Can moisture be considered as an action for the design of timber and composite structures?

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1 Introduction

The Eurocode 5 (CEN 2004) currently considers the effect of moisture on the behaviour of timber structure at Ultimate and Serviceability Limit State through two coefficients:

- the k_{mod} factor, which reduces (or increases) the strength of the timber member or connection;
- the k_{def} factor, which increases the elastic deflection.

The k_{mod} factor considers three different phenomena:

- the decrease of strength with increase in load duration (the "creep rupture" phenomenon);
- the decrease of strength for timber characterised by higher moisture content;
- the decrease of strength for timber exposed to more severe environment (the "mechano-sorption rupture" phenomenon).

The k_{def} factor considers three different phenomena:

- the increase in deflection with increase in load duration (the "creep" phenomenon);
- the increase in deflection for timber characterized by higher initial moisture content;
- the increase in deflection for timber exposed to more severe environment (the "mechano-sorption" phenomenon).

The influence of the load duration on strength and deflection (first phenomenon) is accounted for through the load duration class, which depends on the nature of the load. The second and third phenomena are accounted for by dividing all possible types of environment which the structure may be exposed to in three service classes (1, 2 and 3). The service class, therefore, accounts for both the total value of the moisture content u at the time of loading, and the variations of moisture content during the service life, usually represented in the most common mechano-sorption models by the accumulation function $U = \sum|\Delta u|$.

This approach is easy to use at the design level as no complex rheological model needs to be used. However, it may suffer from inaccuracy as it may be hard to choose the right service class of a structure and, last but not least, the actual behaviour may differ significantly between small and massive timber structures since no allowance for the size of the structure is made in the service class.

2 Effect of moisture on timber members

When a timber member is exposed to the atmosphere, a variation of moisture content $\Delta u=\Delta u(x,y,z,t)$ will take place over time in the different points $P=P(x,y,z)$ of the timber volume. Those variations Δu are governed by the diffusion laws and will depend upon the histories of relative humidity $RH=RH(t)$ and temperature $T=T(t)$ of the environment. Since the moisture content variations Δu are not the same over the cross-section (they will be larger in amplitude on the outer fibres and lower on the inner core), the corresponding inelastic strains $\Delta\varepsilon=\alpha_{w,u}\Delta u$ will induce eigenstresses and deflections in the timber member. The most important effect is eigenstresses perpendicular to the grain, where the hygroscopic coefficient $\alpha_{w,u}$ is large and the tensile strength low, leading to potential failure (cracks due to drying). This effect may be particularly evident when stiff members such as steel plates and bolts are introduced in the structure and prevent the free expansion and contraction of the timber.

2.1. Statically determinate structures

Eigenstresses and deflections are generally negligible parallel to the grain, provided the timber member is free to expand and shrink such as in statically determinate timber structures. The only regions where eigenstresses may be produced are connection regions, particularly when stiff members such as steel plates and bolts are introduced in the structure.

Initial moisture content u and moisture content variations Δu also increase the deflection, since they affect the total creep coefficient due to the mechano-sorption phenomenon, but only when an external load such as a permanent or imposed load is applied on the structure. Note that no deflection would be produced parallel to the grain by moisture if no load was to be applied on the timber. This is the main reason why *moisture variations cannot be considered only as a load in the design of statically determined timber members parallel to the grain*. In other words, it is necessary to consider the influence of the moisture content on the strength and deflection of the members, for example by introducing the k_{mod} and k_{ser} factors, or by introducing a more accurate way as described in the following.

2.2. Composite and statically indeterminate structures

In addition to the influence on the deflection (increase in deflection over time due to creep and mechano-sorption) and strength (decrease of strength over time due to creep and mechano-sorption) of timber, moisture content variations Δu cause significant eigenstresses and additional deflection in composite (such as timber-concrete) and statically indeterminate structures. In a composite structure, in fact, an increase in moisture content in the timber member cannot freely occur due to the restraint provided by the concrete slab. A similar effect is caused by a variation in temperature ΔT as timber and concrete are characterized by different dilation coefficients (the concrete thermal expansion coefficient $\alpha_{c,T}$ is about twice as large as the timber one $\alpha_{w,T}$). As a consequence of that, a different expansion/contraction will occur at the interface between concrete and timber leading to eigenstresses and additional (positive or negative) deflection.

Thus, *for composite and statically indeterminate structures, moisture and temperature variations should be considered as additional loads* ΔU and ΔT, to be combined with the other loads (permanent G, imposed Q, concrete shrinkage ε_{cs}, etc.) for SLS and ULS:

$$F_U = \gamma_G G + \gamma_Q Q + \gamma_s \varepsilon_{cs} + \gamma_U \Delta U + \gamma_T \Delta T \quad \text{for ULS} \tag{1}$$

$$F_S = G + \psi_2 Q + \varepsilon_{cs} + \Delta U + \Delta T \quad \text{for SLS} \tag{2}$$

An important question to be discussed is which load amplification factor γ should be used for moisture content variations, temperature variations, and concrete shrinkage for ULS and SLS. Since those actions will be applied during the service life of the structure, they may assume the same load amplification factor of the permanent load: γ=1.35 if their effect is destabilising, 0.9 if their effect is stabilising, for ULS verification; 1 for SLS verifications. As far as the load duration class is concerned, the concrete shrinkage is long-term as the shrinkage will take place throughout the service life. As far as temperature and moisture variations are concerned, their duration class will need to be evaluated based on the type of environmental variation taken into account (daily or annual).

Some load amplification factors are provided in regulations for bridge design like, for example, the DIN Fachbericht 104 (2005). Clause 2.3.3.1.6 of such regulation recommend the use of a γ_F-value for shrinkage of concrete in steel-concrete composite bridges equal to 1.0 for ULS as well as for SLS. However it should be clarified whether the same value can also be used for timber-concrete-composite structures, since in steel-concrete composite systems the eigenstresses due to shrinkage can be reduced by yielding of the steel cross section. Since timber does not yield, timber-concrete-composite structure is not directly comparable with steel-concrete-composite beams. Due to the huge variation of concrete shrinkage and environmental variations, a reliability analysis should be carried out in order to investigate the effect of the scatter of those quantities on the structural response and to calculate the actual value of the load amplification factor to be assumed in the load combinations.

Note that the effect of moisture content on the increase in deflection and decrease in strength of the timber component over time due to creep and mechano-sorption must be considered as a separate contribution on the material strength (coefficients k_{mod} and k_{ser}).

More information on the actual values of the load due to moisture content and temperature variations will be provided in the following.

3 A proposal to evaluate the load equivalent to moisture content and temperature variations

3.1. Basics

The moisture content variations that can be expected in a point P of a timber member over the service life depend on: (i) environmental variations of relative humidity $RH=RH(t)$; (ii) size of the timber cross-section; (iii) location of the point P in the cross-section; (iv) wood species; (v) application of coating on the surface of the member; and (vi) environmental variations of temperature $T=T(t)$ (usually negligible).

In order to simplify the problem, the following procedure may be followed:

(i) a number of yearly environmental histories of relative humidity $RH=RH(t)$ should be identified in order to cover all the cases of possible interest. For example, it is suggested that at least three cases representative of countries with very different environment (for example: Sweden, Germany and Italy) are considered. For each of those countries, four different conditions should be considered: outdoor with timber unprotected by the rain (service class 3 according to the Eurocode 5), outdoor with timber protected by the rain (service class 2), unheated indoor (service class 2), and heated indoor (service class 1).

(ii) for all those cases, also a maximum yearly temperature variation (the difference between the highest and the lowest daily temperature, T_{max}-T_{min} and the difference between the temperature at the time of construction and the minimum/maximum temperatures, T_{constr}-T_{min} and T_{max}-T_{constr}, see for example DIN-Fachbericht 101(2003)) should be identified.

(iii) a number of cross-section of interest should be identified: for example, a small, medium and large timber cross-section.

(iv) the diffusion problem of the moisture content over the cross-section due to the yearly environmental history of relative humidity should then be resolved in order to compute the history of the moisture content averaged over the cross-section, $u_{avg}=u_{avg}(t)$. Note that the problem is quite complex if the timber beam is in outdoor conditions and exposed to the rain as in that case not only should be considered the diffusion of the vapour (moisture content), but also the diffusion of the water (rain) over the cross-section.

(v) based on the yearly history of average moisture content, the maximum differences $\Delta u_{max}=u_{avg,max}$-$u_{constr}$ between the annual maximum and the moisture content at the time of construction, and $\Delta u_{min}=u_{constr}$-$u_{avg,min}$ between the moisture content at time of construction and the annual minimum can then be obtained and used to calculate the corresponding yearly variation of inelastic strain due to environmental changes, $\Delta\varepsilon_{u,max}=\alpha_{w,u}\Delta u_{max}$ and $\Delta\varepsilon_{u,min}=\alpha_{w,u}\Delta u_{min}$. These variations of inelastic strain will be used together with the variations of inelastic strain due temperature, $\Delta\varepsilon_{T,max}=(\alpha_{c,T}-\alpha_{w,T})(T_{max}-T_{constr})$ and $\Delta\varepsilon_{T,min}=$ $=(\alpha_{c,T}-\alpha_{w,T})(T_{constr}-T_{min})$, to calculate the eigenstresses and deflection due to moisture content and temperature variations. Note that in some approaches (see, for example, Schänzlin 2003, Schänzlin and Fragiacomo 2008, and Appendix A) an external uniformly distributed load equivalent to the moisture content and temperature variations can be computed. Also note that the aforementioned procedure is approximated since the actual histories of moisture content will vary over the timber cross-section depending on the location of the point where they are evaluated. However it has been proved (Fragiacomo 2006) that the approximation achievable is more than adequate for design purposes.

The values of $\Delta u=u_{avg,max}$-$u_{avg,min}$ and $\Delta T=T_{max}$-T_{min} could then be provided in codes of practice (for example the Eurocode 5) for the different region (3), different type of exposure (4) and type of cross-section (3), leading to a total of 36 tabular values. Alternatively, the designer may calculate on his/her own the values of Δu and ΔT in the case of more demanding applications by assessing the history of relative humidity and temperature in the specific location where the structure will be erected, and by solving the diffusion problem over the actual cross-section.

3.2. Worked example

The approach proposed above has been used to evaluate the moisture-induced and temperature-induced loads for four different timber cross-sections, which were employed in timber-concrete composite beams investigated by different authors (see for more details Fragiacomo and Ceccotti 2006). The geometrical properties of the fours beams are listed in Table 1, where the symbols l, b_t, h_t, b_c, h_c and t signify the span length, breadth and depth of the timber beam, breadth and depth of the concrete slab, and thickness of the timber flooring in between the concrete slab and the timber beam. The 'Florence' beam is a long-span composite beam with glued rebar connection and deep glulam beam (Capretti and Ceccotti 1996). The 'Padua' beam is a medium-span composite beam with glued rebar connection typical of upgrading of ancient wooden floors (Turrini and Piazza 1983). The 'Cardington' beam is a short-span beam with narrow timber joists and inclined SFS screws representative of a possible upgrading of a domestic wooden floor (Grantham et al. 2004). The 'Fort Collins' beam is a short-span wood-concrete composite floor/deck system with shear/key anchor connection detail (Fragiacomo et al. 2006).

Table 1: Geometrical and mechanical properties of the beams analysed

	Florence	Padua	Carding.	Fort Coll.		Florence	Padua	Carding.	Fort Coll.
g_1 [kN/m]	2.34	1.65	0.86	0.404	A_s [mm^2]	94	57	85	158
g_2 [kN/m]	0.6	0.6	0.36	0.114	t [mm]	50	25	15	0
$\psi_2 q$ [kN/m]	1.2	1.2	0.45	1.14	K_{ser} [N/mm]	25000	15750	9357	156213
l [mm]	10000	5800	3600	3600	$s_{\min}$ [mm]	300	110	100	454.5
b_c [mm]	1000	550	600	190.5	$s_{\max}$ [mm]	450	250	200	454.5
h_c [mm]	50	60	50	63.5	b_t [mm]	125	160	38	190.5
f_{cm} [MPa]	30.43	31.24	31.24	17.89	h_t [mm]	500	230	225	88.9
h [mm]	100	120	100	47.6	E_t [GPa]	10	9	8	8.605

All beams have been regarded as being exposed to the environmental conditions monitored in Florence during the period 9 June 1994 - 8 June 1995 assuming sheltered outdoor conditions (see Ceccotti et al. 2006). The trends in time of the maximum and minimum daily temperatures are displayed in Fig. 1 (top) including the approximating piecewise-linear curves (Fragiacomo and Ceccotti 2006). The average day-to-night variation is 8 Celsius degrees, while the amplitude of the yearly fluctuation is 29 Celsius degrees, leading to a maximum temperature variation ΔT=33 Celsius degrees.

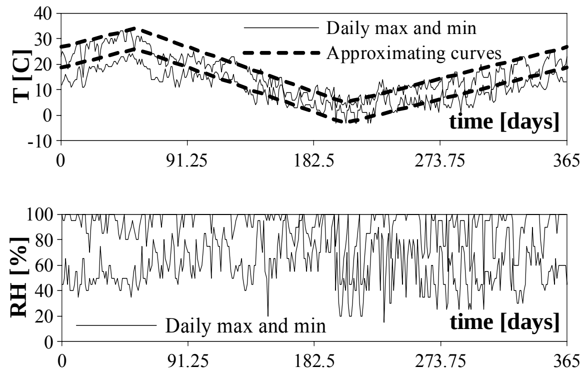

Fig. 1: maximum and minimum daily temperature (top) and relative humidity (bottom) monitored in Florence

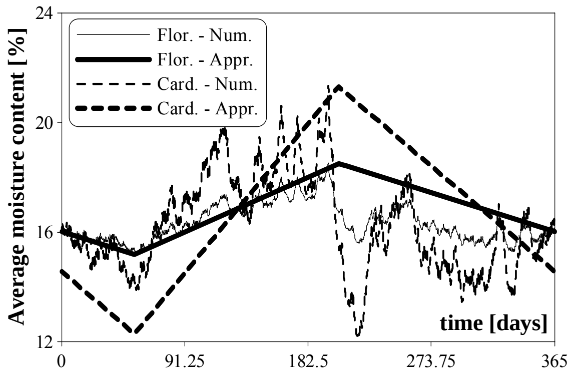

Fig. 2: trend in time of the moisture content averaged over the timber cross-section

The trends in time of the maximum and minimum daily relative humidity are displayed in Fig. 1 (bottom), with an average daily fluctuation of 34%. The trends in time of the average moisture content over the timber cross-section are displayed in Fig. 2 for the Florence and Cardington beams, together with the approximating piecewise-linear curves ("appr." curves in the legend). The numerical ("num" in the legend) curves have been obtained using a numerical program which solves the diffusion problem of moisture content based on the relationships suggested by Toratti (1992). The problem is solved over the timber cross-section for the history of environmental relative humidity monitored in Florence and displayed in Fig. 1, on the bottom, as maximum and minimum daily values (Fragiacomo 2005). It can be demonstrated that the daily variations of environmental relative humidity history does not markedly affect the trend of the average moisture content over the time (Fragiacomo and Ceccotti 2006). The breadth of the timber beam plays a significant role on the amplitude of the yearly moisture content variation: Δu=$u_{aver,max}$-$u_{aver,min}$=3.3% for the Florence beam (b_t=125 mm), and 9% for the Cardington beam (b_t=38 mm), with values closed to those of the Florence beam for the Padua (3%) and Fort Collins (3.8%) beams, respectively.

The inelastic strains parallel to the grain equivalent to the moisture content variations in the timber beams can then be calculated by multiplying the moisture dilation coefficient by the corresponding moisture content variation: $\Delta\varepsilon_u=\alpha_{w,u}\Delta u$. Similarly, the effect of the temperature variations on the timber beam is given by: $\Delta\varepsilon_{w,T}=\alpha_{w,T}\Delta T$. In the case of a timber-concrete composite beam, the deflection and eigenstresses resulting from the inelastic strains listed above as well as the thermal expansion of the concrete slab $\Delta\varepsilon_{c,T}=\alpha_{c,T}\Delta T$ and the concrete shrinkage ε_{cs} can then be transformed into an equivalent uniformly distributed load (Schänzlin 2003, Schänzlin and Fragiacomo 2008, and Appendix A) or used in closed form solutions (Fragiacomo 2006, Fragiacomo and Ceccotti 2006, Schänzlin and Fragiacomo 2008, and Appendix A).

4 A proposal to explicitly take into account the effect of the moisture content on the material properties

4.1. Basics

The evaluation of the yearly moisture content variations depending on the type of environment and size of the cross-section can also be used in order to evaluate in a more accurate way the corresponding effect on timber in term of creep and mechano-sorption, and reduction in strength.

The method is based on the introduction of a rheological model where the total creep coefficient of timber can be obtain as a sum of a “pure” creep coefficient and a “mechano-sorption” creep coefficient:

$$\phi_t(t)=\phi_{tc}(u,t)+\phi_{tms}\left(t,\sum\Delta u\right) \tag{3}$$

If the actual history of the average moisture content over the timber cross-section is approximated with a piecewise-linear characterized by a yearly period Δt=365 days and an amplitude Δu calculated as suggested in the previous Section 3, the dependency of the mechano-sorption component of the total creep coefficient on the summation of the moisture content variations Δu over time can be simplified and expressed in a closed form solution. If, for example, the Toratti’s model is used (Toratti 1992), the total creep coefficient can be easily calculated as in the following:

$$\phi_t(t)=\phi_{tc}+\phi_{tms}=\left(\frac{t}{t_d}\right)^m+\phi^\infty\left[1-e^{-c\frac{2\Delta ut}{100\Delta t}}\right] \tag{4}$$

This formula can be derived from the general rheological model:

$$
\begin{aligned}
\varepsilon(t) &= \int_{t_0}^{t} J_0(u(\tau))d\sigma(\tau) + \int_{t_0}^{t} J_c(t-\tau)d\sigma(\tau) + \int_{t_0}^{t} \sigma(\tau)dJ_0(u(\tau)) + \\
&+ J^{\infty}\int_{t_0}^{t}\left\{1-e^{\left[-c\int_{\tau}^{t}|du(\tau_1)|\right]}\right\}d\sigma(\tau) - \int_{t_0}^{t} b\varepsilon(\tau)du(\tau) + \int_{t_0}^{t}\alpha_u du(\tau) + \int_{t_0}^{t}\alpha_T dT(\tau)
\end{aligned}
\tag{5}
$$

by ignoring the dependency of the Young's modulus on the moisture content (3rd integral), the dependency of the total strain on the moisture content (5th integral), the inelastic strains (6th and 7th integral), and by resolving the mechano-sorption integral (4th integral) for a piecewise-linear history of moisture content. Note that the Toratti's model does not allow for the dependency of the pure creep coefficient of the initial moisture content.

This equation allows an easy evaluation of the total creep coefficient to be used for SLS verifications once the yearly variation of moisture content Δu and, therefore, the size of the cross-section, the type of environment, and the time after the load application are known. Alternatively, tabular values of $\phi_t(\infty)$ (creep coefficient at the end of the service life) which would replace the k_{def} factors currently reported in the Eurocode 5, could be provided for different environments, different climates, and different cross-sections. The advantage of this approach would be a more accurate evaluation of the creep coefficient depending on the actual effect of moisture content on the timber cross-section.

In addition, a strength degradation factor should be provided in order to account for the reduction in strength due to long-term loading. Again, a rheological model should be used in order to evaluate this reduction factor k_{mod}' as a function of the load duration t (load duration class), initial moisture content u, and summation of moisture content variations Δu:

$$
k_{\mathrm{mod}}' = \frac{f\left(t, u, \sum \Delta u\right)}{f_0} \tag{6}
$$

with $k_{\mathrm{mod}}' = k_1(u)\left[1 - k_1(t) - k_2\left(\sum \Delta u\right)\right]$ (7)

This equation allows an easy evaluation of the strength reduction factor to be used for ULS verifications once the size of the cross-section, the type of environment, the initial moisture content, and the load duration class are known. Alternatively, tabular values of $k_{\mathrm{mod}}'(t)$ (strength reduction factors for different load duration classes) which would replace the k_{mod} factors currently reported in the Eurocode 5, could be provided for different initial moisture content, different environments, different climates, different cross-sections, and different load duration classes. The advantage of this approach would be a more accurate evaluation of the strength reduction factor depending on the actual effect of moisture content on the timber cross-section.

4.2. Worked example

In the Toratti's model, the parameters t_d, m, ϕ^{∞} and c are assumed equal to 29500 days, 0.21, 0.7 and 2.5, respectively, and Δu is measured in [%]. Fig. 3 depicts a comparison between the curves of the total creep coefficient calculated according to the approach proposed before, and the current creep coefficients suggested by the Eurocode 5 for different service classes (Fragiacomo and Ceccotti 2006). It can be observed that the yearly variation of moisture content affects the rate of increase in time of the creep coefficient in the Toratti's model. However, the final value is independent of Δu for amplitudes larger than 1.65%, and is slightly lower than the value suggested by the Eurocode 5 for the 3rd service class. Note that the moisture content variation was in the range 3 to 9% for all four timber beams analysed in the previous Section, therefore well above the 1.65% value mentioned above. For no moisture content variations (Δu=0), the Toratti's model leads to a final value slightly higher than the Eurocode 5 final value for the 2nd service class, which may be considered as representative of an environmental condition with no significant moisture content variations and, therefore, no mechano-sorptive effect. The Toratti's model with the values of the material parameters suggested by the author leads to results different from the current creep coefficient suggested by the Eurocode 5. If the approach proposed in this paper was to be adopted, a thorough revision of the current rheological models of timber should be undertaken in order to identify the best model and parameters to effectively represent the material behaviour for different climates and wood cross-sections.

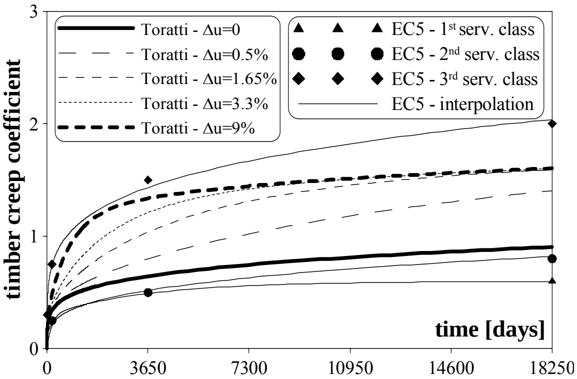

Fig. 3: comparison between current EC5 creep coefficients and values calculated according the proposed approach

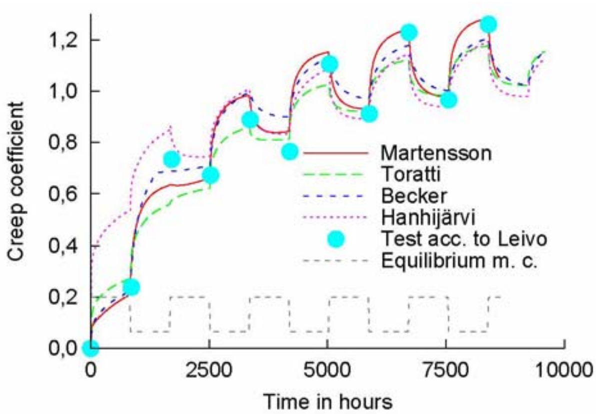

Fig. 4: comparison of different rheological models for timber over a time range of 10000 hours

A first comparison of different rheological models of timber has already been performed in Schänzlin (2008). As it can be seen in Fig. 4, the models provide a good fit with the experimental results. Little difference can be notice among the models themselves. If these models are extrapolated to the interesting range of 50 years (duration of the service life), however, significant differences can be recognized (see Figs. 5 & 6).

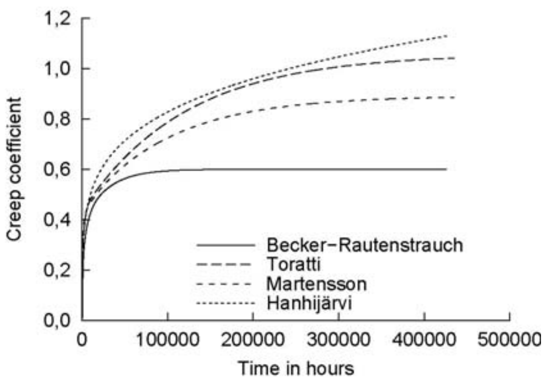

Fig. 5: creep coefficient in constant environmental conditions evaluated using different rheological models

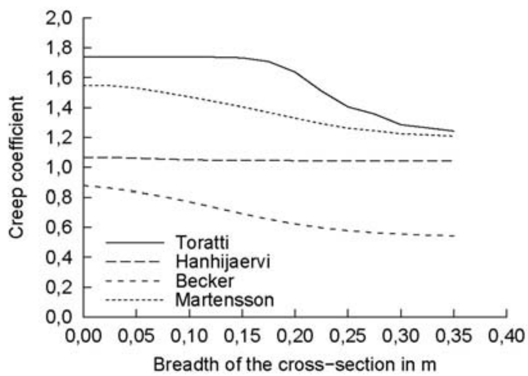

Fig. 6: creep coefficient at the end of the service life (50 years) in variable climate ($RH_{average}$= 65%, annual ΔRH=15%) evaluated using different rheological models

These differences are caused by the structure of the models, since in some of them the normal creep and the mechano-sorptive creep are modelled as a series of simple rheological models, whereas in others the simple rheological models are linked in parallel. By extrapolating the parallel models, the total creep strain appears to be too limited, since the model was probably calibrated on experimental tests performed over a limited amount of time. Those models should therefore be recalibrated if the end of the service life is of interest rather than the first days after the load application. For example, only a very little influence of the breadth and, therefore, of the average moisture variation of the cross section can be found for the model according to Hanhjärvi (1995). Since the other models are series ones, the creep limit is reached when both “pure” creep and mechano-sorptive creep reach their limit. In this case, a large influence of the breadth of the cross section can be noted (see Fig. 6).

Giving the significant difference among the models, some investigations should be undertaken in order to decide which one should be used in order to predict the deflection with good accuracy. First measurements of the deflection of existing structural elements subjected to sustained load applied for several years (Schänzlin 2008) suggest that the use of Toratti’s B model (1992) is the best choice for the prediction of the time dependent deflection in the long-term, at least for the region of Tübingen, in South West Germany (see Fig. 7).

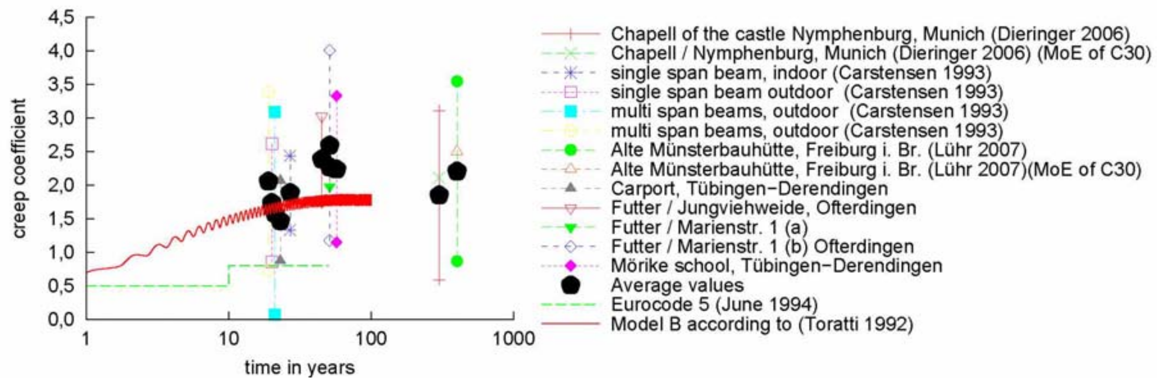

Fig. 7: comparison between measured creep coefficients and the evaluation based on the Toratti’s B rheological model

The evaluation can be improved a little if some parameters of the Toratti’s model describing the normal creep are slightly modified, as reported in Table 2. It is not clear, however, whether the differences are only due to differences in the “pure” creep coefficient, as assumed in Table 2, or also to an inaccurate evaluation of the mechano-sorptive creep.

Table 2: Modified input values for Toratti’s B model

element	1	2	3	4	5	6
τ_i	0,01	0,1	1	10	193,23	11079,51
J_i	0,0686	-0,0056	0,0716	0,0409	0,2201	1,8052

5 Timber-concrete composite beams

5.1. Use of the proposed approach to design composite beams

Currently, concrete shrinkage, moisture and temperature variations are ignored when designing timber-concrete composite beams (Ceccotti 1995). The purpose of this Section is to show the importance of those effects, as already proved in previous papers (Fragiacomo 2006, Fragiacomo and Ceccotti 2006, Schänzlin 2003, Schänzlin and Fragiacomo 2007, 2008).

Figures 8 and 9 reports the trend over time of deflection, top and bottom fibre stresses in the concrete slab and timber beam at mid-span, and connector shear force over the support for the four beams of Table 1 (Fragiacomo and Ceccotti 2006). Those beams were analysed over time by assuming they were loaded with the self weight of the concrete slab g_1, the additional permanent load g_2, and the quasi-permanent part of the imposed load $\psi_2 q$ listed in Table 1 at the times t_1=14, t_2=35 and t_3=180 days, respectively, from the concrete pouring. The beams were exposed to the history of relative humidity and temperature monitored in Florence, as described in Section 3. Concrete creep and shrinkage, timber creep and mechano-sorption, and connection creep and mechano-sorption were all considered, as well as the inelastic strains in concrete due to thermal variation, and the inelastic strains in timber due to moisture and thermal variations. In all the solutions, the Toratti's rheological model was used. The Young's modulus, creep function and shrinkage of concrete were computed according to the CEB formulas (CEB 1993) by assuming an average environmental relative humidity RH=75%, the mean compressive strength f_{cm} and the notational thickness h specified in Table 1.

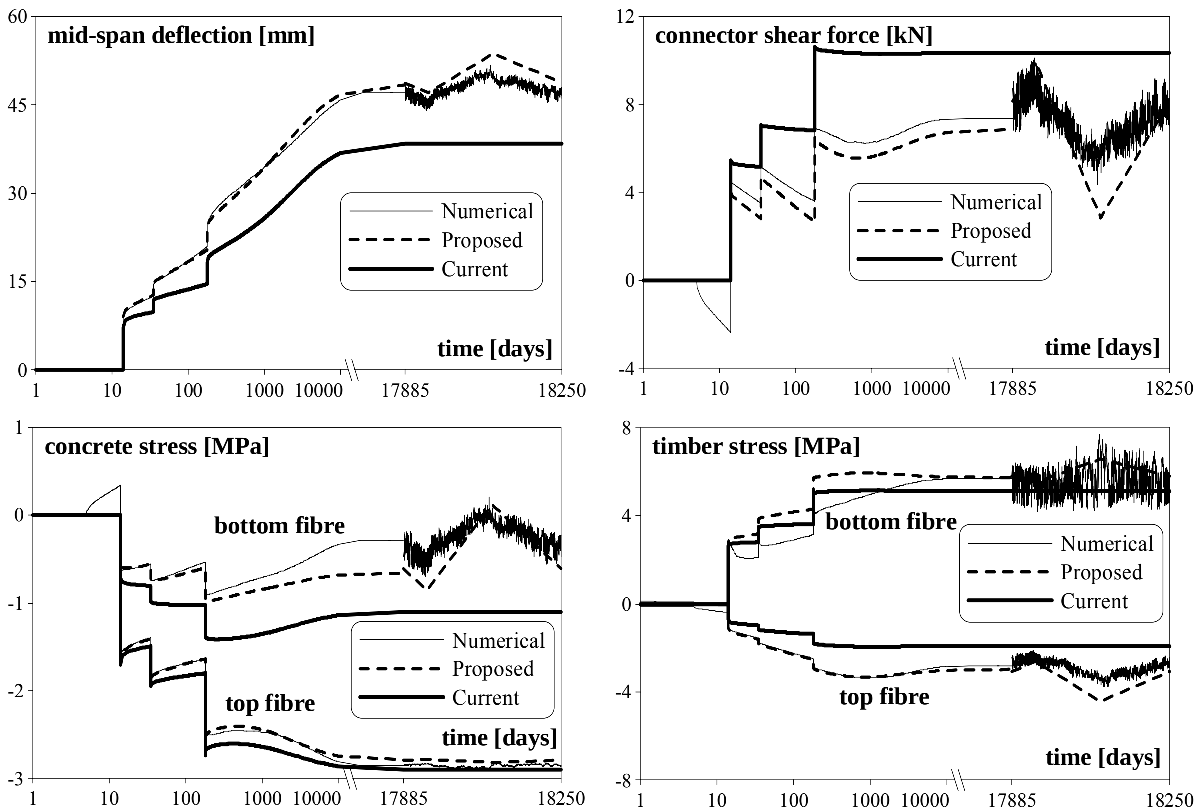

Fig. 8: trends in time of the mid-span deflection (top, left), connector shear force over the support (top, right), outer fibre concrete stresses at mid-span (bottom, left), and outer fibre timber stress at mid-span (bottom, right) for the Florence beam

The values of the area of reinforcement A_s in the concrete slab, slip modulus of the connector K_{ser}, and minimum, maximum connector spacing $s_{\min}$, $s_{\max}$ are reported in Table 1. The rigorous numerical solution obtained using a purposely developed FE model (Fragiacomo 2005 - thin solid line) is compared with the current analytical approach which neglects the concrete shrinkage and inelastic strains due to environmental changes (Ceccotti 1995 - thick solid line), and with a proposed analytical approach which does consider the aforementioned phenomena with the approximations on the history of moisture content described in Sections 3-4 (Fragiacomo 2006, Fragiacomo and Ceccotti 2006 - dashed line). For the sake of clarity, the yearly and daily fluctuations due to environmental variations have been plotted only for the last year. The abscissa is in logarithmic scale until the 49th year, and in linear scale for the 50th year.

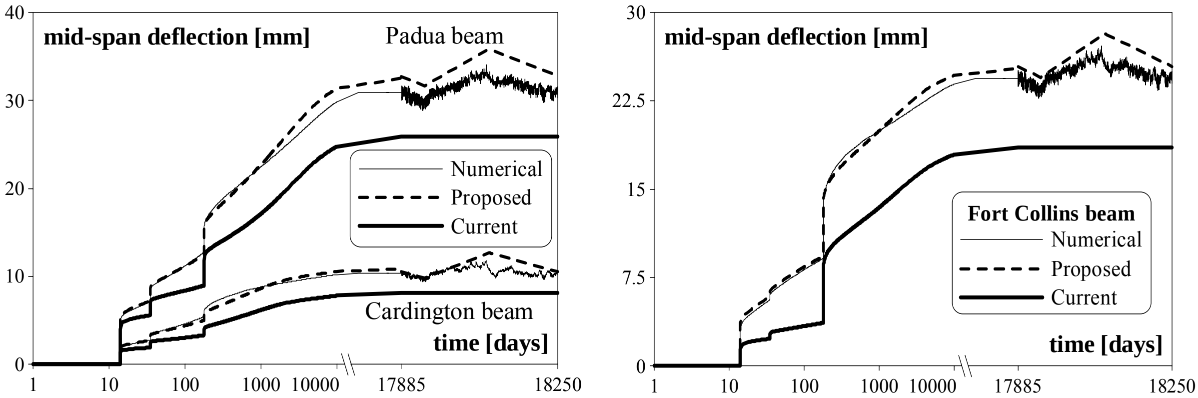

Fig. 9: trend in time of the mid-span deflection for the Padua and Cardington beams (left) and for the Fort Collins beam (right)

The use of the proposed approach leads to very accurate results in terms of deflections and stresses. Conversely, the current approach markedly underestimates deflection and stresses. The concrete shrinkage, in fact, represents a significant component of the long-term deflection and, as such, should not be neglected. The importance of the yearly and daily fluctuations of moisture content and temperature can also be noted. The proposed method should hence be used for the design of the composite beam in the long-term, especially when a more accurate evaluation of the deflection is required.

5.2. Influence of different environmental conditions on the long-term behaviour

The analyses carried out above refer to the case of a timber-concrete composite beam exposed to outdoor conditions. For timber-concrete composite beams in heated indoor conditions, the environmental variations are characterised by reduced fluctuations. Some research suggested that in indoor conditions the moisture content variations should be halved with respect to outdoor conditions (Limträhandbok 2001). However, other recent studies (Häglund and Thelandersson 2005) pointed out that the same amplitude of the moisture content variations can be expected in timber beams exposed to outdoor and heated indoor conditions, with a value of about 7 to 10%. In terms of temperature variations, the whole yearly indoor fluctuation including daily variations can be assumed as little as one-third of the environmental fluctuations.

It is then interesting to investigate which differences can be expected in the response of composite beams in heated, indoor conditions (service class 1 and 2 according to the Eurocode 5) compared to unheated, protected outdoor conditions. The Florence and Cardington beams have been analysed under indoor conditions by assuming one-third of the environmental temperature variations, and the three cases of equal, half and no moisture content variation in each fibre of the timber beam. The outcomes are reported in Figure 10 as analytical solutions using the proposed approach (Fragiacomo and Ceccotti 2006). For the sake of clarity, the yearly fluctuations due to environmental variations have been plotted only for the last year, and the numerical solutions have not been reported being very close to one another.

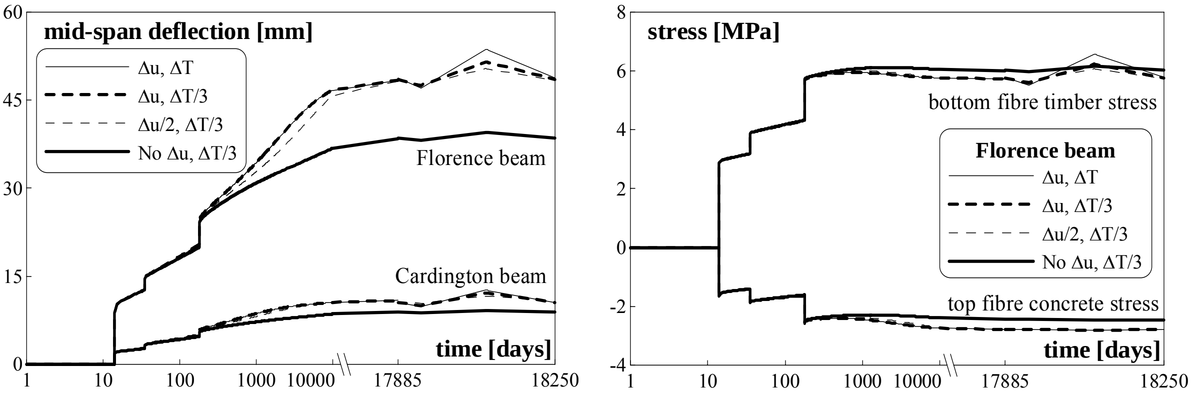

Fig. 10: comparison among the deflections (left) and stresses (right) at mid-span for the Florence and Cardington beams exposed to different environmental conditions

It can be observed that the differences among the solutions in outdoor (Δu, ΔT) and indoor (Δu, $\Delta T/3$, and $\Delta u/2$, $\Delta T/3$) conditions are generally low (max 17%). Those differences are mainly due to the reduced amplitude of the environmental yearly fluctuations, while the long-term effects of the load and concrete shrinkage are hardly affected by the reduction of the yearly moisture content variation from Δu to $\Delta u/2$. This can be justified by the mechano-sorptive effect being almost independent, in the long-term, of the yearly moisture content variations for values of technical interest (Δu>1.65%), as previously discussed and depicted in Fig. 3. A significant difference is observed only when no moisture content variation is considered (Δu=0) where, according to the Toratti's model, the mechano-sorptive effect becomes zero for timber and connection. In this case the creep coefficient of timber and connection approaches the value suggested by the Eurocode 5 for the 2nd service class. The stresses are less affected by the environmental conditions than the deflection. This outcome agrees with the evidence that the rheological phenomena mainly affect the deflection of a composite beam, while the effect on the stresses is generally limited (Fragiacomo et al. 2007).

6 Conclusions

This paper deals with the question whether the moisture content can be considered as an action for timber and composite structures. In the authors' opinion, the answer is "yes": yearly variations of timber moisture content should be considered as an additional action as well as temperature variations. However, it should be pointed out that this additional load will lead to increase (or variation) in deflection as well as to eigenstresses mainly in statically indeterminate timber structures and in composite structures. Note that connection regions where the timber is prevented from free movement due to stiff steel plates and dowels is a statically indeterminate part and should be analysed under the load equivalent to moisture variations, particularly in order to calculate the stresses perpendicular to the grain which may lead to failure in tension perpendicular to the grain.

Another important point is that in the authors' opinion the effect of the moisture content on the strength and deflection (creep coefficient) cannot be removed. However a more accurate method for the evaluation of this influence can be developed. This method is based on the evaluation of the yearly variation of average moisture content for a number of timber cross-sections of technical interest when exposed to a number of climates such as different climatic regions, and indoor/outdoor conditions. The use of those variations of average timber moisture content in a rheological model such as the Toratti's one can then allow a better evaluation of the strength reduction factor and creep coefficient depending upon the actual conditions. Furthermore, the same variation of average moisture content can be used to evaluate the equivalent load to be used in statically indeterminate and timber-concrete composite beams.

Some numerical-analytical comparisons carried out on long-term behaviour of timber-concrete composite beams have pointed out the need to consider the yearly moisture content variations as well as the temperature variations and the concrete shrinkage as additional loads to combined with the imposed and permanent loads. Some important questions yet to be clarified are which load factors to consider for SLS and ULS verifications. Last but not least, a discussion on the type of rheological model and the material coefficients to be used is needed as, for example, the influence of the initial timber moisture content on the pure creep coefficient is currently disregarded by the Toratti's model.

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Appendix A: Consideration of inelastic strains in the design of timber-concrete composite beams

This appendix reports the procedures to calculate the eigenstresses and deflection in a timber-concrete composite beam due to inelastic strains in the timber and/or concrete part. Those procedures were developed independently by the authors and jointly presented in the paper by Schänzlin and Fragiacomo (2007). In the following, the relevant parts of the aforementioned paper are reported.

General approach

The internal forces and deflection of a timber-concrete composite beam resulting from inelastic strains due to different thermal expansion or different shrinkage of concrete and timber cannot be computed using the EC5 Annex B formulas. Since recent investigations pointed out that their influence is significant (Fragiacomo (2006), Schänzlin (2003)), it is quite important to propose a method for their evaluation.

The elastic solution of a simply supported composite beam with smeared flexible connection subjected to vertical load and different inelastic strains in the concrete slab and timber beam can be obtained by solving a differential equation. Since the problem is quite complex, simplified approaches have been developed and proposed:

- Superposition of the effects (deflection and internal forces) of vertical load with the effects of inelastic strains. For the former effects, the EC5 Annex B formulas can be used. For the latter effects, closed form formulas derived by integrating the differential equation of the composite beam with flexible connection are employed (Fragiacomo (2006), Fragiacomo and Ceccotti (2006)).
- Transformation of the inelastic strains into a fictitious vertical load and modification of the effective bending stiffness suggested by the EC5 Annex B (see Schänzlin (2003)). In this case all effects (deflection, internal forces) are evaluated using the EC5 Annex B formulas with the modification of the effective bending stiffness and the addition of a fictitious load equivalent to the inelastic strains.

Superposition of the effects according to Fragiacomo (2006)

In elastic phase, the principle of superposition can be used to separate the effects of vertical loads and inelastic strains. The effects (deflection, internal forces) of vertical loads can be computed using any design method which considers the flexibility of connection, such as the EC5 Annex B approach. The effects of inelastic strains in the concrete slab and timber beam of a simply supported composite beam with flexible connection can be computed using the rigorous formulas reported in the following:

- Input values
  - Difference of inelastic strains between concrete (subscript 1) and timber (subscript 2)

    $$\Delta\varepsilon = \Delta\varepsilon_2 - \Delta\varepsilon_1 \qquad \text{(A1)}$$
  - Geometrical properties (lever arm *z*, area *A*, second moment of area *I*, *b* and *h* being the breadth and depth of the i^{th} component, *t* being the distance between the top fibre of the timber beam and the bottom fibre of the concrete slab)

    $$z = 0.5 \cdot h_1 + t + 0.5 \cdot h_2\,;\; A_i = b_i \cdot h_i\,;\; I_i = \frac{b_i \cdot h_i^3}{12}\,;\; i = 1,2 \qquad \text{(A2)}$$
  - Stiffness (Young's modulus *E*)

$$(EA)^* = \frac{E_1 \cdot A_1 \cdot E_2 \cdot A_2}{E_1 \cdot A_1 + E_2 \cdot A_2}; \; (EI)_{abs} = E_1 \cdot I_1 + E_2 \cdot I_2 \tag{A3}$$

$$(EI)_{full} = (EI)_{abs} + (EA)^* \cdot z^2 \tag{A4}$$

- ○ Coefficient (Slip modulus of connection K_{ser}, connector spacing s_{ef}, span length L, distance from the left support to the cross-section x)

$$\alpha = \sqrt{\frac{K}{s_{ef}(EA)^*} \cdot \frac{(EI)_{full}}{(EI)_{abs}}} \tag{A5}$$

$$\gamma_\vartheta(x) = 1 + \tanh(0.5\alpha L) \cdot \sinh(\alpha x) - \cosh(\alpha x) \tag{A6}$$

- ○ Mid-span deflection due to inelastic strains

$$u_{\max} = u_{\max, full} \cdot \gamma_u \qquad \text{where} \tag{A7}$$

$$u_{\max, full} = \frac{\Delta\varepsilon}{z} \cdot \frac{(EI)_{full} - (EI)_{abs}}{(EI)_{full}} \cdot \frac{L^2}{8} \tag{A8}$$

$$\gamma_u = 1 - \frac{8}{(\alpha \cdot L)^2} \cdot \left[1 - \frac{1}{\cosh(0.5 \cdot \alpha \cdot L)}\right] \tag{A9}$$

- ○ Internal forces due to inelastic strains

$$N_2(x) = -N_1(x) = N_{2,\max, full} \cdot \gamma_\vartheta(x) \quad \text{and} \quad M_i(x) = M_{i,\max, full} \cdot \gamma_\vartheta(x) \tag{A10}$$

where $$N_{2,\max, full} = -\frac{\Delta\varepsilon}{z} \cdot \frac{(EI)_{full} - (EI)_{abs}}{(EI)_{full}} \cdot \frac{(EI)_{abs}}{z}, \tag{A11}$$

$$M_{i,\max, full} = \frac{\Delta\varepsilon}{z} \cdot \frac{(EI)_{full} - (EI)_{abs}}{(EI)_{full}} \cdot E_i \cdot I_i \quad \text{and} \tag{A12}$$

$$\gamma_\vartheta(x) = 1 + \tanh(0.5 \cdot \alpha \cdot L) \cdot \sinh(\alpha \cdot x) - \cosh(\alpha \cdot x) \tag{A13}$$

- Shear forces in the connection due to inelastic strains

$$F(x) = K \cdot s_f(x) \qquad \text{where} \tag{A14}$$

$$s_f(x) = s_{f,\max,abs} \cdot \gamma_s(x), \qquad s_{f,\max,abs} = -\Delta\varepsilon \cdot \frac{L}{2}, \tag{A15}$$

and $$\gamma_s(x) = \frac{1}{0.5\alpha L} \cdot [\tanh(0.5 \cdot \alpha \cdot L) \cdot \cosh(\alpha \cdot x) - \sinh(\alpha \cdot x)] \tag{A16}$$

The design procedure is summarized in the flowchart of Fig. A.1

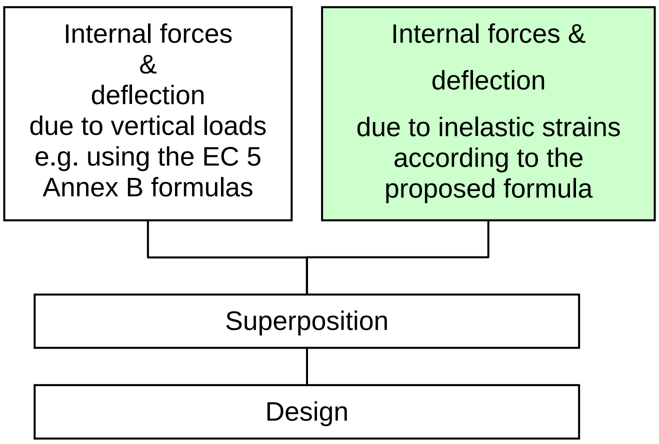

Fig. A.1: Design procedure using the superposition of effects according to Fragiacomo (2006)

Consideration of inelastic strains using a fictitious load according to Schänzlin (2003)

The determination of the fictitious vertical load producing the same effects as the inelastic strains was performed in a first step by comparing the deflection of a composite beam subjected to both a sinusoidal loading and a sinusoidal inelastic strain along the beam length with the deflection of a homogenous beam with an unknown effective bending stiffness and the same loading (see Fig. A.2).

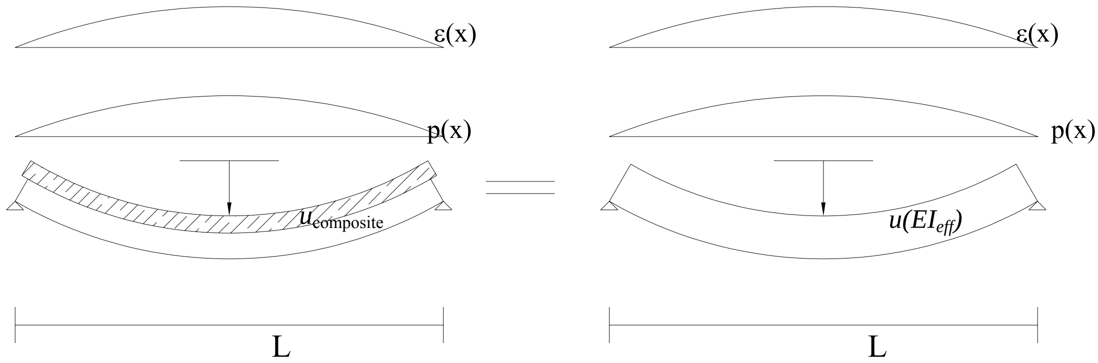

Fig. A.2: Comparison of the deflection for the evaluation of the effective bending stiffness

Then the value of the effective stiffness EI_{eff} which makes the curvatures of both beams equal is calculated. Final formulas similar to those suggested by the EC 5 Annex B are obtained. Such formulas are listed below.

- Fictitious vertical load equivalent to the inelastic strains

$$p_{sls} = C_{p,sls} \cdot \Delta\varepsilon \tag{A17}$$

where p_{sls} Fictitious vertical load, which represents the effects of inelastic strains on the structure

$C_{p,sls}$ Coefficient

$$= k_N \cdot \frac{E_1 \cdot A_1 \cdot E_2 \cdot A_2 \cdot z \cdot \gamma_1}{\left(E_1 \cdot A_1 + E_2 \cdot A_2\right) \cdot L^2} \tag{A18}$$

$\Delta\varepsilon$ Difference in the inelastic strain between the timber beam (sub. 2) and the concrete slab (sub. 1)

$$= \varepsilon_2 - \varepsilon_1 \tag{A19}$$

(A19) k_N $= \pi^2$

- Effective bending stiffness

$$EI_{eff,sls} = C_{I,sls} \cdot EI_{EC5\,Annex\,B} \tag{A20}$$

where $EI_{EC5\,Annex\,B}$ Effective bending stiffness according to EC5 Annex B

$C_{J,sls}$ Coefficient, which considers the interaction between vertical load q_d and inelastic strains in terms of slip in the joint

$$= \frac{p_{sls} + q_d}{\dfrac{E_1 \cdot A_1 + E_2 \cdot A_2}{\gamma_1 \cdot E_1 \cdot A_1 + E_2 \cdot A_2} \cdot p_{sls} + q_d} \tag{A21}$$

γ_1 Coefficient calculated according to EC5 Annex B

- Bending moment of the concrete slab (sub. 1) and timber beam (sub. 2): since the curvature is assumed to be the same in both beams, the bending moment can be calculated with

$$M_i = \frac{E_i \cdot I_i}{EI_{eff,sls}} \cdot M\left(q_d + 0.8 \cdot p_{sls}\right) \tag{A22}$$

where $EI_{eff,sls}$ — Effective bending stiffness according to EC5 Annex B which accounts for the interaction between vertical load and inelastic strains (see Eq. (18))

M_i — Bending moment of the i component

$M\left(q_d + 0.8 \cdot p_{sls}\right)$ — Resulting bending moment due to vertical load and part (80%) of the fictitious load equivalent to inelastic strains

- Axial forces: the axial forces are determined using the equilibrium equation:

$$N_i = \frac{M(q_d) - \sum_{i=1}^{2} M_i}{z} \tag{A23}$$

where $M\left(q_d\right)$ — Resulting bending moment due to vertical load only

z — Distance between the centroids of the concrete slab and timber beam

- Shear forces F in the connection due to
  - shrinkage of the concrete slab:

$$V_{\max,res} = -\pi \cdot E_2 \cdot A_2 \cdot \frac{E_1 \cdot I_1 + E_2 \cdot I_2}{\left(\gamma_1 \cdot E_1 \cdot A_1 + E_2 \cdot A_2\right) \cdot L \cdot a_2} \cdot \Delta\varepsilon + V(q_d) \tag{A24}$$

where $V\left(q_d\right)$ — Resulting shear force due to vertical load only, calculated using the formulas (Eq. B.10) suggested by the Annex B of the EC5.

  - shrinkage of the timber beam

$$F = \frac{K}{s_{eff}} \cdot L \cdot \left[\frac{M_{\max,2} \cdot z}{\pi \cdot E_2 \cdot I_2} - \frac{E_1 \cdot A_1 + E_2 \cdot A_2}{\pi \cdot E_1 \cdot A_1 \cdot E_2 \cdot A_2} \cdot N_{\max,2} - \frac{\Delta\varepsilon}{2}\right] \tag{A25}$$

The flowchart of the design procedure is displayed in Fig. A.3.

As visible, the proposed design approach is directly linked to the EC5 Annex B formulas where only some modifications such as the effective bending stiffness, the introduction of a new load case, and some changes in the way of calculating the internal forces are introduced.

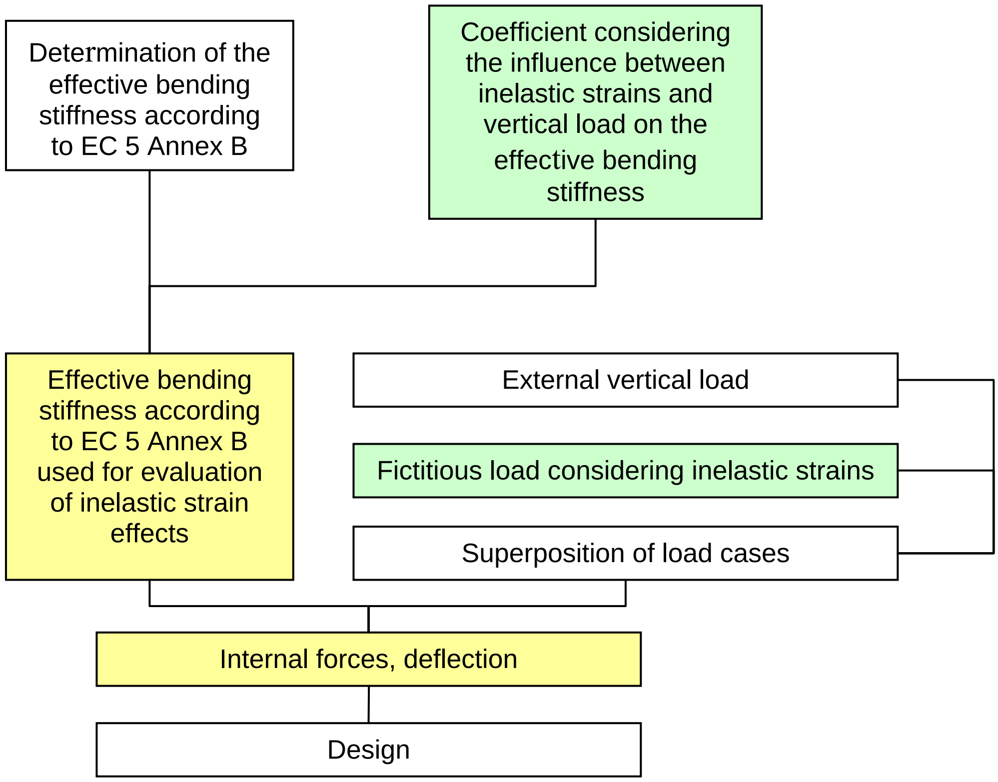

Fig. A.3: Design procedure based on transformation of the inelastic strains into a fictitious load (Schänzlin 2003)