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Fast autotuning of process cascade controls

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Abstract: A procedure is presented for the automatic tuning of cascade control systems. The main goal of the procedure is to achieve fast tuning, and moderate process upset. That goal is pursued by proper combination of an *ad hoc* relay-based identification procedure based on a single test, a specific controller structure, and a tuning rule based on a particular use of the IMC principle. Simulations are reported to illustrate the effectiveness of the proposal within its applicability limits, that are characterised as rigorously as possible (and may be widened by some of the envisaged extensions).

Keywords: Cascade control; process control; autotuning.

1. INTRODUCTION

Cascade controls are very frequently used in many important domains, such as process control systems and motion control applications. This is witnessed in the former case by works such as Buschini et al. (1994); Ha et al. (1997); Wellenreuther et al. (2006), while examples in the latter context are e.g. Dumur and Boucher (1994); Schmidt et al. (1999); Pham et al. (2000). Therefore, improvements in the synthesis of cascade controls have a beneficial effect on a significant part of the entire control application field. As quite intuitive a consequence of the mentioned importance, the cascade control structure has been devoted a particular research effort also from a more general and methodological point of view, see e.g. Wang et al. (1993); Lee et al. (1998); Song et al. (2002); Visioli and Piazzi (2006), and many other works spread over the last decades.

This manuscript presents a procedure for the automatic tuning of cascade controls, limiting the scope to linear, time-invariant control systems (as in almost the totality of the literature on the matter). Also, the research presented herein focuses at the moment essentially on process controls; extensions to other domains - such as motion control systems - are however underway, and will be addressed in the future. The two main original characteristics of the proposed synthesis procedure, and therefore the contributions of this manuscript, are that a single relay test is used to gather the necessary process information, which is in favour of small tuning time and moderate process upset, and explicit use is made of some conceptual characteristics of the typical cascade structure encountered in process control, as detailed and explained in the following.

2. RATIONALE OF THE PROPOSED METHOD

Many works on cascade control synthesis (probably the majority) do not comprehend any treatise of the problem of collecting the process information necessary to synthesise the cascade control: with reference to the block diagram of figure 1, knowledge of 'models' for $P_I(s)$ and $P_E(s)$. whatever is meant for that, is simply assumed *a priori*. Some other works do address the problem of gathering process information, thus greatly enhancing the strength of the proposed synthesis methods; the interested reader can find in Leva and Piroddi (2007) a discussion of the potential pitfalls arising when a tuning procedure, irrespectively of the addressed control structure, is discussed without considering the process information gathering phase. Interestingly enough, among works that account for how process information is obtained and used, there is a definite prevalence of relay-based identification schemes, see e.g. Hang et al. (1994); Vivek and Chidambaran (2004) and the review Hang et al. (2002).

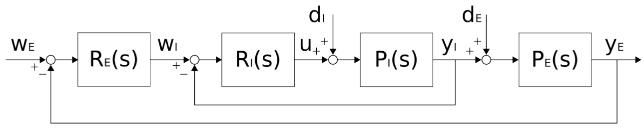

Fig. 1. Cascade control scheme.

Such a prevalence of relay-based identification may be explained by two reasons. First, the family of relay identification methods is by far the less subject to arbitrary choices in the experiment design and implementation phase, see again Leva and Piroddi (2007) for a discussion. Second, relay identification does not require to lead the process to a steady state prior to the identification phase—a great practical advantage indeed.

Most relay-based cascade control synthesis procedures use *two* relay tests, one to tune the internal loop, and one to tune the external loop once the inner is closed. Such methods can use the classical idea of 'moving one point' of the open-loop Nyquist curve, see Hang et al. (2002), or some of the numerous variations of the closed-loop Ziegler/Nichols rule derived along the same idea, like e.g. Hang et al. (1994); alternatively, they can apply some model-based tuning rule, the required model being drawn from the relay experiment data, like e.g. Vivek and Chidambaran (2004). All the mentioned methods are effective proposals, but require two relay tests, which take time. A minority of methods, e.g. Song et al. (2002), use a single relay test, and for quite obvious reasons have to pass through the identification of some process model (or, better, of some *couple* of process models, see again figure 1). In doing so, the model structure is typically chosen along the strongly established tradition of single-loop tuning, well exemplified by the vast collection of procedures reported in O'Dwyer (2003) for the PI/PID case. Taking that approach, given the information obtained by typical single relay tests, almost invariantly results in the adoption of a First Order Plus

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Dead Time (FOPDT) structure for the models of $P_I(s)$ and $P_E(s)$, and on a conveniently coordinated and sequenced use of tuning rules for single-loop controls— see again O'Dwyer (2003).

The rationale of the research presented herein is to abandon model structures suitable for the synthesis of 'general' single-loop regulators, since some peculiar characteristics of the cascade case allow to do so, and to take advantage from doing so. To understand that rationale, it is therefore necessary to list and briefly comment those 'peculiar characteristics'. If the process and equipment design is not pathological (in the opposite case no autotuner can be of great help, incidentally) one can expect, with reference to figure 1, that

- a suitable band for the cutoff frequency of the external loop is around the frequency where the phase of the frequency response $P_E(j\omega)$ equals -90°;
- the dynamics of $P_I(s)$, that typically represents 'the actuator', are significantly faster than those of $P_E(s)$, that is typically thought of as 'the process', so that when the phase of $P_I(j\omega)P_E(j\omega)$ is -90°, the contribution of $P_E(j\omega)$ dominates that of $P_I(j\omega)$, although the contribution of $P_I(j\omega)$ may be significantly nonzero;
- the 'process' $P_E(s)$ has a low (say first or second at most) order *dominant* dynamics, the phase contribution of which reaches -90° when the slope of the corresponding asymptotic magnitude Bode plot is -20 or -40 dB/decade, the sloper of the exact plot tending to approach -20 rather than -40 dB/decade;
- the dynamic separation between $P_I(s)$ and $P_E(s)$ is such that a separation of about 0.5–1 decades between the *closed* internal and external loops is reasonable to achieve a good overall control result.

The first two points are quite obvious, while the others requires some brief explanation. Regarding the third point, the contained assumptions are surely a loss of generality from a completely abstract point of view, but are very reasonable if the considered arena is that of industrial process controls. Processes are ruled by balance equations that seldom (not to say, almost never) result in models that do not exhibit a dominant mass or energy storage phenomenon, i.e., a low-order *dominant* dynamics, coupled to higher frequency singularities typically related to secondary storages (e.g., the energy in a fluid container), measurement dynamics, and so forth. By the way, if the assumption under question were not very reasonable, nobody would use FOPDT models in regulator tuning. Hence, despite no theorem can be derived to prove that the third point above holds true, one can safely admit it as true under the sole hypothesis that the process design and the component sizing make sense.

As for the fourth point, an actuator can be for constructive reasons much faster than the process it acts upon, but to obtain a well functioning cascade control, it is very often unnecessary to have equally large a bandwidth separation between the two nested loops. In other words, the role of the internal loop is not to 'speed up' the actuator, but rather to make y_I track w_I up to a frequency higher than the external loop cutoff (and 5–10 times is more than enough in virtually any real-life case) and to reject the internal load disturbance d_I with a response speed significantly higher than it would be if the control had to rely only on $R_E(s)$—and again, one decade is more than enough in general.

Given all this, the idea proposed herein is to perform a single test with a relay cascaded to an integrator (see the scheme of figure 2) so as to identify the point of $P_I(j\omega)P_E(j\omega)$ with phase -90°, and to obtain two models $\widehat{P}_I(s)$ and $\widehat{P}_E(s)$ by somehow 'factoring' that point as explained in section 3.

3. THE RELAY-BASED IDENTIFICATION PROCEDURE

As an additional but important remark with respect to the assumptions above, the same assumptions allow to adopt for $\widehat{P}_I(s)$ and $\widehat{P}_E(s)$ a delay-free structure. This is an advantage, because - except for a minority of cases where a physical delay exists, owing e.g. to transport phenomena - there is no objective need for delay in process models.

When identifying process models in the context of 'general purpose' tuning methods, given the small numbers of poles and zeroes allowed for the derivation of tractable tuning relationships, a delay is often introduced, but essentially as a 'bucket' where to throw all of the observed phase lag that cannot be rendered by rational dynamics, i.e., as a sad necessity that often turns into an undue performance request reduction, as illustrated e.g. in Leva and Colombo (2004) with respect to the well known IMC-PID tuning rules.

Introducing model delays can be avoided in the situation addressed herein, given the phase ranges involved. This may result in some applicability *caveats*, as briefly discussed later on, but significantly enhances the obtained performance. Recall, from this point of view, that the chosen model structures aim only at representing the process behaviour in the vicinity of the cutoff frequencies, *not* at adhering to the true structure of the process dynamics, whatever is meant for it.

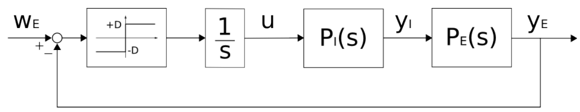

Fig. 2. Relay experiment scheme.

The scheme of the relay experiment employed in this work is shown in figure 2. The relay has no hysteresis (or, better, has so small a hysteresis to allow assuming its critical point locus to be the real negative semiaxis) and is cascaded to an integrator, so that $\mp D$ turns out to be the slope of the triangle wave signal $u(t)$. The signals considered during the relay test are $u(t)$, $y_I(t)$, and $y_E(t)$.

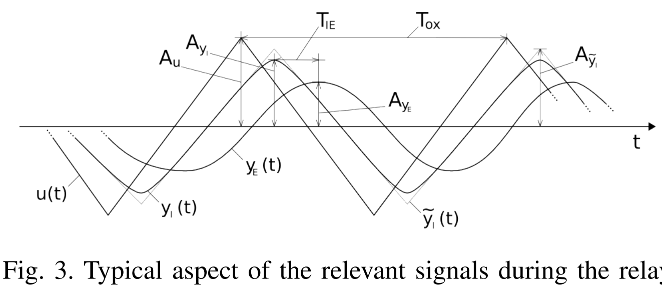

Fig. 3. Typical aspect of the relevant signals during the relay experiment.

The key point of the proposed identification procedure is that, under the relay plus integrator excitation and the assumptions introduced above, the permanent oscillation arising is characterised by a $y_I(t)$ more or less resembling a triangle wave, and a $y_E(t)$ exhibiting an almost sinusoidal behaviour. The signals in a typical experiment thus resemble the (idealised) situation of figure 3. The relay-based identification procedure can be summarised as follows.

(1) Connect the relay/integrator cascade to the process as in figure 2 and wait for a permanent oscillation; there are plenty of methods to detect a permanent oscillation, see e.g. Hang et al. (2002), so details on the matter are omitted.

(2) With reference to figure 3, compute the amplitudes A_u and A_{y_E}; here too, the literature reports a wealth of techniques to obtain reliable values in the presence of realistically noisy signals (recall that figure 3 is idealised). Also measure the oscillation frequency $\omega_{ox} := 2\pi/T_{ox}$. Note that, to lighten the notation, the following formulæ assume that the relay half swing D, see figure 2, equals one: this simply means that the recorded variables $u(t)$, $y_I(t)$ and $y_E(t)$ are to be divided by D prior to the computations described in the following.

(3) Compute the amplitude $A_{\widetilde{y}_i}$ of the 'equivalent triangle wave' $\widetilde{y}_i(t)$ relative to $y_i(t)$, see again figure 3. A simple, yet reliable way to obtain a reasonable value also in the presence of asymmetric oscillations and noise is

$$A_{\widetilde{y}_i} = \frac{2}{T_{ox}} \int_0^{T_{ox}} |y_i(t)|dt \quad (1)$$

(4) Estimate the magnitudes $|P_I(j\omega_{ox})|$ and $|P_E(j\omega_{ox})|$, respectively, as

$$P_{Iox} = \frac{A_{\widetilde{y}_i}}{A_u}, \quad P_{Eox} = \frac{\pi^2 A_{y_E}}{8A_{\widetilde{y}_i}}. \quad (2)$$

It is worth noting that the above magnitude estimates are quite accurate, of course under the assumptions of section 2: the errors committed are comparable to those involved in the measurement of the necessary signal amplitudes, and are tolerated well enough by the subsequent regulator synthesis. Things are a bit more tricky for the phase estimates, however, as shown in the following step.

(5) Estimate first guess values for the phases $\arg^\circ(P_E(j\omega_{ox}))$ and $\arg^\circ(P_I(j\omega_{ox}))$, respectively, as

$$\varphi^0_{Eox} = 360^\circ \frac{T_{IE}}{T_{ox}}, \quad \varphi^0_{Iox} = -90^\circ - \varphi^0_{Eox} \quad (3)$$

Determine then the ratio between the average absolute slope of $\widetilde{y}_i(t)$ and the quantity DT_s, where D is the relay half swing (see figure 2) and T_s the sampling time, as

$$S_r = \frac{8}{T_{ox}^2 DT_s} \int_0^{T_{ox}} |y_i(t)|dt \quad (4)$$

and finally obtain the estimates of $\arg^\circ(P_I(j\omega_{ox}))$ and $\arg^\circ(P_E(j\omega_{ox}))$, respectively, by computing a corrective coefficient k_φ as a function of S_r in the form

$$k_\varphi(S_r) = \max\left(k_\varphi^{min}, \min\left(1, 1 + \frac{k_\varphi^{min} - 1}{1 - S_r^{min}}(S_r - S_r^{min})\right)\right) \quad (5)$$

and then correcting the first guess values as follows:

$$\varphi_{Iox} = k_\varphi(S_r)\varphi^0_{Iox}, \quad \varphi_{Eox} = -90^\circ - \varphi_{Iox} \quad (6)$$

The correction of equations (5,6) has essentially the role of avoiding excessive phase errors in the case of small values of $\arg^\circ(P_I(j\omega_{ox}))$, which could adversely affect the synthesis of $R_I(s)$. It is apparently an empirical correction, derived from extensive numerical simulations (details are omitted here for space reasons). The values chosen for k_φ^{min} and S_r^{min}, based on the same simulations, are 0.25 and 0.98, respectively. The efficiency of the presented technique is witnessed by the examples reported later on.

The relay-based identification procedure summarised above is quite simple, and also computationally light. By convenient use of accumulator registers and simple code optimisations, the memory occupation of the procedure can be made very small, which allows its implementation also on low-end devices, like those frequently used for 'peripheric' controls (where cascade structures are frequent) in process control applications.

4. THE CONTROL SYNTHESIS PROCEDURE

Once ω_{ox}, P_{Iox}, P_{Eox}., φ_{Iox}, and φ_{Eox} are available, the two blocks $R_I(s)$ and $R_E(s)$ of figure 1 are to be synthesised. To do this, the following procedure is followed.

(1) According to the assumptions of section 2, $P_I(s)$ is *a priori* described with the delay-free first order model

$$\widehat{P}_I(s) = \frac{\mu_I}{1 + sT_I} \quad (7)$$

where T_I and μ_I are simply obtained as

$$T_I = -\frac{1}{\omega_{ox}} \tan^\circ(\varphi_{Iox}) \quad \mu_I = P_{Iox}\sqrt{1 + (\omega_{ox}T_I)^2}. \quad (8)$$

(2) The internal regulator $R_I(s)$ is then tuned, adopting for it the PI structure

$$R_I(s) = K_I\left(1 + \frac{1}{sT_{iI}}\right) \quad (9)$$

and employing the IMC tuning formulæ (Leva and Colombo, 2004)

$$T_{iI} = T_I, \quad K_I = \frac{T_I}{\mu_I \lambda_I} \quad (10)$$

where parameter λ_I, interpreted as the desired closed-loop dominant time constant of the internal loop, is computed by dividing the time constant T_I by an 'acceleration factor' A_{fI}, i.e.,

$$\lambda_I = \frac{T_I}{A_{fI}} \quad (11)$$

A_{fI} being thus the first design parameter of the overall cascade autotuning method.

(3) Having tuned $R_I(s)$, it is now possible to compute the (nominal) transfer function of the closed internal loop, i.e.,

$$\widehat{T}_I(s) := \left.\frac{Y_I(s)}{W_I(s)}\right|_{P_I(s)=\widehat{P}_I(s)} = \frac{R_I(s)\widehat{P}_I(s)}{1 + R_I(s)\widehat{P}_I(s)} \quad (12)$$

(4) The availability of $\widehat{T}_I(s)$ in turn allows to compute the point at frequency ω_{ox} of the frequency response of the

system to be controlled by $R_E(s)$ once the internal loop is closed, i.e., of

$$\widetilde{P_E}(s) := \widehat{T_I}(s)P_E(s) \quad (13)$$

Doing so means accounting - to the extent permitted by the relay experiment and identification information as represented by $\widehat{P_I}(s)$ - for the non-ideality of the internal loop. The mentioned correction is accomplished by estimating the magnitude and phase of $T_I(j\omega_{ox})P_E(j\omega_{ox})$, respectively, as

$$\widetilde{P}_{Eox} = P_{Eox}|\widehat{T}_I(j\omega_{ox})|, \; \widetilde{\varphi}_{Eox} = \varphi_{Eox} + \arg^\circ(\widehat{T}_I(j\omega_{ox})) \quad (14)$$

(5) Recalling again the assumptions of section 2, a suitable way to describe $\widetilde{P_E}(s)$ is a delay-free second order model. As typically done in model parametrisation techniques based on relay data, it can be assumed that the two poles of the required model coincide, thus expressing that model as

$$\widehat{\widetilde{P_E}}(s) = \frac{\mu_E}{(1+sT_E)^2} \quad (15)$$

where

$$T_E = -\frac{1}{\omega_{ox}}\tan^\circ\left(\frac{\widetilde{\varphi}_{Eox}}{2}\right), \; \mu_E = P_{Iox}\left(1+(\omega_{ox}T_E)^2\right) \quad (16)$$

(6) According again to the IMC principle, see e.g. Leva and Colombo (2004) for details, $R_E(s)$ is determined as

$$R_E(s) = \frac{Q_E(s)F_E(s)}{1-Q_E(s)F_E(s)\widehat{\widetilde{P}}_E(s)} \quad (17)$$

where $Q_E(s)$ is the inverse of $\widetilde{P_E}(s)$, and $F_E(s)$ is chosen as the desired closed-loop transfer function for the external loop, with the constraint of making $Q_E(s)F_E(s)$ causal. The adopted choice is

$$F_E(s) = \frac{1}{(1+s\lambda_E)(1+s\lambda_E/10)} \quad (18)$$

where parameter λ_E, that in force of (18) can still be interpreted as the desired closed-loop time constant for the external loop, is chosen as

$$\lambda_E = B_s\lambda_I \quad (19)$$

thus making B_s, the required bandwidth separation between the internal and the external loops, the second design parameter of the overall cascade autotuning method.

5. TWO SIMULATION EXAMPLES

5.1 Example 1

The first example reported considers the process described by

$$P_I(s) = \frac{1}{1+2s}, \quad P_E(s) = \frac{1}{(1+10s)(1+4s)(1+s)^2} \quad (20)$$

where the output of $P_I(s)$ is subject to a band-limited noise of amplitude 4 and bandwidth 1 r/s, and the output of $P_E(s)$ to a similar noise, but with bandwidth 0.2 r/s. As shown by the Bode diagrams of figure 4, the situation is quite consistent with the assumptions of section 2. The example is then aimed at showing that the mentioned assumptions make sense in a realistic case, and that the proposed method works well and does take profit of those assumptions. Employing the proposed method with $A_{fI} = 3$ and $B_s = 10$ produces

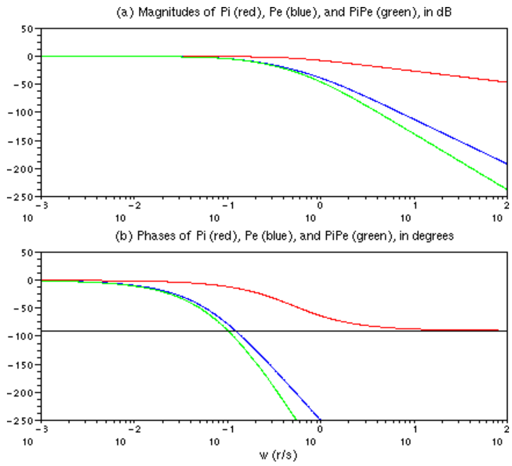

Fig. 4. Example 1 - frequency responses of $P_I(s)$, $P_E(s)$, and $P_I(s)P_E(s)$; the phase -90 ° is marked with a horizontal black line.

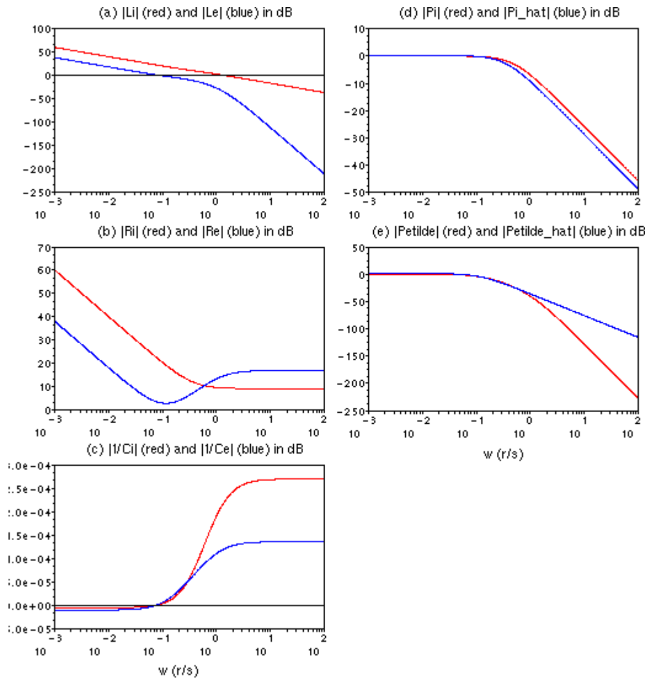

Fig. 5. Example 1 - relevant frequency responses.

$$R_I(s) = 1.033\frac{1+2.85s}{s}, \; R_E(s) = 0.083\frac{1+17.32s+74.98s^2}{s(1+0.86s)} \quad (21)$$

and the results of figures 5 and 6. To show the effectiveness of the proposed relay-based identification procedure, the estimated and exact values of the magnitude and phase of $P_I(j\omega)$ and $P_E(j\omega)$ at the oscillation frequency are given in table 5.1

	Estimated	Real
$\lvert P_I(j\omega_{ox})\rvert$	0.984	0.982
$arg^\circ(P_I(j\omega_{ox}))$	-15.375°	-10.941°
$\lvert P_E(j\omega_{ox})\rvert$	0.684	0.664
$arg^\circ(P_E(j\omega_{ox}))$	-74.624°	-76.210

Table 1. Example 1 - identification results.

Figure 5 shows that the required bandwidth separation is obtained (plot a), that the identified models are precise in the

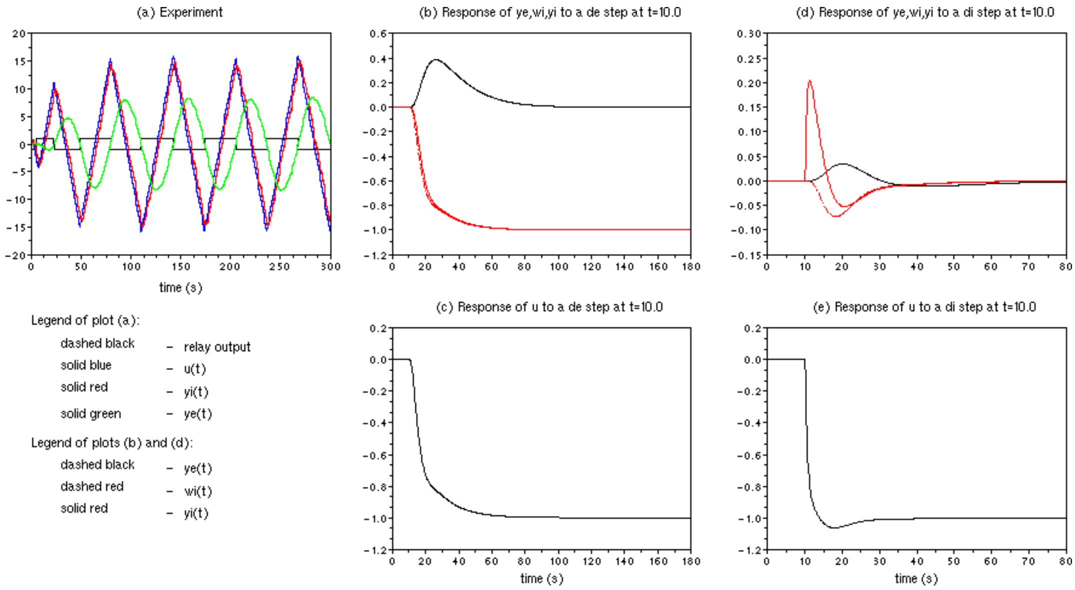

Fig. 6. Example 1 - relevant transients.

correct bands (plots d and e), and that the aspect of the obtained regulators is reasonable (plot b). Also, the achieved degree of robustness is quantified *a posteriori* (plot c) by showing the inverse of the magnitude of the frequency response of the control sensitivity functions - $C_I(s)$ and $C_E(s)$ for the internal and external loops, respectively, that in this context are defined respectively as

$$C_I(s) := \frac{R_I(s)}{1+R_I(s)\widehat{P}_I(s)}, \quad C_E(s) := \frac{R_E(s)}{1+R_E(s)\widehat{\widetilde{P}}_E(s)} \tag{22}$$

The frequency responses of the above transfer functions provide an overbound for the magnitude of the admissible additive mode error Leva and Colombo (2004). In the case at hand, both magnitudes are practically 0 dB, which is quite good a result. Figure 6 shows the behaviour of the relevant variables during the relay identification phase, and the responses of the controlled variables $y_I(t)$ and $y_E(t)$ and the control signal $u(t)$ to unit step load disturbances $d_I(t)$ and $d_E(t)$, see also the scheme of figure 1. Only load disturbance responses are considered, since that type of transient best evidences the feedback characteristics of the obtained control: set point tracking can be recovered and/or improved by exploiting the two-degree-of-freedom structure that most industrial regulators nowadays encompass. In figure 6, it can be noticed that the obtained tuning is satisfactory, and in particular (plots b and d) that $y_I(t)$ reaches its set point $w_I(t)$ well before the external loop settle, which is a further demonstration of the actual efficacy of the tuned cascade control.

5.2 *Example 2*

The second example considers

$$P_I(s) = \frac{1}{1+8s}, \quad P_E(s) = \frac{1}{(1+10s)(1+2s)} \tag{23}$$

the output of $P_I(s)$ being subject to a noise of amplitude 4 and bandwidth 1 r/s, and that of $P_E(s)$ to a noise of amplitude 4 and bandwidth 0.5 r/s. This example is for the proposed method a somehow off-design situation with respect to the assumptions of section 2. In fact, see (23), the 'actuator' is not so fast compared to the 'process', and the phase contributions of the two at the oscillation frequency (where the total phase of the two is -90°) are comparable. The proposed method is here applied with $A_{fI} = 5$ and $B_s = 5$. The choice of A_{fI} is suggested by the fact that there is room to achieve an internal loop response speed greater than that of the 'actuator', as can be guessed by observing that the relay response of y_I (see figure 7 later on) is quite different from a triangle wave. As for parameter B_s, the chosen value calls for a demanding (i.e., fast) outer loop. In this example, omitting for brevity the (good) identification results, the obtained regulators are

$$R_I(s) = 0.668\frac{1+7.5s}{s}, \; R_E(s) = 0.116\frac{1+13.85s+47.93s^2}{s(1+0.68s)} \tag{24}$$

The relevant transients of this example are shown in figure 7, where it can be seen that the obtained tuning is satisfactory. As for the frequency responses of interest, omitted for brevity too, the same considerations of example 1 apply, of course considering the smaller bandwidth separation here requested.

Space limitations do not allow to report other examples, particularly to further illustrate and discuss the role of the design parameters. In extreme synthesis, however, it can be stated (based on the numerous examples omitted) that the effect of those parameters on the obtained tunings is consistent with expectations. Also, according to many simulations, one could assume $A_{fI} = 1-5$ and $B_s = 5-10$ as reasonable default ranges for virtually any realistic case. It is finally worth observing that, as suggested by example 2, the aspect of the relay responses obtained in the identification phase appears to contain some useful information to help selecting values for A_{fI} and B_s; the matter is being studied, and results will be presented in future works.

6. CONCLUSIONS

A method was presented to tune a cascade control system based on a single relay experiment. The rationale of the proposed method relies on some assumptions that basically hold true if the process and control equipment design is not pathological,

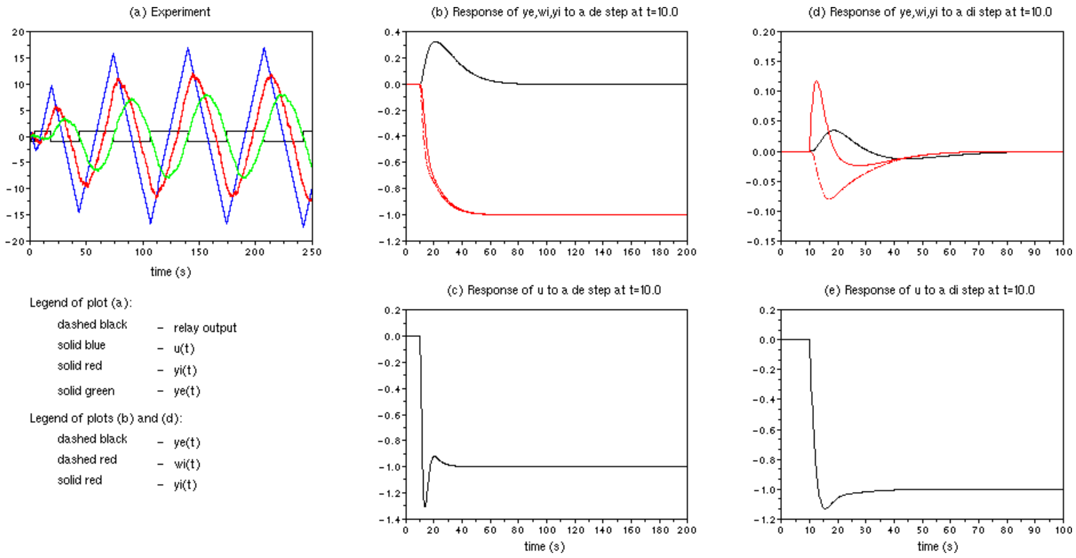

Fig. 7. Example 2 - relevant transients.

and - if assumed true - complement the information gathered from the relay test, permitting to achieve satisfactory tuning in all the cases of practical interest. The proposed method is quite simple to use and computationally light, so as to be implementable also in low-end control devices—an important feature for potential acceptance in the process control domain. The method has only two design parameters, the meaning of which is easy to understand also for a non-specialist user. Also, thanks to the use of a single relay plus integrator test, the identification phase (thus the tuning) is short, and it is easy to limit the process upset.

Simulations show the effectiveness of the proposed tuning procedure, and more in general, of the basic idea of 'trusting' the process and equipment sizing up to a reasonable extent, and use the consequent assumptions as the basis to develop tuning strategies tailored to a particular control structure. Extensions of the proposed cascade tuning method to motion control applications, that require different assumptions with respect to the process control case treated herein, are being considered. Also extensions of the 'basic idea' above to other control structures (e.g., decoupling and feedforward/feedback control schemes) are being studied, and the results will be presented in future works.

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