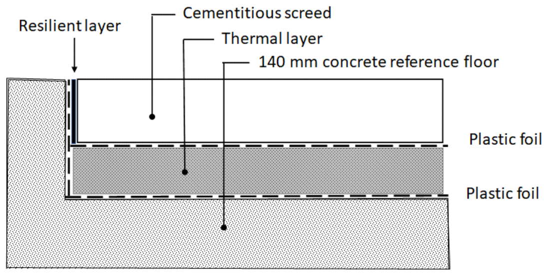


Figure 2. The configuration of the floating floor systems investigated

The improvement of impact sound insulation *ΔL* was measured in the frequency range of 50 Hz – 5 kHz according to the ISO Standard 10140 – series [6] in the acoustics laboratory of Buildwise. The floating floors were installed on a 140 mm concrete reference floor with a density of 2400 kg/m³ and a surface area of 11.5 m². The density of the finishing screed was 1930 kg/m³. Its thickness for the first two configurations was around 50 mm and 60 mm for the third and the fourth. Measurements were performed after 28 days of floating slab curing time. In Fig. 3 the application of the EPS layer is shown.

The experimental values (with the related standard uncertainties) of the material properties of the EPS layers used in the floating floor systems are given in Tab. 1.

Figure 3. Application of the EPS thermal layer

The measurements of the EPS density show a deviation of around 6% due to the inhomogeneity of the product. The apparent dynamic stiffness, s_t', of the layers was measured according to ISO 9052-1 [7]. The measurements were carried out after a loading period of 28 days in order to be consistent with the *ΔL* measurements. The loss factor η of the resilient components was determined from the width of the experimental resonance peak ω_r by applying the half-power bandwidth method, $\eta = \Delta\omega/\omega_r$.

Table 1. Material properties for EPS thermal layers: experimental values and standard uncertainties

		Thermal/resilient layer			
		ρ /kg·m^{-3}	L /mm	η /-	s_t' /MN·m^{-3}
1	EPS 330 - 50 mm	327±19	48±2	0.16±0.034	150±16
2	EPS 330 - 100 mm	327±19	93±3	0.218±0.026	195±10
3	EPS 180 - 100 mm	180±9	100±6	0.20±0.03	197±4
4	EPS 125 - 65 mm	125±6	65±3	0.30±0.032	303±11

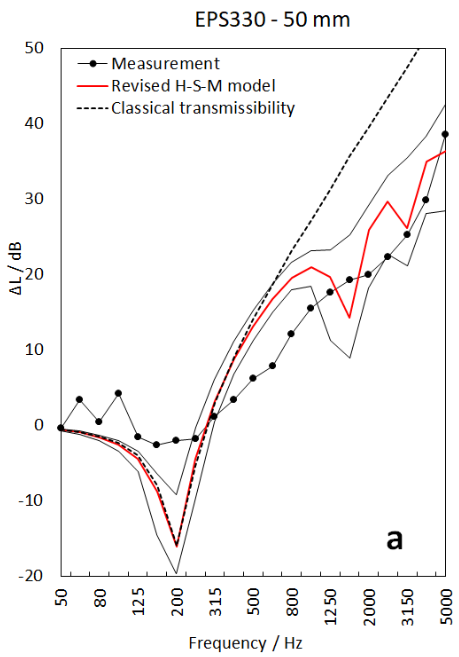
3.2 Application of revised H-S-M model and comparison with experimental data

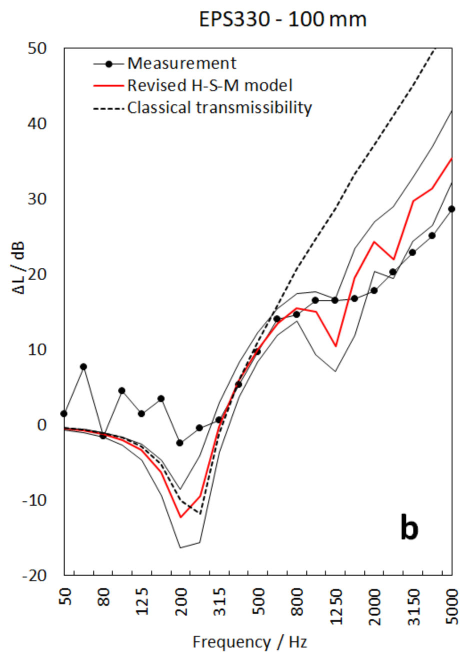
From the experimental data collected in Tab. 1, it is possible to calculate the fundamental mass-spring-resonance frequency $f_0 = \frac{1}{2\pi}\sqrt{\frac{s_t'}{M}}$ and the thickness-resonance frequencies of standing waves $f_i = f_0 i\pi\sqrt{\frac{M}{m}}$ in the EPS layers. Here, M is the mass per unit area of the actual floating screed and m is the mass per unit area of the EPS layers.

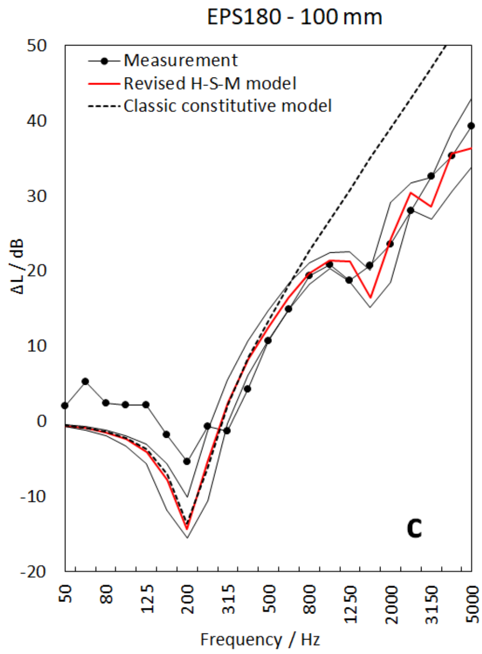
Table 2. Calculated resonance frequencies in Hz of the floating floors

		f_0	f_1	f_2	f_3	f_4
1	EPS 330 - 50 mm	**202**	1545	3091	4637	> 5000
2	EPS 330 - 100 mm	**230**	1266	2532	3798	> 5000
3	EPS 180 - 100 mm	**207**	1654	3308	4962	> 5000
4	EPS 125 - 65 mm	**260**	3054	> 5000		

The measured impact sound insulation improvements ΔL, in one-third octave bands, are compared with predictions on the basis of the revised H-S-M model in Fig. 4. Reference predictions with the classical transmissibility model for acoustically thin layers are also shown. In the graphs the maximum admissible range, the absolute minima and absolute maxima among all values (at a confidence level of 95 %) are also indicated for the H-S-M predictions [2].







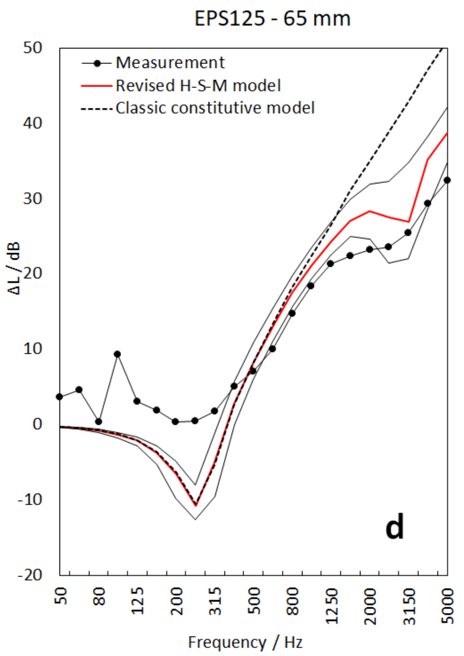


Figure 4. Measured *ΔL* (bullets points), the estimated *ΔL* according to the classical transmissibility model (dotted line) and the estimated *ΔL* according to the revised H-S-M model (red thick line) with the admissible range (black thin lines), for floating floors with a thick thermal resilient layer (a) type 1, (b) type 2, (c) type 3 and (d) type 4

The thickness-wave effects occurring in the EPS layer reduce the impact noise insulation of the floating floor in the medium and high frequencies by as much as 20 dB below what would be expected from the transmissibility model. The revised H-S-M model clearly takes better account of these effects and predicts the frequency position of the resonances (mass-spring resonance and thickness-wave resonances) relatively well, but the estimated average data show more pronounced resonance dips compared to the measured data.

The doubling of the EPS layer (Fig. 4b vs Fig. 4a) does not show a shift of the fundamental mass-spring-resonance frequency by a factor of $\sqrt{2}$ towards the lower frequencies contrary to what is expected from theory. This is due to the fact that EPS sample 2 with thickness 100 mm has a higher dynamic stiffness than sample 1 with thickness 50 mm (Tab.1), meaning that the Young's modulus is not a constant material property. This is due to the inhomogeneity of the mix and the greater compaction of the product for larger thicknesses. However, doubling the thickness of the layer leads to a shift of the thickness-resonance frequencies of standing waves towards the lower frequencies (Tab. 2).

The "EPS180 100 mm" thermal layer does not show a significant difference in the position of the different resonance frequencies compared to the "EPS330 100mm" (Fig. 4c vs Fig. 4b). In fact, the dynamic stiffness of this layer 3 is slightly higher than for layer 2, despite being lighter. This is probably due to the fact that more air is trapped in the layer.

The polystyrene beads in layer 4 are mixed with a different binder than that used in the first three layers. This explains the higher dynamic stiffness for this product and the increase of the fundamental mass-spring-resonance frequency. The higher dynamic stiffness, combined with a lower product thickness, leads to higher thickness-resonance frequencies (Fig. 4d and Tab. 2).

The slope of the 50 mm EPS curve above the mass-spring resonance frequency is not well predicted, particularly in the mid frequency range (Fig. 4a). Discrepancies could possibly be related to different dissipative properties of the actual floating screeds, slightly affecting the amplitude of the input force. In addition, it is possible that the H-S-M model does not adequately handle the effect of the two distinct types of damping: the hysteretic damping which acts more on the resonance peaks and the viscous damping which acts more on the slope. The mechanical response of the system can also be affected by laboratory test conditions such as the limitation of the free lateral expansion of the resilient layer, frictional effects due to lateral couplings and the mobility of the base floor.

4. ADDITIONAL REMARKS

4.1 Influence of the constrained conditions in the lateral direction

The original H-S-M model was intended for isolation mounts for which the diameter is small compared to the wavelength. The one-dimensional derivation is not exact due to the presence of lateral waves. The wave velocity c_d of the longitudinal waves in the resilient layer will be affected by the constrained conditions in the lateral direction of the resilient layer. The influence of the infinite layer can thus be accounted for by using the constrained Young's modulus $E_c = E\frac{(1+\nu)}{(1-\nu)(1-2\nu)}$ in the H-S-M model [2].

4.2 Influence of the base floor mobility

The constitutive models assume a rigid base floor and calculate the improvement in impact sound insulation ΔL from the force transmissibility ratio $\mathcal{T}$. This approach neglects the mobility of the base floor. While the mobility can be disregarded at high frequencies, it has a significant effect below and around the mass-spring resonance frequency of the floating floor systems. The H-S-M prediction can be improved below the resonance frequency of the mass-spring system by accounting for the mobility of the base floor [2].

4.3 Combination of a filling layer with an acoustic underlay

The revised H-S-M formula presented in this paper works for a floating screed laid on a single resilient layer represented here by the filling layer. But the model must be adapted for a multi-layer system (i.e. the combination of a filling layer with an acoustic underlay). Indeed, it is expected that the theoretical model cannot be applied to systems composed of two different underlayers since at high frequencies, standing waves occur in each layer independently. These standing waves are independent of the resonant frequency of the combined mass-spring system. Thus, the relation in Eqn. (5) is not valid anymore for multi-layered systems

4.4 Floating floors with a thermal resilient layer composed of polyurethane

The revised H-S-M model has also been applied to two floating floors with a resilient thermal layer consisting of a closed cell sprayed polyurethane (PU) foam (Fig. 5). The improvement of impact sound insulation was measured at Buildwise on a 140 mm concrete reference floor with surface area 11.5 m². The floating floors had a surface area of 6.75 m², i.e. half the surface area of the base floor. The density of the finishing screed was 1930 kg/m³ and its thickness was around 50 mm. The measured material properties of the PU layers are given in Tab. 3. The PU layers had an irregular surface, leading to a deviation of 10 mm in the thickness measurements. The measured dynamic stiffnesses indicate that the Young's modulus for the PU is relatively uniform and independent of the thickness of the layer.


A


B

Figure 5. (a) Application of the thermal layer composed of sprayed polyurethane foam (PU) and (b) illustration of the apparent dynamic stiffness and the loss factor measurement

Table 3. Material properties for PU thermal layers: experimental values and standard uncertainties

	Thermal/resilient layer			
	ρ /kg·m^{-3}	L /mm	η /-	s'_t /MN·m^{-3}
PU - 50 mm	45.9±0.5	45±6	0.12±0.017	179±12
PU - 100 mm	45.9±0.5	91±5	0.08±0.017	88.6±9.2

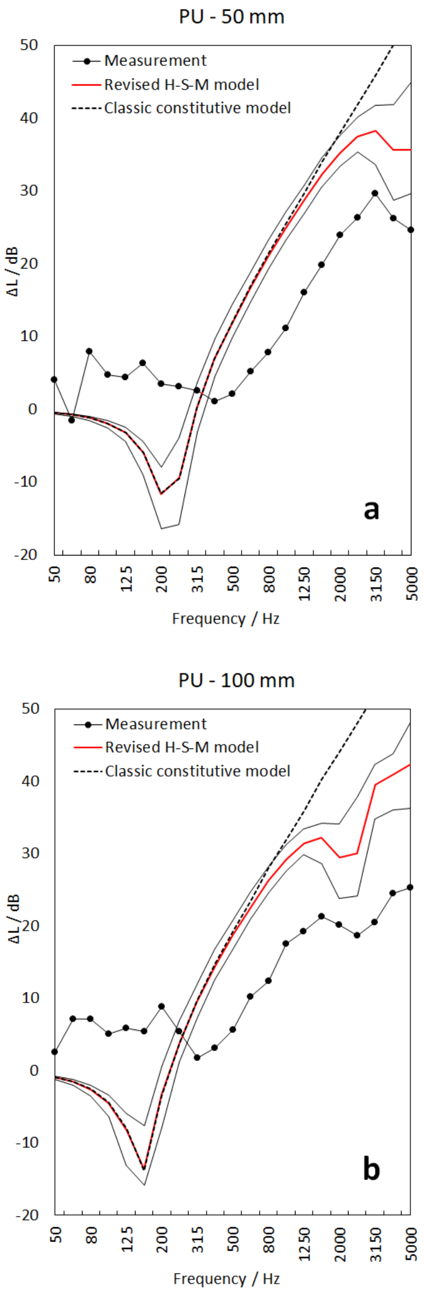


Figure 6. Measured ΔL (bullets points), the estimated ΔL according to the transmissibility model (dotted line) and the estimated ΔL according to the revised H-S-M model with the admissible range, for floating floors with a PU thermal resilient layer of thickness (a) 50 mm and (b) 100 mm.

The H-S-M model clearly overestimates the measured ΔL at medium and high frequencies for the floating floors with PU thermal layer (Fig. 6). The predicted mass-spring resonance frequency is too low in frequency for both PU thicknesses. This means that the apparent dynamic stiffness, s_t', measured according to ISO 9052-1 does not provide a reliable input to the model in this case. The first thickness-wave resonance frequency is however well predicted (around 5000 Hz for PU 50 mm and around 2500 Hz for PU 100 mm). This indicates that the stiffness is correctly estimated at higher frequencies, but underestimated at low and mid frequencies, which would mean that the Young's modulus of the PU foam is frequency dependent.

4.5 Frequency dependence of dynamic properties

The model could be further improved by taking into account the frequency dependence of the dynamic properties. Indeed, as Harrison reminds us in his article, the velocity tends to decrease with increasing frequency because of the radial motion while the damping tends to increase

5. CONCLUSIONS

The mechanical model of Harrison-Sykes-Martin, revised and corrected, is applied to estimate the acoustical performance of floating floors with a single resilient layer represented here by the thermal layer. The presented model provides a more detailed estimation of the acoustical performance of floating floors, with respect to existing analytical models, since the effect of thickness wave resonances on ΔL is incorporated.
In order to be validated, the theoretical model is compared with measurement results for four EPS thermal layers. The input data for the model are derived from experimental measurements of apparent dynamic stiffness, loss factor, and surface mass of the resilient/thermal layer and surface mass of the floating screed. The simulations and experiments generally show a good compatibility: the model is able to identify the thickness-wave resonances fairly well, although the effect of damping is not well handled. The model accuracy is affected by the proper determination of the material parameters. Particular care should be taken when determining the dynamic stiffness according to ISO 9052- 1. Furthermore, the elastic material parameters can vary strongly with time and frequency (e.g.

polyurethane). Frequency dependent material properties cannot be incorporated directly in the analytical H-S-M model.
Nevertheless, the proposed analytical model allows, at the building physics design level, to identify suitable and optimized solutions for acoustical and thermal insulation, by opportunely combining the properties of involved materials. Moreover, it can be applied to identify in advance the acoustical performance of different typologies of resilient materials, in comparative survey tests and in quality product management.

6. ACKNOWLEDGMENTS

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