

Effect of damage on permeability and hygro-thermal behaviour of HPCs at elevated temperatures: Part 2. Numerical analysis

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Abstract. In the Part 1 paper (Gawin, *et al.* 2005) some experimental results concerning micro-structural tests, permeability measurements and stress-strain tests of four types of High Performance Concrete, exposed to elevated temperatures (up to 700°C) are presented and discussed. On the basis of these experimental results parameters of the constitutive relationships describing influence of damage and temperature upon material intrinsic permeability at high temperature were determined. In this paper the effects of various formulations of damage-permeability coupling on results of computer simulations are analysed and compared with the results obtained by means of the previously proposed approach, that does not take into account the thermo-chemical concrete damage directly. Numerical solutions are obtained using the recently developed fully coupled model of hygro-thermal and damage phenomena in concrete at elevated temperatures. High temperature effects are considered by means of temperature and pressure dependence of several material parameters. Based on the mathematical model, the computer code HITECOSP was developed. Material parameters of the model were measured by several European laboratories, which participated in the "HITECO" research project. A model problem, concerning hygro-thermal behaviour and degradation of a HPC structure during fire, is solved. The influence of two different constitutive descriptions of the concrete permeability changes at high temperature, including thermo-chemical and mechanical damage effects, upon the results of computer simulations is analysed and discussed.

Keywords: high-performance concrete; temperature; permeability; thermo-chemical degradation; finite element analysis.

1. Introduction

Many researchers studied problems related to high temperature behaviour of concrete structures, e.g., Bazant (1988) and Bazant, *et al.* (1996, 1978, 1979). The mathematical model proposed by

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Bazant and Thonguthai (1978, 1979) and its modifications are widely used for this purpose. However that model has some limitations, e.g., phase changes of water are not considered because moisture is treated as a one-phase fluid. Hence there is still need for a more accurate description of mass and energy transport in concrete, especially at high temperatures, accounting for multi-phase nature of concrete.

A new multi-physics, fully coupled and non-linear mathematical model of hygro-thermal phenomena and material degradation in concrete structures exposed to high temperature was proposed by Gawin, *et al.* (1998, 1999). Recently this model has been generalized for the temperature range above the critical point of water, (Gawin, *et al.* 2002a), and thermo-chemical material degradation, (Gawin, *et al.* 2003), as well as Load Induced Thermal Strain, (Gawin, *et al.* 2004), have been taken into account. In the Part I paper, some experimental results of the “HITECO” research project, Brite Euram III (1999), were analysed and discussed in the context of this mathematical model. The parameters of the constitutive relationship describing influence of damage on concrete intrinsic permeability for 4 types of HPCs were deduced from these tests.

In this paper the effects of a new, mechanistic formulation of damage-permeability coupling on the results of computer simulations concerning high temperature performance of concrete are numerically analysed. These results are compared with those obtained using the previously proposed, phenomenological approach, that does not take into account the thermo-chemical damage of concrete directly. The difference between these two formulations is shown to be of importance for the results of numerical simulations concerning performance of concrete structures at elevated temperatures.

2. Mathematical model

The full description of the model can be found in Gawin, *et al.* (1999, 2002a, 2003, 2004). Here, for convenience, only its main assumptions and governing equations are briefly summarised.

Concrete is considered as a multiphase system, being locally in state of thermodynamic equilibrium. The voids of the skeleton are filled partly with liquid water and partly with gas phase.

The liquid phase consists of physically adsorbed water (gel water), which is present in the whole range of normal moisture contents, and capillary water, which appears above a certain value, i.e. when relative humidity of air inside the pores exceeds about 40-50%. The gas phase, a mixture of dry air and water vapour, is assumed to behave as an ideal gas.

The chosen primary state variables of the model are: gas pressure, p^g , capillary pressure, p^c , temperature, T , and displacement vector of the solid matrix, \mathbf{u} . In the equations below the superscripts and subscripts: s , ga , gw and g are related to solid skeleton, dry air, water vapour and gas phase, respectively.

In the capillary moisture range, capillary pressure is defined as, (Gawin, *et al.* 2002a),

$$p^c = p^g - p^w \quad (1)$$

where p^w denotes water pressure, and for the physically adsorbed moisture (including temperatures higher than the critical point of water, $T_{cr} = 374.15^\circ\text{C}$) it only formally substitutes the water potential Ψ , defined as, (Gawin, *et al.* 2002a),

$$\Psi = \frac{RT}{M_w} \left(\frac{p^{gw}}{p^{gws}} \right) = -\frac{p^c}{\rho^w} \quad (2)$$

where M_w is the molar mass of water, R – universal gas constant, ρ^w – water density and p^{gws} – pressure of water vapour in thermodynamic equilibrium with saturated film of physically adsorbed water (for $T < T_{cr}$ it is equal to the saturated vapour pressure).

The final mathematical model consists of four balance equations. The mass balance of the dry air includes both Fickian (diffusive) and Darcian (advective) flows:

$$\begin{aligned} & -n\rho^{ga} \frac{\partial S_w}{\partial t} + S_g \nabla \cdot \frac{\partial \mathbf{u}}{\partial t} + S_g n \frac{\partial \rho^{ga}}{\partial t} + \nabla \cdot \mathbf{J}_d^{ga} + \\ & \nabla \cdot (nS_g \rho^{ga} \mathbf{v}^{gs}) - \frac{\rho^{ga}(1-n)S_g}{\rho^s} \frac{\partial \rho^s}{\partial \Gamma_{dehydr}} \frac{\partial \Gamma_{dehydr}}{\partial t} = \frac{\rho^{ga}}{\rho^s} S_g \dot{m}_{dehydr} \end{aligned} \quad (3)$$

where n is porosity, S_w – water saturation degree, $S_g = 1 - S_w$ – gas saturation degree, ρ - density, t - time, Γ_{dehydr} – dehydration degree, \dot{m}_{dehydr} - mass source term related to the dehydration process, $\mathbf{v}^{\pi s}$ - velocity of the π -phase relative to the solid skeleton, \mathbf{J}_d^{π} -diffusive flux of π -particles ($\pi = ga, gw$) in the gas phase.

The mass balances of the liquid water and of the vapour, summed together to eliminate the source term related to phase changes (evaporation- condensation or adsorption-desorption), form the mass balance equation of water species,

$$\begin{aligned} & n(\rho^w - \rho^{gw}) \frac{\partial S_w}{\partial t} + (\rho^{gw} S_g + \rho^w S_w) \nabla \cdot \frac{\partial \mathbf{u}}{\partial t} + S_g n \frac{\partial \rho^{gw}}{\partial t} + \nabla \cdot \mathbf{J}_d^{gw} + \nabla \cdot (nS_g \rho^{gw} \mathbf{v}^{ws}) + \nabla \cdot (nS_w \rho^w \mathbf{v}^{ws}) \\ & - \frac{(1-n)(S_g \rho^{gw} + \rho^w S_w)}{\rho^s} \frac{\partial \rho^s}{\partial \Gamma_{dehydr}} \frac{\partial \Gamma_{dehydr}}{\partial t} = \frac{\rho^{gw} S_g + \rho^w S_w + \rho^s}{\rho^s} \dot{m}_{dehydr} \end{aligned} \quad (4)$$

The enthalpy balance equation of the whole medium includes the heat effects of the phase change and dehydration process, as well as convective and latent heat transfer,

$$(\rho C_p)_{eff} \frac{\partial T}{\partial t} + (\rho_w C_p^w \mathbf{v}^{ws} + \rho_g C_p^g \mathbf{v}^{gs}) \cdot \nabla T - \nabla \cdot (\lambda_{eff} \nabla T) = \dot{m}_{phase} \Delta H_{phase} - \dot{m}_{dehydr} \Delta H_{dehydr} \quad (5)$$

with phase change mass source term obtained from the liquid water mass conservation equation and given by,

$$\begin{aligned} \dot{m}_{phase} &= -\rho^w S_w \nabla \cdot \frac{\partial \mathbf{u}}{\partial t} - \rho^w n \frac{\partial S_w}{\partial t} - \nabla \cdot (\rho^w \mathbf{v}^w) + \\ & - (1-n) \frac{\partial \rho^s}{\partial \Gamma_{dehydr}} \frac{\partial \Gamma_{dehydr}}{\partial t} \frac{\rho^w S_w}{\rho^s} + \left(1 - \frac{\rho^w S_w}{\rho^s} \right) \dot{m}_{dehydr} \end{aligned} \quad (6)$$

where $(\rho C_p)_{eff}$ is effective thermal capacity, C_p -isobaric specific heat, λ_{eff} -effective thermal conductivity, ΔH_{phase} and ΔH_{dehydr} - specific enthalpies of the phase change (evaporation or adsorption, depending on the relative humidity value) and dehydration, respectively.

Introducing Bishop's stress tensor σ' , called also effective stress tensor, (Schrefler 1995, Schrefler

and Gawin 1996, Lewis and Schrefler 1998), responsible for the deformations of the concrete skeleton, the linear momentum balance equation of the whole medium is given by,

$$\operatorname{div}[\boldsymbol{\sigma}' - \mathbf{I}(p^s - S_w p^c - p_{atm})] + [(1-n)\rho^s + nS_w\rho^w + n(1-S_w)\rho^g]\mathbf{g} = 0 \quad (7)$$

with p_{atm} being atmospheric pressure, \mathbf{I} - unit, second order tensor, \mathbf{g} - acceleration of gravity.

The balance equations have to be completed by so called evolution equations for the dehydration degree $\Gamma_{dehydr}(T)$ (obtained from the laboratory tests), as well as for the mechanical- and thermo-chemical damage parameters, d and V . The latter parameter change with the temperature increase has to be determined experimentally, as shown in section 3 of the Part 1 paper (Gawin, *et al.* 2005).

The mechanical damage evolution depends on the concrete strains and can be found by means of the scalar non-local damage theory, (Mazars 1984, 1986, Mazars and Pijaudier-Cabot 1989), which is briefly summarised below. Thermo-chemical effects are additionally introduced into this theory, as proposed by Gerard, *et al.* (1998) Nechnech, *et al.* (2001) and Gawin, *et al.* (2003). This theory defines a modified effective stress, $\tilde{\sigma}$, and takes into account both the mechanical damage d ($0 \leq d \leq 1$) as a parameter measuring the reduction of resistant area due to cracks, and thermo-chemical damage V ($0 \leq V \leq 1$) as a parameter describing thermo-chemical material degradation at elevated temperatures (see Gawin, *et al.* 2005),

$$\tilde{\sigma} = \sigma' \frac{S}{\tilde{S}} = \frac{\sigma'}{(1-d)(1-V)} \quad (8)$$

where S and \tilde{S} mean total- and resistant area of the damaged material.

In Mazars' theory the damage of concrete grows with an increase of the total strain, i.e., hygro-thermal and mechanical one, and cannot diminish because of restrictions imposed by the Clausius-Duhem inequality, Mazars and Pijaudier-Cabot (1989). Taking into account different behaviour of concrete in tension and in compression, the mechanical damage parameter is obtained from its two components,

$$d = \alpha_t d_t + \alpha_c d_c \quad (9)$$

where α_t and α_c (subscript t means tension, c – compression) are weighting coefficients dependent on the average equivalent strain, $\bar{\varepsilon}$, which can be obtained from the relation, Mazars and Pijaudier-Cabot (1989):

$$\sigma'_i = \left\{ \frac{\kappa_0(1-A_i)}{\bar{\varepsilon}} + \frac{A_i}{\exp[B_i(\bar{\varepsilon} - \kappa_0)]} \right\} E \varepsilon_i, \quad (i = t, c) \quad (10)$$

where κ_0 is initial value of the hardening/softening parameter $\kappa(D)$, which satisfies de Saint-Venant's criterion of maximum principal strain. The temperature dependent parameters, A_t , A_c , B_t , B_c , are characteristics of material and can be identified from the stress-strain relationships obtained during the concrete compression and traction experimental tests.

For the model closure, the balance and evolution Eqs. (3)-(10) must be completed by an appropriate set of constitutive and state equations, initial and boundary conditions, as well as some thermodynamic relationships. For more details concerning the model and its physical basis the

interested reader may refer to Gawin, *et al.* (1999, 2002a, 2003, 2004).

The computer code HITECOSP, developed on the basis of the presented mathematical model, was calibrated with several available experimental data concerning high temperature behaviour of concrete walls, cylinders, columns and special structures, like tunnel sectors and nuclear waste containers, Gawin, *et al.* (1998, 1999, 2002a, 2003, 2004).

3. Modelling of damage-intrinsic permeability coupling

From the analysis performed in the Part 1 paper (Gawin, *et al.* 2005) and the data presented in Gawin, *et al.* (1999), Schneider and Herbst, (1989) and Bary, (1996), it is possible to deduce a realistic and suitable form of the constitutive relationship for numerical modelling, describing changes of concrete intrinsic permeability at elevated temperatures. However, in addition to the parameters analysed in section 4 of the Part 1 paper (Gawin, *et al.* 2005), one should take into account also effect of gas pressure, causing additional increase of permeability, Gawin, *et al.* (1999), Schneider and Herbst (1989), resulting from cracks opening.

In the previous paper, Gawin, *et al.* (2002b), the following constitutive relationship was proposed on the base of general analysis of physical phenomena involved and some available experimental data for normal strength concretes:

$$k = k_0 \cdot 10^{A_T(T-T_0)} \cdot \left(\frac{p^g}{p_{atm}}\right)^{A_p} \cdot 10^{A_D d} \quad (11)$$

where k_0 means intrinsic permeability of undamaged material at reference temperature $T_0 = 293.15$ K and atmospheric pressure $p_{atm} = 101325$ Pa, whilst A_T , A_p and A_D are material constants. In this formula only mechanical damage, d , was considered by Gawin, *et al.* (2002b).

The results of experimental tests presented and analysed in the first part of this paper (Gawin, *et al.* 2005), confirmed in general, that the relationship (11) describes properly physical behaviour of damaged concrete at elevated temperature. However, it is, at least in part, of a phenomenological type, whilst the mathematical model presented in section 2 is, in principle, mechanistic. Thus all physical quantities should be expressed in terms of state variables, which govern a considered phenomenon. In the case of intrinsic permeability, the real reason of its considerable increase at high temperature is appearing and then development of micro-cracks and cracks within the concrete skeleton. The state variable describing the latter process in the present model is the damage parameter, D . It is multiplicatively composed of the mechanical damage (arising from macroscopic effective stresses), d , and the thermo-chemical damage (induced by elevated temperature and dehydration, deterioration of concrete in self-equilibrated stress state), V . For most of the analysed HPCs, in the temperature range up to 400-500°C, the latter damage component is caused prevalently by material cracking.

As shown in section 4 of the Part 1 (Gawin, *et al.* 2005, Fig. 3), there exists a good correlation between the HPC thermo-chemical damage and its intrinsic permeability. Now we shall use this relation for constitutive description of concrete permeability at elevated temperatures, which is postulated in the following form:

$$k = k_0 \left(\frac{p^g}{p_{atm}}\right)^{A_p} \cdot 10^{A_D D} \quad (12)$$

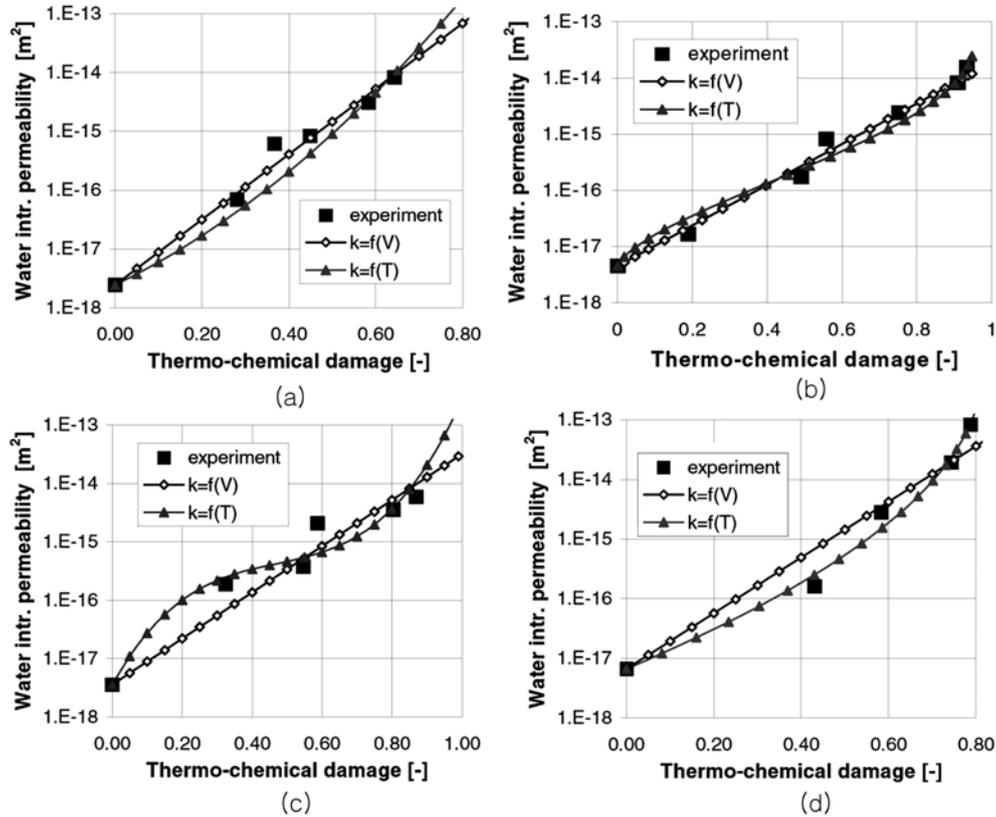


Fig. 1 Dependence of the HPCs intrinsic permeability at elevated temperatures on thermo-chemical damage according to formulas (11) and (12): (a) C60, (b) C60 SF, (c) C70, (d) C90.

where k_0 means intrinsic permeability of undamaged material at reference pressure $p_o = 101325$ Pa, D is the total damage, given by Eq. (8) in Gawin, *et al.* (2005).

Both constitutive relationships, (11) and (12), for the case when $p^g = p_{atm}$ and $d = 0$, are compared in Fig. 1 for various HPCs. The curves corresponding to equation (11) are obtained by using of approximate formulas $V(T)$, determined for the experimental data (“total damage” in Fig. 5 in the Part 1) with the least square method. The square of the correlation coefficient R^2 for these $V(T)$ relations had high values, i.e., 0.993 for C60, 0.987-C60 SF, 0.981-C70 and 0.998-C90.

As may be seen in Fig. 1, both analysed formulations differ distinctly for the considered types of HPC. Explanation of these differences may provide some extra information about relative contributions of the chemical- (caused by dehydration, governed by temperature increase) and thermal- (caused mainly by cracking) components into the total thermo-chemical damage.

The results of correlation analysis, presented in Figs. 2 and 3 in the Part 1, reflect a general, averaged character of temperature- and thermo-chemical damage- effect upon the concrete intrinsic permeability. In case of Fig. 1 (in the Part 1), we may expect that influence of phenomena governed directly by temperature, e.g., dehydration, will be reflected stronger than other factors. Similarly, in the case of Fig. 3 (in the Part 1), the physical phenomena related directly to thermo-chemical damaging will be reflected to a greater extent than others. Hence, greater sensitivity of the

permeability, $d = f[d(T)]$, on a change of temperature in Fig. 1 (i.e. higher value of the derivative, $\partial \log(k)/\partial V$, since the logarithmic scale is used) physically means that phenomena strictly dependent on temperature (mainly dehydration in this case) cause greater permeability changes than, in average, all the phenomena contributing to the thermo-chemical damage (i.e. dehydration and cracking together). Such a qualitative analysis allows for indicating in Fig. 1 ranges of damage values (hence temperatures, as well), where dominating mechanism of the thermo-chemical damage is caused by chemical changes (dehydration), decrease of concrete stiffness, and where dominates cracking; these two mechanisms are there of similar intensity. Results of this type analysis are graphically presented in Fig. 2a-d.

From Fig. 2a it results that for C60 concrete, the rate of thermal damage (cracking) progress is greater for temperatures $T < \sim 200^\circ\text{C}$, and the rate of damage caused by dehydration dominates for $T > \sim 300^\circ\text{C}$. In the case of C60 SF, Fig. 2b, cracking develops faster than dehydration-induced reduction of material stiffness in the temperature range $\sim 200^\circ\text{C} < T < \sim 450^\circ\text{C}$, whilst for $\sim 105^\circ\text{C} < T < \sim 450^\circ\text{C}$ and $T > \sim 550^\circ\text{C}$ (the latter range is not confirmed by Fig. 5b in Gawin, *et al.* 2005) the trends are just the opposite. For the C70 concrete, Fig. 2c, dehydration effects develop with higher intensity for temperatures $\sim 105^\circ\text{C} < T < \sim 270^\circ\text{C}$ and $T > \sim 560^\circ\text{C}$, whereas for $\sim 330^\circ\text{C} < T < \sim 500^\circ\text{C}$ prevails cracking. Finally, in the C90 concrete thermal damage develops very rapidly above $\sim 260^\circ\text{C}$, and below $\sim 170^\circ\text{C}$ dominates material cracking, Fig. 2d.

It should be underlined that, results presented in Figs. 5 (in the Part 1) and 2, obtained by means of very different methods, are qualitatively coherent. They confirm that assumptions done in section 4 of the Part 1 paper (Gawin, *et al.*), in order to estimate the thermo-chemical damage components

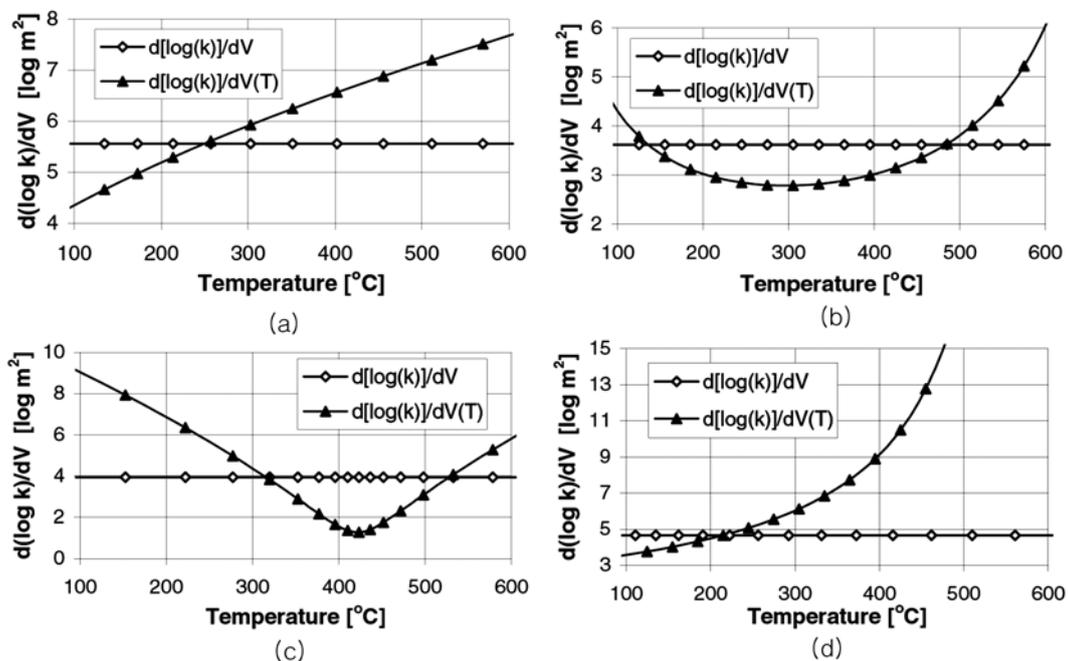


Fig. 2 Dependence of the intrinsic permeability derivative, defined by formulas (11) and (12), with respect to the thermo-chemical damage $\frac{\partial \log(k)}{\partial V}$ upon temperature: (a) C60, (b) C60 SF, (c) C70, (d) C90

are, at least qualitatively, true. In the case of C60 SF there is an incoherence, indicating that for this material, dehydration effects at higher temperatures have been probably underestimated in Fig. 5b (Gawin, *et al.* 2005).

The presented analysis may be a base for selection of the constitutive relationship expressed in terms of temperature, Eq. (11), or thermo-chemical damage, Eq. (12), or even a their combination. In the next section we present the numerical analysis, regarding the effect of the formulation used on the numerical results concerning hygro-thermal phenomena and concrete degradation at elevated temperatures.

4. Influence of permeability constitutive description on hygro-thermal behaviour and damage of HPCs at elevated temperature

A model problem is solved to analyse the influence of the constitutive description of material permeability, including damage effects, on the numerical results concerning hygro-thermal phenomena and damage evolution in concrete elements at high temperature.

This 1-D problem deal with a HPC wall exposed from one side to fire. The material parameters are assumed according to the results of the experimental tests for the C-60 concrete, Brite Euram III (1999). Its basic data for the dry material at ambient temperature are summarized in Table 1.

The C-60 concrete wall, having thickness of 30 cm, initially is at temperature of 293.15 K and relative humidity of 60% RH. It is subjected to transient heating from one side according to the standard ISO fire temperature curve. On the heated surface mixed-convective and radiative-boundary conditions for energy exchange, with heat exchange coefficient $\alpha_c = 7 \text{ W/m}^2\text{K}$ and Boltzmann's coefficient $\epsilon\sigma_0 = 5.1 \cdot 10^{-8} \text{ W/(m}^2\text{K}^4)$, are assumed. For mass exchange convective boundary conditions with $p_v = 1000 \text{ Pa}$ and mass exchange coefficient $\beta_c = 0.028 \text{ m/s}$ are considered. The other surface of the wall is assumed to be adiabatic and impermeable for mass exchange.

The problem is solved for two different formulations of the constitutive relationship describing the intrinsic permeability:

- (A) according to (11), where changes of permeability are expressed in terms of the temperature and the mechanical damage values, with $A_T = 0.0629$, $A_D = 5.56$, $A_P = 0.369$;
- (B) according to (12), where changes of permeability are expressed directly in terms of the total

Table 1 Basic parameters of the C-60 concrete (dry material at 20 °C)

Parameter	Symbol	Value
Porosity	n [-]	0.086
Intrinsic permeability	k_0 [m^2]	2.4×10^{-18}
Apparent density	ρ [kg/m^3]	2575
Effective specific heat	C_{eff} [J/kgK]	851
Eff. thermal conductivity	λ_{eff} [W/mK]	1.95
Young's modulus	E [GPa]	34.5
Poisson's ratio	ν [-]	0.18
Compressive strength	f_c [MPa]	60
Tensile strength	f_t [MPa]	5.83

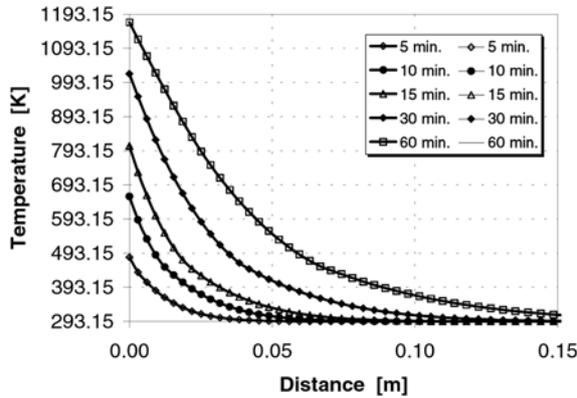


Fig. 3 Comparison of the time evolution of temperature distribution in the C-60 HPC wall heated from one side (solid line-case A, thin line-case B)

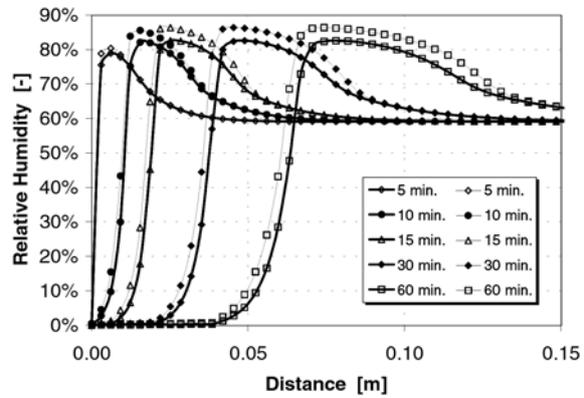


Fig. 4 Comparison of the time evolution of relative humidity distribution in the C-60 HPC wall heated from one side (solid line-case A, thin line-case B)

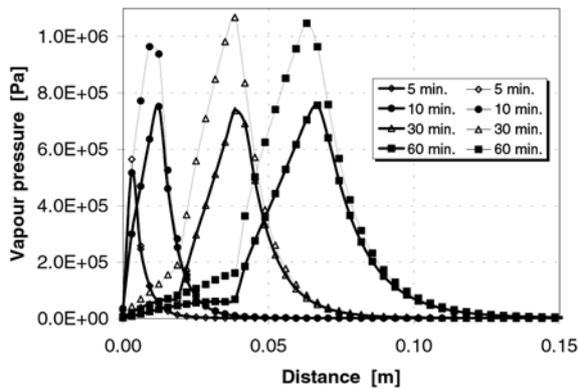


Fig. 5 Comparison of the time evolution of vapour pressure distribution in the C-60 HPC wall heated from one side (solid line-case A, thin line-case B)

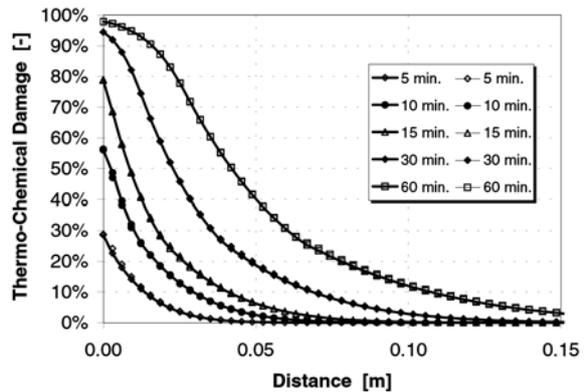


Fig. 6 Comparison of the time evolutions of thermo-chemical damage distribution in the C-60 HPC wall heated from one side (solid line-case A, thin line-case B)

damage value, with $A_D = 5.56$, $A_P = 0.369$.

The first 60 minutes of transient behaviour are simulated using a mesh consisting of 69 eight-noded serendipity elements (348 nodes).

The resulting profiles of temperature, relative humidity, vapour pressure, thermo-chemical damage, mechanical damage and total damage parameters for the two analysed cases at different time stations $t = 5, 10, 15, 30$ and 60 minutes are compared in Figs. 3-8. The solutions are shown only for the 15-cm layer from the heated surface of the wall, because there significant changes of the wall parameters are observed.

The wall surface temperature increases gradually, reaching value of about 900°C after 60 minutes. Considerable temperature gradients appear in the surface zone of the wall, Fig. 3, causing the so called moisture thermo-diffusion phenomenon, i.e., transport of water, both in the liquid and gas phases, in the direction of the heat flow. Due to this process and additionally a very intensive

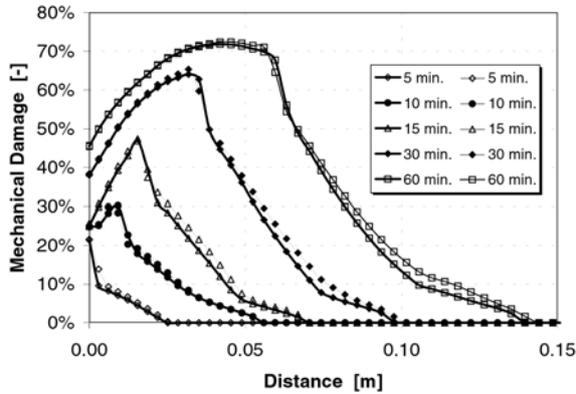


Fig. 7 Comparison of the time evolutions of mechanical damage distribution in the C-60 HPC wall heated from one side (solid line-case A, thin line-case B)

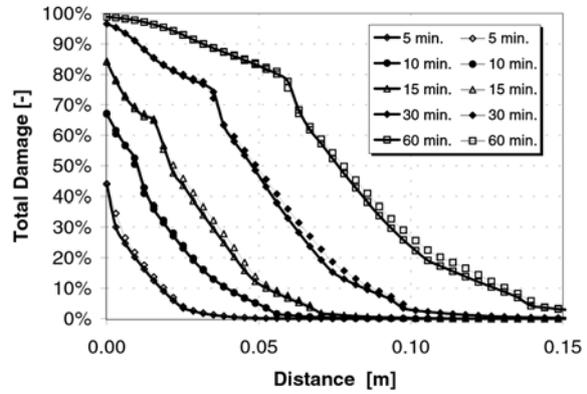


Fig. 8 Comparison of the time evolutions of total damage distribution in the C-60 HPC wall heated from one side (solid line-case A, thin line-case B)

evaporation of capillary water in the zone where temperature is higher than 100°C , the relative humidity reaches value of about 86%, creating characteristic humps on the graphs shown in Fig. 4. A sharp front of water desaturation moves toward the inner part of the wall, Fig. 4, indicating the zone of intensive evaporation, where also the highest values of gas and vapour pressures, up to about 0.75-1.1 MPa (with distinctly higher values for the case B), are observed. Thermo-chemical degradation process develops gradually, according to the temperature increase history, (Fig. 6), reaching value of about 98% on the surface at the end of the analysed process. It favours development of a considerable mechanical damage in the surface zone of the wall where its maximal value reaches about 72.5% at the distance of 5 cm from the heated surface. The combined action of mechanical and thermo-chemical damage cause that about 6 cm layer of the wall is deteriorated in more than 80%, Fig. 8.

Analysis of the results in Figs. 3-8 shows that temperature and consequently, thermo-chemical damage solutions are almost unaffected by the applied form of the permeability constitutive relationship, see Figs. 3 and 6. Small differences are observed for the mechanical and total damage solutions, Figs. 7 and 8. However, the permeability relation has a considerable influence on the solutions concerning moisture transport, i.e., the relative humidity and especially, the gas pressure, Figs. 4 and 5. For the latter physical quantity, differences up to about 0.35 MPa between the analysed cases of permeability description are observed, especially in the zone with lower temperatures. These phenomena may be explained by the differences between the analysed cases of permeability for the lower thermo-chemical damage values, Fig. 1a, where the case B is in better accordance with the experimental data. The observed pressure differences are probably not of importance for the evaluation of risk of explosive spalling, since they are not reflected specifically by the damage model. For the C-60 concrete differences between the analysed formulations of permeability dependence upon damage are relatively small, see Fig. 1a. However, one may expect that they will be more evident for the C-70 and C-90 concretes (Fig. 1c, d). It should be also noted a better numerical performance of the numerical code for the case A, i.e., for the phenomenological one, which allowed application of slightly higher time step values.

6. Conclusions

The constitutive relationship describing the influence of damage on concrete intrinsic permeability can be expressed both in terms of temperature (phenomenological approach) and damage parameter (mechanistic form). Effects of the mechanistic formulation of damage-permeability coupling on results of computer simulations are compared with the results obtained using the previously proposed, phenomenological formula. These effects are shown to be of importance for the results of numerical simulations concerning the behaviour of High Performance Concrete structures at elevated temperatures, especially concerning the gas and vapour pressures, and to the less extent, also the water saturation. At the moment there is lack of a sufficient experimental evidence to assess which formulation of the damage-permeability coupling gives more realistic results. Such an experimental verification is needed for a proper choice of the constitutive description of concrete at high temperature.

For the analysed C-60 concrete wall it was found that a dominating mechanism of material deterioration at high temperature is the thermo-chemical degradation of the zone close the wall surface, and the mechanical damage for the zone between 5 cm and 10 cm from the surface.

Summarising, one can say that assumption about direct dependence of permeability on damage (case B in the solved example), what is physically better justified than indirect, phenomenological dependence through temperature (usually measured during experimental tests), has complex effects on hygro-thermal and mechanical performance of concrete at high temperature. To analyse these phenomena properly, complex numerical models are necessary together with possibly full set of material data, which allow for introduction into the models realistic constitutive relations, most of them being complex functions of temperature, pressure and damage state of concrete.

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