



**HAL**  
open science

# Thermomechanical modelling for fatigue damage of bituminous materials

Didier Bodin, F Belzung, Chantal de La Roche, Armelle Chabot

► **To cite this version:**

Didier Bodin, F Belzung, Chantal de La Roche, Armelle Chabot. Thermomechanical modelling for fatigue damage of bituminous materials. Fifth International RILEM Conference on Reflective Cracking in Pavements, May 2004, LIMOGES, France. pp. 675-682. hal-01537188

**HAL Id: hal-01537188**

**<https://hal.science/hal-01537188>**

Submitted on 16 Dec 2020

**HAL** is a multi-disciplinary open access archive for the deposit and dissemination of scientific research documents, whether they are published or not. The documents may come from teaching and research institutions in France or abroad, or from public or private research centers.

L'archive ouverte pluridisciplinaire **HAL**, est destinée au dépôt et à la diffusion de documents scientifiques de niveau recherche, publiés ou non, émanant des établissements d'enseignement et de recherche français ou étrangers, des laboratoires publics ou privés.

## Thermomechanical Modelling for Fatigue Damage of Bituminous Materials

D. Bodin, F. Belzung, C. de La Roche and A. Chabot

*MSC, Laboratoire Central des Ponts et Chaussées  
Route de Bouaye – BP 4129 – F44341 Bouguenais Cedex - France*

### Abstract

To assess fatigue cracking performances, pavement materials are tested under cyclic loading during fatigue laboratory tests. Fatigue damage is generally assessed using the global specimen stiffness decrease. This classical fatigue life interpretation can be modified by the softening effect of thermal increase due to viscoelasticity which is not real micro-cracking induced damage.

Here the material is modelled as a thermo-viscoelastic damageable material tested under sinusoidal loading. Both thermal and mechanical aspects and their coupling are taken into account leading to a complete constitutive modelling.

Simulations are performed on cylindrical tension compression tests assuming a purely radial heat transfer. Comparison with temperature measurements and the global stiffness evolution lead to a good agreement between the model and experimental data.

This approach points out the importance of thermal effects in fatigue tests and the limitation of classical approaches when thermal effects become important.

### 1. Introduction

Cyclic mechanical loading in pavement layers lead to material fatigue damage. The accumulation of these micro degradations can initiate very injurious cracks which propagate through the pavement structure. Laboratory cyclic tests on bituminous materials allow to predict and take into account material fatigue performances in mixture and pavement design. Classical laboratory approach is based on sinusoidal constant amplitude loading. The specimen stiffness is given as a decreasing function of the number of cycles.

These fatigue tests are widely used to assess fatigue performances of bituminous materials. Nevertheless, the interpretation of the material behaviour in terms of intrinsic damage is not direct. Viscoelastic dissipation induced during cyclic loading leads to thermal increases which affect the material stiffness [1,2]. It is an artefact effect of experimental data and is not representative of the intrinsic fatigue damage due to microcracks in the material.

A simplified approach can be performed considering that the mechanical damage is not dependant of the temperature in the range of temperature change due to viscous effects. This method offers a first step for the evaluation of the intrinsic damage of the

bituminous pavement material [3]. It gives a way to fit a intrinsic damage model corrected from thermal effects [4]. With this uncoupled approach limitations may appear to simulate high loading levels as damage law of the material is certainly dependant of local temperature. In this paper the complete coupled thermomechanical model is proposed to take into account the local coupling between temperature and damage during laboratory fatigue tests.

## 2. Thermomechanical modelling

The constitutive modelling of the viscoelastic damaged material under cyclic loading takes into account two major phenomena: the viscoelastic induced thermal dissipation and its effect on the material stiffness, and the mechanical damage given by the material degradation due to diffuse macrocracking.

The whole set of equations presented below, gather up the complex strain-stress relation, the damage rate equation and the heat problem.

### 1.1 Complex strain-stress law

Under sinusoidal loading, viscoelastic behaviour can be expressed by complex mechanical variables. Micro-cracks of the material lead to damage modelled by a scalar variable  $D$ . It affects the material complex modulus tensor  $C_{ijkl}^*$  which is a function of the temperature  $\theta$  (Eq. 1) :

$$\sigma_{ij}^* = (1 - D) C_{ijkl}^*(\theta) \varepsilon_{kl}^* \quad (1)$$

where  $\sigma_{ij}^*$  and  $\varepsilon_{kl}^*$  are respectively the complex stress and strain tensor components. The complex stiffness tensor  $C_{ijkl}^*$  is function of the complex Young modulus  $E^*$  and the Poisson ration  $\nu$ . According to the French pavement design method,  $\nu$  is assumed to be a constant equal to 0.35.

Viscoelastic behaviour and thermal dependence of the  $E^*$  value are described by the Huet and Sayegh's model proposed in 1963 for bituminous mixtures behaviour (2). It has been found since then to give a very accurate description of asphalt mixes complex modulus over a large range of temperatures and frequencies. It includes the well-known "equivalence principle" between frequency and temperature:

$$E^*(\omega, \theta) = E_0 + \frac{E_\infty - E_0}{1 + \delta (i \omega \tau(\theta))^{-k} + (i \omega \tau(\theta))^{-h}} \quad (2)$$

$E_0$  and  $E_\infty$  are respectively the limit of the complex modulus when  $\omega$  tends to 0 and infinity. For fatigue tests  $\omega$  is fixed and the complex modulus is a function of the temperature  $\theta$ .  $k$  and  $h$  are exponents such as  $1 > h > k > 0$ .  $\delta$  is a dimensionless constant. Function  $\tau = \tau(\theta)$  is the characteristic time, function of temperature, which

Huet and Sayegh suggested to approximate by an Arrhenius or Eyring type law. In fact  $\log(\tau)$  is usually identified in lab by the parabolic law:

$$\tau(\theta) = \exp(A_0 + A_1\theta + A_2\theta^2) \text{ with } -10^\circ C < \theta < 50^\circ C \quad (3)$$

The complex value modulus is fixed by its modulus  $|E^*(\theta)|$  and its phase angle  $\varphi(\theta) = \text{Arg}(E^*(\theta))$ .

### 1.2 Damage evolution

The model used to describe the mechanical degradation due to material microcracking is an elasticity based damage model for fatigue [4,5]. The elasticity framework is applied to the complex modulus and matches the viscoelastic formalism for sinusoidal loading with is offset equal to zero. Evolution of damage is controlled by the strain state of the material by a scalar equivalent strain  $\tilde{\varepsilon}$ . The strains induced by positive principal stresses  $\langle \sigma_i \rangle$  are assumed to lead to the damage growth as follow:

$$\tilde{\varepsilon} = \sqrt{\sum_{i=1}^3 \left( \left( \frac{\langle \sigma_i \rangle}{E(1-D)} \right)^2 \right)} \quad (4)$$

where E the Young modulus of the virgin material. For structural computation, the non-local enhancement of the presented model is used. It replaces the equivalent strain value  $\tilde{\varepsilon}$  by the weighted average of the local equivalent strain taken over a region about the considered point: the non-local equivalent strain  $\bar{\varepsilon}$  [6]. It depends on an internal length which is fixed to three times the maximum aggregate size according to results for cement concrete [7]. When the strain state is homogeneous over the specimen, as in the tension-compression following application, the integral expression of  $\bar{\varepsilon}$  is equal to the local equivalent strain  $\tilde{\varepsilon}$ . An evolution law rules the growth of damage. The damage rate is given as a function of the positive non-local equivalent strain rate, written as:

$$\dot{D} = C D^\alpha \bar{\varepsilon}^\beta \langle \dot{\bar{\varepsilon}} \rangle \quad (5)$$

This leads to a damage evolution law depending on three material parameters C,  $\alpha$  and  $\beta$ . These parameters are fitted on displacement controlled tension-compression tests during sinusoidal loading [4]. Low loading amplitude are used to avoid the mix between damage and thermal effects when fitting the model on experimental data.

### 1.3 Heat conduction equation

The thermal diffusion problem is given by the general heat conduction differential equation:

$$\rho c \frac{\partial \theta}{\partial t} = \text{div} ( k \text{ grad}(\theta) ) + W_{\text{diss}}(\sigma, \varepsilon, \theta) \quad (6)$$

For fatigue of bituminous materials, the last term of (5) is given by the viscoelastic dissipation during a sinusoidal loading cycle :

$$W_{diss}(\sigma, \varepsilon, \theta) = \pi f \sigma_{ij}^* \varepsilon_{ij}^* \sin(\varphi(\theta)) \quad (7)$$

### 3. Tension compression test modelling

Tension compression cyclic tests can be used to assess fatigue performances of bituminous materials [8]. For the beginning of these tests, strain field is quite homogeneous in the sample. But due to the material heterogeneity, the softening behaviour of the material leads to strain localisation around the weakest zone. Before strain field loses its homogeneity, the test is considered homogeneous in terms of strain. These tests are considered in this paper as the modelling of heat transfer is much more simple for this geometry than for the non-homogeneous tests usually used for fatigue. For the simulations, the mechanical stress field is uniaxial in the  $z$  longitudinal direction (Fig. 1).

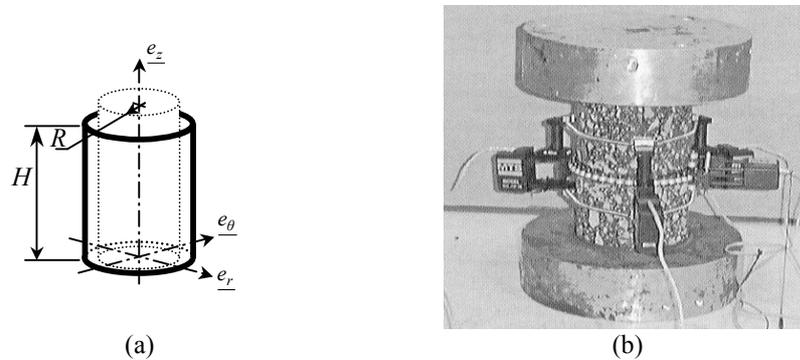


Fig. 1 – Tension compression test: (a) specimen geometry (b) testing device [9].

The thermal problem is assumed to be unidirectional in the  $r$  radial direction from the center ( $r = 0$ ) to the bounding surfaces of the cylindrical sample ( $r = R$ ) (the influence of the clamped surfaces are neglected). Thermal boundary condition is modelled by a linear convection exchange (coefficient  $\lambda$  [W/m<sup>2</sup>/°C]) taken at the boundary of the cylinder ( $r = R$ ). In this idealised case, local equations and boundary conditions can be rewritten.

The whole set of equations is the following one, expressed for both displacement and force controlled conditions :

Damage evolution:

- displacement 
$$\frac{\partial D}{\partial t} = f C D^\alpha \frac{\varepsilon_a^{\beta+1}}{\beta+1} \quad (8)$$

$$\text{- force} \quad \frac{\partial D}{\partial t} = f C D^\alpha \frac{\left(\frac{\sigma_a}{1-D}\right)^{\beta+1}}{\beta+1} \quad (9)$$

Heat conduction:

$$\text{- displacement} \quad \rho c \frac{\partial \theta}{\partial t} = \frac{\partial^2 \theta}{\partial r^2} + \frac{1}{r} \frac{\partial \theta}{\partial r} + \pi f (1-D) \varepsilon_a^2 \left| E^*(\theta) \right| \sin(\varphi(\theta)) \quad (10)$$

$$\text{- force} \quad \rho c \frac{\partial \theta}{\partial t} = \frac{\partial^2 \theta}{\partial r^2} + \frac{1}{r} \frac{\partial \theta}{\partial r} + \pi f \frac{\sigma_a^2}{(1-D) \left| E^*(\theta) \right|} \sin(\varphi(\theta)) \quad (11)$$

Loading mechanical boundary condition at the top of the specimen:

$$\text{- displacement} \quad u_z(r, H) = H \varepsilon_a \sin(\omega t) \quad (12)$$

$$\text{- force} \quad u_z(r, H) = H \frac{\sigma_a}{(1-D) \left| E^*(\theta) \right|} \sin(\omega t) \quad (13)$$

Loading mechanical boundary condition at the bottom of the specimen:

$$u_z(r, 0) = 0 \quad \text{and} \quad u_r(0, 0) = 0 \quad (14)$$

Thermal boundary conditions and initial conditions (independent of loading type):

$$\begin{cases} \frac{\partial \theta}{\partial r}(0, z) = 0 \\ k \frac{\partial \theta}{\partial r}(R, z) = -\lambda(\theta - T_{ext}) \end{cases} \quad \begin{cases} D(r, z)_{t=0} = 0 \\ \theta(r, z, t=0)_{t=0} = T_{ext} \end{cases} \quad (15)$$

These equations lead to a pure radial problem. In that conditions, the numerical implementation is done using the standard Matlab® function to solve initial boundary value problems for system of parabolic and elliptic partial differential equations in one space variable and time.

#### 4. Comparison with experimental data

In this paper, we use experimental data part of the RILEM fatigue interlaboratory project [8] that include a large experimental campaign on the same material for 10°C and 10 Hz conditions. Tests carried out on cylindrical samples (height 120 mm and diameter 80 mm) made of a 0/6 mm dense asphalt concrete containing 6.4% of 50/70 pure bitumen are presented here. They gather tests performed under constant strain amplitude  $\varepsilon_a$  (80, 100, 140 and 180  $10^{-6}$ ) and constant stress amplitude  $\sigma_a$  (0.8, 1.0 and 1.2 MPa) at KTH [9]. During the tests, the force applied to the sample is recorded and allows to calculate the stress to which the material is subjected. A three displacement transducer extensometer gives the strain values applied to the sample. A PT100 temperature transducer, glued at the middle of the sample, records the surface temperature evolution during the test with the least clamped surface influence.

For the tested bituminous mix, the viscoelastic model parameters (3) are the following:  $E=29700$  MPa,  $E_0=25$  MPa,  $h=0.675$ ,  $k=0.21$ ,  $\delta=2.16$  and  $A_0=1.71$ ,  $A_1=-0.393$  and  $A_2=0.00226$ .

Parameters of the damage evolution law (4) have been fitted for this material and these temperature and loading frequency for low strain level tests ( $\epsilon_a=80 \cdot 10^{-6}$ ) as  $\alpha=-2.25$ ,  $\beta=4$  and  $C=2.87 \cdot 10^{12}$  [4].

Volumetric heat capacity and heat conductivity are respectively set as  $\rho c = 1.6 \cdot 10^6$  J/m<sup>3</sup>/°C and  $k=0.8$  W/m/°C. The surface convection coefficient characteristic of the testing device, has been adjusted from a standard value to  $\lambda=34$  W/m<sup>2</sup>/°C in order to obtain the same maximal temperature increases for displacement controlled tests (Fig. 2(a)).

The comparison between experimental data and value given by the model is based on two thermomechanical variables. First the temperature at the surface of the sample can be compared with the calculated value for  $r=R$  (Fig. 2). Then the macroscopic specimen stiffness data are compared to the model computation result.

#### 1.4 Displacement controlled tests

For displacement controlled tests the agreement between simulation and the temperature measures is very good (Fig. 2(a)). For the highest loading levels (i.e. 140 and 180  $\cdot 10^{-6}$ ) the temperature curves confirm the decrease after a maximum value. A damage free thermomechanical model leads to an asymptotic value. In the model this decrease is due to the damage coupling in the dissipated energy term which decreases with  $D$  (10).

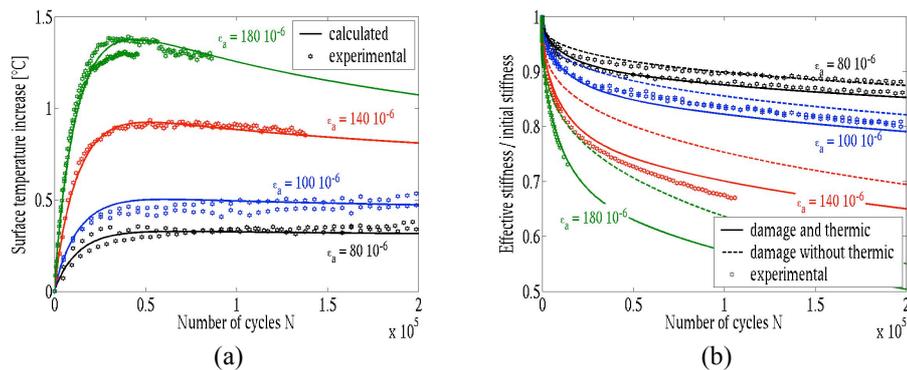


Fig. 2 – Comparison between model and experimental data for displacement controlled tests: (a) surface temperature increase (b) specimen stiffness decrease.

The global specimen stiffness is then compared with the experimental data figure 2(b). The complete modelling presented here (solid line) and the damage model without thermal effects (dashed line) are presented with experimental data. For low loading

levels (i.e. 80 and 100  $10^{-6}$ ) the difference between the both approaches is in the range of the experimental scattering. This model then allows to simulate high loading levels for which thermal effects become important (more than 10 %).

### 1.5 Force controlled tests

When looking the thermal increase presented figure 3(a), the accuracy of the temperature increase is acceptable for the lowest tested stress amplitude (i.e. 0.8 MPa). However it becomes less good when the loading level increases. For 1.0 and 1.2 MPa constant stress amplitude, the thermal prediction is acceptable (difference less than 0.1°C) at the beginning of the fatigue process ( $N < 50000$  cycles).

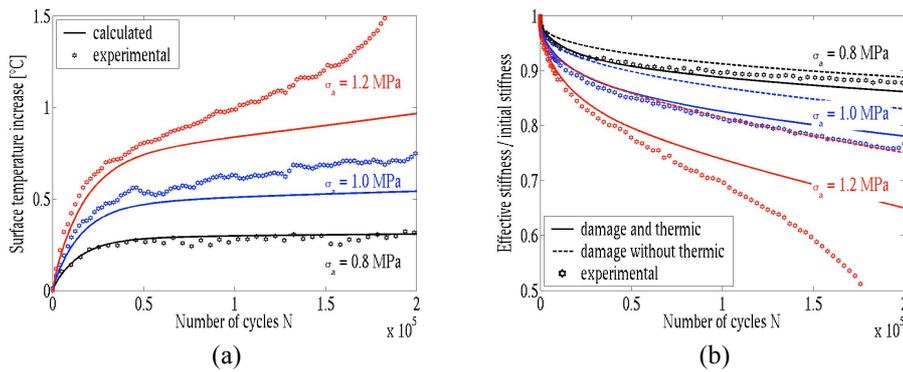


Fig. 3 – Comparison between model and experimental data for displacement controlled tests: (a) surface temperature increase (b) specimen stiffness decrease.

For the global stiffness calculation (Fig. 3(b)) the simulation results show that simulated value is acceptable (difference between 5 %) since the temperature increase is well simulated. Like for displacement controlled test, the thermomechanical enhancement (solid line) provide the ability of simulating high loading levels which is not possible considering the damage without thermal effects (dashed line) .

The present modelling gives very encouraging results however differences appear for force controlled tests performed at “high” strain levels. An explanation of this discrepancy can be based on the time dependant viscoelastic effect which leads to an increase of the strain offset during the tests. This phenomenon may lead to a higher damage rate not taken into account in the presented damage model.

## 5. Conclusion

The thermomechanical model for bituminous material under cyclic fatigue tests consists in taking into account thermal and damage local effects in the material stiffness decrease. Coupling both phenomena leads to a complete thermomechanical damage model. For

uniaxial tension compression tests, simulations are compared to experimental measurements. The thermomechanical approach is validated by the temperature field obtained by the model compared to the experimental ones. For displacement controlled test, temperature evolution shows a decrease due to the influence of damage on the dissipated energy. For both displacement and force controlled fatigue tests agreement between the model and experimental results of specimen stiffness decrease is good since the temperature calculation is acceptable.

Future prospect will consist in a finite element implementation to perform simulation for any fatigue test geometry (trapezoidal cantilever beam, three or four point bending, ...). This modelling of laboratory fatigue tests corresponds to an efficient way to assess the intrinsic damage which could be performed on high loading levels which are shorter fatigue tests.

## 6. Acknowledgements

Bituminous material modelling is part of an LCPC research area. The experimental results presented here have been obtained by the KTH laboratory in Sweden. They are part of an international interlaboratory program on fatigue tests of bituminous mixtures conducted in the frame of RILEM, TC 180 PEB.

## 7. References

1. Piau, J.-M., 'Modélisation thermomécanique du comportement des enrobés bitumineux', *Bulletin de Liaison du LCPC* **163** (1989) 41-55.
2. de La Roche, C., Piau, J.-M., and Dangla, P., 'Thermal effects induced by Viscoelastic dissipation during fatigue tests on bituminous mixtures', Proceedings of the 6<sup>th</sup> Int. Symp. On Creep and Coupled Process, Bialowieza, September, 1998.
3. Bodin, D., de La Roche, C., Piau, J.-M. and Pijaudier-Cabot, G., 'Prediction of the intrinsic damage during bituminous mixes fatigue tests', Proceedings of the 6<sup>th</sup> RILEM Symposium PTEBM, Zurich, 2003, 380-386.
4. Bodin, D., 'Modèle d'endommagement par fatigue : Application aux enrobés bitumineux', PhD thesis, Ecole Centrale, Nantes, 2002.
5. Bodin, D., Pijaudier-Cabot, G., de La Roche, C. and Chabot, A., 'A continuum damage approach of Asphalt Concrete fatigue tests', Proceedings of the 15<sup>th</sup> ASCE Engineering Mechanics Conference, New York, June, 2002
6. Pijaudier-Cabot, G. and Bazant, Z. J., 'Nonlocal damage theory', *Journal of Engineering Mechanics*, **113** (10) (1987) 1512-1533.
7. Bazant, Z. J. and Pijaudier-Cabot, G., 'Measurement of characteristic length of nonlocal continuum', *Journal of Engineering Mechanics*, **115** (4) (1989) 755-767.
8. Di Benedetto, H., de La Roche, C., Baaj, H., Pronk, A., Lundström, R., 'Fatigue of Bituminous mixtures: different approaches and Rilem group contribution', Proceedings of the 6<sup>th</sup> RILEM Symposium PTEBM, Zurich, 2003, 15-38.

9. Lundström, R., 'Rheological and fatigue characterisation of asphalt concrete mixtures using uniaxial testing', Licenciate Thesis, Stockholm, 2002.