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New Developments in the Control of Fluid Dynamics of wells and risers in oil production systems ⋆

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Abstract: A practical control algorithm for stabilizing flow in risers and oil production wells should meet several requirements. i) be simple, ii) able to operate with low-cost measurements and possibly contaminated with noise and iii) stabilize the flow without setting a value for the bottom pressure. An algorithm has been proposed which does not fix any reference for the bottom pressure. It uses as reference a value equal to zero for the derivative of the bottom pressure. This paper presents some changes in the algorithm in order to avoid the difficulties with derivatives and to simplify the tuning of its parameters. It also proposes a control methodology to suppress oscillations in the absence of automated production choke and downhole measurements.

Keywords: Fluid dynamics, well control, riser control, gas-lift, well simulation.

1. INTRODUCTION

Fluid flow rate oscillations are known as source of problems in oil production systems. The primary fluid treatment process including oil-water-gas separation and gas compression is strongly affected. In extreme cases part of the produced gas has to be directed to the flare and the quality of the separated water and oil is compromised. Besides, production risers may suffer with the fluid acceleration resulting in premature mechanical fatigue. It is also worth mentioning the production loss resulting from the intermittent flow when compared to a stabilized one. The efforts to deal with the problem can be divided in reactive and active control. Reactive control is the name used to describe those systems designed on the assumption that the risers and wells do develop some kind of oscillatory flow-rate. The reactive control system is designed to enable the operation of the primary fluid processing system even with the existence of oscillatory flow behavior. The active control system, on the other hand, acts to eliminate or decrease the flow-rate oscillations delivered by wells and risers. The low number of applications of active control can be attributed to

- lack of instrumentation for measuring and actuation,
- lack of thrust on the control algorithm robustness,
- difficulty on choosing set points,
- conservatism.

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Several control algorithms have been proposed to the control of the fluid flow dynamics of wells and risers, F. Di Meglio and Alstad (2012), Sinegre (2006), Meglio et al. (2012), Jahanshahi et al. (2012),Ogazi et al. (2009), Storkaas and Skogestad (2007), Godhavn et al. (2005), Siahaan et al. (2005), Eikrem et al. (2008). Unfortunately there is no space to make a proper review. In Plucenio et al. (2012) an algorithm has been proposed which does not fix any reference for the process variables. It uses as reference a value equal to zero for the derivative of the bottom pressure. The control algorithm has been applied with success in simulations and real wells. This paper presents some advances in well and risers active control including changes in the proposed algorithm in order to avoid the difficulties with derivatives and simplify the tuning of its parameters. This paper is organized as follows: In section 2 the new algorithm is discussed. In section 3 some simulation results are presented. In section 4 a solution is proposed for the case when neither downhole measurement nor active production choke is available. Section 5 concludes the paper.

2. DERIVATION OF THE NEW CONTROL LAW

As explained in Plucenio et al. (2012) the algorithm is based on the equation (1) presented in Sinegre (2006) which derives a relationship between the gas mas fraction x in time t and space z assuming an average gas velocity V_g. Applying Laplace Transform to equation (1) it becomes evident that the gas mass fraction at a position z_2 on the tubing is equal to the gas mass fraction at position z_1, with $z_2 > z_1$ at a time $t - \tau$ with $\tau = \frac{z_2 - z_1}{V_g}$.

$$\frac{\partial x}{\partial t} + V_g \frac{\partial x}{\partial z} = 0 \tag{1}$$

This result is used on the control strategy by assuming that if the gas mass fraction is stabilized at $z = z_1$ then, provided no other action happens to disturb the flow, the gas mass fraction will remain stabilized at $z = z_2$. Down-hole measurements are scarce but new wells are being equipped with permanent down-hole gauges that measure pressure and temperature. Is there other process variable that when stabilized induces the stabilization of the gas mass fraction? In the sequence it is shown that assuming certain flow conditions, stabilizing the pressure in a position of a flowing pipe also stabilizes the gas mass fraction at that position.

Theorem 1. Stabilizing the bottom pressure in a gas-liquid flow stabilizes the gas mass fraction.

Proof. Consider the pressure difference between the bottom $p(z + \Delta z, t)$ and top $p(z, t)$ of a short pipe section of length Δz with inclination $\theta > 0$ with the horizontal axis where a gas-liquid flow takes place. It is assumed that the top pressure is a constant pressure boundary and there is no mass exchange between the liquid and gas phase. Disregarding the pressure drop due to friction and considering the void fraction $\alpha(t)$ and gas density $\rho_g(t)$ not varying along Δz, the pressure drop can be written as

$$p(z + \Delta z) = p(z, t) + (\alpha(t)\rho_g(t) + (1 - \alpha(t))\rho_l)\, g\Delta z sin(\theta). \quad (2)$$

The density of the gas is

$$\rho_g(t) = \frac{(p(z + \Delta z, t) + p(z, t))}{2}\phi, \quad \text{with} \quad \phi = \frac{M}{ZRT} \quad \text{and} \quad (3)$$

$$p(z + \Delta z, t) = p(z, t) + \alpha(t)\frac{(p(z + \Delta z, t) + p(z, t))}{2}\phi g \Delta L sin(\theta)$$
$$-\alpha(t)\rho_l g \Delta L sin(\theta) + \rho_l g \Delta z sin(\theta). \quad (4)$$

The time derivative of equation (4) is

$$\frac{\partial p(z + \Delta z, t)}{\partial t} = \frac{\partial \alpha(t)}{\partial t}\left(\frac{(p(z + \Delta z, t) + p(z, t))}{2}\phi - \rho_l\right) g\Delta L sin(\theta)$$
$$+\frac{\partial p(z + \Delta z, t)}{\partial t}\frac{\phi}{2}\alpha(t) g \Delta L sin(\theta). \quad (5)$$

In order to have $\frac{\partial p(z+\Delta z,t)}{\partial t}$ equal to zero it becomes necessary to have $\frac{\partial \alpha(t)}{\partial t} = 0$ or $\left(\frac{(p(z+\Delta z,t)+p(z,t))}{2}\phi - \rho_l\right) = 0$. This last alternative means a gas density equal to the liquid density and will be disregarded. That is, $\frac{\partial p(z+\Delta z,t)}{\partial t} = 0$ implies in $\frac{\partial \alpha(t)}{\partial t} = 0$. But,

$$x(z + \Delta z, t) = \frac{\alpha(t)\rho_g(t)}{\alpha(t)\rho_g(t) + (1 - \alpha(t))\rho_l}, \quad \text{or} \quad (6)$$
$$x(z + \Delta z, t) = \frac{\alpha(t)\,(0.5p(z + \Delta z, t) + 0.5p(z, t))\,\phi}{\alpha(t)\,(0.5p(z + \Delta z, t) + 0.5p(z, t))\,\phi + (1 - \alpha(t))\rho_l}.$$

Then, if $\frac{\partial p(z+\Delta z,t)}{\partial t} = 0$ implies in $\frac{\partial \alpha(t)}{\partial t} = 0$, using equation (7) shows that it also implies in

$$\frac{\partial x(z + \Delta z, t)}{\partial t} = 0. \quad (7)$$

. □

To avoid the time derivative used in the previous algorithm, Plucenio et al. (2012) a new approach is proposed. The bottom pressure is again assumed to be composed of a mean value and a zero mean value,

$$p_b(t) = \overline{p} + \widetilde{p}_b(t). \quad (8)$$

A wash-out filter is used to obtain $\widetilde{p}_b(t)$, Hassouneh et al. (2004), Colling and Barbi (2001). Assume the following low pass filter in s to obtain the auxiliary variable $v(s)$ with a frequency cut w_c:

$$v(s) = F(s)p_b(s)$$
$$F(s) = \frac{w_c}{s + w_c} = \frac{1}{\frac{s}{w_c} + 1} \quad (9)$$

The following discrete version of the filter can be obtained computing the discrete pole

$$z^* = 1 - d = e^{-w_c T_s}. \quad (10)$$

The equivalent difference equation can be written to obtain the discrete version of the zero mean pressure $\tilde{p}_b(k)$

$$v(k + 1) = dp_b(k) + (1 - d)v(k),$$
$$\tilde{p}_b(k) = p_b(k) - v(k). \quad (11)$$

Starting with $\tilde{p}_b = 0$, there is a change in the riser or wellhead pressure, Δp_h that induces an oscillatory behavior in $\tilde{p}_b$. If the well or riser head is connected to a separator through a choke and a short flow-line,

$$p_h(t) = p_{sep} + p_{ch}(t) \quad \text{and} \quad \Delta p_h = \Delta p_{ch}. \quad \text{Thus,} \quad (12)$$

$$\tilde{p}_b(s) = H(s)\Delta p_{ch}(s), \quad \text{and} \quad H(s) = \frac{A w_o}{s^2 + w_o^2}. \quad (13)$$

The constant A was inserted to allow for a better tuning of the controller parameters. The angular frequency of the downhole oscillatory pressure can be computed from the period T_o using the time normalized to T_s,

$$w_o = \frac{2pi}{T_o/T_s}. \quad (14)$$

Using a simplified relation between z and s,

$$H(z) = \frac{A w_o}{(z - 1) - (1 - z^{-1}) + w_o^2}$$
$$H(z) = \frac{A w_o z^{-1}}{1 - (2 - w_o^2)z^{-1} + z^{-2}} \quad (15)$$

The Z transform shown in equation (15) is an approximation to the exact expression

$$H(z) = \frac{A sin(w_o)z^{-1}}{1 - 2cos(w_0)z^{-1} + z^{-2}} \quad (16)$$

for small w_0 with $sin(w_0)$ and $cos(w_0)$ respectively approximated to first and second order Taylor expansion around zero .

$$\Delta p_{ch}(k) = \frac{1}{A w_o}\tilde{p}_b(k + 1) - \frac{(2 - w_o^2)}{A w_o}\tilde{p}_b(k)$$
$$+\frac{1}{A w_o}\tilde{p}_b(k - 1). \quad (17)$$

Defining a set-point for $\widetilde{p}_b$ equal to zero, the error $e(k)$ can be written as

$$e(k) = 0 - \widetilde{p}_b(k). \quad (18)$$

Substituting in equation (17),

$$\Delta p_{ch}(k) = -\frac{1}{Aw_o}e(k+1) + \frac{(2-w_o^2)}{Aw_o}e(k) - \frac{1}{Aw_o}e(k-1). \quad (19)$$

As discussed in Plucenio et al. (2012) the term $e(k+1)$ in equation (19) is unknown at time k. All other terms $e(k)$ and $e(k-1)$ already happened. $\Delta p_{ch}(k)$ is computed in order to obtain a desired behavior for $e(k+1)$. It is required that

$$e(k+1) = Ge(k) \quad \text{with} \quad 0 < G < 1 \quad (20)$$

It was demonstrated that by choosing $\Delta p_{ch}(k)$ that forces $e(k+1) = Ge(k)$, for $0 < G < 1$, ensures stability.

After substitution,

$$p_{ch}(k) = p_{ch}(k-1) + \frac{(2-G-w_o^2)}{Aw_o}e(k) - \frac{1}{Aw_o}e(k-1). \quad (21)$$

Comparing equation (21) with a Proportional Integrative (PI) controller,

$$K_c = \frac{1}{Aw_o} \quad \text{and} \quad T_i = \frac{T_s}{1-G-w_o^2}. \quad (22)$$

As in Plucenio et al. (2012) the final expression has another term that will force the choke opening to a desired opening. This is expressed as a desired choke pressure using an approximate choke model.

$$p_{ch}(k) = p_{ch}(k-1) + \frac{(2-G-w_o^2)}{Aw_o}e(k) - \frac{1}{Aw_o}e(k-1)$$
$$+\beta\left(p_{ch}^{des} - p_{ch}(k-1)\right) \quad \text{with} \quad p_{ch}^{des} = \frac{B}{\phi_{ch}^2}. \quad (23)$$

2.1 Tuning $\bar{B}$

The value of B is obtained initially with the average past measurements of choke pressure and opening. If enough past measurements of choke pressure are available for a choke opening ϕ_{ch},

$$\bar{B} = \frac{\overline{p}_{ch}}{\phi_{ch}} \quad (24)$$

When applying control the value of $\bar{B}$ can be updated with new measurements of choke pressure and opening. In order to avoid noise propagation an exponential filter is applied over the last N_h computed values of $\bar{B}$. A recursive filtering is proposed. Consider the following pseudo-code:

- $x = -\frac{5}{N_h}$
- Start with
  - $Den(k) = 1$, $B(k) = p_{ch}(k-1)\phi_{ch}(k-1)^2$,
  - $\bar{B}(k) = B(k)$,
- Next,
  - $B(k) = p_{ch}(k-1)\phi_{ch}(k-1)^2$
  - $Den(k) = Den(k-1)e^{-x} + 1$
  - $\bar{B}(k) = \frac{\bar{B}(k-1)e^{-x}Den(k-1)+B(k)}{Den(k)}$

With this code only the values of $\bar{B}(k)$ and $Den(k)$ have to be stored. The implemented filter weights the computed value of $B(k-N_h)$ with a zero weight. The weight increases exponentially from zero at $t = k - N_h$ to one at $t = k$.

2.2 Tunig G

In the development of the control law it is required that

$$e(k+1) = G(ek), \quad (25)$$

Writing the expected error forward in time,

$$e(k+2) = Ge(k+1) = G^2e(k),$$
$$e(k+3) = Ge(k+2) = G^2e(k+1) = G^3e(k), \quad \text{and,}$$
$$e(k+N) = G^Ne(k). \quad (26)$$

One way to choose G consists in establishing the time required for the error to reach a value close to zero. That is, the number of future sample times N is specified in order to have $e(k+N) = e^{-5}e(k)$. N can be chosen as a fraction of the period of oscillation as

$$N = \frac{0.5T_{osc.}}{T_s}. \quad (27)$$

With this choice, $G = e^{\frac{-10T_s}{T_{osc}}}$.

2.3 Tuning β

The control law expressed in equation (28) has two objectives. To drive $\tilde{p}_b$ to zero and the choke opening to a desired value. The choice of β is crucial in order to reach the two objectives. A value too high for β might take the choke opening to the desired value without driving $\tilde{p}_b$ to zero. A too small value, on the other hand could cause the choke opening taking too long to reach the desired opening.

$$p_{ch}(k) = p_{ch}(k-1) + \gamma_o e(k) + \gamma_1 e(k-1)$$
$$+\beta\left(p_{ch}^{des} - p_{ch}(k-1)\right) \quad \text{with}$$
$$\gamma_1 = -K_c \quad \text{and} \quad \gamma_o = K_c\left(1 + \frac{T_s}{Ti}\right). \quad (28)$$

With the objective of deriving an expression for β the equation (28) will be analyzed trying to separate the error and choke pressure dynamics. Writing equation (28) in Z for $e(z)$,

$$e(z) = \frac{1}{\gamma_o}\left(\frac{p_{ch}\left(1-(1-\beta)z^{-1}\right) + \beta p_{ch}^{des}}{1 + \frac{\gamma_1}{\gamma_o}z^{-1}}\right). \quad (29)$$

The discrete pole of the characteristic equation describing the error dynamics is

$$z_e = -\frac{\gamma_1}{\gamma_o}, \quad \text{or} \quad z_e = -\frac{-K_c}{K_c\left(1+\frac{T_s}{T_i}\right)} = \frac{1}{2-G-w_o^2}. \quad (30)$$

Ignoring the contribution of $e(k)$, the choke pressure dynamics can be written as

$$p_{ch}(z) = \frac{\beta p_{ch}^{des}}{1-(1-\beta)z^{-1}}. \quad (31)$$

The discrete pole of the choke dynamics characteristic equation is

$$z_\phi = (1 - \beta). \tag{32}$$

A discrete pole z^* can be written in the s domain as s^*

$$z^* = e^{s^* T_s} \tag{33}$$

For a first order system the pole is related with the time constant by $s^* = -\frac{1}{\tau^*}$, then

$$z_e = \frac{1}{2 - G - w_o^2} = e^{-\frac{T_s}{\tau^e}} \text{ and } z_\phi = 1 - \beta = e^{-\frac{T_s}{\tau^\phi}},$$
$$\frac{T_s}{\tau^e} = ln(\frac{1}{z_e}), \text{ and } \frac{T_s}{\tau^\phi} = ln(\frac{1}{z_\phi}),$$
$$\frac{\tau^\phi}{T_s} = \frac{1}{\ln(\frac{1}{z_\phi})}, \text{ and } \frac{\tau^e}{T_s} = \frac{1}{\ln(\frac{1}{z_e})} \tag{34}$$

Using the expressions for the time constants given by equation (34) it is possible to establish a relation between τ^ϕ and τ^e,

$$\tau^\phi = K\tau^e, \quad \frac{1}{\ln(\frac{1}{z_\phi})} = K\frac{1}{\ln(\frac{1}{z_e})},$$
$$\ln(\frac{1}{z_e}) = K\ln(\frac{1}{z_\phi}), \quad -\ln z_e = -K\ln z_\phi,$$
$$z_\phi = z_e^{1/K}, \quad 1 - \beta = \frac{1}{(2 - G - w_o^2)^{1/K}},$$
$$\beta = 1 - \frac{1}{(2 - G - w_o^2)^{1/K}} \tag{35}$$

The value of K in equation (35) is an approximation for the ratio of the settling time of the choke pressure and the $\tilde{p}_b$ error. A choice of $K = 1$ means that the time needed for $\tilde{p}_b$ reach zero will be about the same for the choke pressure or choke opening reach the desired value.

3. SIMULATION RESULTS

3.1 Control tuning for the Di Meglio model of riser

In Meglio et al. (2009) a simplified model of a riser is presented. It consists on the application of mass conservation laws to three different volumes of the riser. The volume of a gas bubble situated at the horizontal section of the riser and the volume of gas and liquid in the inclined part of the riser. Simplified equations are used for flow-rates of gas and liquid as function of the pressure differences. In particular a virtual valve is used to describe the gas flow-rate from the gas bubble volume to the gas volume in the riser. The control algorithm was tested in a simulated scenario composed with a Di Meglio model of riser connected to a separator through a production choke at surface. Simulation tuning is shown in table (1).

The choke was modeled with a first order dynamics by using a time constant τ_{ch}.

3.2 Controller Tuning

Figure 1 shows the behavior of the riser bottom and top pressure and gas flow-rate out of the elongated bubble for the choke fully opened. Using this figure it is possible to obtain the data needed for the tuning of the main controller parameters:

Table 1. Simulator tuning

Symb.	Description	Value
ρ_l	Liquid density	$[887.15\ Kg/m^3]$
H_h	Riser horizontal length	$[800\ m]$
H_v	Riser height	$[1200\ m]$
D	Riser int. diameter	$[0.1024\ m]$
θ	Riser inclination	$[\pi/2\ rad.]$
P_{sep}	Separator pressure	$[689476\ Pa]$
C_g	Virtual valve constant	$[0.0004495 kg/sPa^{0.5}]$
C_c	Production choke constant	$[0.003\ kgs^{-1}Pa^{-0.5}]$
ϵ	Gas frac. by-passing bubble	$[0.02\ -]$
V_{eb}	Volume of elong. bubble	$[6.4859\ m^3]$
V_r	Riser volume	$[9.7288\ m^3]$
τ_{ch}	Choke time constant	$[10\ s]$
w_g^{in}	Gas flow-rate entering riser	$[0.2\ kg/s]$
w_l^{in}	Liq. flow-rate entering riser	$[20\ kg/s]$
m_{lr}	Init. riser liquid mass	$[5.695E+3\ kg]$
m_{gr}	Init. riser gas mass	$[24.148\ kg]$
m_{eb}	Init. gas mass in elong. bubble	$[384.07\ kg]$
p_r^{bh}	Init. riser bottom pressure	$[8.02E+6\ Pa]$
p_r^{top}	Init. riser top pressure	$[1000000\ Pa]$
p_{eb}	Init. pressure in elong. bubble	$[8.116E+6\ Pa]$

- T_{osc} oscillation period in open loop,
- x_1 maximum value of riser bottom pressure,
- x_2 minimum value of the riser bottom pressure,
- y_1 maximum value of riser top pressure,
- y_2 minimum value of riser top pressure,
- y_B mean value of riser top pressure,
- u_{ch} choke opening.

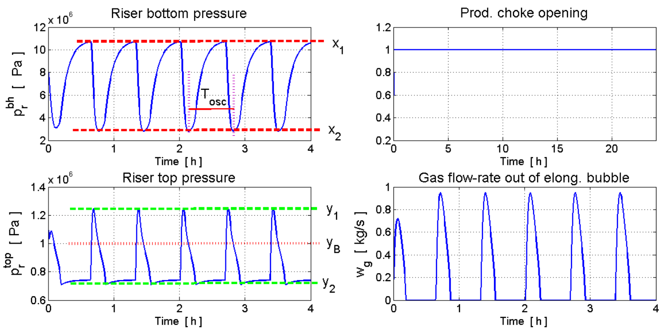

Fig. 1. Open loop simulation

A sampling time $T_s = 30\ s$ was adopted and the controller parameters were computed:

$$w_o = \frac{2\pi T_s}{T_{osc}} = 0.0785, \quad A = \frac{x_1 - x_2}{y_1 - y_2} = 8.2,$$
$$K_c = \frac{0.5}{Aw_o} = 0.775, \quad a = -5T_s/(0.5T_{osc}) = -0.125,$$
$$G = e^a = 0.8825, \quad T_i = \frac{T_s}{1 - G - w_o^2} = 269.5,$$
$$w_c = 0.5w_o/T_s = 6.5417e-04,$$
$$z^*_{wash-out} = e^{-w_c T_s} = 0.9615$$
$$d_{wash-out} = 1 - z^*_{wash-out} = 0.0385$$
$$B(1) = (y_B - P_{sep})u_{ch}^2 = 310524, \quad N_h = \frac{T_{osc}}{T_s} = 80.$$

At first a conservative value of $K = 1$ will be used on the computation of β. This means an attempt to have similar dynamics to the desired choke opening and $\tilde{p}_b$.

$$\beta = 1 - \frac{1}{(2 - G - w_o^2)^{1/K}} = 0.1. \qquad (36)$$

The controller gain was chosen more conservatively as $0.5/Aw_o$ instead of $1/Aw_o$. The desired choke opening is 70%.

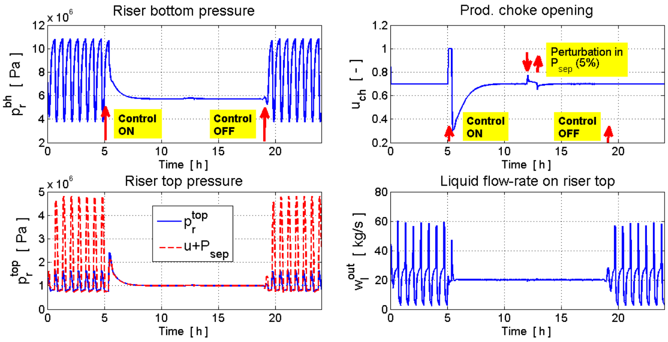

Fig. 2. Results with the application of control

Figure 2 shows the results obtained with the control. The simulation starts with the choke opening at 70% without control applied. The oscillations are clearly seen. At time $t = 5.1\ h$ control is applied. The choke opening reacts moving first to a full open position and then decreasing to a low value. From there it rises to the desired opening position. A disturbance was simulated with an increase on the separator pressure of 5% its nominal value in the time interval $12.0 < t < 12.83\ h$. The choke opening reacts keeping the bottom pressure and the liquid flow-rate stabilized.

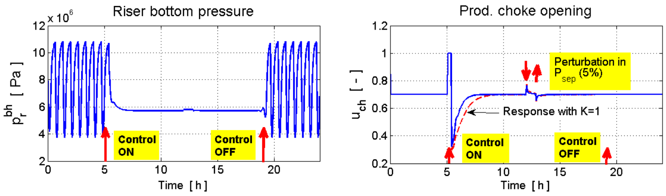

Fig. 3. Results using β with K=0.5

Figure 3 shows the results obtained with β computed with $K = 0.5$. This means that the desired choke opening is to be reached faster and this can be observed in the figure.

3.3 *Control of well with sub-sea head and dedicated riser*

Figure 4 shows the diagram of a well installation with sub-sea production head and a dedicated riser. The well simulator presented in Plucenio et al. (2012) was combined with the riser model presented in Meglio et al. (2009). A virtual valve was used to model the fluid flow passage from the wellhead to the riser. The set of differential and algebraic equations were solved at each sample time. Table 2 shows the main parameters used in the simulations.

Figure 5 shows the simulation results for open loop with different openings of the surface gas and production choke. The simulation starts with a stabilized flow with a gas choke opening equal to 50% and the production choke fully

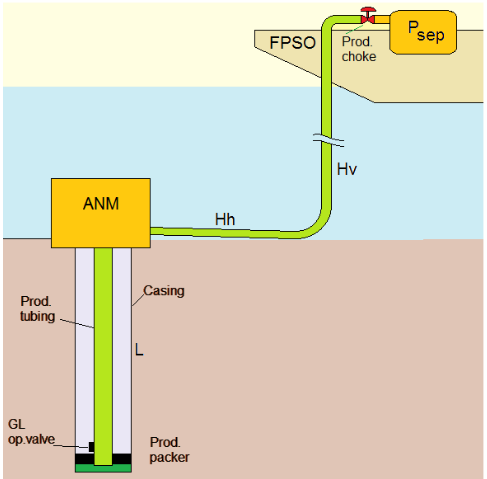

Fig. 4. Well installation diagram

Table 2. Well model parameters

Symb.	Description	Value
g	Gravity acceleration	$[9.81 m/s^2]$
T	Well fluid temperature	$[300\ K]$
M	Gas molecular weight	$[0.0216\ Kg/mol]$
R	Universal gas constant	$[8.31\ J/Kmol]$
L	Well depth from seabed	$[2500\ m]$
D_r	Riser int. diameter	$[0.1092\ m]$
H_h	Horiz. length riser	$[800\ m]$
H_v	Riser vert. section	$[1200\ m]$
D_T	Prod. Tub. ID	$[0.1092\ m]$
D_A	Casing ID	$[0.221\ m]$
D_{ah}	Annular hyd. ID	$[0.1787\ m]$
Φ	GL orifice valve ID	$[0.0127\ m]$
API	Oil API	[22]
GOR	GOR	$[20 stm^3/d/stdm^3/d]$
BSW	BSW	[15%]
μ_o	Oil viscosity	$[0.15\ Pas]$
μ_w	Water viscosity	$[0.001\ Pas]$
μ_g	Gas viscosity	$[0.00002\ Pas]$
ϵ_A	Annular rugosity	$[.0001\ m]$
ϵ_A	PT rugosity	$[.0001\ m]$
q_o^{max}	Max. well mass flow-rate	$[54\ Kg/s]$
rho_a	Water density	$[1000\ Kg/m^3]$
P_r	Mean reserv. pressure	$[30 \times 10^6\ Pa]$
P_s	Separator pressure	$[689476\ Pa]$
τ_{vi}	Time const. inj. choke	$[10\ s]$
τ_{ch}	Time const. prod. choke	$[10\ s]$
τ_q	Time const. Inflow	$[30\ s]$

opened. After one hour the gas choke opening is changed to 26% which causes the bottom pressure to develop a self sustained oscillation with impact in the flow-rates. In the sequence the production choke opening is changed to 50%, 40% and finally to 30% when the oscillations are killed. It is interesting to notice that as the choke opening is diminished, the oscillation periods decrease. This same behavior has been noticed in a Petrobras well with sub-sea head and dedicated riser. At time $t = 12\ hours$ the control is turned off and the choke opening is fixed at 80%. After some time the oscillations return as expected. It is easily seen that the integral of the oil flow-rates for one oscillation period is much smaller during the oscillations than with the stabilized flow.

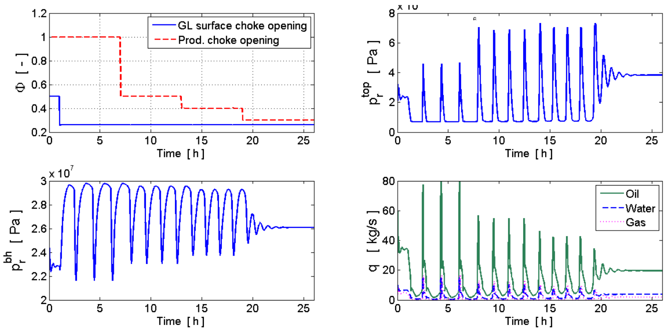

Fig. 5. Simulated results in open loop

In figure 6 the control law developed was applied using the well bottom hole pressure after applying the wash-out filter. The control tuning as done as explained for the riser case. The control gain used was $K_c = 1/Aw_o$, β was computed using $K = 0.5$ and G was computed as before. The desired choke opening was chosen as 80%. The simulation starts as in open loop until $t = 4$ *hours* when the control is applied. It is clear that the choke realize several moves which are progressively less aggressive until it reaches the desired opening. This is exactly what is expected and the choke moves differ from the riser example probably due to the more representative model of the well riser combination.

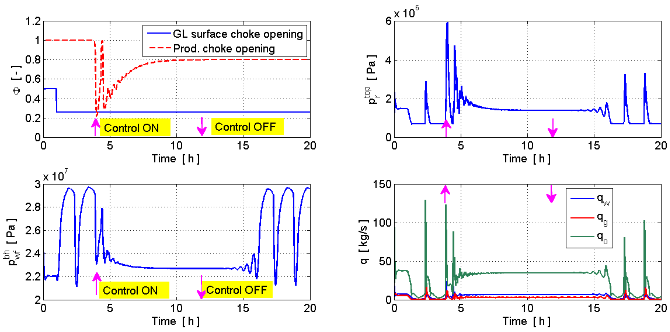

Fig. 6. Simulated results in closed loop

4. OSCILLATION SUPPRESSION WITHOUT P_B MEASUREMENT AND WITHOUT ACTUATED CHOKE

There are situations where the production choke can not be actuated by the automation system and there is no bottom pressure measurement available. Lets consider a situation where a gas-lift well has an active choke to control the gas flow-rate being injected. It is also assumed that the pressure upstream the gas choke p_s^{an} is measured as well as the gas flow-rate q_s^{an} and the pressure at the wellhead p_s^{PT}. The gas mass fraction on the production tubing above the operating valve can be approximated by

$$x(t) = \frac{q_g^{tot}(t)}{q^{tot}(t)}, \quad \text{where} \tag{37}$$

$q^{tot}(t)$ is the local total mass flow-rate and $q_g^{tot}(t)$ is the local gas mass flow-rate. None of these flow-rates are normally measured. The idea is to use surface measurements in order to obtain their estimations.

$$q_g^{tot}(t) = q_g^{GLV}(t) + q_g^{form}(t), \text{where} \tag{38}$$

$q_g^{GLV}(t)$ is the gas flow-rate from the gas-lift operating valve and $q_g^{form}(t)$ is the gas-flow rate being produced from the perforated zone. How to obtain $q_g^{GLV}(t)$ from surface measurements? Applying the mass conservation equation in the well annular,

$$\begin{aligned}
\frac{\partial m_g^{an}(t)}{\partial t} &= q_s^{an}(t) - q_g^{GLV}(t)\text{where} \\
m_g^{an}(t) &\cong V_{an} p_s^{an}(t)\frac{M}{RT} \quad \text{then} \\
q_g^{GLV}(t) &\cong q_s^{an}(t) - \frac{V_{an}M}{RT}\frac{\partial p_s^{an}(t)}{\partial t}, \text{where}
\end{aligned} \tag{39}$$

V_{an} is the volume of the annular, M is the gas molecular weight, T is the mean annular temperature and R is the universal gas constant. The estimation of q^{tot} and q_g^{form} also uses surface measurements. First an estimate of p_b is derived based in the inflow performance relation (IPR) for the well. Assuming for example a case where the IPR is represented by a Vogel equation,

$$\begin{aligned}
Q_o^b(t) &= Q_o^M\left(1 - 0.2\frac{p_b(t)}{\overline{P}} - 0.8\left(\frac{p_b(t)}{\overline{P}}\right)^2\right), \\
Q^b{}_o(t) &= J(p_b(t))\left(\overline{P} - p_b(t)\right) \\
J(p_b(t)) &= \frac{Q_o^M}{\overline{P}}\left(1 + 0.8\frac{p_b(t)}{\overline{P}}\right) \\
p_b(t) &= \overline{P} - \frac{Q_o^b(t)}{J(p_b(t))}
\end{aligned} \tag{40}$$

An estimation of $Q_o^b(t)$ is obtained computing the surface flow-rate as

$$\begin{aligned}
Q_o^s(t) &= k_1\left(p_s^{PT}(t) - p_{SEP}\right)^{0.5} \\
Q_o^b(t) &\approx Q_o^s(t-\tau), \text{ where}
\end{aligned} \tag{41}$$

p_{SEP} is the separator pressure and τ is an estimation of the fluid time travel from bottom to surface and depends on the flow-rate and well length. With $p_b(t)$, BSW, GOR and fluid gravities good estimations of $q^{tot}(t)$ and $q_g^{form}(t)$ can be obtained.

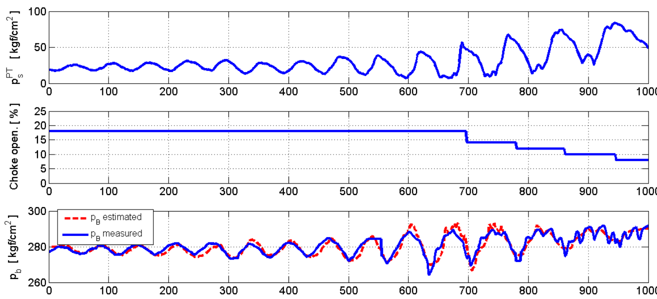

Fig. 7. Estimation of bottom pressure

Figure 7 shows the estimation of the bottom pressure for a real data set (Petrobras) of choke opening and tubing top pressure data.

Figure 8 shows the results obtained applying the technique to a simulated gas-lift well using simulator presented in Plucenio et al. (2012). An average of the gas mas fraction $x(t)$ is computed on the last N_{hor} values. N_{hor} being the

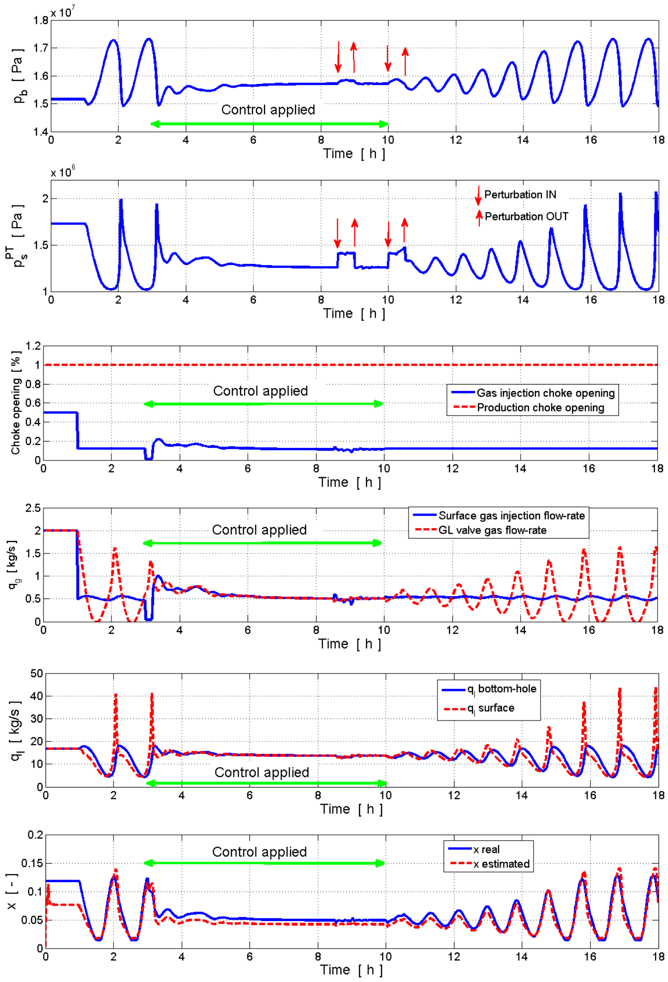

Fig. 8. Simulation results

approximate number of measurements of an oscillation period. The surface gas injection flow-rate is manipulated in order to drive the error between the current gas mass fraction to the average. A second objective is to drive the surface gas flow-rate to the desired value. The computed surface gas flow-rate is implemented manipulating the surface gas-lift choke opening. On figure 8 the injection gas flow-rate at surface starts with 2.0 $kg.s^{-1}$. At $t = 1.0$ hour the gas flow-rate is changed to 0.5 $kg.s^{-1}$. A heading oscillation develops. At $t = 3$ *hours* the control is applied. In spite of the perturbation on the separator pressure rising 20% between time 8.5 and 9.0 hours the control manages to stabilize the well. At time equal to 10 hours the control is turned off and the same perturbation enters between 10 and 10.5 hours and drives the system to a limit cycle. It is worth to point out that the control effort to deal with the perturbation is minimum. At the bottom of figure 8 the gas mass fraction obtained with the simulator is compared with the estimated one showing a good agreement. The desired gas flow-rate of 0.5 $kg.s^{-}1$ is achieved after stabilization. The production choke was kept fully opened during the whole simulation.

5. CONCLUSION

The application of control techniques in oil production wells and risers have several advantages. It enables to operate wells with higher production rates, decrease riser fatigue, avoid trips in process automation systems, simplify the control of the primary production process system, etc. The idea is simple. It consists in active control of wells and risers instead of reactive control. Hopefully this paper may contribute to this objective.

REFERENCES

Colling, I.E. and Barbi, I. (2001). Ac-dc converter controlled by sliding mode. *IEEE Transactions on Power Electronics*, 16(2), 223–230.

Eikrem, G.O., Aamo, O.M., and Foss, B.A. (2008). On instability in gas lift wells and schemes for stabilization by automatic control. *Society of Petroleum Engineers*, May. doi:10.2118/101502-PA.

F. Di Meglio, G. O. Kaasa, N.P. and Alstad, V. (2012). Stabilization of slugging in oil production facilities with or without upstream pressure sensors. *Journal of Process Control*, 33(4), 809–822.

Godhavn, J., Fard, M.P., and Fuchs, P. (2005). New slug control strategies, tuning rules and experiments results. *Journal of Process Control*, 15, 547–557.

Hassouneh, M.A., Lee, H.C., and Abed, E.H. (2004). Washout filters in feedback control: Benefits, limitation and extensions. *Report of the Institute for Systems Research University of Maryland*, 11.

Jahanshahi, E., Skogestad, S., and Helgesen, A.H. (2012). Controllability analysis of severe slugging in well-pipeline-riser systems. *Automatic Control in Offshore Oil and Gas Production-NTNU-Norway*, 1, 109–115. doi:10.3182/20120531-2-NO-4020.00014.

Meglio, F.D., Kaasa, G.O., Petit, N., and Alstad, V. (2012). Model-based control of slugging: Advances and challenges. *Automatic Control in Offshore Oil and Gas Production-NTNU-Norway*, 1, 109–115. doi: 10.3182/20120531-2-NO-4020.00029.

Meglio, F.D., Kaasa, G.O., and Petit, N. (2009). A first principle model for multiphase slugging flow in vertical risers. *48th IEEE Conference on Decision and Control and 28th Chinese Control Conference*.

Ogazi, A., Cao, Y., Lao, L., and Yeung, H. (2009). Robust control of severe slugging at open loop unstable condition to increase oil production. *International Conference on System Engineering-Coventy,UK*.

Plucenio, A., Ganzaroli, C.A., and Pagano, D.J. (2012). Stabilizing gaslift well dynamics with free operating point. *Automatic Control in Offshore Oil and Gas Production*, 1, 95–100. doi:10.3182/20120531-2-NO-4020.00016.

Siahaan, H.B., Aamo, O.M., and Foss, B.A. (2005). Suppressing riserbased slugging in multiphase flow by state feedback. *Proceedings of the 44th Conference on Decision and Control*, 1, 12–15.

Sinegre, L. (2006). *Etude des instabilités dans les puits activés par gas-lift*. Ph.D. thesis, Ecole des Mines de Paris, Paris, France.

Storkaas, E. and Skogestad, S. (2007). Controllability analysis of two-phase pipeline-riser systems at riser slugging conditions. *Control Engineering Practice*, 15, 567–581.