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Impact of the asphalt binder rheological behavior on the value of the ΔT_c parameter

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ABSTRACT

The current Performance Grading system for asphalt binders in the USA, determines limiting temperatures for appropriate binder use. Practically, the low limiting temperature is based on rheological measurements made using the Bending Beam Rheometer (BBR). The absolute value of the creep modulus and/or its slope vs loading time in a log scale, are being used. However, the current specifications are being challenged and it has been proposed adding a new criterion, called ΔT_c ; its ability to better predict thermal cracking is the subject of intense discussions.

In this paper, we mathematically show how ΔT_c relates to other binder rheological properties. Then, we discuss the practical consequences in order to highlight the relevant parameters controlling ΔT_c . Experimental data are used to substantiate the main findings. This shows that ΔT_c is of “pure” rheological origin and care must be taken when trying to predict cracking results from rheological data only.

Keywords:

Asphalt Rheology, Linear Viscoelasticity, Rheological Cracking Surrogates, ΔT_c parameter

1. INTRODUCTION

The Performance Grading system for asphalt binders was developed in the USA thanks to the Strategic Highway Research Program (SHRP) in the late 1980's [1,2]. Also called Superpave binder specifications (AASHTO M320 / ASTM D6373), it relies essentially on two parameters: one characterizing the binder in the high temperature range, the other one in the low temperature range. As a result, the binder is said to be PG H-L where H is the limiting high temperature and -L, the limiting low temperature. For example, a binder with PG 58-28 means that its limiting high temperature is 58°C while its limiting low temperature is -28°C. In engineering terms, this means that the binder shouldn't be exposed to operating temperatures outside of this range. More precisely, an asphalt mixture made with the said binder would have an increased risk of rutting (permanent deformation) or thermal cracking if the outside temperature falls outside of the binder operating range.

Thermal cracking is largely observed in cold climatic zones. In these regions, the low temperatures experienced by the asphalt, make it essentially perform in or close to its glassy state where it becomes brittle. As a consequence, the thermal shrinkage occurring during cooling leads to the development of stresses that can overcome the materials strength, hence generating cracks in the pavement [3].

In the SHRP days, a new device, the Bending Beam Rheometer (BBR), was developed as a tool to predict binder propensity for thermal cracking [4]. As a consequence, the limiting low temperature, $-L$, now corresponds to the highest between the temperature $T_c(S)$ at which the flexural creep modulus of the binder becomes superior to 300 MPa and the temperature $T_c(m)$ where the slope of the stiffness vs time becomes inferior to 0.3. Both parameters must be measured with the BBR at a loading time of 60 s (AASHTO T313). Grades are defined by 6°C steps starting from -10°C down to -46°C. $-L$ stated in the paving grade is in fact 10°C lower than that of the creep test. In other words, if the binder has a creep modulus of 300 MPa at -12°C and 60s, $-L$ will be -22°C. The idea behind this criterion is that too-rigid a binder generates a risk of cracking for the pavement.

The reason why a criterion based on the slope was also added, is because it was observed that some binders, for example airblown materials, were still brittle even if the modulus was rather low. The modulus was then associated with a low ability to dissipate mechanical loads by viscous flow, hence a criterion on the slope [2,5].

Since the implementation of Superpave, the formulation and manufacture of paving grade binders have changed considerably owing to a number of economic, technical, and environmental reasons. More than ever, blending of crude oils, refining processes producing residues different from straight run vacuum residues classically used to make asphalts, and modification of base binders with various additives are being implemented. Even more changes are likely to occur soon, with the implementation of new sulfur specifications on bunker fuels that may release more unstable residues (conversion residues such as visbroken residues) in the asphalt pool.

As a consequence, the current specifications are being challenged and several authors proposed to add a new criterion, called ΔT_c . It is defined as the difference between $T_c(S)$ and $T_c(m)$, and was indeed introduced by Robertson et al. in 2001 [6] in order to highlight the specific behavior of waxy and airblown asphalts. However, the ΔT_c parameter did not become popular as a research tool and even a possible specification parameter until 10 years later when it was revisited by Anderson et al. [7], where it was shown that ΔT_c correlated with non-load related cracking at the pavement surface observed in the field. Since then, it has been largely used and its ability to better predict thermal cracking than existing criteria is the subject of intense discussions [8,9].

In this paper, we show how ΔT_c relates to other binder rheological properties, based on a mathematical argument. Then, we discuss the practical consequences with the objective to highlight the relevant parameters controlling ΔT_c . Experimental data are used to substantiate the main findings. This clearly shows that ΔT_c is of “pure” rheological origin, in the sense that it considers only the deformation of the material and not its fracture, and care must be taken when trying to predict cracking results from rheological data only [10].

2. BACKGROUND

2.1 Asphalt binders rheology

Asphalt binders are viscoelastic materials with a mechanical response that strongly depends on loading time and temperature [11]. They are therefore characterized by methods such as dynamic mechanical spectroscopy using a Dynamic Shear Rheometer (DSR) or creep tests using the BBR, that either measure the complex modulus or the creep stiffness at a given temperature, as a function of testing frequency or loading time.

At low temperature (typically below -20 °C for loading times of order of a few seconds), the tensile modulus reaches a constant value close to 3 GPa independent of temperature and frequency corresponding to the glassy state. This value is typical of amorphous organic materials such as polymers. In parallel, the phase angle diminishes down to values close to 0°.

At high temperature (typically significantly above its softening temperature of ~40-60 °C), bitumen is a viscous Newtonian liquid characterized by a temperature-dependent viscosity η_0 . In this region, the phase angle increases up to values close to 90°.

In-between these two asymptotic behaviors, the mechanical response of an asphalt binder is intermediate between that of an elastic solid and a viscous liquid and is thus viscoelastic. In all cases, bitumen at room temperature is a highly viscous viscoelastic liquid and the often found confusing terms such as pasty solid or a semisolid should be avoided.

Note also that the thermal history must be carefully controlled in order to obtain reliable rheological data on bituminous binders [12].

Several mathematical models have been proposed to describe asphalt binders rheological behavior [11,13,14,15,16,17]. A very convenient one is the Christensen-Anderson model (CA model - [18]) because of its accuracy and limited number of fitting parameters. The CA model expresses the creep stiffness $S(t)$ of an unmodified asphalt binder as the following function of loading time t :

$$S(t) = S_g [1 + (\omega_x t)^\beta]^{-\frac{1}{\beta}} \quad (1)$$

where S_g is the glassy modulus, ω_x is the crossover frequency and β is an exponent. β is directly related to the so-called rheological index R by the following relationship:

$$\beta = \frac{\log 2}{R} \quad (2)$$

where the rheological index is defined as:

$$R = \log(S_g) - \log(S_x) \quad (3)$$

where S_x is the crossover modulus, obtained for a specific value of the loading time corresponding to the crossover time $t_x = 1/\omega_x$.

The CA model gives the following value for the slope $m(t)$ of the $-\log S(t)$ vs $\log t$ curve:

$$m(t) = [1 + (\omega_x t)^\beta]^{-1} \quad (4)$$

Equations (1) and (4) are known to accurately describe the low-temperature behavior of unmodified asphalt binders [18,19] and is therefore very relevant in the temperature range of BBR measurements. It is generally accepted that, for temperatures below room temperature, only ω_x is temperature-dependent [18,19]; R and S_g can therefore be considered to be essentially constant for a given binder.

It is also well accepted that time-Temperature Superposition principle (t-TSP) holds for asphalt binders in the same temperature range [18,19], except maybe for highly waxy or asphaltenic materials [20]. This means that the data at a given temperature can be used to reliably estimate the response of the same binder at a different temperature. In order to do so, shift factors a_T are generally used, based on the Williams-Landel-Ferry (WLF) master curve construction method [21]. They simply relate the crossover frequency ω_x at the reference temperature T_r to the crossover frequency $\omega_{x,T}$ at any given temperature T by:

$$\omega_{x,T} = \frac{\omega_x}{a_T} \quad (5)$$

The simplest way to describe how the shift factors a_T depend on temperature, is:

$$\log a_T = \frac{E_A}{2.303 R} \left(\frac{1}{T} - \frac{1}{T_r} \right) \quad (6)$$

where E_A is the activation energy, \bar{R} , the ideal gas constant (8.314 J/mol.K) and T_r , the reference temperature [18,19,22]. The reference temperature was also called the “defining temperature” [13,18] and can be left constant (generally the reference temperature chosen for the shifting procedure) or can even be chosen so that the activation energy is constant regardless of binder origin [13].

As reviewed elsewhere [13,19], researchers generally prefer to use the celebrated Williams-Landel-Ferry (WLF) equation instead of equation (6). Yet other fitting functions have also been proposed [23,24], for example the Kaelble equation that was particularly recommended

at low temperature [23]. Still, a detailed comparison between fitting functions showed that the Arrhenius fit was very accurate even if the correlation could be improved by using other functions [24]. Given the narrow temperature range considered in this work (essentially limited to negative temperatures), making use of a different function would therefore only provide a very minor improvement if any. Therefore, we will stick to equation (6) for the sake of simplicity.

2.2 Thermal cracking in performance-based specifications

As explained above, thermal cracking of asphalt mixtures is largely observed in cold climatic zones and is found when the asphalt binder approaches its glassy state where it becomes brittle [3].

Thermal cracking is generally studied in the lab by measuring stresses and specimen temperature in a restrained asphalt mixture specimen subjected to a cooling cycle (10°C/hr is typical) as in the Thermal Stress Restrained Specimen Tensile Strength (TSRST). The dimensions of the prismatic specimens are maintained constant such that thermal stresses build up as the specimen contracts. The temperature at which the specimen breaks is then recorded. The test is known to be quite accurate at identifying the risk of thermal cracking [25].

During SHRP when the Bending Beam Rheometer (BBR) was being developed as a binder predictor for thermal cracking of mixtures in AASHTO MP1-97 [1], one of the most convincing pieces of evidence that rheological parameters could predict fracture behavior, came from correlations between binder parameters, especially $T_c(S)$, the temperature at which the creep stiffness after 60 s reaches 300 MPa, and mix TSRST data [25]. However, it was understood from the beginning that some materials could exhibit a high stiffness and yet withstand crack propagation. This is why the Direct Tension Test, then AASHTO TP3, could be used [26]. If a minimum of 1% strain at failure could be observed at a temperature $T_{1\%}$ lower than the cracking temperature $T_c(S)$, then $T_{1\%}$ could be used instead, as long as the stiffness remained below 600 MPa [2]. Thus, it was anticipated that some binders could be better at resisting thermal cracks than anticipated by BBR only. However, the repeatability of the DTT method proved much lower than that of BBR and it was difficult to measure $T_{1\%}$ with good enough precision for specification purposes. The criterion was therefore abandoned when AASHTO MP1a was released in 2002. It is also important to note that although early versions of the SHRP binder specification relied only on BBR stiffness to predict thermal cracking, eventually the log-log slope of the BBR creep curve (m -value) was added to the requirements. In fact, the requirement for a minimum m -value of 0.30 in the BBR test is most often the controlling limit, rather than the maximum stiffness of 300 MPa. Unfortunately, there is little documentation explaining why m -value should be a critical factor in thermal cracking. It is possible that BBR stiffness relates to single-event thermal cracking, while m -value limits the susceptibility for thermal fatigue. In any case, these are both rheological parameters that might work well for many binders, but are problematic when applied to many binders modified by polymers or other additives.

Currently, the specifications are being challenged and several authors have proposed adding a new criterion [9], ΔT_c , defined as:

$$\Delta T_c = T_c(S) - T_c(m) \quad (7)$$

where $T_c(S)$ is the temperature at which the creep stiffness of the binder reaches 300 MPa, and $T_c(m)$, is the temperature at which the slope reaches 0.3, both parameters being measured in the BBR after 60 s loading time.

ΔT_c was indeed introduced by Robertson et al. [6] in order to highlight the specific behavior of waxy and airblown asphalts. It was indeed shown that the higher risk of thermal cracking with these specific binders, could be anticipated by their more negative ΔT_c values.

Still, ΔT_c did not become popular until 10 years later when it was revisited by Anderson et al. [7], where it was shown that ΔT_c correlated with non-load related cracking at the pavement surface observed on airports. Since then, it has been largely used and its ability to better predict thermal cracking than other criteria is the subject of intense research [8,10,27,28,29]. The fact is that current criteria are being especially challenged for complex materials such as aged, polymer-modified binders or those with unusual compositions such as those incorporating Re-refined Engine Oil Bottoms (REOB - [10,30,31,32]. The determination of ΔT_c has thus become common practice and can be made based on existing standard AASHTO M323 [8].

3. THEORETICAL DERIVATION OF ΔT_c FROM FUNDAMENTAL RHEOLOGICAL PARAMETERS

3.1 Theoretical derivation

Based on the above description of asphalt rheological behavior in the time domain (equations 1-4), it is possible to relate ΔT_c to basic rheological parameters. It has indeed already been shown that ΔT_c can be calculated based on a rheological modeling of the BBR curves [37]. In particular, the Christensen-Anderson model (CA model) proved very valuable to do so because of its excellent fit at low temperature and the limited number of parameters needed [18]. This means that there is a theoretical relationship between ΔT_c and other rheological parameters such as the rheological index, the glassy modulus and the crossover frequency. What remains to be clarified is whether one of these parameters has a stronger impact than the others. This is the objective of the current derivation.

The starting point is to recognize that $T_c(S)$ can be written from equation (1) as the temperature at which the stiffness is 300 MPa when measured after 60s loading time. This gives:

$$S(60s) = 300 \text{ MPa} = S_g \left[1 + \left(\frac{\omega_x}{a_{T_c(S)}} 60 \right)^\beta \right]^{-\frac{1}{\beta}} \quad (8)$$

This can be further developed to show that the shift factor $a_{T_c(S)}$ at $T_c(S)$ is given by (with S_g in MPa):

$$\log a_{T_c(S)} = \log 60 + \log \omega_x - \frac{1}{\beta} \log \left[\left(\frac{300}{S_g} \right)^{-\beta} - 1 \right] \quad (9)$$

In the same way, from equation (4), the shift factor $a_{T_c(m)}$ at $T_c(m)$, the temperature where the slope measured after 60s loading time is 0.3, is shown to be:

$$\log a_{T_c(m)} = \log 60 + \log \omega_x + \frac{1}{\beta} \log \left[\frac{1}{0.3} - 1 \right] \quad (10)$$

Injecting equations (8) and (10) in equation (6) gives the following expression for ΔT_c :

$$\Delta T_c = \frac{E_A}{2.303\bar{R}} \frac{1}{\beta} \frac{\log \left[\frac{1}{0.3} - 1 \right] - \log \left[\left(\frac{300}{S_g} \right)^{-\beta} - 1 \right]}{\left(\log a_{T_c(S)} + \frac{E_A}{2.303RT_d} \right) \left(\log a_{T_c(m)} + \frac{E_A}{2.303RT_d} \right)} \quad (11)$$

In order to better highlight the weight of the rheological index, it can be rewritten as:

$$\Delta T_c = R \frac{E_A}{2.303\bar{R} \log 2} \frac{\log \left[\frac{1}{0.3} - 1 \right] - \log \left[\left(\frac{300}{S_g} \right)^{-\beta} - 1 \right]}{\left(\log a_{T_c(S)} + \frac{E_A}{2.303RT_r} \right) \left(\log a_{T_c(m)} + \frac{E_A}{2.303RT_r} \right)} \quad (12)$$

where E_A is the activation energy, \bar{R} , the ideal gas constant, $\beta = \log 2/R$, T_r , the reference temperature, S_g the Glassy modulus (in MPa).

3.2 Parameters controlling ΔT_c

The main consequences of equation (12) are illustrated as follows. As a reminder, the derivation assumes t-TSP holds in the low temperature range and shift factors can be described using an Arrhenius law. It also assumes the CA model correctly describes the rheological behavior of the binder. Both assumptions are known to be valid for unmodified binders in the temperature range where BBR data are measured [18,19].

So, equation (12) shows that ΔT_c is essentially proportional to R and to the activation energy, but with a proportionality factor that also depends on both of them and of other parameters. Note that the crossover frequency ω_x is not apparent in equation (12) but is indeed hidden in the shift factors. Therefore, it is difficult to intuitively estimate which of the rheological parameters would predominantly control the absolute value of ΔT_c .

In order to make more sense of equation (12), it was first observed that the rheological index R generally lies in the 1.2-2.0 range for neat asphalt cements (Table 1). After short-term aging (TFOT), it increases to the 1.7-2.3 range. After long-term aging (TFOT+PAV), it tends to increase to the 1.4-2.7 range. Clearly, the higher the aging extent, the higher R. Note that R was proposed to be correlated to the total amount of colloidal matter in the asphalt cement and would therefore be proportional to the asphaltenes content for a given crude oil source [33]. We thus decided to take the 1.2-3.5 range for R variation, which is therefore thought to cover all existing unmodified asphalt binders, aged or unaged.

Likewise, the values taken for the crossover frequency cover the 10^{-5} - 10^5 rad/s range for a fixed reference temperature of -5°C . This represents a huge range given that values found in the literature, regardless of the aging level, remain between $10^{-1.5}$ and $10^{4.5}$ rad/s at 25°C (Table 1).

Values for the activation energy are scarce in the literature. The value of 261 kJ/mol given in the initial SHRP report [13], looks rather high compared to other published data (Table 2). A reason for that is that it was chosen to fit only the temperature range below the reference temperature T_r . Given the values found for T_r for the 8 reference binders (the so-called “SHRP core asphalts” - Table 2), the constant activation energy was therefore higher than others because it concentrated on the lower end of the temperature range.

Another very complete data set is the one from [24], who found a range of 140-203 kJ/mol for 7 modified and unmodified binders in the 5 - 75°C range (Table 2). They noted that activation energy was “relatively constant for this set of binders with only a slight decrease with increasing temperature” [24], a fact that was confirmed by the calculations at several temperatures made by Yin et al. [34] (Table 2). More precisely, the activation energy was found to vary by $\sim\pm 10\%$ over the whole temperature range in both studies.

Given the range covered by published values (Table 2), we decided to let the activation energy vary between 100 and 300 kJ/mol, a range also consistent with our own set of data (see below).

Table 1. Rheological index R and crossover frequency (ω_x) and reference Temperature (T_r) for the 8 SHRP core asphalt cements at different aging levels (from [13]).

Binder	neat			TFOT			TFOT+PAV (60°C 144hrs)		
	R	log ω_x (log rad/s)	T_r (°C)	R	log ω_x (log rad/s)	T_r (°C)	R	log ω_x (log rad/s)	T_r (°C)
AAA-1	1.50	-2.03	-19.3	1.75	-1.98	-14.3	1.90	-2.95	-14.5
AAB-1	1.76	-1.97	-11.6	2.06	-2.02	-5.3	2.13	-2.87	-6.0
AAC-1	1.63	-1.16	-5.5	1.80	-1.89	-3.8	2.10	-1.58	3.5
AAD-1	1.66	-2.01	-17.1	1.80	-2.25	-13.3	2.07	-2.54	-8.7
AAF-1	1.60	-2.07	-7.0	1.77	-2.08	-1.4	2.02	-2.22	5.2
AAG-1	1.24	-1.43	-3.9	1.35	-1.21	0.8	1.44	-1.74	2.7
AAK-1	1.60	-2.33	-14.7	1.80	-2.39	-9.3	1.94	-3.1	-9.2
AAM-1	1.93	-1.26	1.0	2.21	-1.51	4.8	2.61	-2.61	6.0

Table 2. Some values of the activation energy found in the literature.

Study	Binder set	Testing range	T_r range	E_a range (kJ/mol)
Anderson et al. [13]	24 binders: 8 SHRP core asphalt at 3 aging levels	-40 to 80°C	-19 to 6°C	261
Vargas et al. [35]	5 binders: 5 aging levels for a neat 70/90 pen grade asphalt cement	40 to 100°C	60°C	117-145
Yusof et al. [24]	7 binders: 1 neat asphalt cements modified with 3 contents (3, 5, 7 wt.%) of SBS or EVA polymer	5 to 75°C	5 to 75°C	140-203
Mouazen et al. [36]	1 neat 70/100 pen grade asphalt cement	-20 to 100°C	50°C	168
Airey et al. [22]	9 binders: 5 neat, 2 polymer-modified, 2 crumb-rubber modified and 1 synthetic	5 to 75°C	25°C	102-191
Yin et al. [34]	4 binders: 3 neat and 1 polymer-modified	-10 to 60°C	-10 to 60°C	125-174

The tensile glassy modulus was kept constant at 3 GPa, a very reasonable value [11,13]. When allowing the various parameters to vary in the above-mentioned ranges, results given in Figure 1 were obtained.

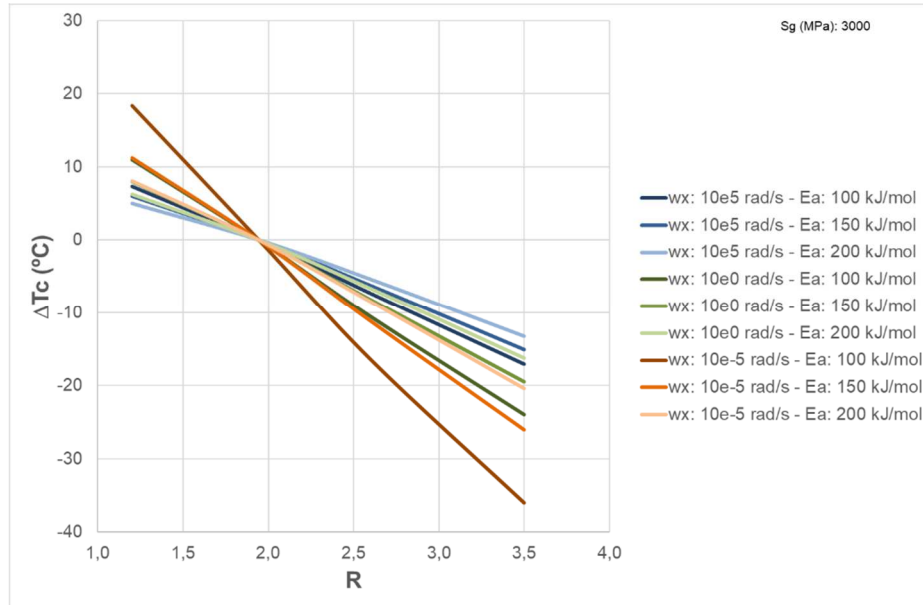


Figure 1. Theoretical ΔT_c as a function of the crossover frequency for various values of the rheological index (R) for various values of the crossover frequency ω_x and the activation energy E_A . Calculations were made for a glassy modulus $S_g = 3$ GPa and a reference temperature $T_r = -5^\circ\text{C}$.

Figure 1 clearly illustrates that the rheological index controls the sign of ΔT_c .

If R is lower than 1.94, which is the case for many typical unmodified binders, then ΔT_c is positive. A large change in crossover frequency, has then a limited impact on ΔT_c , except for extremely low values of order 10^{-5} when combined with low activation energies (Figure 1): for $R = 1.2$, corresponding to a lower limit for this parameter (the lowest rheological index for the 8 SHRP core asphalts was 1.24 for unaged AAG-1 – Table 1), the mean ΔT_c is around 7.5°C . Only extremes values for the crossover frequency (10^{-5} rad/s) and the activation energy (100 kJ/mol), are then needed to obtain a marked additional change in ΔT_c .

For $R = 1.94$, $\Delta T_c = 0$ regardless of all the other rheological parameters (crossover frequency and activation energy).

For $R > 1.94$, ΔT_c becomes negative. The higher R , the more negative ΔT_c . For $R = 2.8$, which would be higher than the highest rheological index observed after TFOT+PAV on the 8 SHRP core asphalts (namely aged AAM-1 with 2.61 - Table 1), the effect of crossover temperature and crossover frequency is somewhat symmetrical to what was observed for $R = 1.2$. It takes $R = 3.5$ to start seeing a pronounced impact of the crossover frequency and activation energy, as shown in Figure 1.

Of course, this derivation holds if t-TSP holds. For binders that exhibit thermo-rheologically complex behavior [20], the proposed correlation between rheological index and ΔT_c might not apply.

So, why is it that the rheological index, which governs the shape of the master curve, has such a big impact on ΔT_c , which would intuitively be more related to the temperature dependency? This is because ΔT_c is indeed a shape parameter! More precisely, ΔT_c is related to the difference in position on the master curve of two specific points: one of constant stiffness value and the other of constant slope in the time domain (Figure 2). Therefore, the distance between these 2-points is indeed controlled by the shape of the curve, and they will coincide or not depending mostly on the Rheological index.

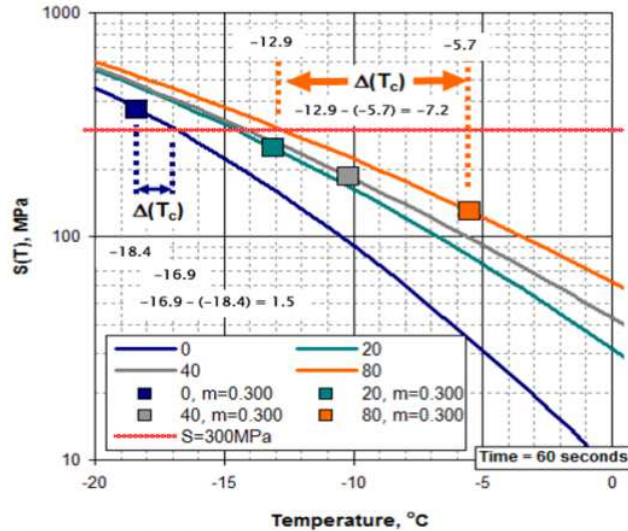


Figure 2. Graphical interpretation of ΔT_c : The creep stiffness at 60 s loading is plotted vs temperature for different aging levels (0, 20, 40 or 80 hours in PAV) of a given asphalt binder. The longer the aging, the higher the rheological index R . The red line marks a constant value of 300 MPa; T_S thus represents the intercept between the stiffness curve and the red line. $T_c(m)$ is identified on the curve by a square. Increasing R clearly moves towards more negative values (from [37]).

So, what is so special about $R = 1.94$? In fact, this value has no specific meaning and arises only because of the choice of limiting parameters. If the limiting stiffness had been fixed at 400 MPa and the limiting slope at 0.28, then the critical rheological index making ΔT_c change sign, would be different. Therefore, the reason why critical $R = 1.94$, is due to the fact that this set of limiting parameters was chosen based on the available data back then, making sure most known binders would have more or less similar $T_c(S)$ and $T_c(m)$. Given that the limiting temperatures must be evaluated on aged binders, the set of values shown for the corresponding SHRP core asphalts in Table 1 clearly illustrate that $R = 1.94$ is indeed close to their mean value after TFOT+PAV ($R = 2.03$). In other words, the definition of the limiting temperatures made it such that ΔT_c would be close to 0 for the available neat binders at that time, which happened to have a rheological index close to $R = 1.94$.

Note that is also linked to the fact that the glassy modulus was fixed to 3 GPa. If it were allowed to change, then the rheological index making $\Delta T_c = 0$, would also change. Therefore, the critical value of $R = 1.94$ must not be taken as an exact threshold, but more as an estimate. We will return to this point later on.

As a matter of fact, Rowe [37] already observed that a threshold R -value of 1.923 would make the material m -controlled or R -controlled. This means that the critical temperature fixing the limiting low temperature (called $-L$ in the introduction) would be either $T_c(m)$ (m -controlled corresponding to $\Delta T_c < 0$) or $T_c(S)$ (S -controlled corresponding to $\Delta T_c > 0$). Our findings are therefore fully reminiscent of this earlier observation.

In summary, the above derivation shows that the main rheological parameter controlling ΔT_c is indeed the rheological index. This is because the very definition of ΔT_c is in fact related to the shape of the master curve, since it compares the position of two very specific points on the master curve, one of constant stiffness and the other of constant slope in the time-domain. For a given value of R (close to 1.94 for the parameters shown in Figure 1), ΔT_c becomes 0 meaning that the shape of the master curve is such that both points coincide. A value of R

significantly below would then make ΔT_c positive while a higher value would make ΔT_c negative.

3.3 Practical consequences

There are several immediate practical consequences to these results.

First, the effect of using a harder grade versus a softer one for a given crude source and process, would increase the rheological index and slightly decrease the crossover frequency [33]. Both factors should displace ΔT_c towards more negative values. It would still remain positive as long as R remains below 1.94. The exact same effect is expected with binder aging, as observed in several studies [8,37].

Second, the use of REOB is known to affect the colloidal stability of asphalt binders by precipitating asphaltenes yet plasticizing the maltenes (ie reducing their viscosity). Given the positive correlation between solid fraction (similar in concept to precipitated asphaltenes) and rheological index [8], this should increase the rheological index but decrease the crossover frequency. Given that the rheological index has more impact on ΔT_c than the crossover frequency (Figure 1), we expect the global effect to be rather controlled by the rheological index. Thus, REOB addition should displace ΔT_c towards more negative values, as observed in former studies [32,37,38].

Finally, polymer-modification is known to generate a two-phases structure within the binder. With the usual polymer-contents found for in most current paving applications, a continuous phase concentrated in asphaltenes is obtained together with a dispersed phase rich in polymer [11]. On a rheological standpoint, it was demonstrated based on the Palierne model, that the continuous phase indeed controls the rheology of the binder [39]. Given that the asphaltenes content inside the continuous phase is increased versus the asphaltenes content of the parent asphalt, the effect becomes similar to that of aging explained above. Therefore, polymer-modification is expected to shift ΔT_c towards more negative values as compared to that of the parent asphalt, a fact consistent with published data [8].

As a matter of fact, the effect of any additive on ΔT_c can therefore be easily anticipated if its impact on basic rheological parameters is documented.

4. COMPARISON WITH MEASURED DATA

4.1 Measured data

In order to validate these findings, 8 binders were tested using the BBR. Table 3 summarizes the binders and corresponding field sections that were selected in this study. The binder selection was carefully done to represent a wide range of mostly unmodified binders and includes an airblown binder (AZ1-1), and another binder modified with REOB.

Determination of ΔT_c was made using AASHTO M323 on all binders after RTFOT+PAV aging.

Table 3. Binder Matrix, Corresponding Field Sections, and Performance. Pavement distresses were observed (1) after 11 years the trend being unchanged during the last 6 yrs of monitoring for AZ section and (1) after 11 years the trend being unchanged during the last 6 yrs of monitoring (AZ sections) or (2) after 6 years the trend being unchanged during the last 3 yrs of monitoring (MN sections)

Project	Binder ID	PG	Binder Source/ Composition	Corresponding Field Sections	Observed Pavement Distresses	Predicted Performance from Previous Lab Results
SHRP	AAA-1	PG 58-28	Lloydminster	N/A	N/A	Good
	AAK-1	PG 64-22	Boscan	N/A	N/A	Good
AZ	AZ 1-1	PG 76-16	Airblown, WTI/WTS blend	AZ 1-1, US 93 2001	Severe cracking (1)	Poor
	AZ 1-2	PG 76-22	Unmodified/ Venezuelan	AZ 1-2, US 93 2001	Moderate cracking (1)	Moderate
	AZ 1-3	PG 76-16	Unmodified/ Rocky Mountain	AZ 1-3, US 93 2001	Low cracking (1)	Moderate
MN	MN 1-3	PG 58-28	Unmodified/ Canadian	MN 1-3, Rochester 2006	Low thermal cracking (2)	Moderate
	MN 1-4	PG 58-28	REOB (8%)/Canadian	MN 1-4, Rochester 2006	High thermal cracking (2)	Poor
	MN 1-5	PG 58-28	Unmodified/ Venezuelan	MN 1-5, Rochester 2006	Low thermal cracking (2)	Good

In parallel to directly measuring $T_c(S)$, $T_c(m)$ and ΔT_c , they were all fitted to the CA model and the corresponding data are given in Table 4.

Table 4. Rheological data for the tested asphalt binders.

		AAA-1	AAK-1	AZ 1-1	AZ 1-2	AZ 1-3	MN 1-3	MN 1-4	MN 1-5
Crossover frequency ω_x	10^{-5} rad/s	71,3	95,8	7,9	25,5	27,1	18,2	18,2	63,8
Reference temperature T_r	$^{\circ}C$	-18	-12	-6	-12	-6	-18	-18	-18
Glassy modulus S_g	GPa	2,22	2,29	1,33	1,81	2,06	2,50	1,53	2,99
Rheological index R	-	1,78	1,73	2,17	1,74	1,88	1,88	1,94	1,69
Activation energy E_a	kJ/mol	185	201	237	150	225	160	180	164
$T_c(S)$	$^{\circ}C$	-23,2	-17,0	-17,8	-15,8	-11,4	-20,9	-25,6	-19,3
$T_c(m)$	$^{\circ}C$	-24,3	-19,2	-10,3	-15,3	-10,3	-21,2	-22,4	-21,9
ΔT_c	$^{\circ}C$	1,1	2,2	-7,5	-0,5	-1,1	0,3	-3,2	2,6
$T_c(S)$ calc	$^{\circ}C$	-24,3	-17,6	-18,6	-16,8	-11,7	-21,4	-27,8	-19,9
$T_c(m)$ calc	$^{\circ}C$	-23,3	-17,5	-7,9	-14,1	-9,1	-20,3	-20,7	-22,8

ΔT_c calc	$^{\circ}\text{C}$	-1,0	-0,1	-10,7	-2,7	-2,7	-1,1	-7,1	3,0
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4.2 Discussion

The data given in Table 4 show that the calculated value for ΔT_c based on the rheological modeling, compares very well with measured data (Figure 3). A closer look at the data in Table 4, shows that calculated $T_c(S)$ and $T_c(m)$ lie generally within 1°C of the measured value, a difference within the reproducibility of the test method.

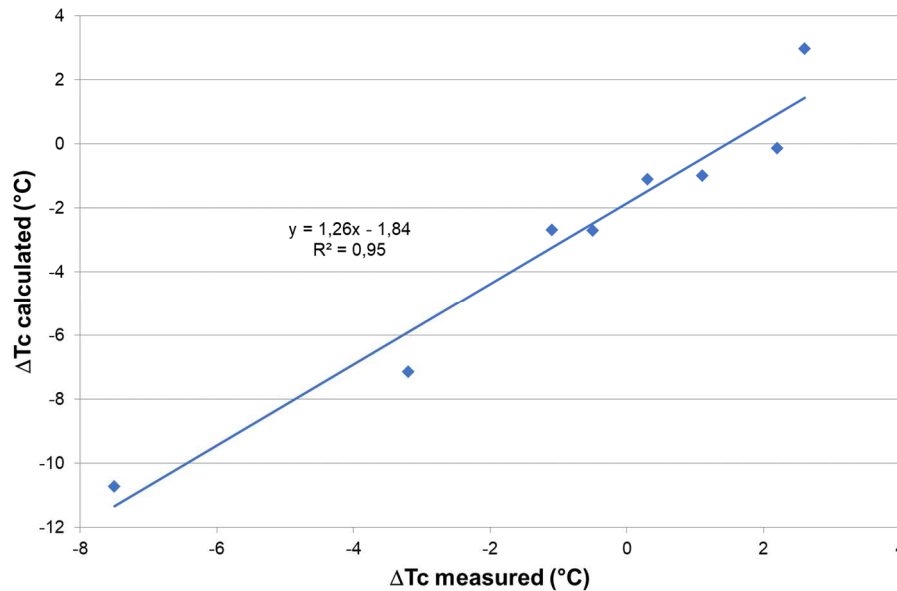


Figure 3. Calculated ΔT_c versus measured ΔT_c for the binders shown in Table 4.

The measured ΔT_c can also be plotted versus the rheological index (Figure 4). The correlation is pretty good, although not perfect. Note that other such correlations were already published [8,10]. The fact that the correlation is not perfect is obvious from the above argument: It would take similar values of the crossover frequency, activation energy and glassy modulus for the correlation to be perfect. Hence, the correlation confirms that the rheological index is the controlling parameter and the scatter shows that the weight of the other parameters is significant but only secondary.

- 1 In addition, the graph shows that the experimentally derived critical value for the rheological
- 2 index is closer to 1.82, somewhat lower than the above theoretical derivation (1.94) but with
- 3 clearly the right order of magnitude. gain, it would take a larger set of rheological data for
- 4 each binder to further investigate these differences.
- 5 Finally, it must be clarified that we are not advocating that the rheological index R would be
- 6 a better criteria than ΔT_c . Our work only shows that both are well correlated and the reasons
- 7 why it is so. In order to propose R as a new candidate for specification purposes, it would
- 8 first take the writing of a standard method for determining it, allowing for evaluating its
- 9 repeatability and reproducibility, which is not the case at this stage.

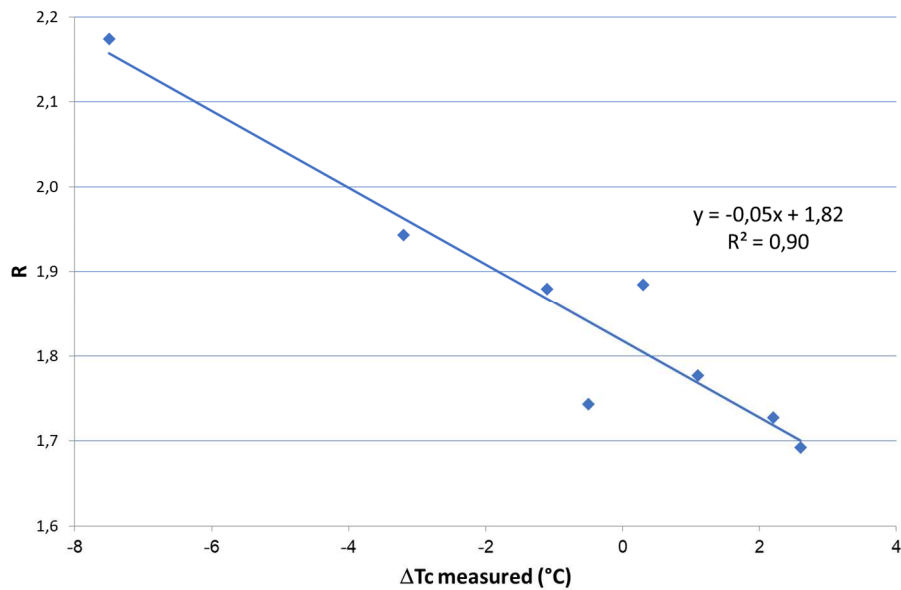


Figure 4. Measured ΔT_c as a function of rheological index for the binders shown in Table 4.

5. CONCLUSIONS

The Performance Grading system for asphalt binders currently specified in the USA (AASHTO M320), is based on determining the high and low limiting temperatures for appropriate binder use. The low limiting temperature is intended to identify temperatures that would become so low that the risk of thermal cracking would dramatically increase.

Practically, the low limiting temperature is defined based on rheological measurements made using the Bending Beam Rheometer (BBR - AASHTO T313). Criteria based on the absolute value of the stiffness and/or the slope of the stiffness vs loading time, are being used. The principle is that too-rigid a binder generates a risk of cracking for the pavement.

However, the current specifications are being challenged and it was proposed to add a new criterion, called ΔT_c and its ability to better predict thermal cracking than existing criteria is the subject of intense discussions.

In this paper, we demonstrated that ΔT_c is indeed mostly controlled by the rheological index of the binder. Other rheological parameters such as the activation energy, the crossover frequency or the glassy modulus, are only secondary in importance. In particular, it is shown that a theoretical value of $R = 1.94$ makes $\Delta T_c = 0$. For R above the critical value, ΔT_c becomes negative and it is positive below. The critical value of R is explained to be a consequence of the average properties of the SHRP asphalt pool in the 1990s. As a consequence, aging and polymer-modification are expected to make ΔT_c more negative, as already documented in the literature.

The comparison between theoretical calculations and measured data for some extreme binders confirm the soundness of the derivation. The measured critical rheological index was found to be somewhat lower than the theoretical value, at 1.82.

This clearly shows that ΔT_c is of “pure” rheological origin and care must be taken when trying to predict cracking results from rheological data only. In this statement, rheology is defined as the science of deformation and flow of matter, which is therefore not considering cracking issues. The way materials break is the subject of another field called fracture mechanics. So, correlations between rheological and fracture data would only be found if the

cracking mechanisms were similar for all binders. However, there is evidence that some polymer-modified binders do better resist crack propagation than unmodified ones, making this assumption incorrect [10,40,41,42]. The early SHRP specifications proposed a way to tackle this by adding an additional criterion based on tensile strength. It proved difficult to measure experimentally due to too-poor a reproducibility and future work should probably focus on adapting the approach to new testing method with a better measuring precision.

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