



OPEN Thermo-mechanical response of layered perlite beams under transient thermal loading

Anna Szymczak-Graczyk¹, Jacek Korentz², Taras Markiv³ & Tomasz Garbowski⁴✉

This paper presents a combined numerical and experimental study on the transient thermo-mechanical response of layered perlite concrete beams subjected to unilateral heating. The motivation arises from the growing application of lightweight, thermally efficient materials in structural elements exposed to temperature gradients, where time-dependent thermal effects may significantly influence deformation behavior. An in-house computational framework is developed that explicitly couples transient heat transfer through the beam thickness with a shear-deformable Timoshenko beam model extended by an axial degree of freedom. The temperature field is reduced to section-level thermomechanical quantities, namely effective axial thermal strain and thermal curvature, which are introduced into the mechanical problem in a transparent and physically interpretable manner. Spatially nonuniform thermal loading along the beam length is accounted for through position-dependent boundary conditions. The numerical model is validated against laboratory experiments conducted on a layered concrete–perlite–concrete beam subjected to controlled top-surface heating up to 35 °C. Measured time-dependent midspan deflections show good agreement with numerical predictions, indicating that the model is able to capture delayed thermal response and thermally induced bending governed by transient heat conduction and material heterogeneity. In addition, a small illustrative nonlinear extension is presented in a purely numerical setting by introducing temperature-dependent stiffness degradation within the same framework. This additional example is not experimentally validated and is included solely to demonstrate the extensibility of the proposed formulation beyond the linear thermoelastic regime. The study should therefore be interpreted as a benchmark linear validation case, complemented by a concise numerical proof-of-concept for extensibility toward nonlinear thermo-mechanical analysis.

Keywords Perlite concrete, Thermo-mechanical behavior, Lightweight concrete beams, Transient heat transfer, Timoshenko beam model

The mechanical performance of layered building elements subjected to thermal gradients is an important issue in both structural safety and energy-efficient construction. Such gradients may arise not only in elevated-temperature scenarios, including fire-related exposure, but also under moderate service conditions such as solar heating, localized environmental exposure, or asymmetric thermal loading^{1–6}. Among lightweight cementitious materials, perlite-based composites have attracted increasing attention because of their low density, favorable thermal insulation properties, and environmental benefits^{7–12}. These features make perlite concrete attractive for building components in which thermal efficiency and reduced self-weight are desirable^{13–20}. At the same time, the heterogeneous and porous nature of perlite-based materials strongly affects heat transfer and may lead to delayed and spatially nonuniform thermo-mechanical response. A reliable prediction of this transient behavior is therefore important from both engineering and modeling perspectives^{21–31}.

The beneficial thermal behavior of perlite concrete is directly related to its highly porous microstructure, in which expanded perlite particles introduce a large volume of closed or partially closed air voids. This microstructural feature reduces density and thermal conductivity, making the material attractive for lightweight insulating components, roof panels, façade elements, and layered structural members exposed to nonuniform thermal actions.

¹Department of Construction and Geoengineering, Poznan University of Life Sciences, Poznań, Poland. ²Institute of Civil Engineering, University of Zielona Góra, Prof. Z. Szafrana 1, 65- 516 , Zielona Góra, Poland. ³Institute of Civil Engineering, Infrastructure and Life Safety, Lviv Polytechnic National University, Lviv, Ukraine. ⁴University Center for Eco-materials, Poznan University of Life Sciences, Poznań, Poland. ✉email: tomasz.garbowski@up.poznan.pl

Conventional approaches to modeling thermal effects in concrete beams typically rely either on empirical reduction factors for stiffness and strength or on fully coupled three-dimensional finite element simulations incorporating nonlinear constitutive laws and transient heat transfer^{32–34}. While empirical methods are computationally efficient, they are unable to capture spatial variations of temperature and material response within heterogeneous cross-sections. On the other hand, detailed three-dimensional models offer high fidelity but are computationally demanding and often impractical for parametric studies or engineering-oriented analyses. Consequently, there is a clear need for intermediate modeling strategies that balance physical realism with numerical efficiency.

Recent studies have further emphasized the importance of thermal gradients and transient effects in concrete and composite structural members^{35–39}. Ruano et al. analyzed the influence of thermal strains in slender prestressed concrete beams, while Nayeem and Billah investigated the effect of temperature distribution and thermal gradients on steel–concrete composite girders. Transient thermal fields in concrete box girders under climatic exposure were studied by Nassar and Amleh, and elevated-temperature or fire-related response of concrete beams has been addressed using advanced numerical models, including the work of Banerji and Kodur. In addition, nonlinear finite element analysis of layered concrete beams has recently been considered by Fares and Bakir. These contributions demonstrate the growing interest in thermally induced structural response, but they also confirm the need for reduced, physically interpretable models suitable for layered lightweight systems.

In this context, beam-based finite element formulations enhanced by the generalized nonlinear constitutive law (GNCL) have been proposed as an attractive alternative^{40–44}. Originally developed for the nonlinear analysis of composite and reinforced concrete beams, the GNCL approach allows complex material behavior to be incorporated without embedding a full constitutive model directly into the finite element solver. Instead, the effective stiffness of the structural element is iteratively updated based on the current deformation state, enabling the use of arbitrary stress–strain relationships derived from experimental data. This concept facilitates the representation of nonlinear phenomena such as stiffness degradation, plasticity, and temperature-dependent material response while retaining the computational simplicity of beam models^{45–48}.

Thermo-mechanical problems introduce an additional level of complexity, as non-uniform temperature fields generate both axial and bending effects through thermal expansion^{49–51}. In porous materials such as perlite concrete, these effects are further amplified by temperature-dependent stiffness, microcracking, and internal stress redistribution^{52–54}. Classical beam models with constant elastic properties are generally unable to reproduce this coupled response. GNCL-based formulations provide a conceptual framework for addressing these limitations by linking the evolving temperature field with deformation-dependent stiffness updates, thus enabling a consistent treatment of thermal and mechanical effects at the structural level^{37–39}. Importantly, such coupling can be achieved without resorting to fully user-defined constitutive models, preserving numerical robustness and implementation flexibility.

Against this background, the present study combines experimental observations with a computationally efficient numerical framework to analyze the thermo-mechanical response of layered perlite concrete beams. Laboratory tests were conducted to monitor transient deflections and surface temperatures under controlled unilateral heating, providing reference data for model validation. In parallel, a beam-level finite element model based on shear-deformable Timoshenko theory was developed to explicitly couple transient heat transfer through the beam thickness with thermally induced axial strain and bending curvature. While the present work focuses on a linear thermoelastic formulation, the adopted structure of the model is fully compatible with GNCL-based extensions aimed at capturing temperature-driven stiffness degradation and nonlinear material behavior^{55–57}.

The primary objective of this work is to demonstrate that a beam-based numerical model, informed by transient thermal analysis and validated against experimental observations, can accurately reproduce the time-dependent thermo-mechanical response of layered perlite concrete beams at a fraction of the computational cost required by three-dimensional simulations. Such an approach provides a practical and physically transparent tool for analyzing lightweight structural elements exposed to thermal gradients, particularly in applications involving transient thermal gradients under service or moderately elevated thermal conditions. Moreover, the open and modular character of the proposed framework establishes a solid foundation for future developments, including the incorporation of GNCL techniques to account for nonlinear degradation phenomena in perlite concrete and other porous building materials.

The contribution of this work is not the introduction of new governing equations, but the development of a transparent and modular thermo-mechanical framework that: (i) explicitly links transient temperature fields with section-level thermal resultants, (ii) enables direct physical interpretation of thermally induced axial strain and curvature, and (iii) provides a computationally efficient alternative to fully three-dimensional simulations.

The presented study should be understood as a controlled benchmark problem, designed to validate the coupling strategy under well-defined conditions and to establish a consistent basis for future extensions toward nonlinear thermo-mechanical behavior, including GNCL-based formulations.

To further support this claim of extensibility, the paper also includes a concise purely numerical example in which the linear constitutive description is replaced by a temperature-dependent stiffness update. This additional example is not intended as a validated nonlinear material model, but rather as a proof-of-concept showing how the proposed framework can be extended in a controlled and transparent manner beyond the linear thermoelastic benchmark.

The remainder of the paper is organized as follows. Section 2 presents the experimental background and the numerical methodology, including the transient heat transfer model and the beam formulation. Section 3 discusses the benchmark numerical and experimental results, followed by a concise illustrative nonlinear extension of the framework. Finally, the main conclusions are summarized.

Materials and methods

Geometry and layered beam configuration

Perlite concrete is a lightweight composite material obtained by replacing traditional aggregates with expanded perlite granules^{1,7–9,11,13,14}. Its microstructure is characterized by a high volume of closed air pores, resulting in low density and excellent thermal insulation, while maintaining moderate mechanical strength^{10,49,53,54}. Depending on the perlite–cement ratio and curing conditions, the compressive strength typically ranges between 4 and 12 MPa, with an apparent density of 600–1100 kg/m³^{53,54}.

The thermal conductivity of perlite concrete remains within 0.12–0.20 W/mK, which is significantly lower than that of normal-weight concrete^{15–17,24,28,50–54}. Owing to this combination of low density and favorable thermal performance, perlite concrete is particularly suitable for structural elements exposed to variable thermal conditions, such as external panels, roof decks, and lightweight beams in hybrid construction systems^{12,22}.

However, the porous nature of perlite aggregates combined with a cementitious matrix leads to pronounced temperature-dependent degradation of mechanical properties. This degradation, manifested primarily as a reduction in stiffness and load-bearing capacity under thermal exposure, must be explicitly accounted for in predictive numerical models intended to capture the thermo-mechanical response of perlite-based structural elements.

The investigated structure is a prismatic, layered beam of length L and width b , composed of n_l perfectly bonded layers forming a total height $H = \sum_{i=1}^{n_l} t_i$, where t_i denotes the thickness of the i -th layer. Each layer is characterized by its own set of thermal and mechanical properties, namely thermal conductivity k_i , mass density ρ_i , specific heat capacity $c_{p,i}$, Young's modulus E_i , shear modulus G_i , coefficient of thermal expansion α_i , and shear correction factor $\kappa_{s,i}$. Material behavior is assumed to be linear elastic at the present stage of the study, with temperature-independent parameters unless stated otherwise.

The beam is discretized independently in two orthogonal directions. Along the beam axis, a one-dimensional finite element mesh consisting of two-node beam elements is employed. Through thickness, a one-dimensional grid is introduced to resolve temperature gradients and layerwise thermal response. Layer properties are mapped onto the thickness grid as piecewise-constant functions, ensuring an accurate representation of the multilayered cross-section.

Experimental investigation of transient thermal deflection of a layered beam

To support numerical investigations and to provide a reference case for validation of the proposed thermo-mechanical framework, a laboratory experiment was conducted on a layered beam subjected to unilateral heating from the top surface. The experiment was designed to reproduce, in a controlled manner, the key physical mechanisms considered in the numerical model, namely transient heat transfer through a layered cross-section and the resulting time-dependent beam deflection.

As shown in Fig. 1, the tested beam had a total length of approximately 1024 mm, with a clear span of 1000 mm between the support axes. The beam was simply supported in a pin–roller configuration. Vertical displacement was restrained at both supports, while axial displacement was restrained at one support only and released at the other support. Rotations were not restrained. This support arrangement was adopted to avoid artificial axial thermal forces and to ensure that the observed response was governed primarily by thermally induced bending.

The cross-section of the beam was rectangular, with a width of 120 mm and a total height of 120 mm. The section consisted of three perfectly bonded layers, each 40 mm thick. The bottom and top layers were made of ordinary concrete, while the middle layer consisted of lightweight perlite concrete. This symmetric concrete–perlite–concrete configuration was selected to ensure mechanical symmetry while introducing strong heterogeneity in thermal properties, particularly thermal conductivity and heat capacity, which promotes the development of nonuniform temperature gradients through the thickness during heating.

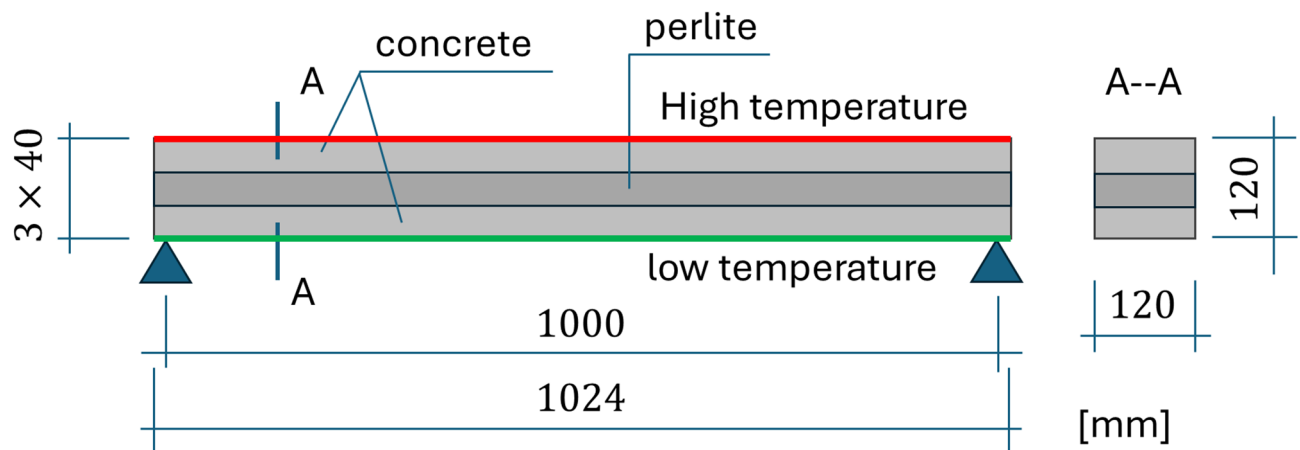


Fig. 1. Geometry and layered configuration of the tested beam subjected to unilateral heating from the top surface.

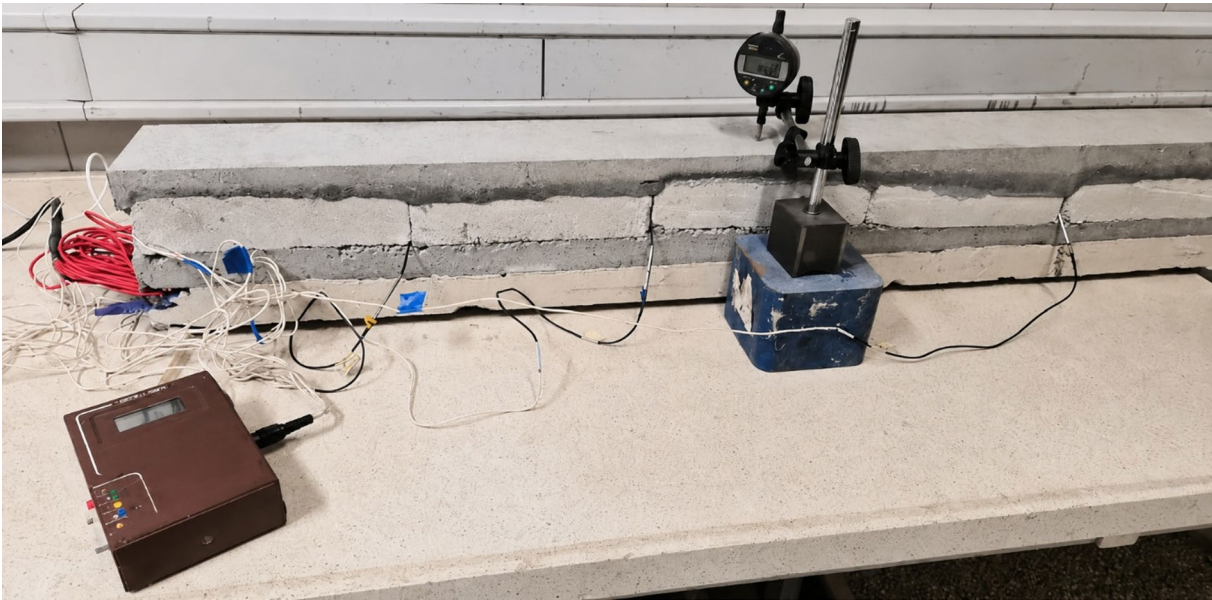


Fig. 2. Experimental setup.

Parameter	Ordinary concrete	Perlite concrete
Layer thickness [mm]	40 (top and bottom)	40 (middle)
Young's modulus E [GPa]	30–35	3–6
Density ρ [kg/m ³]	2300–2400	600–900
Thermal conductivity k [W/(m·K)]	1.6–1.9	0.15–0.25
Specific heat c_p [J/(kg·K)]	850–950	900–1100
Coefficient of thermal expansion α [1/K]	$(8–12)\times10^{-6}$	$(6–10)\times10^{-6}$
Shear modulus G [GPa]	12–14	1–2

Table 1. Geometric and material parameters of the layered beam used in the numerical example.

Thermal loading was applied unilaterally from the top surface using an electrically controlled heating mat placed in direct contact with the beam. The heating mat was operated to gradually raise the surface temperature to approximately 35 °C and then maintain this level for the duration of the test. The bottom surface of the beam was exposed to ambient laboratory air, resulting in a combined condition of top-surface heating and bottom-surface natural cooling, consistent with the unilateral heating assumption adopted in the numerical model. The ambient laboratory temperature remained approximately constant during the experiment.

The experiment focused on the transient phase of heating. The vertical deflection of the beam was monitored continuously as a function of time, with particular attention paid to the midspan displacement, which is most sensitive to thermally induced curvature. Deflection measurements were carried out using a displacement sensor positioned at the beam midspan, while temperature was monitored at selected locations on the top surface and within the laboratory environment to ensure repeatability of thermal conditions (see Fig. 2). No external mechanical loads were applied during the test; therefore, the observed deflections resulted solely from thermally induced bending.

The experimental setup allowed direct observation of the time-dependent evolution of beam deflection under a controlled thermal excitation. This provided a qualitative and quantitative benchmark for assessing the ability of the numerical model to capture both the magnitude and temporal characteristics of thermally induced deformation in layered structural elements. In particular, the experiment highlights the delayed mechanical response resulting from transient heat conduction through the thickness and the strong influence of material heterogeneity on thermal curvature development.

For numerical simulations, representative nominal values were selected from the ranges listed in Table 1 and used consistently throughout the benchmark analyses. The adopted values were: $E_c = 30$ GPa, $E_p = 5$ GPa, $k_c = 1.8$ W/(m·K), $k_p = 0.2$ W/(m·K), $\rho_c = 2350$ kg/m³, $\rho_p = 750$ kg/m³, $c_{p,c} = 900$ J/(kg · K), $c_{p,p} = 1000$ J/(kg·K), $\alpha_c = 10 \times 10^{-6}$ 1/K, $\alpha_p = 8 \times 10^{-6}$ 1/K, $G_c = 12.5$ GPa, and $G_p = 1.5$ GPa.

The essential material, geometric, and thermal parameters used in the experiment and numerical benchmark are summarized in Tables 1 and 2. The listed material properties correspond to nominal values representative of the employed concretes and were used as reference inputs in numerical simulations.

Global beam parameter	Value
Total length [mm]	1024
Support span [mm]	1000
Beam width [mm]	120
Total height [mm]	120
Maximum top surface temperature [°C]	≈ 35
Support conditions	Simply supported

Table 2. Global geometric and thermal parameters of the benchmark beam experiment.

Parameter	Symbol	Value
Total simulation time	$t_{\max}$	3000 s
Time step	Δt	5 s
Number of time steps	N_t	600
Beam length used in analysis	L	1.0 m
Number of beam elements	N_x	80
Beam element length	Δx	12.5 mm
Number of through-thickness nodes	N_z	121
Through-thickness grid spacing	Δz	1.0 mm
Reference / initial temperature	$T_{\text{ref}} = T_0$	20 °C
Top convection coefficient	h_{top}	25 W/(m ² ·K)
Bottom convection coefficient	h_{bottom}	8 W/(m ² ·K)
Bottom ambient temperature	$T_{\infty, \text{bottom}}$	20 °C
Top ambient temperature rise time constant	τ_h	450 s
Shear correction factor	κ_s	5/6

Table 3. Numerical parameters used in the benchmark simulations.

It should be emphasized that the experiment represents a single, controlled realization intended for qualitative and semi-quantitative validation of the proposed framework. No statistical repeatability analysis was performed. The primary objective is to verify the consistency of the coupled thermo-mechanical formulation under well-defined boundary conditions rather than to provide a comprehensive experimental characterization.

The main sources of experimental uncertainty were associated with the displacement measurement accuracy, temperature measurement accuracy, local variations in the heating mat–beam contact, and small fluctuations of laboratory ambient conditions. The uncertainty of the displacement measurement was small compared with the total measured midspan deflection amplitude. However, the effective thermal boundary condition at the heated surface may have had a more pronounced influence on the response, because small changes in contact quality directly affect the rate of heat transfer into the upper concrete layer. For this reason, the experimental comparison should be interpreted as a controlled benchmark validation of the dominant response trend and magnitude, rather than as a statistically complete validation campaign.

Transient heat transfer model with spatially nonuniform heating

Thermal analysis is performed by solving a transient heat conduction problem in the thickness direction, independently for each axial location of the beam. This modeling assumption reflects the physical situation in which dominant temperature gradients develop across the height, while heat conduction along the beam axis is of secondary importance compared to surface heat exchange. Such an approach provides an efficient and physically meaningful approximation for slender beams subjected to surface heating or cooling Tables 3, 4.

The benchmark thermal loading at the top surface was prescribed in the form

$$T_{\infty, \text{top}}(x, t) = 20 + \left[14 + \left(1 - \left(\frac{2x}{L} - 1 \right)^2 \right) \right] \left(1 - e^{-\frac{t}{450}} \right), \tag{1}$$

which yields a final top-surface ambient temperature of approximately 34 °C near the supports and 35 °C at midspan. The top boundary condition was treated as an effective convection-type condition, with the contact effect of the heating mat absorbed into the adopted value of h_{top} . The value $h_{\text{top}} = 25 \text{ W/(m}^2\text{K)}$ was not obtained from an independent calibration procedure or from a formal optimization against the complete deflection history. It was adopted as a physically plausible effective heat-transfer coefficient for the considered laboratory configuration and selected to reproduce the observed time scale of the surface heating. Therefore,

Parameter	Concrete layers	Perlite layer
Reference Young's modulus E_0 [GPa]	30	5
Reference shear modulus G_0 [GPa]	12.5	1.5
Degradation onset temperature T_1 [°C]	60	50
Upper temperature T_2 [°C]	120	100
Minimum modulus ratio $g_{\min}$ [-]	0.65	0.35
Degradation slope β [1/°C]	5.83×10^{-3}	1.30×10^{-2}

Table 4. Parameters used in the illustrative nonlinear constitutive extension.

it should be interpreted as an effective boundary parameter of the benchmark model rather than as a directly measured material or contact property.

The above parameters were used in all benchmark linear simulations unless stated otherwise. At each axial coordinate x , the temperature field $T(z, t; x)$ is governed by the one-dimensional transient diffusion equation with spatially varying coefficients

$$\rho(z) c_p(z) \frac{\partial T}{\partial t} = \frac{\partial}{\partial z} \left(k(z) \frac{\partial T}{\partial z} \right), \quad (2)$$

subject to unilateral or bilateral thermal boundary conditions. In the present formulation, unilateral heating is considered, where the upper surface ($z = H$) is subjected to convection

$$k \frac{\partial T}{\partial z} \Big|_{z=H} = h_{\text{top}}(x, t) (T(H, t) - T_{\infty, \text{top}}(x, t)), \quad (3)$$

while the lower surface ($z = 0$) is assumed insulated or convectively cooled, depending on the analyzed scenario.

Spatial nonuniformity of heating along the beam length is introduced by allowing the ambient temperature $T_{\infty, \text{top}}(x, t)$ and, optionally, the heat transfer coefficient $h_{\text{top}}(x, t)$ to vary with the axial coordinate. This enables the modeling of configurations such as intensified heating in the midspan region and reduced thermal exposure near the supports.

The governing Eq. (2) is discretized using a finite difference scheme in the thickness direction, with harmonic averaging of thermal conductivity at inter-nodal interfaces to ensure continuity of heat flux across material layers. Time integration is carried out using the Crank–Nicolson method, leading to the discrete system

$$\left(C - \frac{\Delta t}{2} A \right) T^{n+1} = \left(C + \frac{\Delta t}{2} A \right) T^n + \Delta t b(x, t), \quad (4)$$

where C is the heat-capacity matrix, A is the diffusion operator, and b represents contributions from convective boundary conditions.

The assumption of one-dimensional heat conduction through the thickness is justified by the characteristic geometric proportions of the beam ($L \gg H$) and the imposed boundary conditions. A simple time-scale estimate shows that thermal diffusion across the thickness dominates over axial conduction during the considered time interval. Therefore, axial heat conduction is neglected as a higher-order effect in the present formulation. Under the present geometric proportions, the characteristic diffusion time across the thickness is substantially shorter than the corresponding axial diffusion time, which supports the adopted dimensional reduction.

Thermomechanical section resultants

To couple the thermal solution with the mechanical beam model, the computed temperature field is reduced to thermomechanical quantities defined at the cross-section level. For each axial position and time instant, the elastic neutral axis of the layered cross-section is first determined using stiffness-weighted averaging

$$EA = \int E(z) dA, \quad z_{NA} = \frac{\int E(z) z dA}{\int E(z) dA}, \quad (5)$$

with $dA = b dz$. The bending stiffness then follows as

$$EI = \int E(z) (z - z_{NA})^2 dA. \quad (6)$$

Two thermal resultants are subsequently evaluated. The effective axial thermal strain is defined as

$$\epsilon_{0T}(x, t) = \frac{1}{EA} \int E(z) \alpha(z) (T(z, x, t) - T_{ref}) dA, \quad (7)$$

while the thermal bending moment about the neutral axis is given by

$$M_T(x, t) = \int E(z) \alpha(z) (T(z, x, t) - T_{ref}) (z - z_{NA}) dA. \quad (8)$$

The corresponding thermal curvature is obtained from

$$\kappa_T(x, t) = \frac{M_T(x, t)}{EI}. \quad (9)$$

These quantities provide a compact and physically transparent description of the thermal loading state of the beam, separating axial and bending effects induced by nonuniform temperature distributions through the cross-section.

Mechanical model: Timoshenko beam with axial degree of freedom

The mechanical response of the beam is described using a shear-deformable Timoshenko beam theory extended by an explicit axial displacement degree of freedom. Each node of the finite element mesh is associated with three degrees of freedom

$$\mathbf{d} = [u, w, \varphi]^T, \quad (10)$$

where u denotes the axial displacement, w the transverse deflection, and φ the rotation of the cross-section.

The kinematic measures are defined as

$$\epsilon_x = \frac{du}{dx}, \quad \gamma = \frac{dw}{dx} - \varphi, \quad \kappa = \frac{d\varphi}{dx}. \quad (11)$$

Effective sectional rigidities are obtained by thickness integration

$$EA = \int E dA, \quad EI = \int E(z) (z - z_{NA})^2 dA, \quad GA = \int \kappa_s(z) G(z) dA. \quad (12)$$

The element stiffness matrix consists of an axial (bar) contribution and a bending-shear contribution corresponding to the Timoshenko formulation. Global stiffness assembly follows standard finite element procedures.

Thermal effects are incorporated into the mechanical problem through imposed eigenstrains. The effective axial thermal strain $\epsilon_{0T}(x, t)$ and thermal curvature $\kappa_T(x, t)$, defined by Eqs. (7) and (9), modify the constitutive relations as

$$N = EA(\epsilon_x - \epsilon_{0T}), \quad M = EI(\kappa - \kappa_T). \quad (13)$$

In the finite element formulation, these relations are introduced via consistent equivalent nodal loads. For each beam element, axial forces of magnitude $\pm EA \epsilon_{0T}$ and nodal bending moments of magnitude $\pm EI \kappa_T$ are applied at the element ends with opposite signs. In the present implementation, ϵ_{0T} and κ_T are interpolated along the element using nodal averaging.

The equilibrium problem at each time step takes the form

$$K q(t) = f_T(t), \quad (14)$$

subject to prescribed mechanical boundary conditions. In the benchmark simulations, the mechanical boundary conditions reproduced the experimental pin-roller support configuration. The transverse displacement was constrained at both beam ends, the axial displacement was constrained at one end only, and rotations were left free at both supports. Thus, no intentional axial restraint was introduced in the model.

Explicit implementation and relation to standard FEM workflows

The procedure described is implemented in an in-house MATLAB environment that provides full transparency and modular control over all stages of the thermo-mechanical analysis, including heat transfer, cross-sectional homogenization, and structural response. This explicit formulation allows direct access to intermediate quantities such as the neutral axis position, thermal bending moment, effective axial thermal strain, and local thermal curvature, which play a key role in understanding and extending the model.

For comparison, standard commercial finite element environments treat thermo-mechanical coupling through built-in thermal strain definitions of the form

$$\epsilon^{th} = \alpha(T - T_{ref})1, \quad (15)$$

which are internally subtracted from the total strain field at the integration-point level. For shear-deformable beam elements, the solver implicitly integrates the temperature field over the cross-section and decomposes its effect into axial and bending contributions. Consequently, the mechanical equilibrium is solved in the implicit form

$$\mathbf{K}(T) \mathbf{q} = \mathbf{f}_{ext}, \quad (16)$$

where thermal effects are embedded in the internal force vector and, when applicable, in temperature-dependent stiffness matrices. While this approach is robust and efficient for standard thermoelastic analyses, it does not expose section-level thermomechanical quantities such as ϵ_{0T} and κ_T to the user.

In contrast, the present framework introduces thermal effects explicitly through Eqs. (7)–(9) and their mechanical counterparts in Eq. (13). This explicit separation between thermal driving mechanisms and structural response provides enhanced interpretability and facilitates systematic verification and modification of individual

model components. This explicit structure is particularly convenient when the constitutive description is later generalized beyond the linear thermoelastic regime.

Illustrative nonlinear constitutive extension

To demonstrate the extensibility of the proposed framework, a simple nonlinear constitutive extension is considered in a purely numerical setting. The extension is not intended as a calibrated material model and is not validated against experimental data. Its sole purpose is to illustrate how temperature-dependent stiffness degradation can be incorporated without changing the overall solution structure.

The onset and upper temperatures used in the piecewise-linear degradation functions were assumed solely for the purpose of this numerical demonstration. They were not calibrated against dedicated high-temperature tests on the investigated perlite concrete and should not be interpreted as normative or generally applicable material parameters.

In the present proof-of-concept, the elastic and shear moduli are updated as temperature-dependent quantities,

$$E(z, t) = E_0(z) g_E(T(z, t)), \quad (17a)$$

$$G(z, t) = G_0(z) g_G(T(z, t)), \quad (17b)$$

where E_0 and G_0 denote the reference moduli used in the benchmark linear model, and $g_E(T)$, $g_G(T)$ are degradation functions satisfying $g_E(T_{\text{ref}}) = g_G(T_{\text{ref}}) = 1$.

For simplicity, piecewise-linear degradation laws are adopted:

$$g_E(T) = \begin{cases} 1, & T \leq T_1, \\ 1 - \beta_E(T - T_1), & T_1 < T < T_2, \\ g_{E,\min}, & T \geq T_2 \end{cases} \quad (18)$$

and analogously for $g_G(T)$. In the illustrative examples presented later, the same degradation pattern is applied to both E and G , with material-specific coefficients assigned separately to the concrete and perlite layers.

At each time step, the transient temperature field is first computed from the heat-transfer problem. The temperature-dependent moduli are then updated through the above relations, after which the effective sectional quantities EA , EI , and GA are re-evaluated and the mechanical equilibrium problem is solved. No additional internal variables, damage evolution laws, or path-dependent effects are introduced in this study.

Consequently, phenomena such as transient thermal creep, temperature-dependent thermal expansion, moisture migration, pore-pressure effects, and thermally induced microcracking are not explicitly represented in the present illustrative extension.

This nonlinear extension should therefore be interpreted as GNCL-inspired rather than as a full GNCL implementation. It serves as a compact proof-of-concept showing that the framework can accommodate constitutive updates driven directly by the transient thermal field.

For the illustrative nonlinear case, the same geometry, discretization, and boundary conditions as in the benchmark model were retained, while the maximum imposed top-surface ambient temperature was increased. The adopted thermal loading was

$$T_{\infty, \text{top}}^{\text{NL}}(x, t) = 20 + \left[90 + 10 \left(1 - \left(\frac{2x}{L} - 1 \right)^2 \right) \right] \left(1 - e^{-\frac{t}{600}} \right) \quad (19)$$

which yields a final top-surface ambient temperature of approximately 110 °C near the supports and 120 °C at midspan. These values were selected solely for illustrative numerical purposes and do not constitute a calibrated constitutive description.

Results and discussion

Transient temperature evolution through the layered cross-section

The first group of results concerns the transient development of temperature within the layered beam subjected to unilateral heating from the top surface. Figure 3 presents representative temperature profiles through the beam thickness at selected time instants during the heating phase. The profiles clearly demonstrate the strongly nonuniform temperature distribution induced by the heterogeneous thermal properties of the layered cross-section.

At early stages of heating, the temperature rise is confined almost exclusively to the upper concrete layer, reflecting its direct exposure to the heating mat. The perlite layer acts as a thermal barrier due to its significantly lower thermal conductivity, resulting in a pronounced temperature gradient at the concrete–perlite interface. As time progresses, heat gradually penetrates the perlite core and reaches the lower concrete layer, although the temperature there remains substantially lower than at the top surface throughout the experiment.

This behavior is consistent with the adopted modeling assumption that the dominant temperature gradients develop through the beam thickness rather than along the beam axis. It also supports the qualitative interpretation of the proposed thermo-mechanical framework and highlights the crucial role of material heterogeneity in governing the transient thermal response. The presence of the perlite layer significantly delays thermal equilibration across the cross-section, which in turn has a direct impact on the time-dependent mechanical response discussed in subsequent sections. The transient response can be interpreted in terms of a characteristic thermal diffusion time scale $\tau \approx H^2/\alpha$, where $\alpha = k/(\rho c_p)$. For the present configuration, this time scale

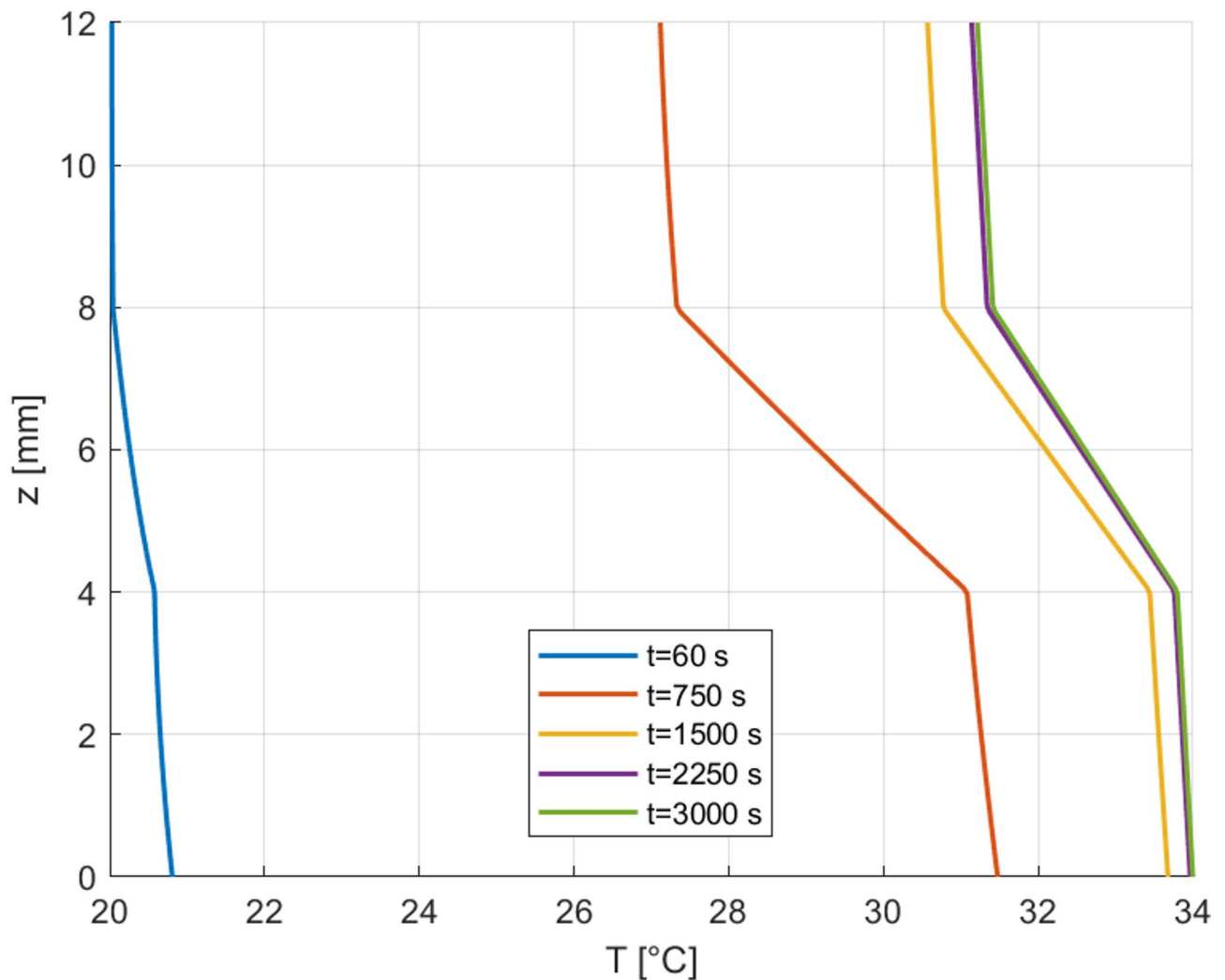


Fig. 3. Transient temperature distributions through the beam thickness at selected time instants during heating.

is of the same order as the duration of the experiment, which explains the delayed development of thermal curvature and deflection.

Verification of the transient thermal response

To provide an additional check of the thermal part of the model, the simulated top-surface temperature history was compared with the temperature recorded during the experiment at the monitored surface location. The comparison is shown in Fig. 4. The numerical curve reproduces the main heating trend and the characteristic time scale of the measured temperature increase. Small differences may be attributed to local variations in the contact between the heating mat and the beam surface, as well as to unavoidable spatial nonuniformity of the laboratory heating conditions.

This comparison should be regarded as an intermediate verification of the imposed effective thermal boundary condition. Since the available temperature measurements were limited to selected surface locations, they were not sufficient to reconstruct the complete temperature field through the beam thickness. Nevertheless, they confirm that the adopted thermal model provides a reasonable representation of the transient heating process used as input for the subsequent thermo-mechanical analysis.

Spatial nonuniformity of thermal loading along the beam length

The imposed heating conditions were intentionally nonuniform along the beam length, with increased thermal exposure in the central region. Figure 5 illustrates the spatial distribution of the prescribed ambient temperature at the top surface and the resulting surface temperature response of the beam.

The results indicate that the spatial variation of thermal loading is effectively transferred into the temperature field within the beam, leading to axial variations in both the magnitude and rate of temperature increase. This axial nonuniformity plays a key role in the development of nonuniform thermal curvature $\kappa_T(x, t)$, which cannot be captured by models assuming uniform heating along the beam length.

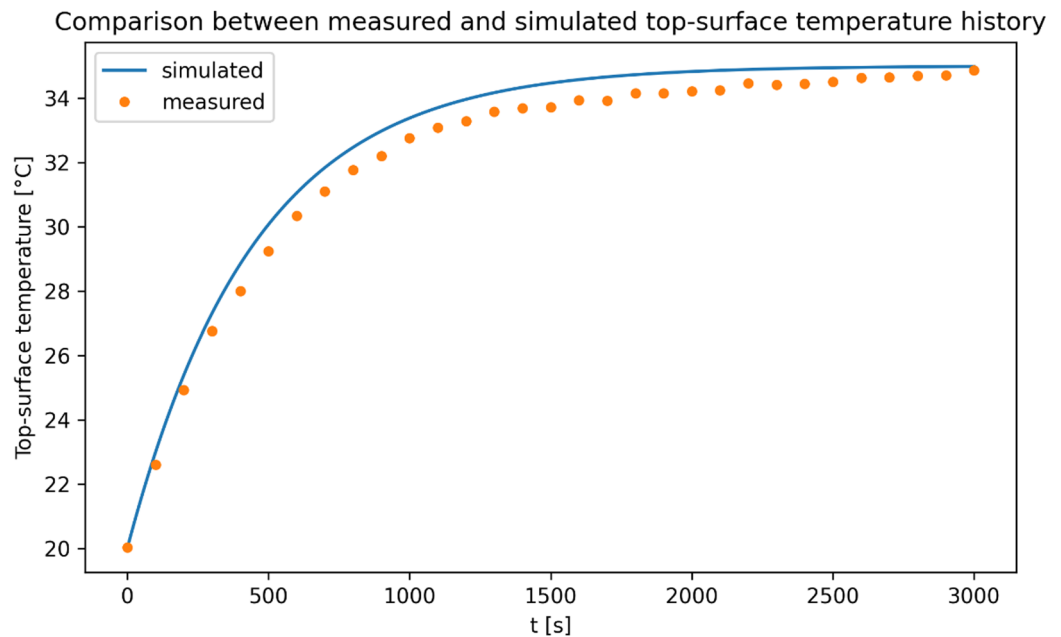


Fig. 4. Comparison between measured and simulated top-surface temperature history at the monitored location.

Development of thermal curvature and axial thermal strain

The reduction of the temperature field to section-level thermomechanical quantities allows direct insight into the mechanisms driving the structural response. Figure 6 presents the evolution of the thermal curvature $\kappa_T(x, t)$ along the beam length at selected time instants.

The results reveal a strongly time-dependent curvature field, with maximum values developing in the central region of the beam, where thermal exposure is highest. At early times, curvature is concentrated near the top layers, resulting in relatively large bending effects even for modest surface temperature increases. As heat diffuses through the cross-section, the curvature gradually redistributes and its rate of increase decreases. In parallel, the effective axial thermal strain $\epsilon_{0T}(x, t)$ evolves as shown in Fig. 7.

Although axial thermal strains are present, their contribution to the observed deflection remains secondary in the considered configuration due to the absence of axial restraint. Nevertheless, their explicit evaluation is essential for future analyses involving partially or fully restrained boundary conditions, where axial thermal forces may significantly influence the global response.

Numerical prediction of transient beam deflection

The combined effect of thermal curvature and boundary conditions leads to a time-dependent deflection of the beam. Figure 8 shows the numerically predicted deflection shapes at selected times during the heating process.

The deflection profiles exhibit smooth shapes characteristic of thermally induced bending without external mechanical loading. The maximum deflection occurs at midspan and increases monotonically with time during the heating phase. Notably, the rate of deflection increase is initially high and gradually decreases, reflecting the transient nature of heat conduction through the layered cross-section. Figure 8 presents the time history of midspan deflection obtained from the numerical model Fig. 9.

The curve shows a characteristic nonlinear time dependence, with an initially steep increase followed by gradual flattening as thermal gradients across the cross-section diminish. This behavior underscores the importance of transient analysis; steady-state assumptions would significantly overestimate early-time deflections and underestimate the delay introduced by thermal inertia.

Experimental observations and comparison with numerical results

The numerical model reproduces the qualitative trend and the correct order of magnitude of the measured deflection. In particular, the delayed onset of significant deformation and the subsequent gradual evolution are well captured. The experimental response remains within or close to the predicted sensitivity envelope for most of the heating process. This indicates that the remaining differences between the nominal prediction and the measured response may reasonably be attributed to material-property variability, imperfect thermal contact between the heating mat and the beam surface, and unavoidable heat losses to the surrounding air.

It should be emphasized that this envelope represents parametric uncertainty in the numerical model and does not replace a statistical repeatability analysis based on multiple experimental realizations.

Overall, the agreement indicates that the proposed modeling framework is able to reproduce the dominant physical mechanisms governing transient thermally induced deformation in layered beams.

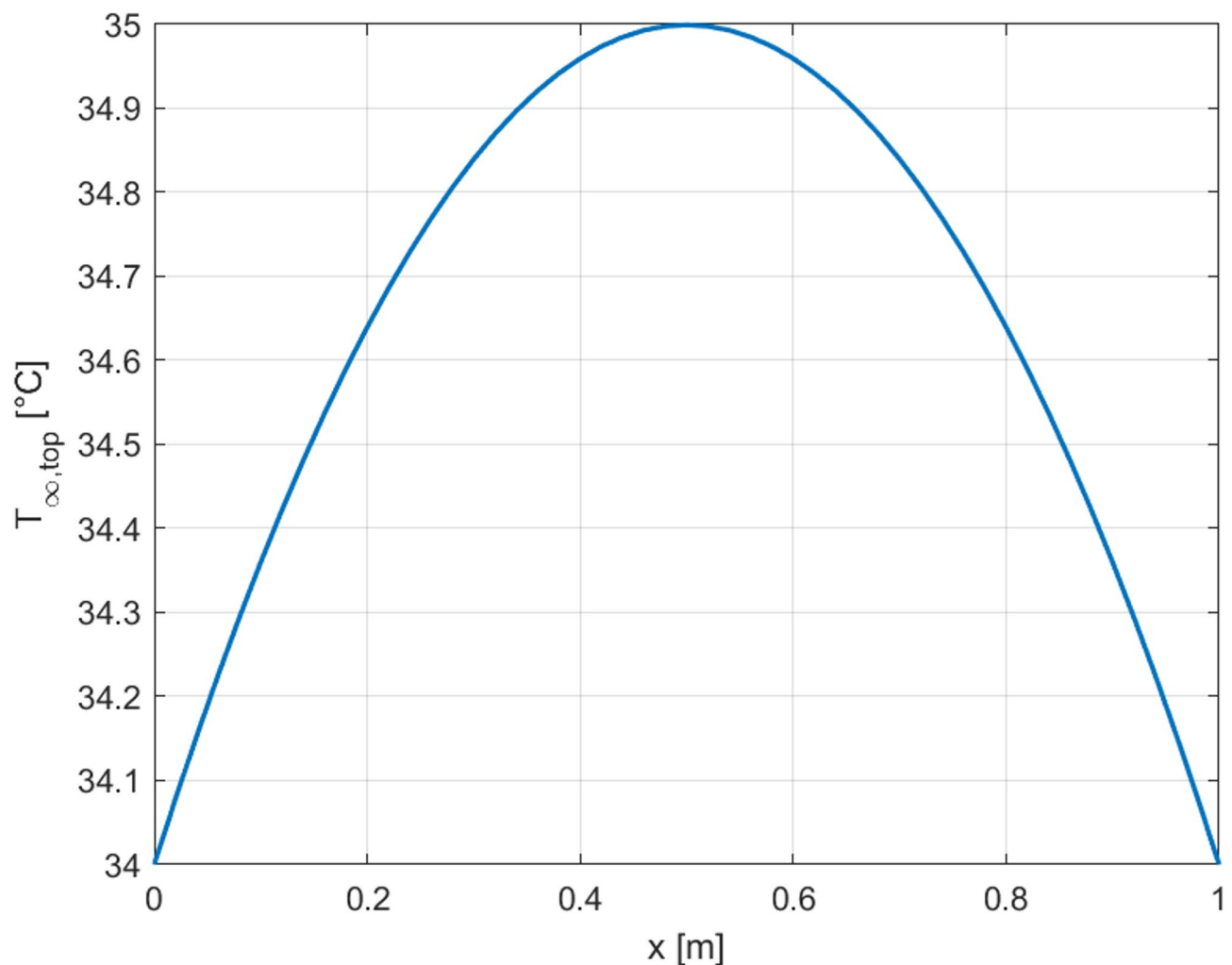


Fig. 5. Spatial distribution of top-surface thermal loading at final step and resulting surface temperature along the beam length.

Figure 10 compares the experimentally measured midspan deflection with the numerical prediction. In addition to the nominal simulation, a sensitivity envelope is shown, obtained by varying the key perlite-layer properties within the representative ranges reported in Table 1. The varied parameters included the Young's modulus and thermal conductivity of the perlite concrete, which directly influence the sectional stiffness and the development of transient thermal gradients.

Sensitivity to material heterogeneity and implications for layered design

The following sensitivity study is purely numerical and should be interpreted as model-based prediction of trends rather than experimentally validated relationships.

To further assess the influence of material heterogeneity, a parametric study was performed by varying the thermal conductivity and Young's modulus of the perlite layer within representative ranges. The results are summarized in Table 5.

The results indicate that thermal conductivity has a stronger influence on transient deflection than elastic stiffness, particularly during early stages of heating. This highlights the importance of considering coupled thermal and mechanical effects when designing layered structural elements exposed to thermal gradients.

To complement the maximum-deflection values summarized in Table 5; Fig. 11 shows the corresponding time histories of the midspan deflection. The curves demonstrate that the material parameters affect not only the final deflection amplitude but also the rate at which the thermo-mechanical response develops. Variations in thermal conductivity are especially visible during the transient phase, because they directly modify the rate of heat penetration through the perlite layer and, consequently, the evolution of thermal curvature.

Illustrative nonlinear thermo-mechanical extension

To complement the benchmark linear validation case, an additional purely numerical example was carried out using the temperature-dependent constitutive update introduced in Sect. 2.7. This example is not experimentally

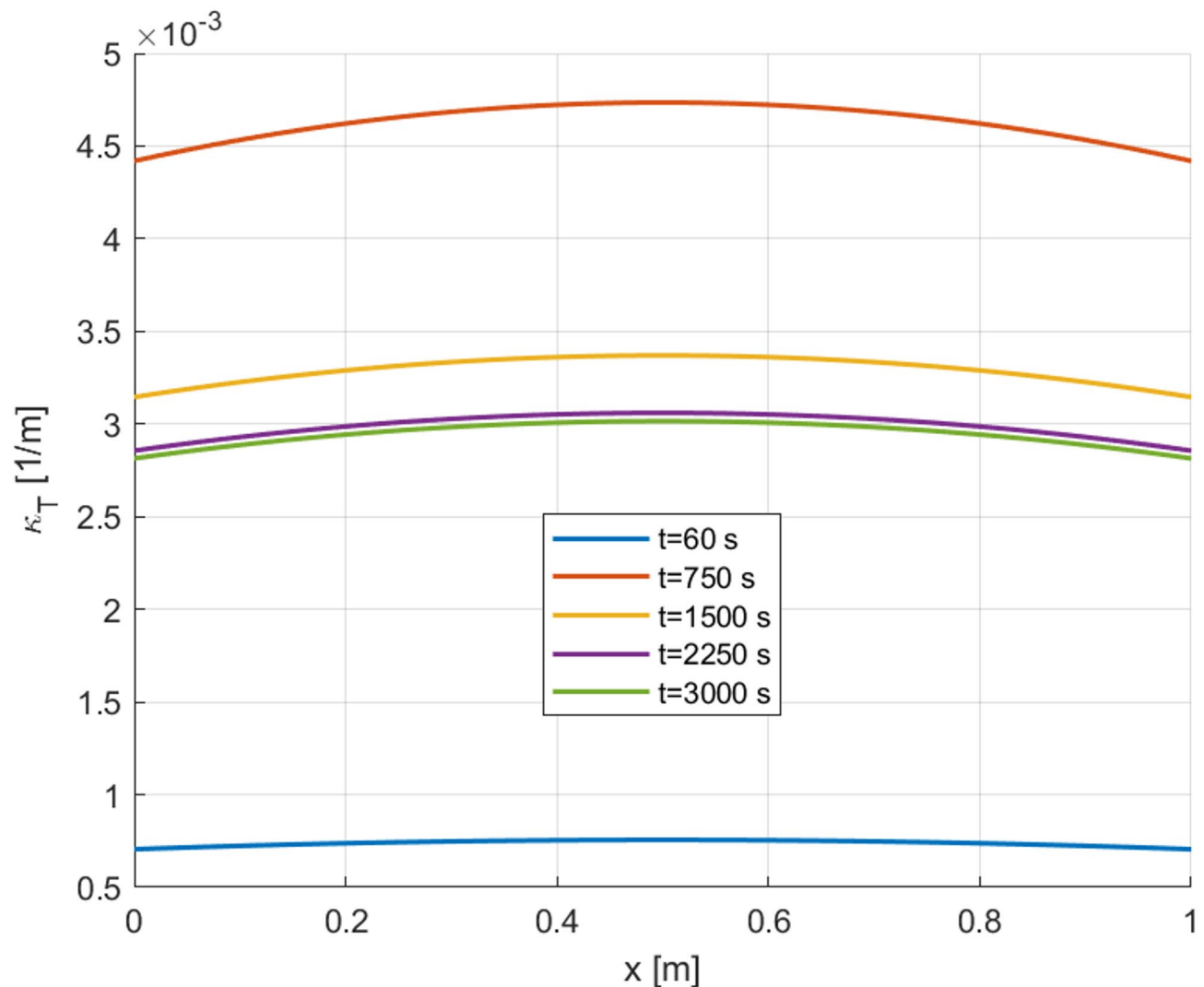


Fig. 6. Thermal curvature distribution along the beam length at selected times.

validated and should be interpreted only as a proof-of-concept illustrating the extensibility of the proposed framework.

In this additional analysis, the same beam geometry and boundary conditions were retained, while the imposed thermal loading was increased beyond the benchmark range in order to activate the adopted stiffness degradation laws. Two cases were considered: a reference linear case and a nonlinear temperature-dependent case.

In the illustrative nonlinear example, the maximum imposed top-surface temperature was increased to 120 °C solely to activate the assumed temperature-dependent degradation functions. This temperature level was not part of the experimental benchmark and was used only to demonstrate how the proposed framework can accommodate temperature-driven stiffness updates.

Figure 12 compares the time evolution of midspan deflection for both variants. At low and moderate temperature levels, the two curves remain relatively close, reflecting the dominance of geometry and transient heat-transfer effects. As the temperature increases and the degradation functions become active, the nonlinear response departs from the linear prediction, leading to larger deflections and a modified time evolution of the structural response.

Figure 13 presents the evolution of the normalized effective bending stiffness EI/EI_0 in the nonlinear case. The results show that the transient thermal field not only generates thermal curvature, but also progressively modifies the sectional stiffness, thereby amplifying the global deformation response.

Therefore, the nonlinear example should be viewed as a demonstration of the computational architecture rather than as a validated prediction of heated perlite concrete behavior. These results show that the present framework can be extended from a benchmark linear thermoelastic model to a temperature-driven nonlinear thermo-mechanical setting without changing its overall computational architecture. A calibrated nonlinear model would require dedicated high-temperature mechanical tests and an extended constitutive description including additional temperature-dependent and time-dependent effects.

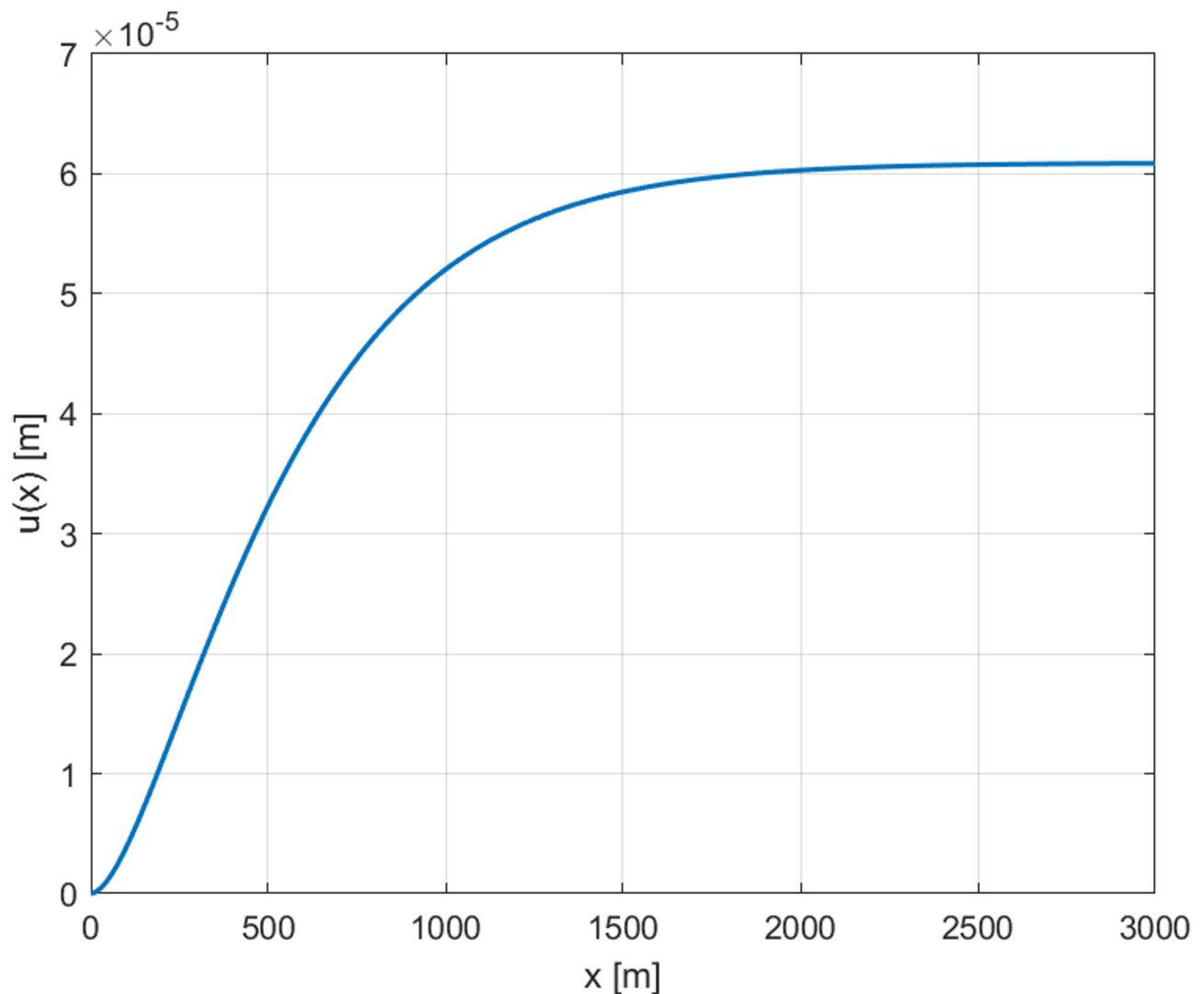


Fig. 7. Effective axial thermal strain $\epsilon_{0T}(x, t)$ along the beam length.

Conclusions

This study presented a combined numerical–experimental investigation of the transient thermo-mechanical response of a layered concrete–perlite–concrete beam subjected to unilateral heating. The main part of the work was intentionally formulated as a benchmark linear thermoelastic problem, in which transient heat transfer through the beam thickness was explicitly coupled with a shear-deformable Timoshenko beam model with an axial degree of freedom.

The proposed reduction of the temperature field to section-level thermomechanical quantities, namely the effective axial thermal strain and thermal curvature, proved useful both from the computational and interpretative point of view. It enabled a transparent representation of the mechanisms driving the delayed deflection response, in particular the role of transient heat conduction and material heterogeneity across the layered cross-section.

The experimental results obtained for the moderate-temperature benchmark case showed qualitative and semi-quantitative agreement with the numerical predictions, indicating that the framework is able to reproduce the dominant features of transient thermally induced bending in the tested configuration.

The validation should be interpreted in light of the fact that only one experimental realization was available; therefore, the comparison confirms the dominant response mechanisms and order of magnitude, but does not provide a full statistical assessment of experimental repeatability.

To complement this benchmark validation, a concise purely numerical nonlinear extension was also presented. By introducing temperature-dependent updates of the elastic and shear moduli, it was shown that the same computational structure can be extended toward a nonlinear thermo-mechanical setting without reformulating the overall analysis workflow. This additional example was included only as a proof-of-concept and was not intended as a validated constitutive model.

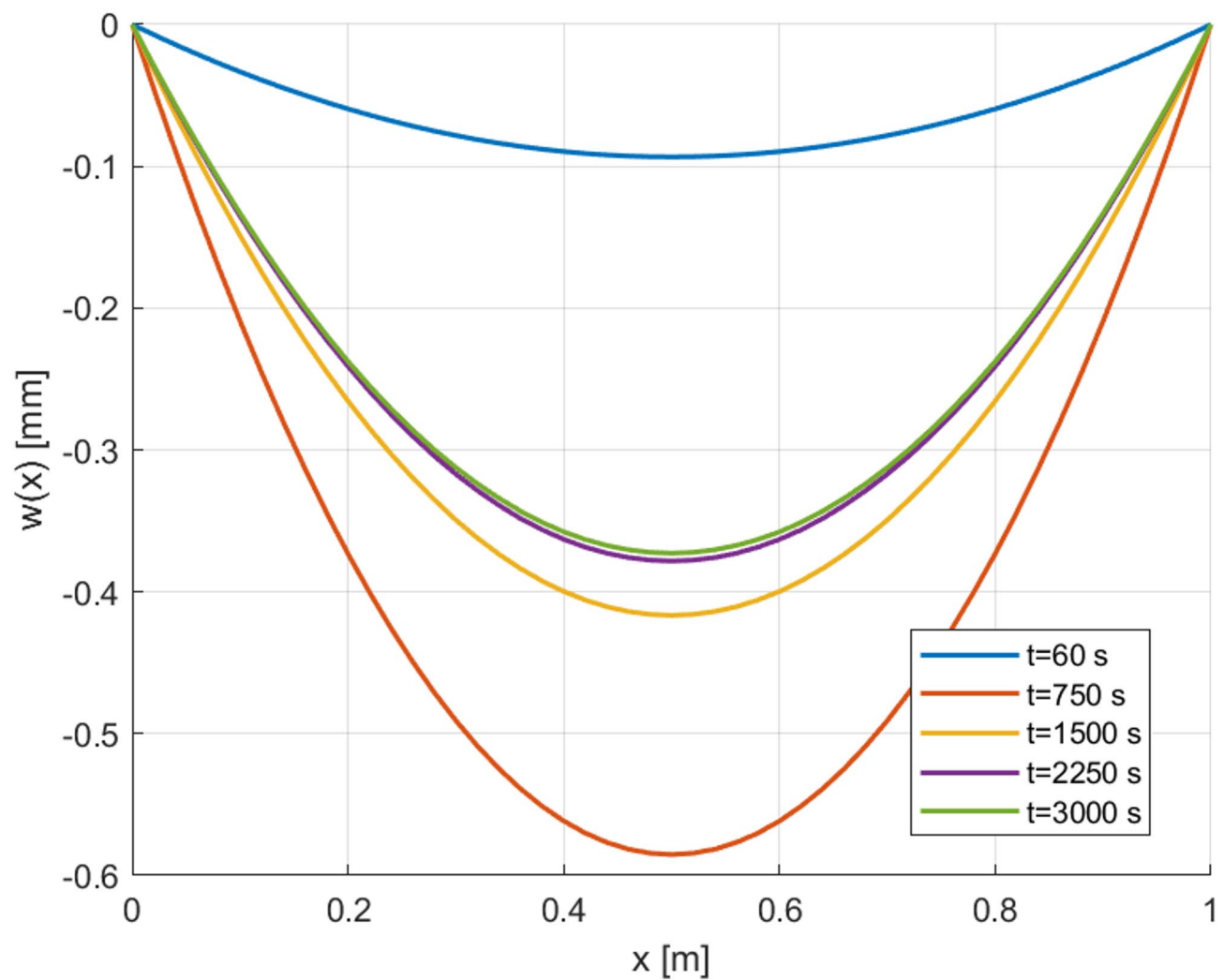


Fig. 8. Numerically predicted deflection shapes $w(x)$ at selected time instants.

Further development of the nonlinear extension will require dedicated temperature-dependent material characterization, including stiffness degradation, thermal expansion, creep-related effects, and possible moisture-driven mechanisms.

Overall, the paper provides a transparent benchmark formulation together with a compact demonstration of extensibility beyond the linear regime, offering both a physically interpretable reference solution and a practical basis for future GNCL-type developments.

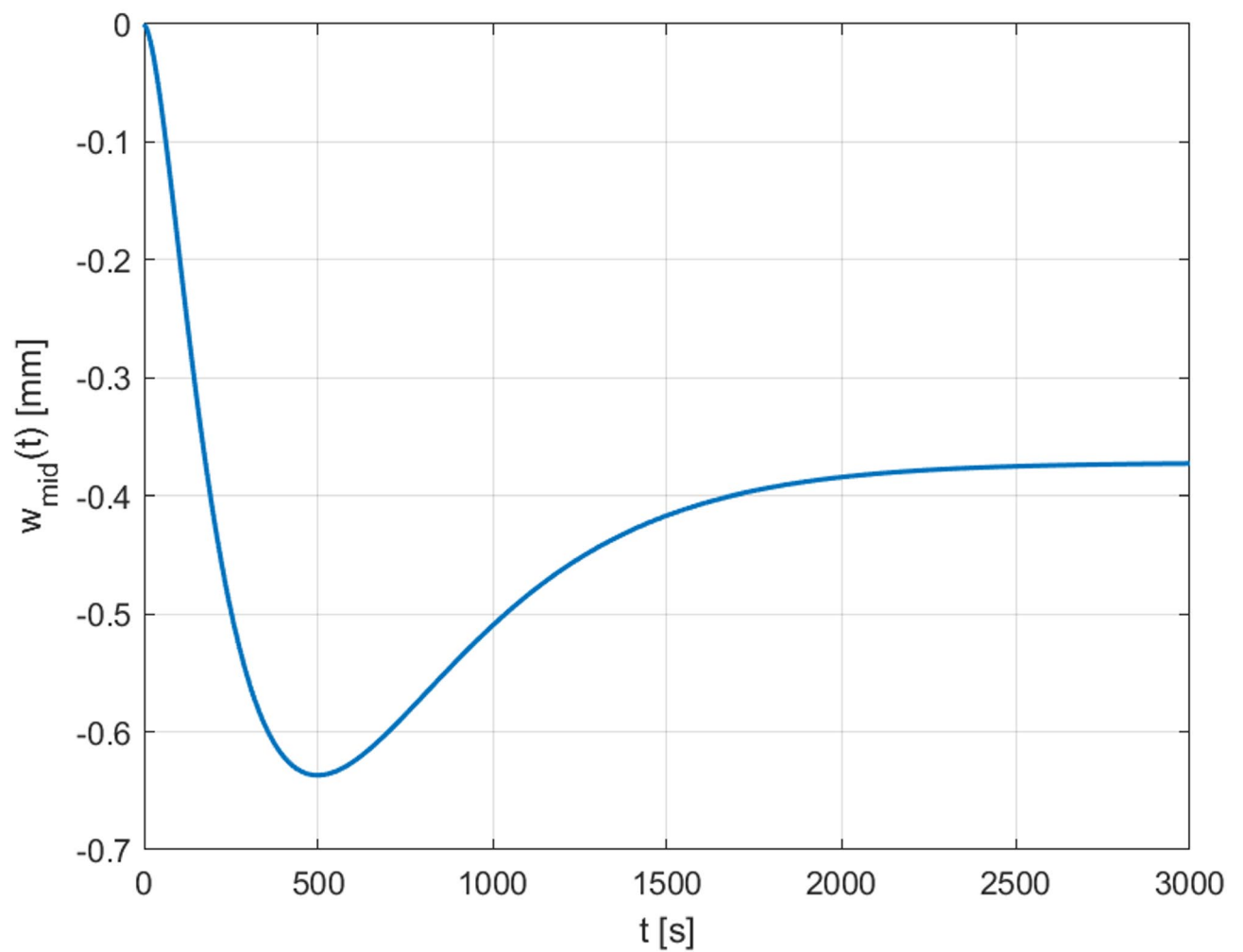


Fig. 9. Time evolution of midspan deflection obtained from numerical simulations.

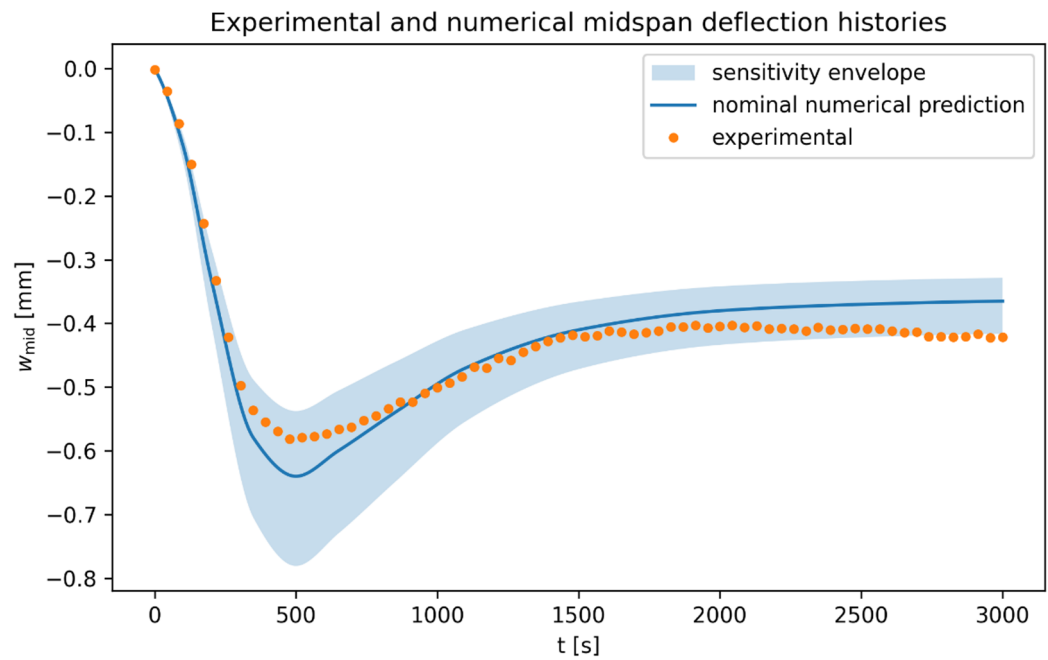


Fig. 10. Comparison of experimental and numerical midspan deflection histories, including the nominal prediction and the sensitivity envelope obtained by varying selected perlite-layer properties within the ranges reported in Table 1.

Parameter variation	Change in max deflection
$k_p + 50\%$	-18%
$k_p - 50\%$	+27%
$E_p + 50\%$	-12%
$E_p - 50\%$	+19%

Table 5. Sensitivity of maximum midspan deflection to perlite layer properties.

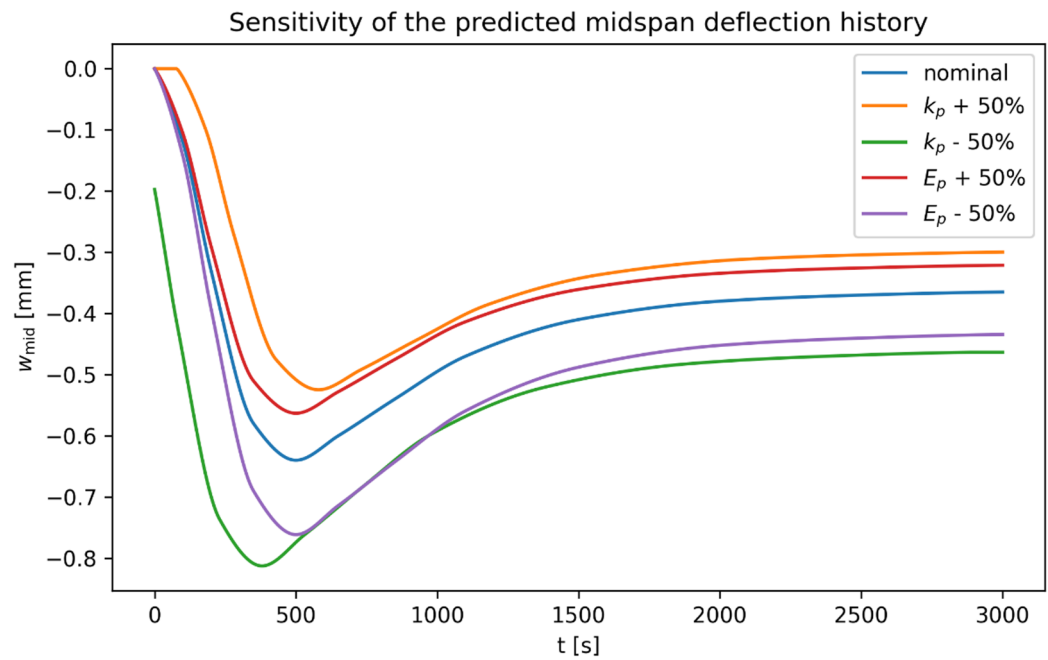


Fig. 11. Sensitivity of the predicted midspan deflection history to selected perlite-layer properties.

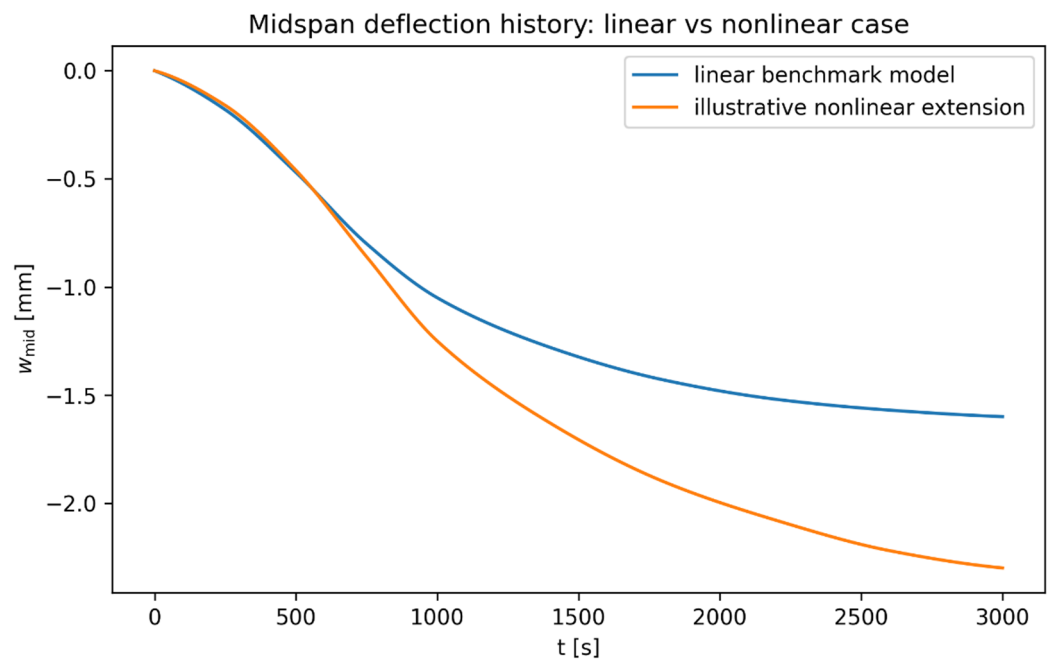


Fig. 12. Comparison of the predicted midspan deflection history for the linear benchmark model and the illustrative nonlinear temperature-dependent extension. The nonlinear case is included only as a numerical proof-of-concept.

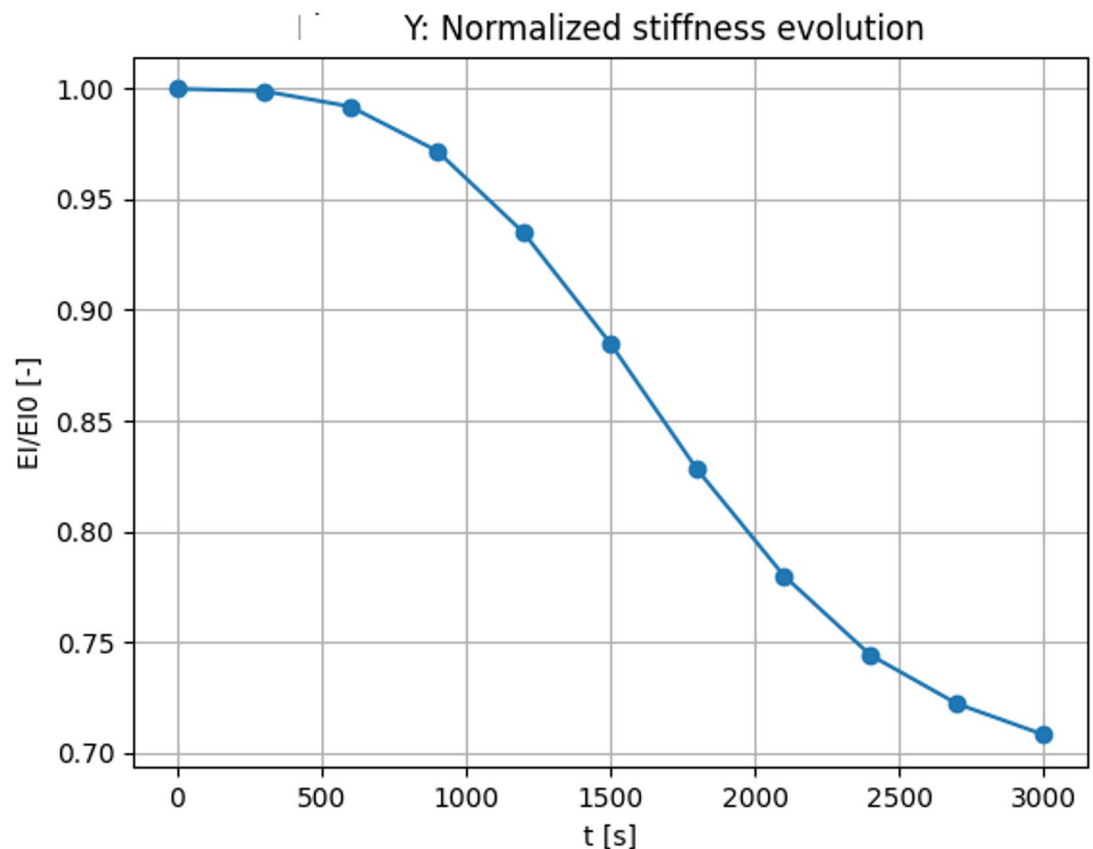


Fig. 13. Time evolution of the normalized effective bending stiffness EI/EI_0 in the illustrative nonlinear case.

Data availability

The benchmark experimental time histories, the numerical input data used in the benchmark simulations, and the data generated for the illustrative nonlinear example are available from the corresponding author upon reasonable request.

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Author contributions

Authors' contributions: Conceptualization: Anna Szymczak-Graczyk, Tomasz Garbowski; Methodology: Anna Szymczak-Graczyk, Tomasz Garbowski; Formal analysis and investigation: Jacek Korentz, Tomasz Garbowski, Taras Markiv; Writing - original draft preparation: Anna Szymczak-Graczyk, Tomasz Garbowski; Writing - review and editing: Jacek Korentz, Anna Szymczak-Graczyk; Tomasz Garbowski, Taras Markiv; Funding acquisition: Jacek Korentz; Resources: Jacek Korentz, Taras Markiv; Project administration: Jacek Korentz, Anna Szymczak-Graczyk; Supervision: Tomasz Garbowski.

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Declarations

Competing interests

The authors declare no competing interests.

Ethics approval and consent to participate

Not applicable. The study does not involve human participants or animals.

Consent for publication

Not applicable.

Additional information

Correspondence and requests for materials should be addressed to T.G.

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