RELEVANT CONSTITUTIVE CHARACTERISTICS OF ASPHALT CONCRETE MIXTURES: AN EXPERIMENTAL AND ANALYTICAL APPROACH

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ABSTRACT

The paper deals with the validation of a one-dimensional (1-D) visco-elastoplastic constitutive model able to reproduce the non-linear time-dependent behaviour of asphalt concrete. In order to determine the constitutive parameters of the model, by means of an appropriate identification procedure, an extensive experimental investigation has been carried out. Creep tests at different temperatures on specimens of bituminous mixtures, usually employed in pavement layers, have been performed. The effectiveness of the model as well as of the identification procedure has been verified by comparison between the material response, obtained numerically, and the experimental data. The analysis of the results of creep tests allowed to find a trend of variation of elastic and viscous parameters with temperature.

Keywords: asphalt concrete, constitutive model, creep test.

1. INTRODUCTION

It is well known that the upper layers of flexible pavements are made of asphalt concrete, which is a composite material made from a mix of various size stones and bitumen. The resulting mix is a rather heterogeneous material and its mechanical behaviour respects the complex mechanical nature of each single component, as well as the mutual interactions within the mixture. For the above reasons, constitutive modelling of asphalt concrete behaviour is an extremely challenging problem and it is almost impossible to introduce all the effects into one single model, Molenaar and Molenaar (2000), such as thermal coupling effects, mechanical damage and plasticity.

In this work, a one-dimensional (1-D) constitutive model, based on the theory of a viscoelasticity generalized Maxwell model coupled with a viscoplastic model of the Perzyna type, is presented. The recalled model, already proposed in a previous work by the authors, Giunta and Pisano (2006), shows the capability of reproducing the evolution of non-linear responses of asphalt concrete when subjected to an assigned time varying load history and to achieve a correct steady-state response to fixed loads.

The formulation is, moreover, demonstrated to be fully consistent with general thermodynamic requirements, see Giunta and Pisano (2006) for more details.

The present paper discusses the validation of the quoted 1-D constitutive model which is "fed" with material data determined by experimental tests. To this purpose, several laboratory tests have been performed. Here, particular attention is focused on creep tests carried out at different temperatures on specimens of bituminous mixtures, usually employed in wearing courses. Creep tests have been considered because they provide essential material information and, in the authors' opinion, represent the starting point for the development of any constitutive model.

The analysis of the experimental results related to the creep tests has shown a variation of the elastic and viscous parameters with temperature.

2. CONSTITUTIVE ASPHALT CONCRETE MODEL

The model presented in this Section is a time dependent elastic-plastic model. To immediately understand the model, a rheological representation is shown in Figure 1.

The rheological scheme can be divided into two main parts: the first is a generalized Maxwell model which interprets the viscoelastic behaviour of the material, while the second is able to reproduce the irreversible viscoplastic behaviour with hardening. The second part of the model is active only when the applied stress reaches the threshold value σ_0 and is of viscoplastic nature. The development of permanent (or unrecoverable) strain is addressed by the viscoplastic element.

The proposed model is not able to represent the tertiary creep response which could only be described by introducing some damage mechanics concepts into the model. However, since it is expected that the load levels are not so high as to produce tertiary creep, in order to keep the material model sufficiently simple, no damage variable has been introduced.

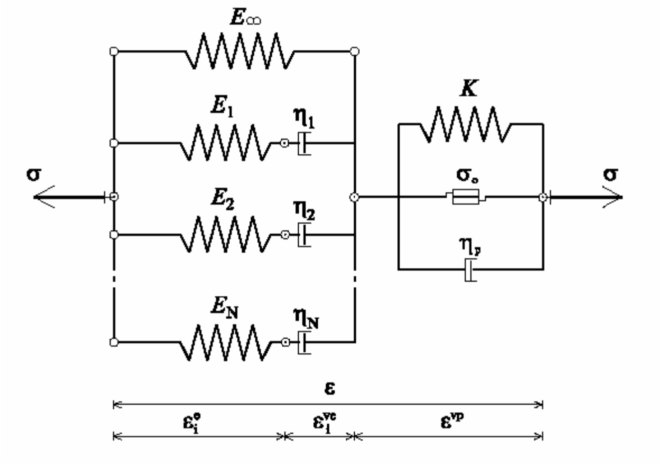

Figure 1 - Rheological model

From a simple inspection of the rheological scheme of Figure 1 it appears that the classical plasticity additive decomposition rule of the total strain holds, namely:

$$\varepsilon(t) = \varepsilon^{ve}(t) + \varepsilon^{vp}(t) \qquad \text{(Eq. 1)}$$

where: $\varepsilon^{ve}(t)$ is the recoverable viscoelastic strain and $\varepsilon^{vp}(t)$ is the irrecoverable viscoplastic strain. More precisely the Maxwell element is conceived as an elastic element, of constant E_∞, which governs the steady-state elastic response, connected in parallel with N Maxwell elements, whose elastic constants and viscous coefficients are respectively E_i and η_i i = 1, 2, … N. Several elements placed in parallel allow us to take into account different relaxation times of the viscoelastic response. The viscoelastic strain component is given by:

$$\varepsilon^{ve}(t) = \varepsilon_i^e(t) + \varepsilon_i^v(t) \qquad \text{(Eq. 2)}$$

where $\varepsilon_i^e(t)$ is the instantaneous strain and $\varepsilon_i^v(t)$ is a time delayed viscous term. The internal strains can be associated to internal stresses defined by:

$$\sigma_i = E_i \varepsilon_i^e(t); \qquad \sigma_i^v = \eta_i \frac{d}{dt}[\varepsilon_i^v(t)] = \eta_i \dot{\varepsilon}_i^v(t), \qquad \text{(Eq. 3)}$$

while for the element without dashpot the associated internal stress can be written as:

$$\sigma_\infty = E_\infty \varepsilon^{ve}(t) \qquad \text{(Eq. 4)}$$

Equations 1 and 2 can be read as internal compatibility equations and, consequently, it is possible to define internal equilibrium equations which relate the external applied tension σ to the internal stresses developed in each component of the model. Keeping Eq. 3 and Eq. 4 in mind, after some algebra, the following equality holds:

$$\begin{aligned}\sigma(t) &= E_\infty[\varepsilon(t) - \varepsilon^{vp}(t)] + \sum_{i=1}^{N} E_i[\varepsilon(t) - \varepsilon_i^v(t) - \varepsilon^{vp}(t)] = \\ &= E_0[\varepsilon(t) - \varepsilon^{vp}(t)] - \sum_{i=1}^{N} E_i \varepsilon_i^v(t),\end{aligned} \qquad \text{(Eq. 5)}$$

with:

$$E_0 = E_\infty + \sum_{i=1}^{N} E_i \; . \qquad \text{(Eq. 6)}$$

For each of the i-th viscoelastic elements, internal equilibrium requires $\sigma_i = \sigma_i^v(t)$ and considering the strain decomposition of Eq. 1 and Eq. 2 and the internal constitutive Eq. 3, one obtains:

$$\sigma_i^v = E_i[\varepsilon(t) - \varepsilon_i^v(t) - \varepsilon^{vp}(t)] = \eta_i \dot{\varepsilon}_i^v(t), \qquad \text{(Eq. 7)}$$

from which the flow law for $\varepsilon_i^v(t)$ can be derived as:

$$\frac{\eta_i}{E_i} \dot{\varepsilon}_i^v(t) = \varepsilon(t) - \varepsilon_i^v(t) - \varepsilon^{vp}(t). \qquad \text{(Eq. 8)}$$

The assumption:

$$\tau_i = \frac{\eta_i}{E_i} \geq 0, \qquad i = 1, 2, \ldots N \qquad \text{(Eq. 9)}$$

allows the definition of the coefficients τ_i as the relaxation time for the i-th dashpot. Under the assumptions that plastic strains are treated as imposed distortions and are considered known in the same way as the total strains, the solution of the differential Eq. 8 is obtained, assuming the following integral expression:

$$\varepsilon_i^{ve}(t) = \varepsilon(t) - \varepsilon^{vp}(t)] - \int_{-\infty}^{t} e^{-(t-\bar{t})/\tau_i} [\dot{\varepsilon}(\bar{t}) - \dot{\varepsilon}^{vp}(\bar{t})] d\bar{t}. \qquad \text{(Eq. 10)}$$

To evaluate the viscoplastic strain-rate it is necessary to refer to the classic plasticity theory, Simo and Hughes (1997).

First, it is observed that the viscoplastic strain is given by the simple time integration of the viscoplastic strain-rate history:

$$\varepsilon^{vp}(t) = \int_{-\infty}^{t} \dot{\varepsilon}^{vp}(\bar{t})d\bar{t} \qquad \text{(Eq. 11)}$$

then, the actual problem is to determine, at each time t, the viscoplastic strain rate $\dot{\varepsilon}^{vp}(t)$.

For this purpose, the viscoplastic activation function is defined in the following form:

$$\phi(\sigma,\lambda) = |\sigma| - K\lambda - \sigma_0, \qquad \text{(Eq. 12)}$$

with

$$\lambda = \int_{-\infty}^{t} \left|\dot{\varepsilon}^{vp}(\bar{t})\right| d\bar{t}\,; \qquad \dot{\lambda} = \left|\dot{\varepsilon}^{vp}\right|. \qquad \text{(Eq. 13)}$$

As is well known, if $\phi > 0$, an overstress, defined by the positive value of ϕ, acts on the frictional device and produces the development of a viscoplastic strain-rate given by the following flow law:

$$\dot{\varepsilon}^{vp} = \frac{\langle \phi(\sigma,\lambda) \rangle}{\eta_p} \mathrm{sgn}(\sigma) \qquad \text{(Eq. 14)}$$

where the symbol $\langle x \rangle$ indicates $(x + |x|)/2$ and then returns to the x value only if x is positive, otherwise it returns to zero. The nonlinear Eq. 14, defines the viscoplastic strain-rate component of the model and, together with Eq. 11-13 completes the constitutive framework of the proposed model, Lemaitre and Chaboche (1990).

The constitutive model obtained in a straightforward manner from the rheological scheme of Figure1 can, alternatively, be obtained by making use of a rigorous approach. More specifically, it is possible to derive the constitutive relations on the basis of thermodynamic arguments and on the choice of an appropriate set of internal variables, Giunta and Pisano (2006).

For practical computational purposes the constitutive time continuous rate relations need to be integrated in a stepwise fashion along an assigned incremental loading path, Lu and Wright (1998). Moreover, in the nonlinear finite element context, structural equilibrium and constitutive consistency, at the Gauss points, are achieved following a predictor-corrector iterative scheme. This scheme consists of the elastic predictor phase, which gives the total strain increments at the end of the time step, followed by the fulfilment of the constitutive relations enforced at each Gauss point. In order to assess this problem it is necessary to integrate into small steps the relations given by the prediction phase (strain driven problem) in which the total strain increment is considered as an assigned quantity, Simo and Hughes (1997).

In the following, for the numerical applications, the Euler backward integration rule, which is a fully implicit numerical procedure, has been employed to enforce the constitutive consistency at the end of the time step.

3. MATERIAL COEFFICIENTS IDENTIFICATION PROCEDURE

In order to accurately reproduce the response of an asphalt concrete an identification procedure for the material constants, which appear in the model, has to be carried out. In particular, the number, N, of the viscoelastic elements, constituting the generalized Maxwell model, the elastic constants E_i and the viscosity parameters η_i together with the constant E_∞ have to be determined.

Moreover, for the viscoplastic response characterization, the slip threshold σ_0, the plastic hardening modulus K and the viscoplastic parameter η_p need to be identified. The values of the above-cited coefficients can be determined by consulting of a standard creep test.

For a fixed stress loading σ, by observing the results of a standard creep test, it is possible to evaluate the steady state strain response ε_∞.

Complete stress unloading from the steady state allows us to evaluate the residual strain which is constituted by the viscoplastic component ε_∞^{vp}. The difference $\varepsilon_\infty - \varepsilon_\infty^{vp} = \varepsilon_\infty^{e}$ gives the elastic strain in the steady state condition. The simple elastic relation $E_\infty = \sigma / \varepsilon_\infty^{e}$ allows the evolution of the first asymptotic elastic constant. Identification of the experimental initial elastic strain ε_0 is a little more problematic. The value of ε_0 is in fact affected by the time acquisition delay of the electronic device and also by the highly idealized instantaneous stress application hypothesis.

In order to derive the other viscoelastic parameters from Eq. 6 it follows that:

$$\sum_{i=1}^{N} E_i = \frac{\sigma}{\varepsilon_0} - E_\infty = \sigma\left(\frac{1}{\varepsilon_0} - \frac{1}{\varepsilon_\infty^{e}}\right). \qquad \text{(Eq. 15)}$$

Once the sum of all the E_i elastic constants is known, the specific evaluation of each E_i constants must be performed in conjunction with the evaluation of the viscosity parameters η_i, considering the condition of relaxation times $\tau_i=\eta_i/E_i$. Evaluation of the relaxation times τ_i can be achieved by observing the strain time history response coming out from a creep test and considering that the analytical trends follow the exponential relation $e^{-t/\tau i}$, which in practice extinguishes its variation for $t \cong 5\tau_i$. By examining our experimental curves it is possible to recognize at least two relaxation times, which means N = 2. As a consequence N = 2 viscoelastic elements are enough to reproduce the elastic response. So the two relaxation times τ_1 and τ_2 are obtained by fixing the two times $5\tau_1$ and $5\tau_2$, in the record of the experimental strain-time response curve, corresponding to the end of the two elastic phases.

The actual values of E_1 and E_2 are obtained by considering that the first phase is stiffer i.e. ($E_1 > E_2$) and so usually $E_1 \cong 1{,}5E_2$. A mean value of the slope E_T of the stress strain curve in the plastic range can be measured and from the relation:

$$E_T = \frac{E_\infty K}{E_\infty + K} \qquad \text{(Eq. 16)}$$

the plastic hardening coefficient K can be derived. Finally the viscoplastic coefficient η_p is evaluated by the relation:

$$\eta_p = (E_\infty + K)\tau_p . \qquad \text{(Eq. 17)}$$

The internal viscoplastic time τ_p can be obtained following the same procedure used for the coefficients τ_i.

4. LABORATORY TESTS

The knowledge of the creep behaviour of any material is fundamental before used in structures that must support loads for long periods.

Excessive creep deformation may produce, in fact, uselessness of the structure.

Unfortunately, the response of the asphalt concrete during a creep test depends on several variables: the mixture characteristics (aggregate blend, amount and type of bitumen), the temperature, the load level, etc.

In the present study creep tests have been performed with a twofold purpose: to validate the theoretical model and to investigate how the elastic, viscous and plastic material coefficients are influenced by the temperature.

To avoid the influence of further variables, the bituminous mixture has kept fixed for all the tested specimens.

In particular a mixture employed in a wearing course having a gradation curve, as shown in Fig.2, centred with respect to the ANAS (Italian Road and Highway Society) master band has been considered.

This mixture has been produced using lime stones and pure bitumen 50/70, representing a 5% of the stones' weight.

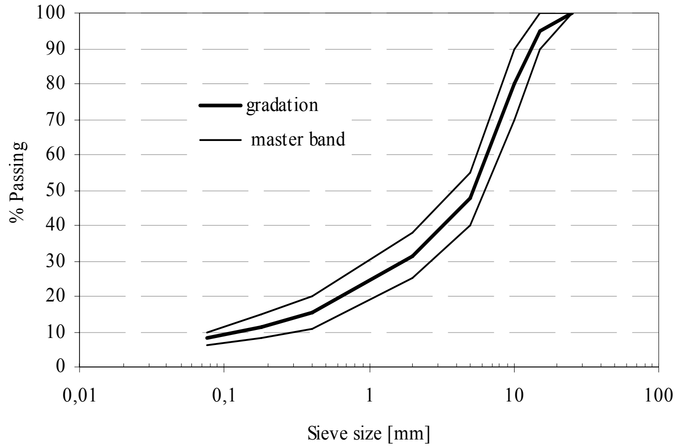

Figure 2 - Gradation curve and ANAS master band

The tests were executed using a creep tester and were performed on standard cylindrical specimens having a diameter of 100 mm and a height of 200 mm. The specimens, previously compacted to reach the 98% of the Marshall bulk density, were subjected to normal load (see Figure 3).

Figure 3 - Creep tester

To obtain a wide range of results, 18 specimens, labelled from P01 to P18, were prepared with the same procedure. In particular, in order to control the effects of both load level or temperature, the tests were performed for load levels varying from 0.05 to

0.2 MPa and temperatures 20, 30 and 40°C. Table 1 synthesizes the 9 examined test conditions.

Table 1 - Load and temperature conditions utilised for each specimen during the creep tests.

Temperature [°C]	Stress [MPa]		
	$\sigma_0 = 0.05$	$\sigma_0 = 0.1$	$\sigma_0 = 0.2$
20	P01-P02	P03-P04	P05-P06
30	P07-P08	P09-P10	P11-P12
40	P13-P14	P15-P16	P17-P18

The axial stress was applied and kept constant after a preloaded phase of 10 min at 0.01 MPa; this preliminary load phase was necessary to allow for a correct adherence between the base of the specimen and the tester machine. Compatible with the technical limit of the utilised creep tester, the load and the unload time, for each test, was fixed to 5000 sec. From the test, at fixed axial load, the deformation of the specimens was automatically recorded every 0.5 sec in the first 5 sec of the test, every 1 sec till 10 sec, every 5 sec till 50 sec and finally every 25 sec till the end.

5. MODEL CALIBRATION AND VALIDATION

The constitutive model developed in Section 2 is applied to reproduce the actual responses of the specimens subjected to the stress levels and temperatures indicated in table 1. To simulate the tests numerically, it is necessary to identify the material parameters first. The identification procedure illustrated in Section 3 is then applied to the curves obtained by fitting the mean value results of the two set of data available for each case. More precisely, parameter identification is carried out from the mean values of the records obtained by testing all the specimens. The trend of variation of each parameter with temperature is shows in Figure 4 while the determined material parameters are reported in Table 2 together with the identified relaxation times.

Table 2 - Material Coefficients utilised

Material Coefficients	Temperature [°C]		
	T = 20	T = 30	T= 40
E_1 *[MPa]*	2500	780	210
E_2 *[MPa]*	1700	520	140
E_∞ *[MPa]*	300	186	160
K *[MPa]*	21.4	51	55
τ_1 *[sec]*	1	1	1
τ_2 *[sec]*	30	30	100
τ_p *[sec]*	1000	1000	1000

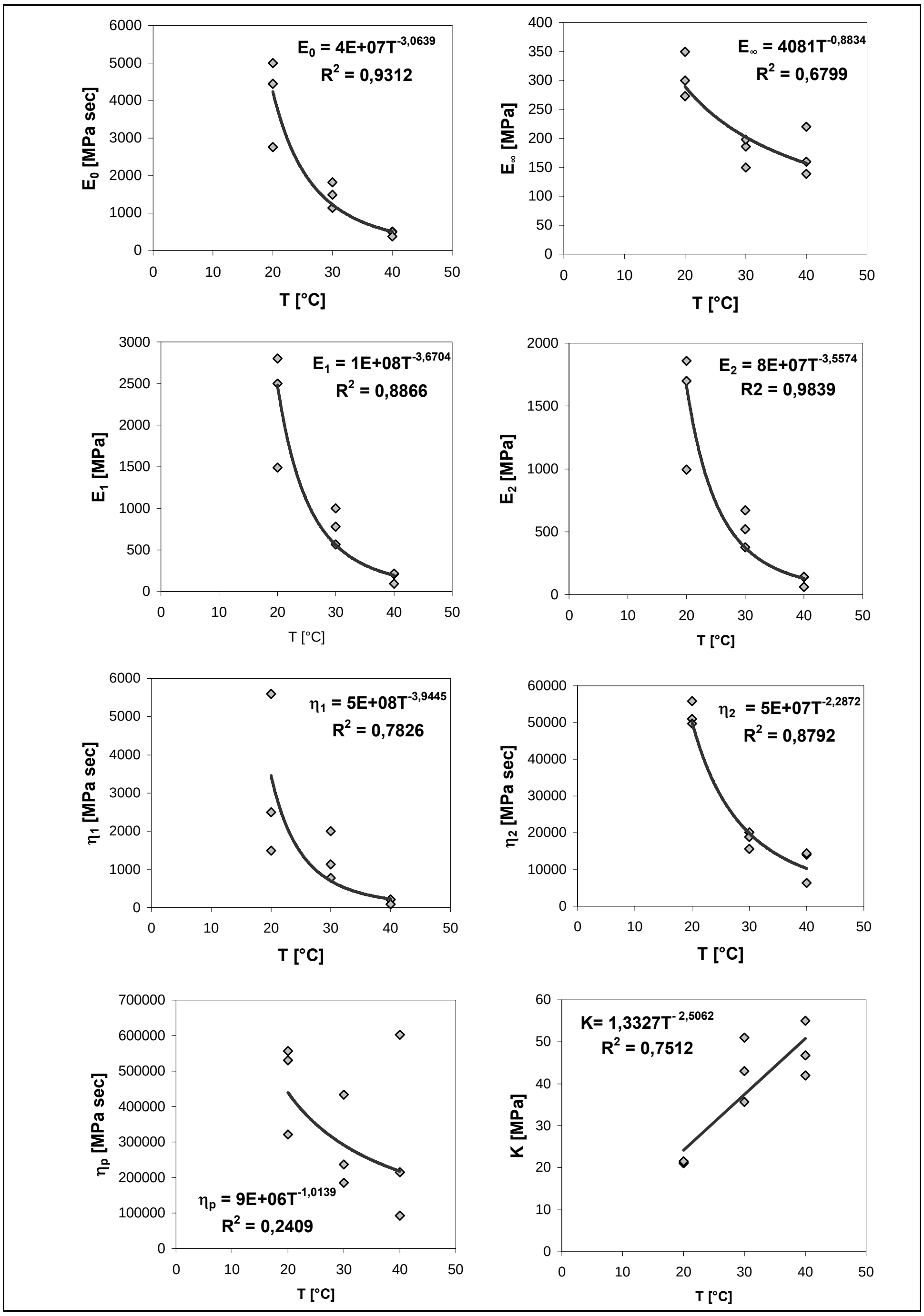

Figure 4 – Elastic and viscous parameters versus temperature

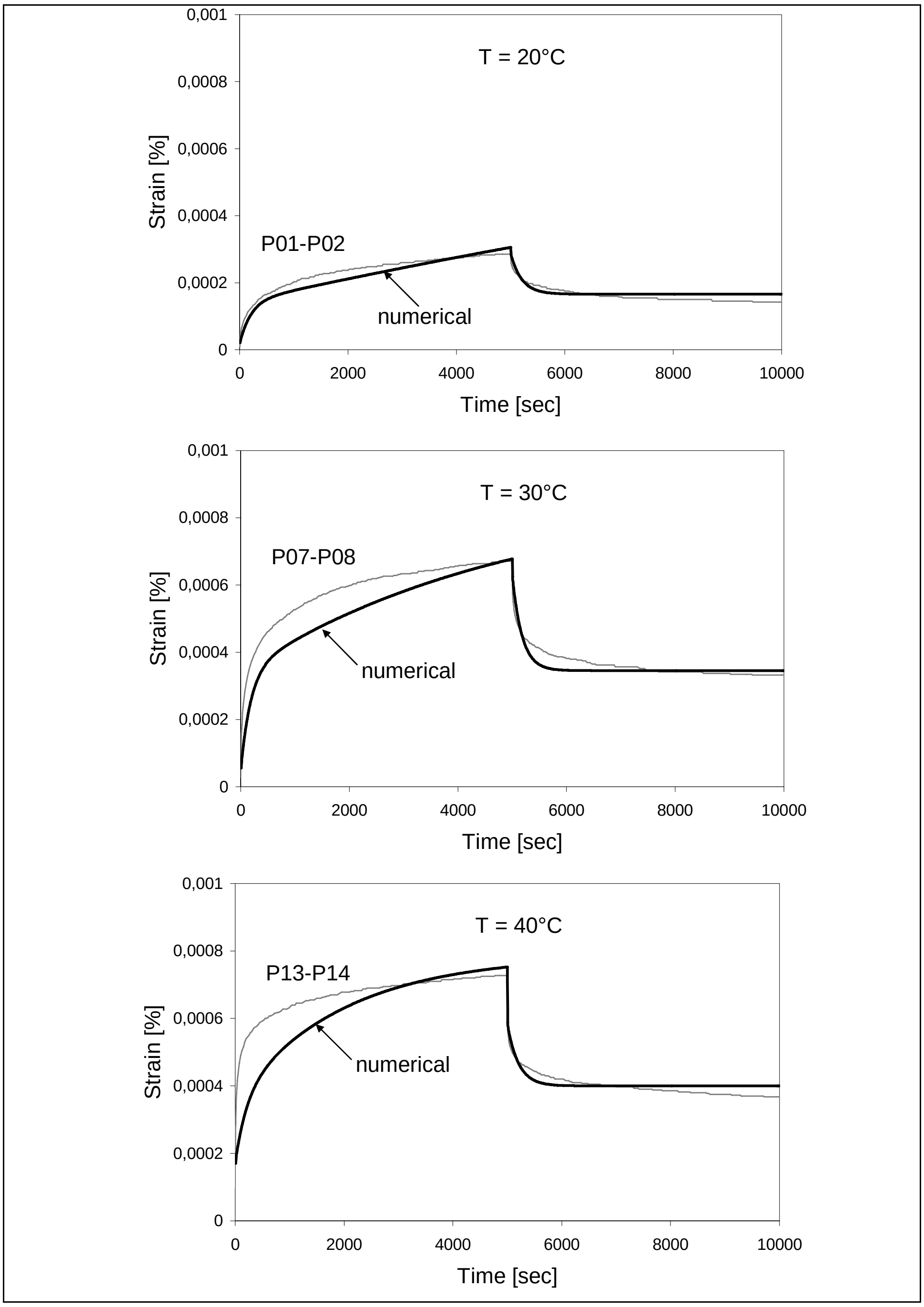

Figure 5 - Numerical and experimental results from creep test at σ = 0.05MPa

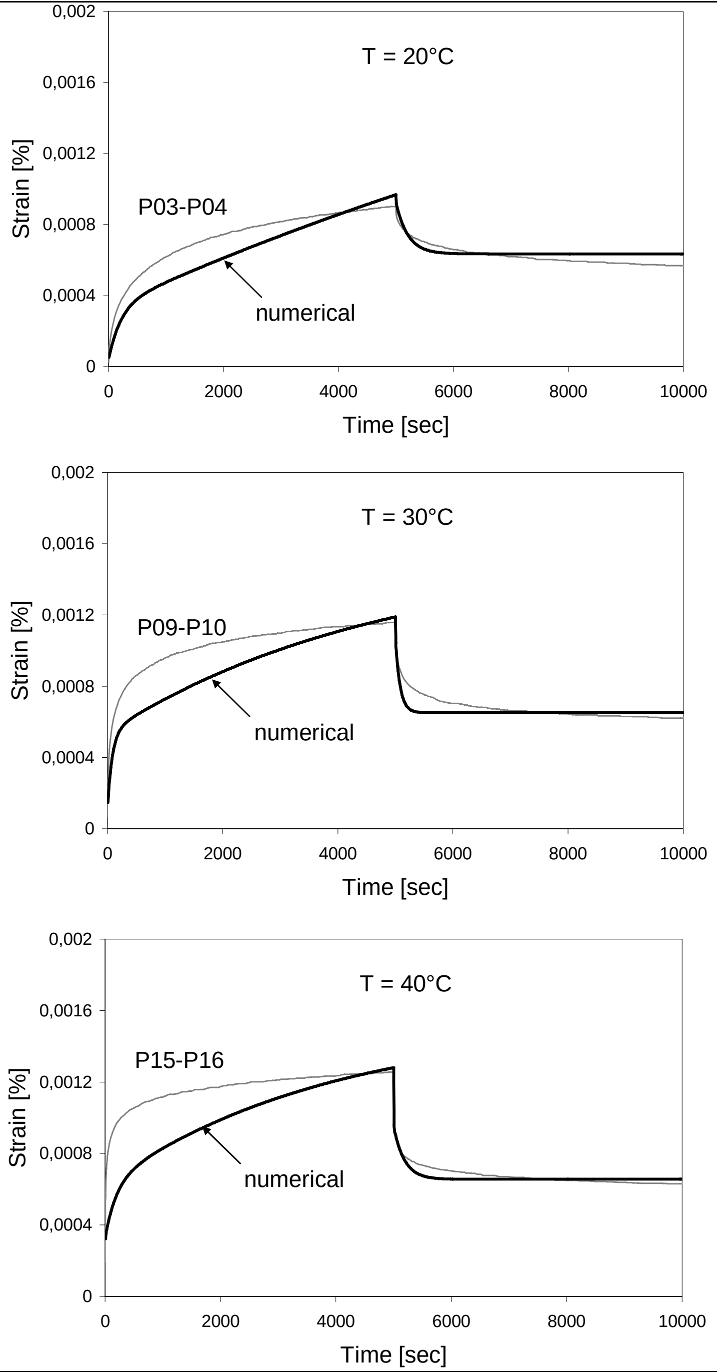

Figure 6 - Numerical and experimental results from creep test at σ = 0.1MPa

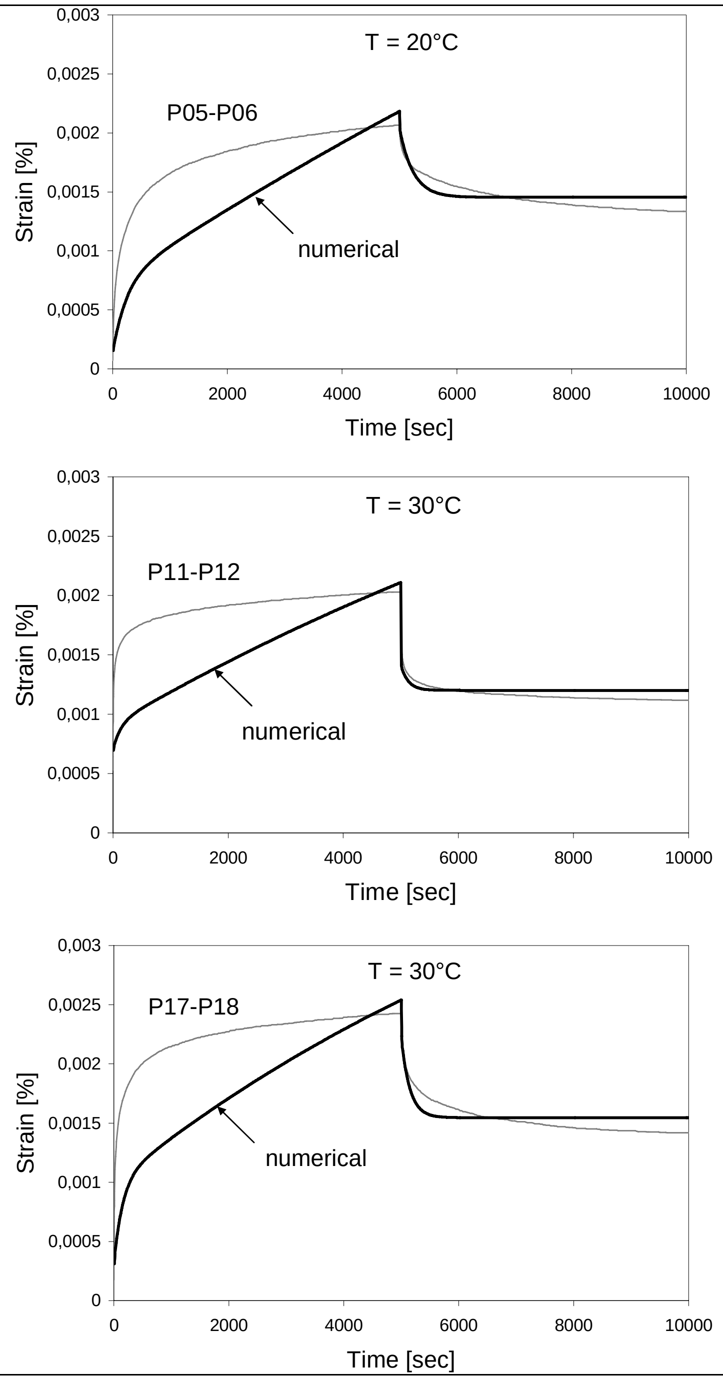

Figure 7 - Numerical and experimental results from creep test at σ = 0.2MPa

More precisely it can be observed that the material coefficients, elastic moduli viscous parameters, decrease exponentially when temperature increases while the modulus K, related to the plastic deformation part, increases with the temperature. It appears natural to derive that, at fixed load level, the material becomes more deformable when the temperature increases.

For the examined cases, Figures 5-7 show the visco elastoplastic strain responses versus time obtained by numerical simulation in comparison with the ones obtained from experimental data.

By examining Figure 5, which is related to a low stress level σ, an overall good agreement between the numerical and the experimental results can be observed. This holds true also varying the temperature during the tests; for these cases the numerical model provides very satisfactory results.

Otherwise, Figure 6 and 7 show some discrepancies between the results obtained by means of the proposed model and the experimental ones more remarkable when the stress level σ increases. Nevertheless, as can be observed, this discrepancies are related to the evolution of the loading phase while the model is able to reproduce in a good manner the strain value at the end load process and the evolution of the unloading phase. Further investigations are necessary to better calibrate the model.

6. CONCLUSIONS

A one-dimensional constitutive model capable of reproducing many relevant features of the mechanical response of asphalt concretes has been presented. The proposed formulation has been developed following a relatively simple and intuitive rheological scheme, but in a previous work it has also been demonstrated that the model complies with the thermodynamic theory of dissipative mechanical processes with internal variables.

In the present paper the model has been validated by comparing some results, in terms of strain, obtained numerically with the ones determined by experimental creep tests. Some effects on the material behaviour related to the variation of the temperature have also been observed.

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