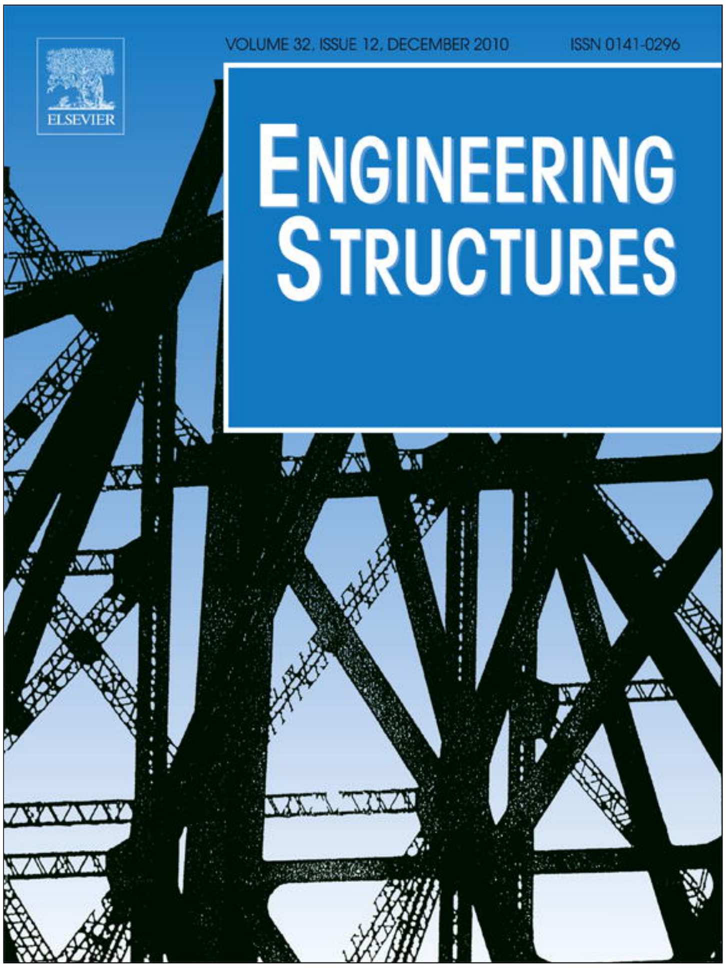
VOLUME 32, ISSUE 12, DECEMBER 2010
ISSN 0141-0296
ELSEVIER
ENGINEERING STRUCTURES

Engineering Structures 32 (2010) 3966–3973

Contents lists available at ScienceDirect

Engineering Structures

journal homepage: www.elsevier.com/locate/engstruct

Time-dependent behaviour of timber lightweight concrete composite floors

L.F. Jorge [a], J. Schänzlin [b,1], S.M.R. Lopes [c,*], H. Cruz [d], U. Kuhlmann [e,2]

[a] Polytechnic Institute of Castelo Branco, Portugal
[b] Konstruktionsgruppe Bauen AG, Kempten, Germany
[c] FCTUC, University of Coimbra, Portugal
[d] National Laboratory of Civil Engineering (LNEC), Portugal
[e] Institute of Structural Design, University of Stuttgart, Germany

ARTICLE INFO

Article history:
Received 7 March 2010
Received in revised form
18 August 2010
Accepted 1 September 2010
Available online 29 September 2010

Keywords:
Composite beams
Creep
Time dependence
Shrinkage
Timber construction
Wooden structures

ABSTRACT

This article presents a study on the time-dependent behaviour of timber lightweight concrete composite floors. The option of using lightweight aggregates in concrete (LWAC), instead of normal weight aggregates, is the focus of this document.

The load-slip behaviour of SFS-screwed joints in timber-LWAC structures is analysed both for the short term and the long term cases. The study takes advantage of an experimental programme carried out at the University of Coimbra where several timber-LWAC specimens were tested for 600 days.

The load-slip behaviour of the connections is very important for the behaviour of the whole structure. The last aspect that is developed in this article concerns the time-dependent behaviour of LWAC composite floors. The calculated results obtained through the computer programme proHBV, developed at the University of Stuttgart, proved to be very close to those obtained from tests carried out at the University of Coimbra. As explained in this article, the direct application of the design rules for creep in concrete and timber, as presented in Eurocodes 2 and 5, results in great deviations from the actual behaviour of structures composed of two materials, timber and concrete. To overcome this difficulty, the authors also present a simplified procedure that can be taken as an extension of the Eurocode design philosophy. This simplified procedure does not need the use of computer programme proHBV, and is much more accurate than the current Eurocode proposals. The deviation of the simple design procedure from the test (even if a bit higher than that of proHBV) is in the order of usually accepted values for practical design, however, the advantage of this approach for the practice is decisive.

1. Introduction

The load-slip behaviour of timber–concrete connections has been studied in the past years but most studies were focused on short term behaviour of timber–concrete composite floors [1–6].

As far as long term behaviour is concerned, the different rheological behaviour of timber and concrete influences the performance of the composite floor: stresses are redistributed and the deflection increases with time due to the different creep and shrinkage behaviour of these materials. Therefore, the design of such structures is often conditioned by the standard rules related with the maximum deflection in service. As the span increases, deflection is more important in the design outcome.

The time-dependent deflection is caused by the creep of the connection (not only the creep of the connection device, but also the creep in the neighbouring area, both in timber and concrete, due to the local stress concentration) and by the creep and shrinkage of the timber and concrete along the whole beam. For the consideration of the time dependent behaviour of all components a global creep coefficient would be the easiest way. For the final deformation the elastic deformation of the composite floor can be multiplied with this global creep coefficient in order to get the increase of the deformation after 50 years. In [7] a first attempt was made in order to determine the global creep coefficient for the range of parameters given in Table 1.

As seen in Fig. 1, the typical global creep coefficients of a timber–concrete composite (TCC) floor evaluated with proHBV lays somewhere between 1.5 and 4.5 [7]. These global creep coefficients consider the deflection caused by creep as well as that caused by shrinkage. In fact, shrinkage should not be neglected, because it may cause a deflection of about 70% of the elastic

* Corresponding author. Tel.: +351 239797253, +351 239797252; fax: +351 239797123.
E-mail addresses: schaenzlin@kb-ke.de (J. Schänzlin), sergio@dec.uc.pt (S.M.R. Lopes), sekretariat@ke.uni-stuttgart.de (U. Kuhlmann).
1 Tel.: +49 831 52156 0; fax: +49 831 52156 60.
2 Tel.: +49 711 685 66245; fax: +49 711 685 66236.

0141-0296/$ – see front matter © 2010 Elsevier Ltd. All rights reserved.
doi:10.1016/j.engstruct.2010.09.007

Table 1
Range of parameter for the determination of the global creep coefficients f.

Parameter	Minimum value	Maximum value
Span in m	4	10
Variation of shrinkage in concrete	0	-60×10^{-6}
RH_{mean} in %	50	800
h_{timber} in cm	12	20
$h_{concrete}$ in cm	4	12
Concrete grade	C20/25 and LC16/18	
Timber grade	C24	
Live load in kN/m^2	1.5	5
h_c/h_t	1/2	
b_c/b_t	1/1	

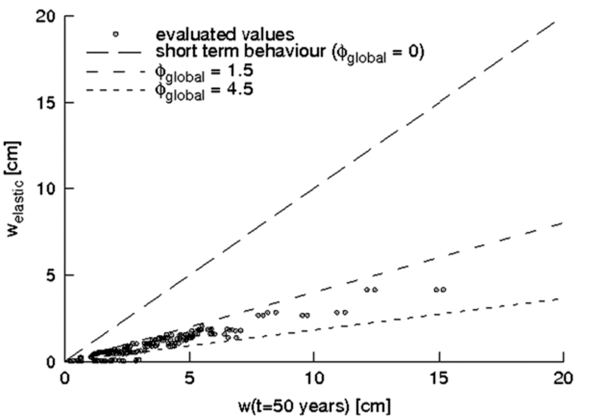

Fig. 1. Range of the effective creep coefficients.

deflection in the whole length. However, the most influencing factor is creep (for both materials and connection), and therefore it should deserve special attention.

Using lightweight aggregated concrete instead of normal weight concrete usually leads to smaller creep deformations, by reducing the creep coefficients and reducing the dead load. However, the stiffness of the concrete is also reduced, and this causes the deflections to increase. Therefore, the advantage of using LWAC is not obvious from this. In order to evaluate if the use of LWAC instead of normal weight concrete is actually advantageous some uncertainties have to be clarified, such us:

- load-slip-behaviour of the connection
- time-dependent behaviour of the global system.

The option of using LWAC in timber concrete composite floors instead of normal weight concrete is evaluated here. The behaviour of the connection is also covered (both for short and long term cases). The time-dependent behaviour of LWAC composite beams is explained and comparative analyses of experimental results with computational programme simulations and with a simplified numerical procedure predictions are also presented here.

2. On the option of LWAC in timber concrete composite floors

The advantages of the composite floors of normal concrete and board stacks in comparison to pure concrete are well known. Some of them are:

- reduced dead load
- increase of the amount of renewable materials
- faster construction due to less *in-situ* concrete casting
- reduction of the required number of props due to the bending stiffness and bending capacity of the board stacks.

In comparison to pure timber floors,

- the load capacity and the stiffness of the floor can be increased and
- the sound and fire insulation can be improved.

So the question is whether the replacement of the normal concrete by LWAC is reasonable? This question cannot be answered directly because two different effects concerning the often critical deflection are obtained by using LWAC instead of normal concrete. On one hand, the deflection is expected to increase if LWAC is used, due to

- the larger shrinkage strains of LWAC
- its lower Young Modulus.

On the other hand, the deflection should be reduced by the use of LWAC because of

- the nominal reduced creep coefficient of LWAC concrete and of
- the reduced dead load.

In order to determine the overall balance of these opposite influences on the deflection, a case study involving composite structures of board stacks and concrete for different spans is performed and the required depth of these spans is determined according the design method proposed by [8]. For this case study a single span girder with grooves as connection devices is chosen [9] (Fig. 2). It has to be mentioned that "only" the smeared stiffness and the smeared ultimate load is considered in the design. Therefore the results should be transferable to other connection devices, as long as the smeared properties are comparable. The live load is assumed to be 1.5 kN/m^2. The concrete as well as the timber depths are determined by iteration, so the following boundaries are fulfilled:

- The complete cross section is compressed.
- The design stresses in the composite floor are lower than the design resistance of the materials.
- The maximum deflection in the long term including creep and shrinkage is lower than L/200 and the increase of the deformation after un-propping due to live load, creep and shrinkage is lower than L/300. The first limit ensures functionality within the whole life time, whereas the second one avoids cracks in the partition walls, which are normally built after unpropping (see DIN 1052:2008 [10]).
- Creep behaviour of the normal and the lightweight concrete, respectively follow the equations given in Eurocode 2.
- The time dependent behaviour of timber can be described by Hanhijärvi's rheological model of timber [11].

As seen in Fig. 3 the required depth of the cross section members using LWAC are about 6% higher than the required depth using normal concrete.

Therefore, only from the point of view of a maximum slenderness of the cross section, LWAC penalises the timber–concrete-composite floors.

Concerning the total dead load of the structure, the use of timber-LWAC floors leads to a reduction of 25% of the dead load. Therefore, the application field of timber-LWAC floors will be mainly in the renovation and upgrading of existing timber floors, where the loading of the load bearing members of the structure limits the load capacity of the whole structure.

This type of system has been chosen in order to reduce the numbers of possible combinations of the input values. However it is expected that the tendency of the comparison between LWAC and NC is comparable.

3. Load-slip behaviour of SFS-screws in timber-LWAC structures

3.1. General

The use of screws as connection devices for timber–concrete composite structures is a common option because of their good

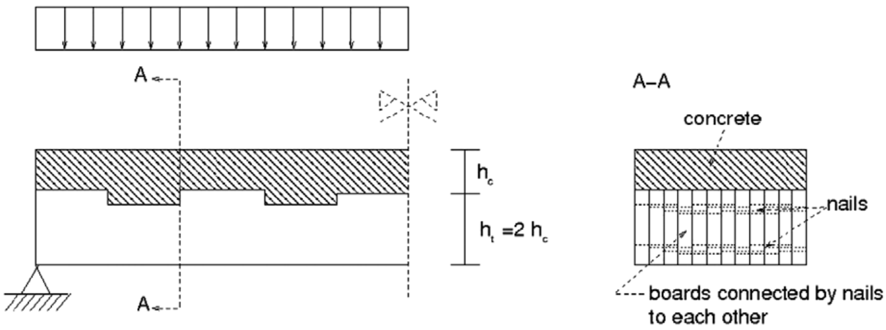

Fig. 2. Floor and cross section of the case study.

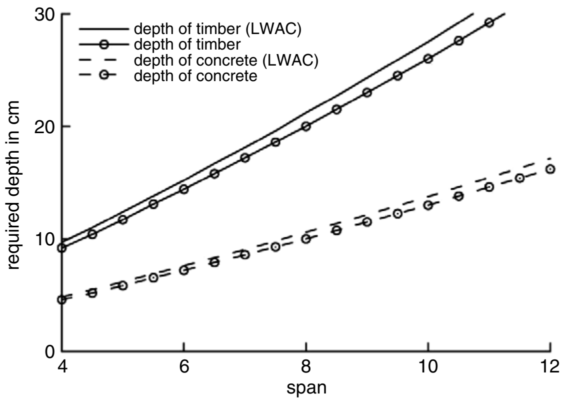

Fig. 3. Required depth of the composite members.

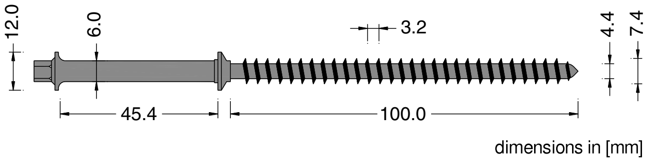

Fig. 4. SFS-screw VB 48 × 7.5 × 100.

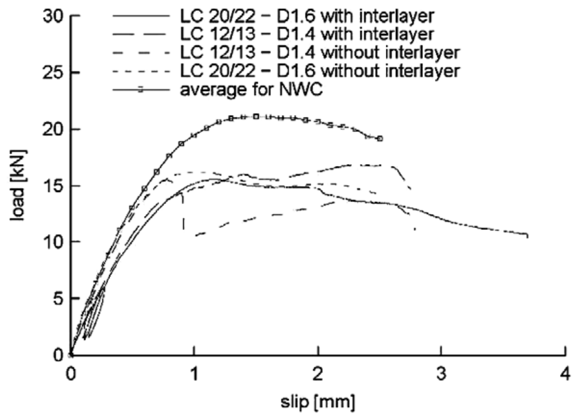

Fig. 5. Load-slip behaviour of SFS-screws in timber–concrete composite LWAC.

mechanical performance associated with the low cost of the material and of the application [3,4].

The choice of SFS-screws in this investigation enables also comparisons between LWAC and NWC composite floors [3,5]. Previous studies with normal concrete did not consider failure modes for the composite connection on the concrete side and focused either on tension strength of the screw or on its withdrawal from the timber. However, when LWAC is used, failure should be expected in the concrete due to the weakness of its aggregates.

Fig. 4 shows the screw that was used in this investigation. It is a special fastener appropriate for timber–concrete composite structures. The size of the outer thread diameter is 7.4 mm and the inside thread diameter is 4.4 mm. This geometry results in a high withdrawal strength, similar to that of the high graded steel tension strength. The penetration length in timber corresponds to the full thread of the screw (100 mm long) but it could be reduced if an interlayer (floor boards, for instance) is used.

3.2. Short term behaviour of joints

The short term performance of this screw connection, where the screws are diagonally crossed driven into the timber, was reported by several authors [12–15]. The use of lightweight concrete was compared with normal weight concrete there.

When compared to NWC, LWAC leads to a strength reduction of the shear joint between 30% and 50%, for joints without the interlayer. In the presence of the interlayer, the differences are much smaller.

Conversely, the use of LWAC instead of NWC has little influence on the joint stiffness for joints without interlayer, but it increases the slip modulus by 15%–35% in the presence of the interlayer. This is particularly promising for repair works and for the strengthening of floors since the interlayer simulates the floor boards that normally exist in most old buildings [12].

From the analysis of the shear tests results using several types of LWAC (Fig. 5) it seems that the load capacity is not related with the LWAC grade or affected by the presence of the 25 mm thick interlayer. Though, it is possible to detect some decrease of the slip modulus for connections with the interlayer.

Fig. 5 shows the load-slip-behaviour in selected specimens of the tested configurations using LWAC and the average load-slip behaviour using NWC within the C20/25 strength class [5,12]. As seen in this figure the maximum load of SFS screw joints in timber-LWAC floors is reduced by about 30% compared to composite floors of NWC, whereas the stiffness is maintained approximately constant for all cases. Considering that the deformation is the critical check in many design examples, the stiffness of the connection can be more relevant than the load capacity. Therefore, the differences between NWC and LWAC concerning the short term behaviour and concerning the required cross section dimensions of the floors seem to be negligible.

3.3. Long term behaviour of joints

The time-dependent behaviour of the connection is a necessary input data in order to evaluate the creep coefficient variation in time together with materials coefficients. Predictive models for

Fig. 6. Long-term tests in connection specimens.

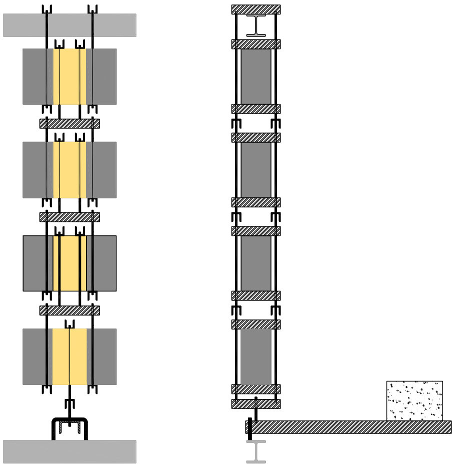

Fig. 7. Long-term shear test setup.

creep coefficients in timber–concrete composite connections are not known [6,16]. Therefore tests are necessary to validate and improve the models used.

Three series of four specimens each are reported here and were carried out as a part of a wider investigation programme [17] aimed to study the development of the creep coefficient in time. These tests were conducted under constant climate (20 °C/65% RH) avoiding moisture variation in materials. These climatic conditions can be considered as Service Class 1, according to Eurocode 5 [16].

The test setup applies the load by means of a handspike (Figs. 6 and 7) and the static load scheme is the same as in the short term tests. In each specimen, slip measurements are taken at 4 points, in order to account for any twist. The load applied to each specimen is not the same, as the ones on the top have to carry the others down. However, this can be neglected because the differences between specimens are within 5% of the total load. This total load applied by the handspike represents 30% of the mean failure load obtained from the short term tests.

Series H, Q and I were fabricated without the interlayer between the timber and concrete. The timber used is glued laminated timber made from Strength Class C18 timber boards [18]. The only difference is in concrete grade, as shown in Table 2. The lightweight aggregate used in this investigation was LECA® with a bulk density between 500 and 550 kg/m^3.

Figs. 8–10 show the measured creep development in the three test series (4 specimens each). The creep coefficient, $\varphi_{(t=t_i)}$, was computed according to Eq. (1) where $w_{(t=t_0)}$ and $w_{(t=t_i)}$ signify the initial and final slip, respectively.

$$\varphi_{(t=t_i)} = \frac{w_{(t=t_i)} - w_{(t=t_0)}}{w_{(t=t_0)}}. \quad (1)$$

The initial slip or elastic slip was assumed to be the slip over 10 min of loading.

The evaluation of the creep coefficients further than the measured data, was done by extrapolating a logarithmic function,

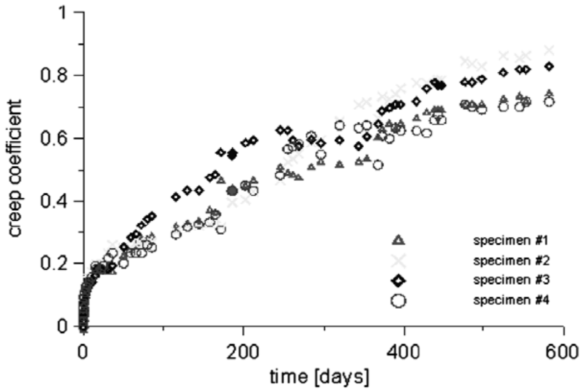

Fig. 8. Creep coefficient measured for Series Q.

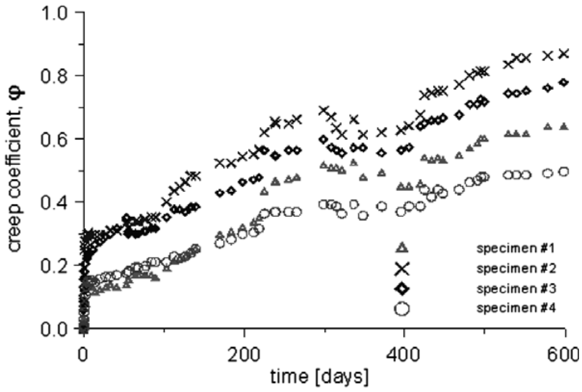

Fig. 9. Creep coefficient measured for Series I.

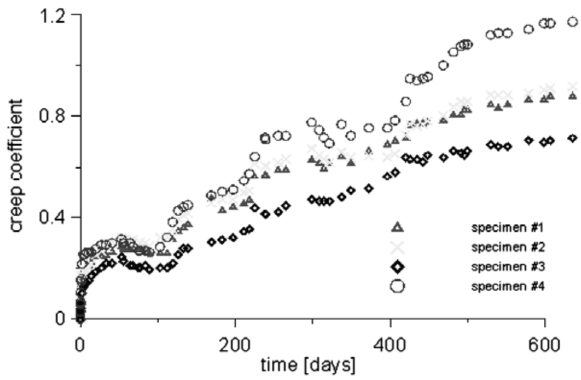

Fig. 10. Creep coefficient measured for Series H.

$\varphi_{(t=t_i)} = a.\ln(t) + b$, fitted to the average measurements of the 4 specimens in each series. Since in the early hours and days the frequency of the measurements was much higher than after the first weeks, this unbalance would influence the fitting calculation. This was overcome by going through a linearization of the measured data and determining a new curve with a fixed time step.

Results expressed on Table 3 indicate that more than 50% of the expected creep had already occurred on the connections after 600 days—which could be interesting when analysing also the development of time-dependent effects on the other structural components (Fig. 11). The different gradient on the time-dependent effects turns to be decisive on design, since it leads to stress redistribution on the composite structure [7].

The measured and estimated values in the three Series H, Q and I, suggest that there is no influence of the concrete

Table 2
Description of long term shear test series.

Series	Compression strength, f_{cm} (MPa)	Strength class	Oven-dry density, ρ (kg/m^3)	Density class (EN 1992)
H	30.7	LC20/22	1538	D1.6
Q	21.0	LC12/13	1376	D1.4
I	24.6	LC16/18	1461	D1.6

Table 3
Creep coefficient recorded from tests and estimated.

Series	φ_{test}		$\varphi_{estimated}$	
	365 days	600 days	5 years	50 years
H	0.640	0.908	1.005	1.465
Q	0.649	0.791	1.012	1.539
I	0.510	0.695	0.734	1.082

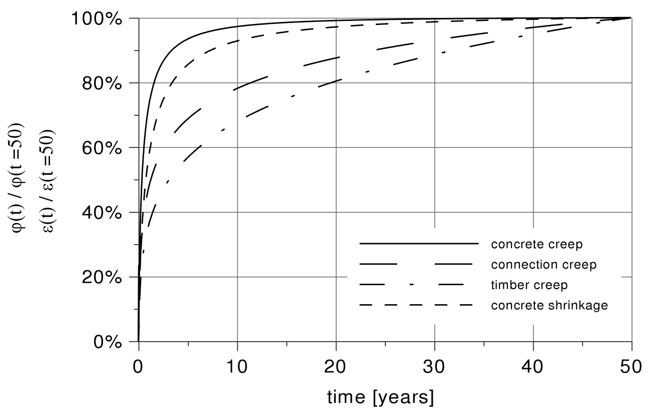

Fig. 11. Time-dependent effects development on the composite structure.

type (compression strength and oven-dry density) on the creep coefficients.

From the creep models of LWAC it is clear that these two concrete properties have an opposite influence on creep development [19]. By increasing compressive strength, creep coefficient values decrease; but, conversely, increasing oven-dry density of LWAC increases creep deformations. For these reasons, it is possible that the influence of both parameters counterbalance each other.

Useful comparisons can be done with other research reports [3,20], to better understand the influence of adopting LWAC instead of NWC for the same connection configuration.

Kenel and Meierhofer [20] have also performed experiments directly on connections under constant climate, obtaining results similar to those obtained in the scope of Jorge's thesis [17]. Using a 20 mm thick interlayer, a creep coefficient of 0.778 and 1.442, respectively for 5 and 50 years can be accepted. These values are similar to results of Table 3, despite some differences on connection configuration, as for example concrete quality and interlayer existence.

However, very different values for this kind of connections were derived by van der Linden [3], who proposed creep coefficients between 5.5 and 6.8 for 1200 and 18250 days respectively, using SFS screws with a 28 mm interlayer and NWC. These values of the creep coefficient in connections were derived from calculations and simulations in long term bending tests, adopting the creep behaviour in timber and concrete as presented in codes and in the literature. Therefore, the accuracy of the simulations cannot be considered very good when compared to experimental results.

These differences may partially be explained by the exterior climatic conditions that the beams were exposed to. In his Ph.D. thesis, van der Kuilen [21] indicates for timber-timber connections, a differential of 2 in the creep coefficient after 50 years when comparing controlled to non-controlled climatic conditions.

Other studies using different connection types can be found in literature. Dias [6] presented some results obtained in timber–concrete connection built with 10 mm dowel, using LWAC. The tests were performed in the same conditioned room in Coimbra University as those reported in Table 2. For 50 years, he estimated a creep coefficient value of 1.23, which is within the range of the values of Table 3.

Results from a different connection reported by Fragiacomo et al. [13] presented creep coefficient values varying between 0.35 and 0.60 after 75 days, which seems to lead to much higher values then those obtained here. These experiments were made with controlled environment (25 °C/70% RH) and using a TECNARIA connector.

4. Time-dependent behaviour of LWAC composite floors

4.1. Purpose of the analysis

For the description of the time-dependent behaviour of timber concrete composite floors of normal concrete, several models have already been developed [22–26]. Based on these models, design methods for composite floors with normal concrete were proposed [7,22,23]. Therefore it is important to know, whether they can also be used for lightweight aggregated concrete.

4.2. Comparison of numerical model with experimental results

For a first validation of the model applied to lightweight aggregated concrete, the tests reported in Jorge's thesis [17] are compared to the results obtained by the numerical model, developed in the scope of Schänzlin's thesis [7,27]. This model is based on the composite theory described by Dabaon [28], complemented with the rheological model of timber by Hanhijarvi [11], the rheological model of concrete according to Eurocode 2 [19] and of the connection according to Kenel and Meierhofer [20]. The deflections and stresses can therefore be determined from consideration of creep and shrinkage of all components. In order to apply this model to composite floors of timber and LWAC, the creep coefficients have to be multiplied by (see Eurocode 2 [19]):

$$\eta_\varphi = \left(\frac{\rho}{2200}\right)^2. \qquad (2)$$

The shrinkage of the LWAC is taken as the value of shrinkage of normal concrete increased by 20%.

The experimental studies were performed on four timber-LWAC-composite beams. The dimensions and the test setup are given in Fig. 12. The connection used was made with pairs of SFS screws driven at ±45° and placed at a constant spacing of 20 cm. LWAC was within a strength class of LC20/22 and a density class of D1.6. Glulam beams of a strength class of GL24h were used. In two of the composite beams, a 20 mm tick interlayer was used.

A two point load of 6.8 kN was applied. This load is equal to 30% of the ultimate load determined by a short term test. In order to avoid the influence of the humidity variation on the creep behaviour, the surrounding conditions are maintained constant at a temperature of 20 °C and at a relative humidity of 65% (variances were within current standards allowances).

The props had to be left in place for a longer time (approx 100 days) than usual in the construction site due to laboratory

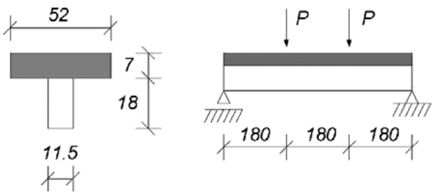

Fig. 12. Test setup of the long-term tests (dimensions in cm).

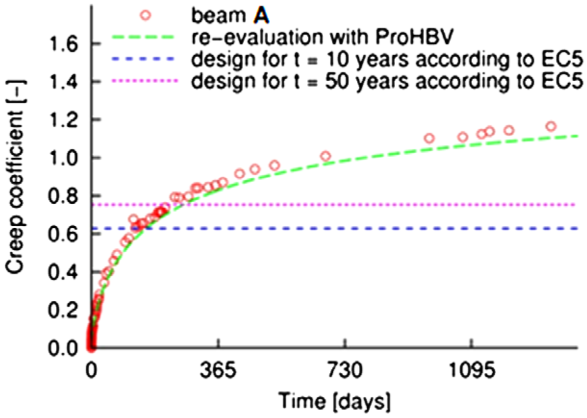

Fig. 13. Test results compared with numerical evaluations (Beam A).

constraints. Defining the conditions, the correspondent input values were used in the numerical simulations that were carried out by the computer programme proHBV, developed by Schänzlin [7,27]. As shown in Fig. 3, the computed theoretical predictions fit quite well in the values obtained from the experimental programme.

However, by comparing the results obtained from tests with the values computed by means of the design procedure given in Eurocode 5, it is obvious that the differences cannot be neglected. As shown in Fig. 13, the deflection estimated by EC5 for 10 years is reached after 150 days only; the deflection, that would be expected for the period of 50 years according to Eurocode 5, is actually reached in 230 days.

The use of LWAC cannot be considered as the reason for these differences. In fact, differences of the same magnitude take place for composite floors of timber and normal weight concrete as reported by Schänzlin [7].

These differences are due to the following points that are neglected in the structural design according to Eurocode 5:

- shrinkage of both materials
- influence of the composite action on the effective creep coefficients (see [21]), which take into account the influence of the stress redistributions within the composite system, e.g. the concrete reduces its stresses due to the larger creep coefficients of concrete compared to timber. Therefore the creep coefficient is increased since the creep coefficient is defined as creep strain divided by the elastic strain
- different temporal development of the creep and shrinkage strain Fig. 13.

These aspects have to be considered in the structural design, in order to obtain realistic deflections of composite beams and floors made of timber and LWAC.

4.3. Comparison between simplified design procedure and experimental results

Due to the fact that the numerical simulation needs a great quantity of input data and that the simulation takes too long for current practical design, the procedure proposed by EC5 could be used after some corrections. In fact, Schänzlin [7] has proposed a modified EC5 procedure in order to take some aspects into account, namely, the influence of shrinkage, the different temporal development of creep and shrinkage and the influence of the composite action on the effective creep coefficients.

Table 4
Coefficients for the effective creep coefficients and the effective shrinkage.

Point in time (years)	ψ_{timber}	ψ_{concrete}	$k_{\text{shrinkage}}$
0	0	0	0
3–7	0.5	1.9	0.5
50	1.0	2.0	0.8

In a composite structure different shrinkage of the composite layers leads to internal forces and consequent deflections. In timber–concrete-composite structures, the concrete is normally under compression stresses. Shrinkage of concrete leads to a decrease of the resulting stresses and, consequently, to a reduction of the effectiveness of concrete. Therefore, the timber part of the cross section takes higher stresses if concrete suffers high shrinkage.

Creep is another phenomenon that changes the relative stresses in both parts (timber and concrete). In this case, the part with the smallest creep coefficient tends to take higher stresses, because the other part (with the highest creep coefficient) reduces its stresses (relaxation effect). However, for design purposes, these effects are accounted for in the creep coefficients (defined as a ratio of the creep strain to elastic strain). In a composite structure, the normal creep coefficient of the materials cannot be used directly, because the material creep coefficients assume a constant elastic strain. The creep coefficient in the part with the highest creep coefficient increases due to the increasing creep strains and decreasing elastic strains.

The determination of effective creep coefficients in composite structures with different end creep values but affine temporal development was introduced by Ruesch and Jungwirth [29] and Kupfer and Kirmair [30]. Kreuzinger [31] extended this determination of effective creep coefficients for the deformability of the connection. A proposal that considers the different temporal development of the strains due to creep and shrinkage is presented by Schänzlin [7].

Due to this development of the creep strains (Fig. 11) not only do the so far considered points in time $t = 0$ and $t = \infty$ have to be considered, but also conditions in between those points in time can become critical. Especially, the stresses in the timber increase within the first 3–7 years because, during this period, concrete is creeping faster than timber. Therefore, the stresses in concrete are decreasing, which leads to an increase of the loading of the timber part of the cross section. After this period, creep variation in concrete is very small, whereas timber is at a stage where the creep has reached only about 40% of the final creep. Therefore, the stresses in the timber part of the cross section may became smaller after $t = 3$–7 years.

The empirical equations that describe the effective creep coefficients in composite structures, considering the different temporal development are quite complex, therefore not very useful for practical design. As consequence, the 95% fractile values of the ratio of the effective creep coefficients to creep coefficients of the materials were determined within the scope of the thesis by Schänzlin [7], and are presented in Table 4. This table can be used in practical design.

Within this extended design method, the points that correspond to $t = 0$, $t = 3$–7 years and $t = \infty$ have to be considered as effective creep coefficients, considering the different temporal developments of the creep strains and the strains due to shrinkage [7,27]. These effective creep coefficients can be determined by

$$\varphi_{\text{composite}} = \psi \cdot \varphi_{\text{material}} \quad (3)$$

where

$\varphi_{\text{composite}}$	Effective creep coefficient in the composite structure considering the composite action and the different temporal development.
ψ	Coefficient according to Table 4.
$\varphi_{\text{composite}}$	Material creep coefficient according the standard

and the effective shrinkage value can be determined by

$$\Delta\varepsilon = k_{s,\text{res}}\,(\varepsilon_{\text{timber}} - \varepsilon_{\text{concrete}}) \quad (4)$$

where

$\Delta\varepsilon$	Effective difference between the inelastic strains of timber and concrete.
$\varepsilon_{\text{timber}}-\varepsilon_{\text{concrete}}$	Inelastic strain (shrinkage < 0).
$k_{s,\text{res}}$	Coefficient according to Table 4.

Due to the fact that inelastic strains resulting from different shrinkage or different thermal expansion cannot be considered directly in the design method according to Eurocode 5, an extension was developed, as follows. The shrinkage can be considered through an effective bending stiffness and an equivalent external uniformly distributed load. The background of the determination of the fictitious load is that the deformation and the bending moment at midspan due to inelastic strains and due to the fictitious external load are equal. So the fictitious external load can be determined from

$$p_{\text{sID},d} = C_{p,\text{sID}} \cdot \Delta\varepsilon_{\text{sID},d} \quad (5)$$

where

$$C_{p,\text{sID}} = \frac{\pi^2 \cdot E_2 \cdot A_2 \cdot E_1 \cdot A_1 \cdot (h_1 + h_2) \cdot \gamma_1 \cdot k_{s,\text{res}}}{2 \cdot l^2 \cdot (E_1 \cdot A_1 + E_2 \cdot A_2)} \quad (6)$$

$\Delta\varepsilon_{\text{sID},d}$ difference of the inelastic strain between timber and concrete $= \varepsilon_{s,\text{timber}} - \varepsilon_{s,\text{ concrete}}$

E	Young's modulus for concrete and timber
A	area of concrete and timber at a section
h	height of the concrete and timber sections
l	span
γ_1	parameter for taking into account the flexibility of the connections (computed according to EC5)
$k_{s,\text{res}}$	effective shrinkage, taking into account the creep behaviour of a developing constraint.

The reduced stiffness, considering the effects of the opposite slip caused by shrinkage of concrete, can be determined by

$$(E \cdot J)_{\text{eff,sID}} = C_{J,\text{sID}} \cdot (E \cdot J)_{\text{eff}} \quad (7)$$

where

$$C_{J,\text{sID}} = \frac{C_{p,\text{sID}} \cdot \Delta\varepsilon_{\text{sID},d} + q_d}{\frac{E_1 \cdot A_1 + E_2 \cdot A_2}{E_1 \cdot \gamma_1 \cdot A_1 + E_2 \cdot A_2} \cdot C_{p,\text{sID}} \cdot \Delta\varepsilon_{\text{sID},d} + q_d} \quad (8)$$

q_d	design load
$\Delta\epsilon_{\text{sID},d}$	difference of the inelastic strain between timber and concrete $= \varepsilon_{s,\text{timber}} - \varepsilon_{s,\text{concrete}}$
E	Young's modulus for concrete and timber
A	area of concrete and timber at a section
γ_1	parameter for taking into account the flexibility of the connections (computed according to EC5)
$C_{p,\text{sID}}$	according to Eq. (6) and
EJ_{eff}	evaluated according to Eurocode 5.

With this extended design method the tests were re-evaluated (Fig. 14).

As visible in Fig. 14, the comparison between the proposed design procedure led to curves that are not too far from each other. Therefore, the proposed procedure seems to be adequate for predicting the actual behaviour in practical design situations. Fig. 14 also shows that shrinkage strongly influences the long-term deflection and EC5 predictions are too distant from reality, so an extension of EC 5 for the consideration of shrinkage and the effective creep coefficients as proposed in [8] seems sensible.

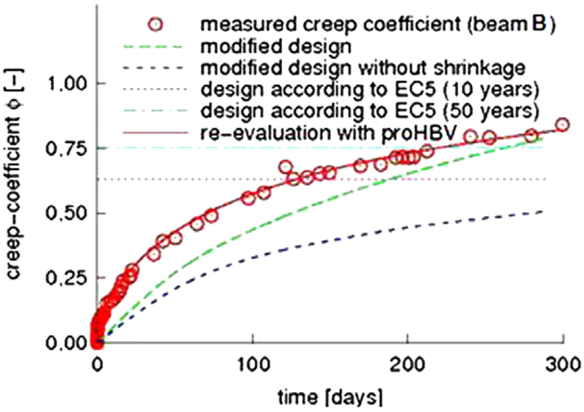

Fig. 14. Test results compared with design method according to [1,25] (Beam B).

Using this design method a global creep coefficient, defined by

$$w_{\text{total}} = \psi_{\text{global}} \cdot w_{\text{elastic}} \quad (9)$$

of 1.8 including shrinkage and 1.1 excluding shrinkage can be obtained. Therefore, the global creep coefficient of timber concrete composite floors will be reduced by using LWAC, since creep coefficients between 1.5 and 4.5 will be obtained by using NWC in the composite floor (Fig. 1).

5. Conclusions

As seen before, the use of lightweight aggregated concrete (LWAC) instead of normal weight concrete (NWC) in timber concrete composite structures strongly influences the load-deformation behaviour of the connection done with SFS-screws, as well as the global performance of the composite floor.

In the connections the most influenced property is the strength capacity, as reductions of almost 50% might be expected which are related to local failure in concrete before achieving the steel tension strength of the screw or its withdrawal capacity. Also the global elastic deformations of floors are increased due to the lower LWAC Young Modulus, when compared with values calculated for NWC floors.

In contrast to these possible drawbacks, time-dependent behaviour was proved to be enhanced and a better performance should be expected for LWAC.

From experiments there is evidence that even for the connection, the use of LWAC does not seem to influence the creep coefficient of connections as compared to available results in NWC. On the contrary, other authors (among others van der Linden [3]) report that the creep coefficient of timber concrete connections is quite similar to the other components of the composite system and lays between the values accepted for timber and for concrete.

The creep strains can be reduced by the use of LWAC, taking advantage of the lower creep coefficient of this material. Consequently lower overall composite creep coefficients are determined by calculations and experimental results.

However due to larger shrinkage strains in lightweight aggregated concrete compared to normal concrete, shrinkage becomes more important for the structural design, so it should not be neglected.

Due to the lack of experimental data for long time periods, the performed creep tests were compared to a numerical model (*proHVB*) and a proposed design method. The difference between tests and numerical model (proposed design method) are acceptable. For this reason, the design method should be used for further studies and analysis. Also comparisons of timber concrete composite floors using LWAC with composite floors using normal concrete are possible.

Using LWAC instead of NWC in timber concrete composite structures has definitely proved to be an advantageous and a reliable solution from the point of view of the time-dependent behaviour. Therefore, it overcomes reliably other possible structural weaknesses in design and takes the obvious advantage of reducing significantly the dead loads of the floor imposed to its supports.

References

[1] Dias A, Van de Kuilen J, Lopes S, Cruz H. A non-linear 3D FEM model to simulate timber–concrete joints. Adv Eng Softw 2007;38:522–30.
[2] Gutkowski R, Brown K, Shigidi A, Natterer J. Laboratory tests of composite woo-concrete beams. Constr Build Mater 2008;22:1059–66.
[3] van der Linden M. Timber–concrete composite floor systems. Doctoral Thesis. Technical University of Delft. Netherland; 1999.
[4] Timmermann K, Meierhofer U, Holz/Beton–Verbundkonstruktionen. Untersuchungen und Entwicklungen zum mechanischen Verbund von Holz und Beton. Forschungsbericht 115/30, Eidgenossische Material — und Prüfanstalt. Swiss. 1993 [in German].
[5] Blass H, Ehlbeck J, van der Linden M, Schlager M. Trag- und Verformungsverhalten von Holz–Beton–Verbundkonstruktionen (load and deflection behaviour of timber concrete composite structures). Versuchsanstalt für Stahl, Holz und Steine Abteilung Ingenieurholzbau Universität Fridericiana Karlsruhe 1995 [in German].
[6] Dias A. Mechanical behaviour of timber–concrete joints. Doctoral Thesis. Technical University of Delft, Netherland; 2005.
[7] Schänzlin J. Zum Langzeitverhalten von Brettstapel–Beton–Verbunddecken (About the long term behaviour of composite floors of board stacks and concrete). Doctoral thesis, Institute of Structural Design, University of Stuttgart 2003 [in German].
[8] Schänzlin J, Fragiacomo M. Extension of EC5 Annex B formulas for the design of timber–concrete composite structures. Meeting forty of the Working Commission W18-Timber Structures. CIB. Bled (Slovenia): International Council for Research and Innovation, August 28–31, paper No. CIB-W18/40–10–1, 2007. p. 10.
[9] Kuhlmann U, Aicher S, Michelfelder B. Trag- und Verformungsverhalten von Kerven mit Schlüsselschrauben als Schubverbindung bei Holz–Beton–Verbunddecken (Load and deformation behaviour of grooves with screws as shear connection in timber–concrete-composite floors). Final report of the project nr.AIF-13204 n/1. Institute of Structural Design. University of Stuttgart. 2004 [in German].
[10] DIN 1052:2008: Entwurf, Berechnung und Bemessung von Holzbauwerken–Allgemeine Bemessungsregeln und Bemessungsregeln für den Hochbau (Design of timber structures—General rules and rules for buildings) 2008
[11] Hanhijärvi A. Modelling of creep deformation mechanisms in wood. Helsinki university of technology. Technical Research Centre of Finland. Espoo (SF): VTT Publications ; 1995 [Dissertation].
[12] Jorge L, Cruz H, Lopes S. Shear tests in timber-LWAC with screw-type connections. 36th CIB-W18 Meeting. Colorado. USA; 2003.
[13] Fragiacomo M, Amadio C, Macorini L. Short- and long-term performance of the Tecnaria stud connector for timber–concrete composite beams. Mater Struct 2007;40(10).
[14] Grantham R, Enjily V, Fragiacomo M, Nogarol C, Zidaric I, Amadio C. Potential upgrade of timber frame buildings in the UK using timber–concrete composites. 8th World Conference on Timber Engineering. Lathi; 2004.
[15] Steinberg E, Selle R, Faust T. Connectors for timber–lightweight concrete composite structures. J Struct Engrg; 129 (11): 1538–45.
[16] CEN. EN 1995. Eurocode 5: Design of timber structures. Part 1.1: General–Common rules and rules for buildings. European Commission, 1995
[17] Jorge L. Estruturas mistas madeira-betão com a utilizaço de betão de agregados leves (Timber-LWAC composite structures behaviour). Ph.D. thesis. University of Coimbra. Portugal: 2005 [in Portuguese].
[18] CEN. EN 338. Structural timber. Strength Classes. European Commission, 2003
[19] CEN. EN 1992. Eurocode 2: Design of concrete structures. Part 1.1: General–Common rules and rules for buildings. European Commission, 2004
[20] Kenel A, Meierhofer U. Holz/Beton-Verbund unter langfristiger Beanspruchung (Timber concrete composite under long term loading). Report 115/39. Timber Department. EMPA Dübendorf (CH). 1998 [in German].
[21] van der Kuilen J. Duraction of load effects in timber joints. Doctoral Thesis. Delft University of Tecnology. 1999.
[22] Fragiacomo M. Comportamento a lungo termine di travi composte legno-calcestruzzo. Doctoral Thesis. University of Trieste. Italy. 2000.
[23] Fragiacomo M, Ceccotti A. A simplified approach for long-term evaluation of timber–concrete composite beams. 8th World Conference on Timber Engineering WCTE 2004. vol. 2. Lahti (Finland): 2004. June 14–17 p. 537–42.
[24] Fragiacomo M, Ceccotti A. Long-term behavior of timber–concrete composite beams. I: finite element modeling and validation. J Struct Engrg 2006;132(1): 13–22.
[25] Fragiacomo M. Long-term behavior of timber–concrete composite beams. II: numerical analysis and simplified evaluation. J Struct Engrg 2006;132(1): 23–33.
[26] Chassagne P, Bou Said E, Jullien J, Galimard P. Three dimensional creep model for wood under variable humidity-numerical analyses at different material scales. Mech Time-Dependent Mater 2005;9(4):1–21.
[27] Schänzlin J, Kuhlmann U. Time dependent behaviour of timber–concrete-composite structures. In: Proceedings of the 8th World Conference on Timber Engineering. 2004. p. 313–8.
[28] Dabaon M, Tschemmernegg F, Hassen K, Lateef TA. Zur Tragfähigkeit von Verbundträgern bei teilweiser Verdübelung (About the load capacity of composite beams with partial shear connection). Stahlbau 1993;62:3–9 [in German].
[29] Ruesch H, Jungwirth D, Stahlbeton. Spannbeton–Berücksichtigung der Einflüsse von Kriechen und Schwinden auf das Verhalten der Tragwerke (Consideration of the influences of creep and shrinkage on the behaviour of structures). Bd. 2. Werner-Verlag; 1976 [in German].
[30] Kupfer H, Kirmair H. Verformungsmoduln zur Berechnung statisch unbestimmter Systeme aus zwei Komponenten mit unterschiedlichen Kriechzahlen (Stiffnesses for the evaluation of statically undetermined systems made of two components with different creep coefficients). Bauingenieur 1987;62:371–7 [in German].
[31] Kreuzinger H. Verbundkonstruktionen Holz/Beton (Composite structures timber–concrete). 1994 [in German].