Calculations executed for the 2-bladed rotor of the VIRYA-5 windmill (λ_d = 7, Gö 711 airfoil) meant for connection to a 34-pole PM-generator for driving the 1.1 kW asynchronous motor of a centrifugal pump. Description of the 34-pole generator.

ing. A. Kragten

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KD 614

 The windmill and the generator are not yet tested.

Engineering office Kragten Design
Populierenlaan 51
5492 SG Sint-Oedenrode
The Netherlands
telephone: +31 413 475770
e-mail: info@kdwindturbines.nl
website: www.kdwindturbines.nl

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1 Introduction

The VIRYA-5 windmill has a 2-bladed rotor with wooden or stainless steel blades which are connected to each other by means of a twisted steel strip. The head of the VIRYA-5 is derived from the head of the VIRYA-4.6B2. The tower is identical to the 12 m tower of the VIRYA-4.6B2. As an alternative, a shorter tower can be used which is built up from two tubular sections. The lower section will be made of 6 m, 5” gas pipe. The upper section will be made of 3 m, 3” gas pipe. The overlap in between both sections will be about 0.4 m, so the total tower height will be about 8.6 m. This tower is described in report KD 582 (ref. 1) for the VIRYA-4.6B2.

The VIRYA-5 makes use of a 34-pole PM-generator. This idea of using a 34-pole generator was already described in report KD 560 (ref. 2) for the VIRYA-3.3S generator. The VIRYA-5 is primary designed to be directly coupled to the asynchronous motor of a centrifugal pump. The generator is made from the housing of a 6-pole, 5.5 kW asynchronous motor frame size 132 with stator lamination of manufacture Kienle & Spiess. The 34-pole generator is described in detail in chapter 7.

The VIRYA-5 has a design tip speed ratio of 7 in stead of 4.5 for the VIRYA-3.3S and the higher design tip speed ratio roughly compensates the reduction of the rotational speed because of the larger rotor diameter. The windmill is provided with the so called hinged side vane safety system and has a rated wind speed V_{rated} of about 9.5 m/s.

In stead of direct coupling to the motor of a centrifugal pump, the windmill can also be used for battery charging if the generator is provided with a low voltage winding and if the winding is rectified in star. Rectification of the winding is described in report KD 340 (ref. 3).

2 Description of the rotor of the VIRYA-5 windmill

The 2-bladed rotor of the VIRYA-5 windmill has a diameter D = 5 m and a design tip speed ratio λ_d = 7. Advantages of a 2-bladed rotor are that no welded spoke assembly is required, that the rotor can be balanced easily and that it can be transported completely mounted. A disadvantage is that the gyroscopic moment in the rotor shaft is fluctuating.

The rotor has blades with a constant chord and no twist and is provided with a Gö 711 airfoil which is flat over 97.5 % of the chord. The aerodynamic characteristics of this airfoil are described in report KD 285 (ref. 4). A blade is made out of a wooden plank with dimensions of 36 * 240 * 2300 mm. The Gö 711 airfoil is made over the whole length of the blade. The blades are connected to each other by a 1 m long twisted connecting strip with a width of 120 mm and a thickness of 10 mm. The overlap in between blade and strip is 0.3 m which results in a free blade length of 2 m. Each blade is connected to the strip by three M12 bolts. A 3 mm thick curved stainless steel strip is placed under the bolt heads to prevent deformation of the wood.

As an alternative, it might be possible to make a blade out of a stainless steel strip size 1.5 * 500 * 2000 mm which is folded into a Gö 711 airfoil. The strip sides are welded together at the tailing edge. Because the back side of the blade is curved, the chord c is a little less than halve the strip width, resulting in about c = 240 mm = 0.24 m. An aluminium dummy with the width of the strip fits inside the airfoil at the position of each bolt. If the blade is made out of stainless steel, the connecting strip must have a length of at least 1.5 m.

The central strip is clamped in between the hub and a clamping disk by means of four bolts M12 and that is why the strip is not loaded by a bending moment at the position of the holes. The rotor is balanced by adding balance weights under the connecting bolts. A sketch of the VIRYA-5 rotor is given in figure 1 for the wooden version.

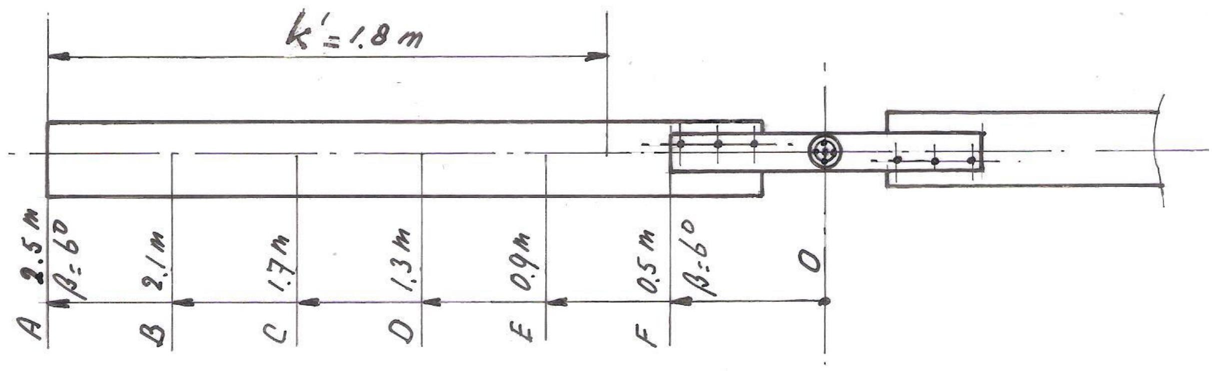

fig. 1 Sketch VIRYA-5 rotor

3 Calculation of the rotor geometry

The rotor geometry is determined using the method and the formulas as given in report KD 35 (ref. 5). This report (KD 614) has its own formula numbering. Substitution of λ_d = 7 and R = 2.5 m in formula (5.1) of KD 35 gives:

$\lambda_{r\,d} = 2.8 * r$ (-)   (1)

Formula's (5.2) and (5.3) of KD 35 stay the same so:

$\beta = \phi - \alpha$ (°)   (2)

$\phi = 2/3 \arctan 1 / \lambda_{r\,d}$ (°)   (3)

Substitution of B = 2 and c = 0.24 m in formula (5.4) of KD 35 gives:

$C_l = 52.360\ r\ (1 - \cos\phi)$ (-)   (4)

Substitution of V = 5 m/s and c = 0.24 m in formula (5.5) of KD 35 gives:

$R_{e\,r} = 0.8 * 10^5 * \sqrt{(\lambda_{r\,d}^2 + 4/9)}$ (-)   (5)

The blade is calculated for six stations A till F which have a distance of 0.4 m of one to another. Cross section F corresponds to the end of the connecting strip. The blade has a constant chord and the calculations therefore correspond with the example as given in chapter 5.4.2 of KD 35. This means that the blade is designed with a low lift coefficient at the tip and with a high lift coefficient at the root. First the theoretical values are determined for C_l, α and β and next β is linearised such that the twist is constant and that the linearised values for the outer part of the blade correspond as good as possible with the theoretical values. The result of the calculations is given in table 1.

The aerodynamic characteristics of the Gö 711 airfoil are given in report KD 285 (ref. 4). This airfoil has only be measured for Re = 4 * 10^5 but the VIRYA-5 rotor is rather big and the design tip speed ratio is rather high and the calculated local Reynolds numbers are therefore rather high. The Reynolds values for the stations are calculated for a wind speed of 5 m/s because this is a reasonable wind speed for a windmill with V_{rated} = 9.5 m/s.

station	r (m)	λ_{rd} (-)	ϕ (°)	c (m)	$C_{l\,th}$ (-)	$C_{l\,lin}$ (-)	$R_{e\,r} * 10^{-5}$ V = 5 m/s	$R_e * 10^{-5}$ Gö 711	α_{th} (°)	α_{lin} (°)	β_{th} (°)	β_{lin} (°)	$C_d/C_{l\,lin}$ (-)
A	2.5	7	5.4	0.24	0.59	0.62	5.63	4	-1	-0.6	6.4	6.0	0.022
B	2.1	5.88	6.4	0.24	0.69	0.70	4.73	4	0.2	0.4	6.2	6.0	0.020
C	1.7	4.76	7.9	0.24	0.85	0.82	3.85	4	2.1	1.9	5.8	6.0	0.016
D	1.3	3.64	10.2	0.24	1.08	1.01	2.96	4	5.2	4.2	5.0	6.0	0.015
E	0.9	2.52	14.4	0.24	1.49	1.34	2.09	4	11.5	8.4	2.5	6.0	0.021
F	0.5	1.4	23.7	0.24	2.21	1.36	1.24	4	-	17.7	-	6.0	0.145

table 1 Calculation of the blade geometry of the VIRYA-5 rotor

No value for α_{th} and therefore for β_{th} is found for station F because the required C_l value can't be generated. The variation of the theoretical blade angle β_{th} is only little for the most important outer stations A up to D and varies in between 6.4° and 5.0°. Therefore it is allowed to take a constant value of 6° for the whole blade. The connecting strip is twisted 6° right hand in between the hub and the position of the blade root.

4 Determination of the C_p-λ and the C_q-λ curves

The determination of the C_p-λ and C_q-λ curves is given in chapter 6 of KD 35. The average C_d/C_l ratio for the most important outer part of the blade is about 0.02. Figure 4.6 of KD 35 (for B = 2) and $\lambda_{opt} = 7$ and $C_d/C_l = 0.02$ gives $C_{p\,th} = 0.46$.

The blade is stalling in between station E and F. For the calculation of the maximum C_p therefore not the whole blade length k = 2.3 m is taken into account but only the part up to half way station E and F. This gives an effective blade length k' = 1.8 m.
Substitution of $C_{p\,th} = 0.46$, R = 2.5 m and effective blade length k' = 1.8 m in formula 6.3 of KD 35 gives $C_{p\,max} = 0.42$. $C_{q\,opt} = C_{p\,max} / \lambda_{opt} = 0.42 / 7 = 0.06$.
Substitution of $\lambda_{opt} = \lambda_d = 7$ in formula 6.4 of KD 35 gives $\lambda_{unl} = 11.2$.
The starting torque coefficient is calculated with formula 6.12 of KD 35 which is given by:

$$C_{q\,start} = 0.75 * B * (R - ½k) * C_l * c * k / \pi R^3 \quad (-) \qquad (6)$$

The blade angle is 6° for the whole blade. For a non rotating rotor, the angle of attack α is therefore 90° - 6° = 84°. The aerodynamic characteristics for the Gö 711 aren't given for large angles of α. However, it is assumed that the characteristics of the Gö 623 airfoil can be used for large angles of α. The estimated C_l-α curve for large values of α is given as figure 5.10 of KD 35 (ref. 5). For $\alpha = 84°$ it can be read that $C_l = 0.21$. The whole blade is stalling during starting and therefore now the whole blade length k = 2.3 m is taken.
Substitution of B = 2, R = 2.5 m, k = 2.3 m, $C_l = 0.21$ and c = 0.24 m in formula 6 gives that $C_{q\,start} = 0.0048$. For the ratio between the starting torque and the optimum torque we find that it is 0.0048 / 0.06 = 0.08. This is acceptable for a rotor with $\lambda_d = 7$.

The starting wind speed V_{start} of the rotor is calculated with formula 8.6 of KD 35 which is given by:

$$V_{start} = \sqrt{\left(\frac{Q_s}{C_{q\,start} * ½\rho * \pi R^3} \right)} \quad (m/s) \qquad (7)$$

The 34-pole generator has not yet been built so the sticking torque has not yet been measured. The sticking torque of the VIRYA-4.6 generator Q_s has been measured at stand still position and it is 1.6 Nm. Assume that the sticking torque of the VIRYA-5 generator is 2 Nm.

Substitution of Q_s = 2 Nm, $C_{q\ start}$ = 0.0048, ρ = 1.2 kg/m^3 and R = 2.5 m in formula 7 gives that V_{start} = 3.8 m/s. This is acceptable low for a 2-bladed rotor with a design tip speed ratio of 7 and a rated wind speed of 9.5 m/s.

In chapter 6.4 of KD 35 it is explained how rather accurate C_p-λ and C_q-λ curves can be determined if only two points of the C_p-λ curve and one point of the C_q-λ curve are known. The first part of the C_q-λ curve is determined according to KD 35 by drawing an S-shaped line which is horizontal for λ = 0. Kragten Design developed a method with which the value of C_q for low values of λ can be determined (see report KD 97 ref. 6). With this method, it can be determined that the C_q-λ curve is about straight and horizontal for low values of λ if a Gö 623 or a Gö 711 airfoil is used. A scale model of a three bladed rotor with constant chord and blade angle and with a design tip speed ratio λ_d = 6 has been measured in the wind tunnel already on 20-11-1980. It has been found that the maximum C_p was more than 0.4 and that the C_q-λ curve for low values of λ was not horizontal but somewhat rising. This effect has been taken into account and the estimated C_p-λ and C_q-λ curves for the VIRYA-5 rotor are given in figure 2 and 3.

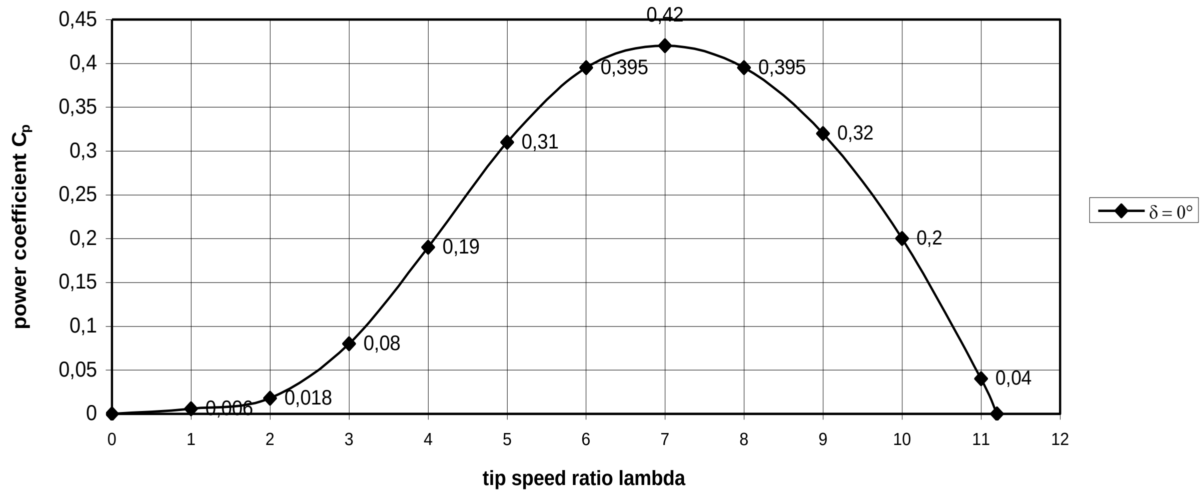

fig. 2 Estimated C_p-λ curve for the VIRYA-5 rotor for the wind direction perpendicular to the rotor (δ = 0°)

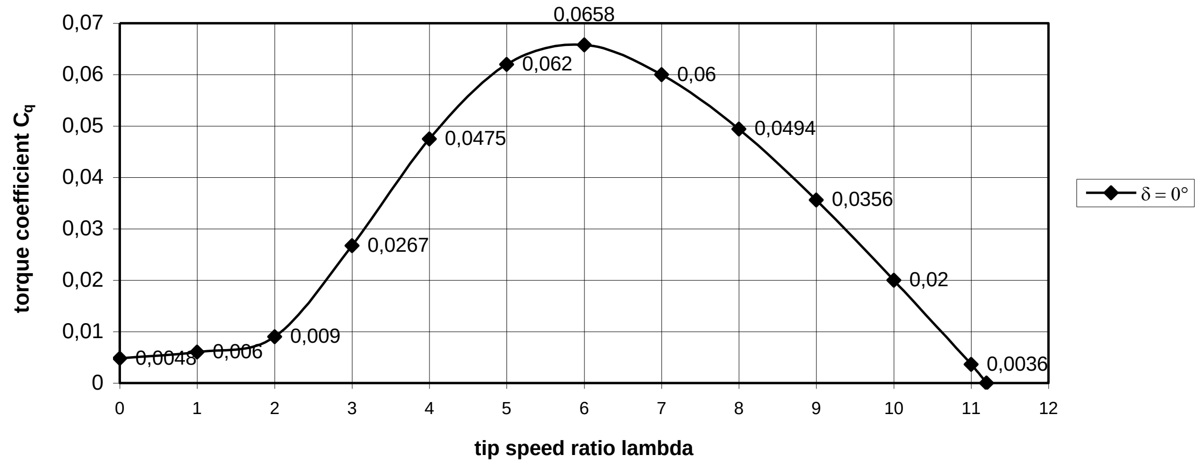

fig. 3 Estimated C_q-λ curve for the VIRYA-5 rotor for the wind direction perpendicular to the rotor (δ = 0°)

5 Determination of the P-n curves, the optimum cubic line and the lines for constant f

The determination of the P-n curves of a windmill rotor is described in chapter 8 of KD 35. One needs a C_p-λ curve of the rotor and a δ-V curve of the safety system together with the formulas for the power P and the rotational speed n. The C_p-λ curve is given in figure 2. The δ-V curve of the safety system depends on the vane blade mass per area. The vane blade is made of 9 mm meranti plywood. This vane blade gives a rated wind speed V_{rated} of about 9.5 m/s. The estimated δ-V curve is given in figure 4. In report KD 213 (ref. 7) a method is given to check the estimated δ-V curve and the estimated δ-V curve of the VIRYA-4.2 windmill is checked as an example. This windmill also has a vane blade made of 9 mm meranti plywood. One may use oucume plywood but oucume has a lower density than meranti and it is assumed that 12 mm oucume has about the same mass per m^2 as for 9 mm meranti. So the δ-V curves will be about the same. The advantage of using 12 mm oucume is that the vane blade is stiffer. The estimated and calculated curves appear to lie very close to each other so it is allowed to use the estimated curve. The estimated curve is given in figure 4.

The head starts to turn away at a wind speed of about 6 m/s. For wind speeds above 9.5 m/s it is supposed that the head turns out of the wind such that the component of the wind speed perpendicular to the rotor plane, is staying constant. The P-n curve for 9.5 m/s will therefore also be valid for wind speeds higher than 9.5 m/s.

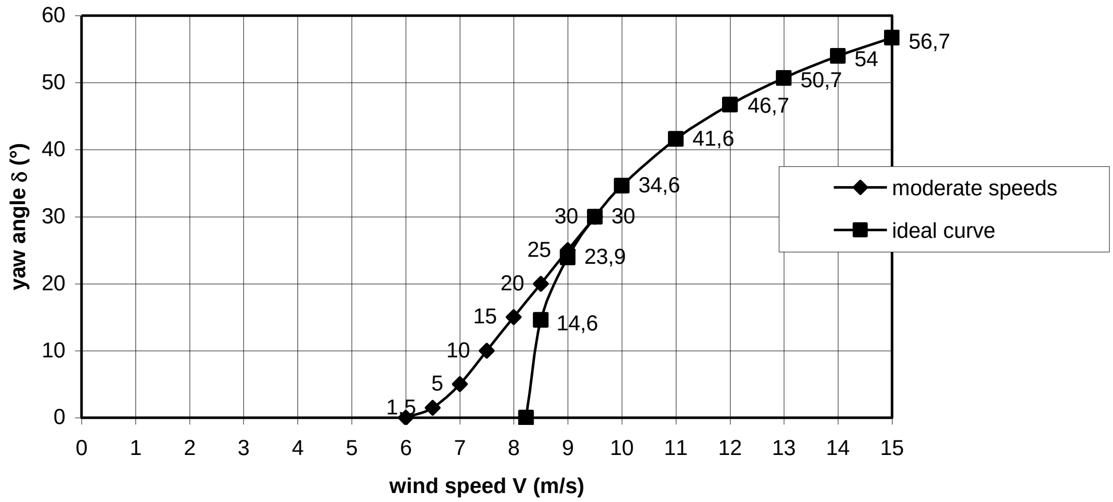

fig. 4 δ-V curve VIRYA-5 safety system with V_{rated} = 9.5 m/s

The P-n curves are used to check the matching with the P_{mech}-n curve of the generator for a certain gear ratio i (the VIRYA-5 has no gearing so i = 1). Because we are especially interested in the domain around the optimal cubic line and because the P-n curve for low values of λ appear to lie very close to each other, the P-n curves are not determined for low values of λ. The P-n curves are determined for wind the speeds 3, 4, 5, 6, 7, 8, 9 and 9.5 m/s. At high wind speeds the rotor is turned out of the wind by a yaw angle δ and therefore the formulas for P and n are used which are given in chapter 7 of KD 35.

Substitution of R = 2.5 m in formula 7.1 of KD 35 gives:

$$n_\delta = 3.8197 * \lambda * \cos\delta * V \qquad \text{(rpm)} \qquad (8)$$

Substitution of ρ = 1.2 kg / m^3 and R = 2.5 m in formula 7.10 of KD 35 gives:

$$P_\delta = 11.781 * C_p * \cos^3\delta * V^3 \qquad \text{(W)} \qquad (9)$$

The P-n curves are determined for C_p values belonging to λ is 4, 5, 6, 7, 8, 9, 10, and 11.2 (see figure 1). For a certain wind speed, for instance V = 3 m/s, related values of C_p and λ are substituted in formula 8 and 9 and this gives the P-n curve for that wind speed. For the higher wind speeds the yaw angle as given by figure 4, is taken into account. The result of the calculations is given in table 2.

		V = 3 m/s δ = 0°		V = 4 m/s δ = 0°		V = 5 m/s δ = 0°		V = 6 m/s δ = 0°		V = 7 m/s δ = 5°		V = 8 m/s δ = 15°		V = 9 m/s δ = 25°		V = 9.5 m/s δ = 30°	
λ (-)	C_p (-)	n (rpm)	P (W)	n (rpm)	P (W)	n (rpm)	P (W)	n (rpm)	P (W)	n_δ (rpm)	P_δ (W)	n_δ (rpm)	P_δ (W)	n_δ (rpm)	P_δ (W)	n_δ (rpm)	P_δ (W)
4	0.19	45.8	60.9	61.1	143.3	76.4	279.8	91.7	483.5	106.5	759.0	118.1	1033	124.6	1215	125.7	1247
5	0.31	57.3	99.4	76.4	233.7	95.5	456.5	114.6	788.9	133.2	1238	147.6	1685	155.8	1982	157.1	2034
6	0.395	68.8	126.6	91.7	297.8	114.6	581.7	137.5	1005	159.8	1578	177.1	2147	186.9	2525	188.6	2591
7	0.42	80.2	134.6	107.0	316.7	133.7	618.5	160.4	1069	186.5	1678	206.6	2283	218.1	2685	220.0	2755
8	0.395	91.7	126.6	122.2	297.8	152.8	581.7	183.3	1005	213.1	1578	236.1	2147	249.3	2525	251.4	2591
9	0.32	103.1	102.6	137.5	241.3	171.9	471.2	206.3	814.3	239.7	1278	265.6	1740	280.4	2046	282.8	2099
10	0.2	114.6	64.1	152.8	150.8	191.0	294.5	229.2	508.9	266.4	799.0	295.2	1087	311.4	1279	314.3	1312
11.2	0	128.3	0	171.1	0	213.9	0	256.7	0	298.3	0	330.6	0	349.0	0	352.0	0

table 2 Calculated values of n and P as a function of λ and V for the VIRYA-5 rotor

The calculated values for n and P are plotted in figure 5. The optimum cubic line which can be drawn through the tops of the P-n curves, is also given in figure 5.

The 34-pole generator has not yet been built and measured, so measured characteristics are not available. However, it is possible to derive the lines for which the frequency has a certain value. A 2-pole PM-generator has a frequency of 50 Hz for a rotational speed of 3000 rpm. So a 34-pole generator has a frequency of 50 Hz for a rotational speed of 3000 * 2 / 34 = 176.47 rpm. As the frequency is proportional to the rotational speed, the rotational speeds for other frequencies can be determined easily. It is found that:

n = 123.53 rpm for f = 35 Hz.
n = 141.18 rpm for f = 40 Hz.
n = 158.82 rpm for f = 45 Hz.
n = 176.47 rpm for f = 50 Hz.
n = 194.12 rpm for f = 55 Hz.
n = 211.76 rpm for f = 60 Hz.
n = 229.41 rpm for f = 65 Hz.

The lines for constant frequencies of 35, 40, 45, 50, 55, 60 and 65 Hz are also given in figure 5. In figure 5 it can be seen that the line for f = 50 Hz is intersecting with the optimum cubic line at a power of about 1420 W. The available electrical power will be lower because of the generator efficiency. Assume the generator efficiency is 0.8, so the electrical power is about 1140 W. This power is generated at a wind speed of about 6.6 m/s. The wind speed for which the line for 50 Hz is intersecting with the optimum cubic line is called the design wind speed V_d. So V_d = 6.6 m/s.

A centrifugal pump with a 1.1 kW pump motor used at a factor 0.8 of its nominal power and with a motor efficiency of 0.75 will absorb an electrical power of about 1170 W, so a 1.1 kW pump motor seems an acceptable choice. The working point will lie about on the optimum cubic line for a pump with a 1.1 kW motor.

In figure 4 it can be seen that the maximum power at a wind speed of 9.5 m/s is 2750 W if the optimum cubic line is followed. The frequency is about 62.5 Hz which is rather high. The load characteristic of a centrifugal pump is about a cubic line which means that the optimum cubic line of the windmill will be followed upwards from the design point if the design point is lying on the optimum cubic line. I expect that a maximum frequency of 62.5 Hz is allowed for the pump and for the pump motor but this must be verified in practice.

Below a frequency of about 35 Hz, belonging to a rotational speed of 123.53 rpm, the pump is no longer able to produce the static water height so no water will be pumped. Probably it is necessary to disconnect the generator and the pump motor by a 3-phase switch below a frequency of about 35 Hz. This makes that the rotor will always start unloaded at low wind speeds. If the connection is broken at f = 35 Hz for a running rotor, this results in acceleration of the rotor. The connection can be made at a frequency of 53 Hz belonging to a rotational speed of about 187 rpm. This frequency will be reached for an unloaded rotor for a wind speed of about 4.5 m/s. So the pump will start pumping at this wind speed but it will stop only if the frequency becomes lower than 35 Hz. This means that even at low wind speeds there will be some intermittent output.

If the pump is a centrifugal pump, the system will probably also work if there is no 3-phase switch which disconnects the generator and the pump motor but in this case water is not pumped intermittently if the wind speed is just above 4.5 m/s. A switch will certainly be needed for a positive displacement pump as such pump demands a torque directly from stand still position.

The generator winding must be chosen such that the loaded voltage is 230 V at a frequency of 50 Hz. This means that the unloaded voltage at 50 Hz must be a lot higher. I expect about 280 V but this must be tested for a prototype of the generator.

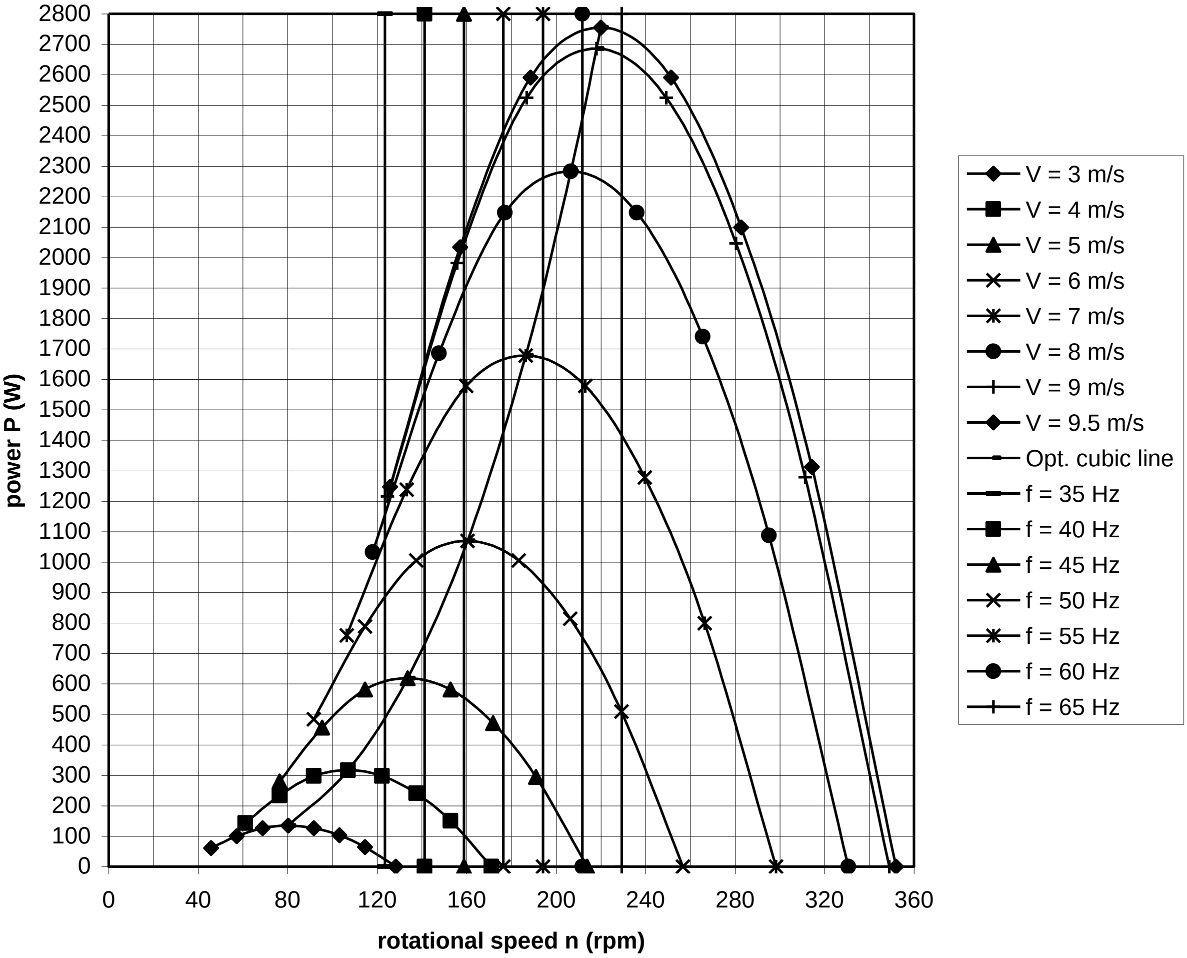

fig. 5 P-n curves of the VIRYA-5 rotor, optimum cubic line and line for frequencies f of 35, 40, 45, 50, 55, 60 and 65 Hz

6 Calculation of the strength of the connecting strip

The two wooden blades are connected to each other by a steel strip with a length of 1000 mm, a width b = 120 mm and a height h = 10 mm. The strip is loaded by a bending moment with axial direction which is caused by the rotor thrust and by the gyroscopic moment. The strip is also loaded by a centrifugal force and by a bending moment with tangential direction caused by the torque and by the weight of the blade but the stresses which are caused by these loads can be neglected.

Because the strip is thin and long it makes the blade connection elastic and therefore the blade will bend backwards already at a low load. As a result of this bending, a moment with direction forwards is created by a component of the centrifugal force in the blade. The bending is substantially decreased by this moment and this has a favourable influence on the bending stress.

It is started with the determination of the bending stress which is caused by the rotor thrust. There are two critical situations:

1e The load which appears for a rotating rotor at V_{rated} = 9.5 m/s. For this situation the bending stress is decreased by the centrifugal moment. The yaw angle is 30° for V_{rated} = 9.5 m/s.

2e The load which appears for a slowed down rotor. The rotor is slowed down by making short-circuit in the generator winding. A graph has been made in which the Q-n curve of the rotor for V = 9.5 m/s has been plotted together with the Q-n curve of the generator for short-circuit in delta. For the working point it is found that the rotor rotates at a rotational speed of about 8 rpm and has a tip speed ratio of about 0.2. For this very low rotational speed the effect of compensation by the centrifugal moment is negligible and a tip speed ratio of 0.2 is very low. Therefore it is assumed that the rotor stands still.

6.1 Bending stress in the strip for a rotating rotor and V = 9.5 m/s

The rotor thrust is given by formula 7.4 of KD 35. The rotor thrust is the axial load of all blades together and exerts in the hart of the rotor. The thrust per blade $F_{t\,\delta\,bl}$ is the rotor thrust $F_{t\,\delta}$ divided by the number of blades B. This gives:

$$F_{t\,\delta\,bl} = C_t * \cos^2\delta * ½\rho V^2 * \pi R^2 / B \qquad \text{(N)} \qquad (10)$$

For the rotor theory it is assumed that every small area dA which is swept by the rotor, supplies the same amount of energy and that the generated energy is maximised. For this situation the wind speed in the rotor plane has to be slowed down till 2/3 of the undisturbed wind speed V. This results in a pressure drop over the rotor plane which is the same for every value of r. It can be proven that this results in a triangular axial load which forms the thrust and in a constant radial load which supplies the torque. The theoretical thrust coefficient C_t for the whole rotor is 8/9 = 0.889 for the optimal tip speed ratio. In practice C_t is lower because of the tip losses and because the blade is not effective up to the rotor centre. The effective blade length k’ of the VIRYA-5 rotor is only 1.8 m but the rotor radius R = 2.5 m. Therefore there is a disk in the centre with an area of about 0.078 of the rotor area on which almost no thrust is working. This results in a theoretical thrust coefficient C_t = 8/9 * 0.922 = 0.82. Because of the tip losses the real C_t value is substantially lower. Assume this results in a real practical value of C_t = 0.7.
Substitution of C_t = 0.7, δ = 30°, ρ = 1.2 kg/m^3, V = 9.5 m/s, R = 2.5 m and B = 2 in formula 10 gives $F_{t\,\delta\,bl}$ = 279 N.

For a pure triangular load, the same moment is exerted in the hart of the rotor as for a point load which exerts in the centre of gravity of the triangle. The centre of gravity is lying at 2/3 R = 1.667 m. Because the effective blade length is only k', there is no triangular load working on the blade but a load with the shape of a trapezium as the triangular load over the part R – k' falls off. The centre of gravity of the trapezium has been determined graphically and is lying at about $r_1 = 1.75$ m.

The maximum bending stress is not caused at the hart of the rotor but at the edge of the hub because the strip bends backwards from this edge. This edge is lying at $r_2 = 0.05$ m. At this edge we find a bending moment $M_{b\,t}$ caused by the thrust which is given by:

$$M_{b\,t} = F_{t\,\delta\,bl} * (r_1 - r_2) \qquad \text{(Nm)} \qquad (11)$$

Substitution of $F_{t\,\delta\,bl} = 279$ N, $r_1 = 1.75$ m and $r_2 = 0.05$ m gives $M_{b\,t} = 474$ Nm = 474000 Nmm.

For the stress we use the unit N/mm^2 so the bending moment has to be given in Nmm. The bending stress σ_b is given by:

$$\sigma_b = M / W \qquad (\text{N/mm}^2) \qquad (12)$$

The moment of resistance W of a strip is given by:

$$W = 1/6\ bh^2 \qquad (\text{mm}^3) \qquad (13)$$

(12) + (13) gives:

$$\sigma_b = 6\ M / bh^2 \qquad (\text{N/mm}^2) \qquad (\text{M in Nmm}) \qquad (14)$$

Substitution of M = 474000 Nmm, b = 120 mm and h = 10 mm in formula 14 gives $\sigma_b = 237$ N/mm^2. For this stress the effect of the stress reduction by bending forwards of the blade caused by the centrifugal force in the blade has not yet been taken into account. The gyroscopic moment has also not yet been taken into account.

Next it is investigated how far the blade bends backwards as a result of the thrust load and what influence this bending has on the centrifugal moment. Hereby it is assumed that the strip is bending only in between the hub and the inner connection bolt of blade and strip. So it is assumed that the blade itself is not bending. The inner connection bolt is lying at $r_3 = 0.23$ m = 230 mm. So the length of the strip l which is loaded by bending is given by:

$$l = r_3 - r_2 \qquad \text{(mm)} \qquad (15)$$

The load from the blade on the strip at r_3 can be replaced by a moment M and a point load F. F is equal to $F_{t\,\delta\,bl}$. M is given by:

$$M = F * (r_1 - r_3) \qquad \text{(Nmm)} \qquad (16)$$

The bending angle ϕ (in radians) at r_3 for a strip with a length l is given by (combination of the standard formulas for a moment plus a point load):

$$\phi = l * (M + \tfrac{1}{2} Fl) / EI \qquad \text{(rad)} \qquad (17)$$

The bending moment of inertia I of a strip is given by:

$I = 1/12\ bh^3$ (mm^4) (18)

(15) + (16) + (17) + (18) gives:

$\phi = 12 * F * (r_3 - r_2) * \{(r_1 - r_3) + ½ (r_3 - r_2)\} / (E * bh^3)$ (rad) (19)

Substitution of F = 279 N, r_3 = 230 mm, r_2 = 50 mm, r_1 = 1750 mm, E = 2.1 * 10^5 N/mm^2, b = 120 mm and h = 10 mm in formula 19 gives: ϕ = 0.0385 rad = 2.21°. This is an angle which can not be neglected. In report R409D (ref. 8) a formula is derived for the angle ε with which the blade moves backwards if it is connected to the hub by a hinge. This formula is valid if both the axial load and the centrifugal load are triangular. For the VIRYA-5 this is not exactly the case but the formula gives a good approximation. The formula is given by:

$$\varepsilon = \arcsin \left(\frac{C_t * \rho * R^2 * \pi}{B * A_{pr} * \rho_{pr} * \lambda^2} \right) \quad (°) \qquad (20)$$

In this formula A_{pr} is the cross sectional area of the airfoil (in m^2) and ρ_{pr} is the density of the used airfoil material (in kg/m^3). For a Gö 711 airfoil made out of wood, A_{pr} is about given by A_{pr} = 0.7 * t * c = 0.7 * 0.0357 * 0.24 = 0.0060 m^2. The blade is made of hard wood with a density ρ_{pr} of about ρ_{pr} = 0.65 * 10^3 kg/m^3. It is assumed that for high wind speeds, the rotor is running at its design tip speed ratio λ_d = 7. Substitution of C_t = 0.7, ρ = 1.2 kg/m^3, R = 2.5 m, B = 2, A_{pr} = 0.0060 m^2, ρ_{pr} = 0.65 * 10^3 kg/m^3 and λ = 7 in formula 20 gives: ε = 2.47°. This angle is larger than the calculated angle of 2.21° with which the blade would bend backwards if the compensating effect of the centrifugal moment is not taken into account. This means that the real bending angle will be less than 2.21°.

The real bending angle ε is determined a follows. A thrust moment M_t = 474 Nm is working backwards and M_t is independent of ε for small values of ε. A bending moment M_b is working forwards and M_b is proportional with ε. M_b = 474 Nm for ε = 2.21°. A centrifugal moment M_c is working forwards and M_c is also proportional with ε. M_c = 474 Nm for ε = 2.47°. The path of these three moments is given in figure 6. The sum total of $M_b + M_c$ is determined and the line $M_b + M_c$ is also given in figure 6.

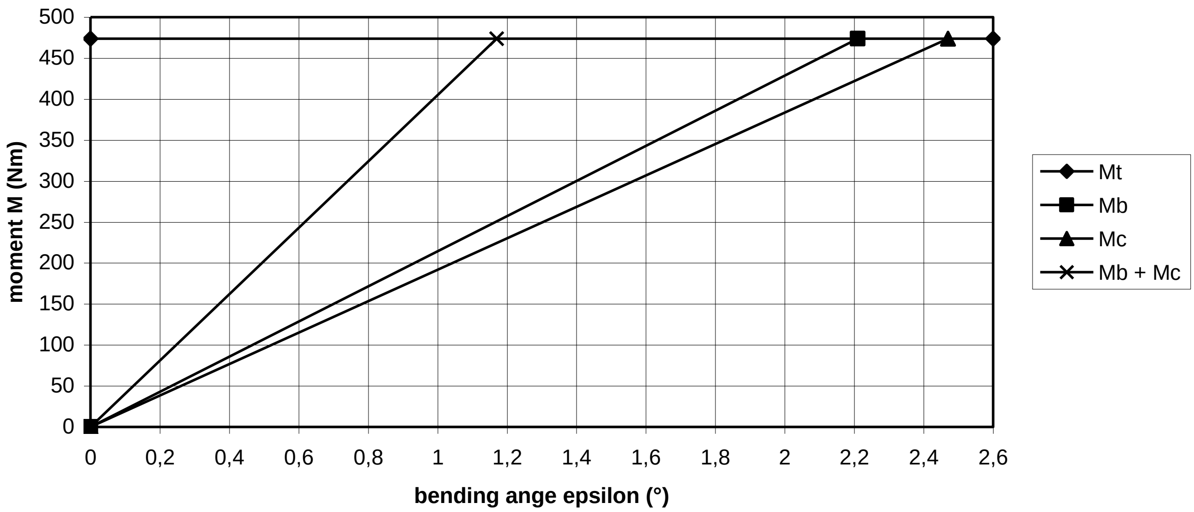

fig. 6 Path of M_t, M_b, M_c, and $M_b + M_c$ as a function of ε

The point of intersection of the line of M_t with the line of $M_b + M_c$ gives the final angle ε. In figure 6 it can be seen that ε = 1.17°. This is a factor 0.529 of the calculated angle of 2.21°. Because the bending stress is proportional to the bending angle it will also be a factor 0.529 of the calculated stress of 237 N/mm^2 resulting in a stress of about 125 N/mm^2. This is a rather low stress but up to now the gyroscopic moment, which can be rather large, has not yet been taken into account.

The gyroscopic moment is caused by simultaneously rotation of rotor and head. One can distinguish the gyroscopic moment in a blade and the gyroscopic moment which is exerted by the whole rotor on the rotor shaft and so on the head. On a rotating mass element dm at a radius r, a gyroscopic force dF is working which is maximum if the blade is vertical and zero if the blade is horizontal and which varies with $\sin\alpha$ with respect to a rotating axis frame. α is the angle with the blade axis and the horizon. So it is valid that $dF = dF_{max} * \sin\alpha$. The direction of dF depends on the direction of rotation of both axis and dF is working forwards or backwards. The moment dF * r which is exerted by this force with respect to the blade is therefore varying sinusoidal too.

However, if the moment is determined with respect to a fixed axis frame it can be proven that it varies with $dF_{max} * r \sin^2\alpha$ with respect to the horizontal x-axis and with $dF_{max} * \sin\alpha * \cos\alpha$ with respect to the vertical y-axis. For two and more bladed rotors it can be proven that the resulting moment of all mass elements around the y-axis is zero.

For a single blade and for two bladed rotors, the resulting moment of all mass elements with respect to the x-axis is varying with $\sin^2\alpha$, so just the same as for a single mass element. However, for three and more bladed rotors, the resulting moment of all mass elements with respect to the x-axis is constant. The resulting moment with respect to the x-axis for a three (or more) bladed rotor is given by the formula:

$$M_{gyr\ x\text{-}as} = I_{rot} * \Omega_{rot} * \Omega_{head} \quad \text{(Nm)} \qquad (21)$$

In this formula I_{rot} is the mass moment of inertia of the whole rotor, Ω_{rot} is the angular velocity of the rotor and Ω_{head} is the angular velocity of the head. The resulting moment is constant for a three bladed rotor because adding three $\sin^2\alpha$ functions which make an angle of 120° which each other, appear to result in a constant value. The resulting moment for a two bladed rotor fluctuates just as it is does for one blade because the moments of both blades are strengthening each other. Formula 21 is still valid for the average value of the moment but the momentary value is given by:

$$M_{gyr\ x\text{-}as} = 2 \sin^2\alpha * I_{rot} * \Omega_{rot} * \Omega_{head} \quad \text{(Nm)} \qquad (22)$$

This function is given in figure 7.

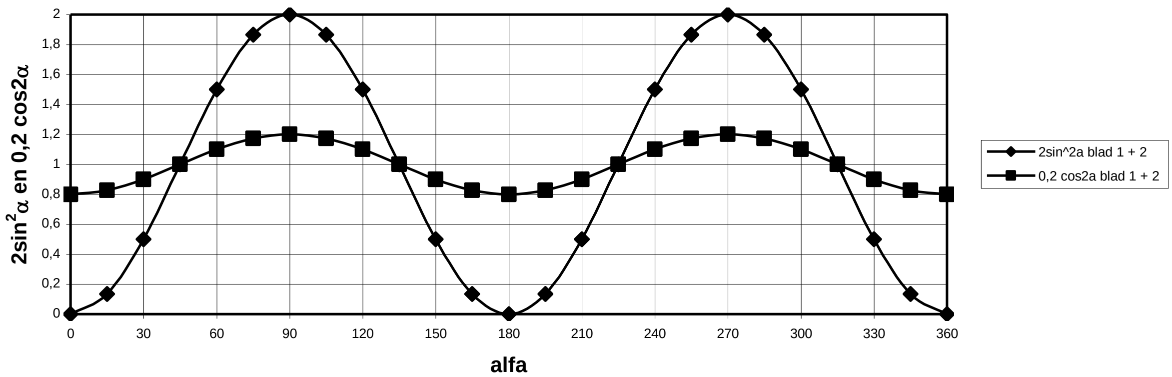

fig. 7 Path of 2 $\sin^2\alpha$ and (1 – 0.2 $\cos 2\alpha$) for a two bladed rotor

Formula 22 can also be written as:

$$M_{gyr\ x\text{-}as} = (1 - \cos 2\alpha) * I_{rot} * \Omega_{rot} * \Omega_{head} \quad \text{(Nm)} \qquad (23)$$

Because the average of $\cos 2\alpha$ is zero, the average of formula 23 is the same as formula 21.

Up to now it is assumed that the blades have an infinitive stiffness. However, in reality the blades are flexible and will bend by the fluctuations of the gyroscopic moment. Therefore the blade will not follow the curve for which formula 22 and 23 are valid. I am not able to describe this effect physically but the practical result of it is that the strong fluctuation on the $2 \sin^2\alpha$ function is rather flattened. However, the average moment is assumed to stay the same as given by formula 21. I estimate that the flattened curve can be given by:

$$M_{gyr\ x\text{-}as\ flattened} = (1 - 0.2 \cos 2\alpha) * I_{rot} * \Omega_{rot} * \Omega_{head} \quad \text{(Nm)} \qquad (24)$$

The function $(1 - 0.2 \cos 2\alpha)$ is also plotted in figure 7. This function has a maximum for $\alpha = 90°$ and for $\alpha = 270°$. The maximum is $1.2 * I_{rot} * \Omega_{rot} * \Omega_{head}$.

For the calculation of the blade strength we are not interested in the variation of the gyroscopic moment with respect to a fixed axis frame but in variation of the moment in the blade itself so with respect to a rotation axis frame for which it was explained earlier that the moment is varying sinusoidal. If the blade is vertical both axis frames coincide and the moment for both axis frames is the same. The maximum moment in one blade is then halve the value of the moment for the whole rotor.
Therefore the maximum moment in one blade is given by:

$$M_{gyr\ bl\ max} = 0.6 * I_{rot} * \Omega_{rot} * \Omega_{head} \quad \text{(Nm)} \qquad (25)$$

For a two bladed rotor, the moment of inertia of the whole rotor I_{rot} is twice the moment of inertia of one blade I_{bl}. Therefore it is valid that:

$$M_{gyr\ bl\ max} = 1.2\ I_{bl} * \Omega_{rot} * \Omega_{head} \quad \text{(Nm)} \qquad (26)$$

For the chosen blade geometry it is calculated that $I_{bl} = 24.6$ kgm^2. The maximum loaded rotational speed of the rotor can be read in figure 5 for $\lambda_d = 7$ and it is found that $n_{max} = 220$ rpm. This gives $\Omega_{rot\ max} = 23$ rad/s (because $\Omega = \pi * n / 30$).

It is not easy to determine the maximum yawing speed. The VIRYA-5 is provided with the hinged side vane safety system which has a light van blade and a large moment of inertia of the whole head around the tower axis. This is because the vane arm is a part of the head. For sudden variations in wind speed and wind direction the vane blade will therefore react very fast but the head will follow only slowly. It is assumed that the maximum angular velocity of the head can be 0.2 rad/s at very high wind speeds.
Substitution of $I_{bl} = 24.6$ kgm^2, $\Omega_{rot\ max} = 23$ rad/s en $\Omega_{head\ max} = 0.2$ rad/s in formula 26 gives: $M_{gyr\ bl\ max} = 135.8$ Nm = 135800 Nmm.
Substitution of M = 135800 Nmm, b = 120 mm and h = 10 mm in formula 14 gives $\sigma_{b\ max} = 68$ N/mm^2. This value has to be added to the bending stress of 125 N/mm^2 which was the result of the thrust because there is always a position were both moments are strengthening each other. This gives $\sigma_{b\ tot\ max} = 193$ N/mm^2. The minimum stress is 125 – 68 = 57 N/mm^2. So the stress is becoming not negative and therefore it is probably not necessary to take the load as a fatigue load.

For the strip material bright drawn strip Fe360 is chosen. For hot rolled strip the allowable stress for a load in between zero and maximum is about 190 N/mm^2 and for a fatigue load it is about 140 N/mm^2.

For bright drawn strip these values are higher because the rolling skin is removed and because the material is strengthened by the deformation. Another point is that the given stresses are for a tensile force and the allowable stresses for a bending moment are a lot higher. Assume the allowable stress for a load in between zero and maximum is about 260 N/mm^2 and for a fatigue load is 200 N/mm^2. The calculated stress is even lower than the allowable fatigue stress so the strip is strong enough.

In reality the blade is not extremely stiff and will also bend somewhat. This reduces the bending of the strip and therefore the stress is the strip will be somewhat lower.

6.2 Bending stress in the strip for a slowed down rotor

The rotational speed for a rotor which is slowed down by making short-circuit of the generator is very low. Therefore there is no compensating effect of the centrifugal moment on the moment of the thrust. However, there is also no gyroscopic moment. The safety system is also working if the rotor is slowed down but a much larger wind speed will be required to generate the same thrust as for a rotating rotor.

In chapter 6.1 it has been calculated that the maximum thrust on one blade for a rotating rotor is 279 N for $V = V_{rated} = 9.5$ m/s and $\delta = 30°$. The head turns out of the wind such at higher wind speeds, that the thrust stays almost constant above V_{rated}. A slowed down rotor will therefore also turn out of the wind by 30° if the force on one blade is 279 N. Also for a slowed down rotor the force is staying constant for higher yaw angles. However, for a slowed down rotor, the resulting force of the blade load is exerting in the middle of the blade at $r_4 = 1.4$ m because the relative wind speed is almost constant along the whole blade. The bending moment around the edge of the hub is therefore somewhat smaller. Formula 11 changes into:

$$M_{b\,t} = F_{t\,\delta\,bl} * (r_4 - r_2) \qquad \text{(Nm)} \tag{27}$$

Substitution of $F_{t\,\delta\,bl} = 279$ N, $r_4 = 1.4$ m and $r_2 = 0.05$ m in formula 27 gives $M_{b\,t} = 376.7$ Nm = 376700 Nmm. Substitution of M = 376700 Nmm, b = 120 mm and h = 10 mm in formula 14 gives $\sigma_b = 188$ N/mm^2. This is a little smaller than the calculated stress for a rotating rotor. The load is not fluctuating and therefore it is surly not necessary to use the allowable fatigue stress. The allowable stress is 260 N/mm^2 for bright drawn strip Fe360, so the strip is strong enough.

Because the strip and the blade are rather flexible, it has to be checked if a slowed down rotor can't hit the tower. In chapter 6.1 it has been calculated, for no compensation of the gyroscopic moment, that the bending angle is 2.21° for a stress of 237 N/mm^2. So for a stress of 188 N/mm^2 the bending angle will be 2.21 * 188 / 237 = 1.75°. For a rotor radius of R = 2.5 m this results in a movement at the tip of about 0.077 m. Because the blade itself will bend too, the movement will be larger and it is expected that it will be about 0.15 m. The minimum distance in between the blade tip and a tower leg is about 0.6 m if the blade is not bending. So there is no chance that the blade hits the tower for a slowed down rotor.

7 Description of the 34-pole PM-generator (see figure 8)

The number of stator poles for a 3-phase winding must be dividable by 3. The armature must have an even number of poles. If there is only a difference of one in between the number of stator poles and the number of armature poles it means that the number of stator poles must be odd. So the number of stator poles can be 3, 9, 15, 21, 27, 33, 39, 45, 51, 57, 63, 69, 75 etc. None of these values matches with the available number of stator poles for standard stator stampings of asynchronous motors.

This problem can be solved by doubling the required number of stator and armature poles. The difference in between the number of stator poles and the number of armature poles must now be two. Doubling of the number of armature poles means that the number of armature poles is always even, also if the number of stator poles is even. In this case the required number of stator poles can be 6, 12, 18, 24, 30, 36, 42, 48, 54, 60, 66, 72 etc. So the numbers 24, 36, 48, 54 and 72 match with available numbers for standard stator stampings.

If the number of armature poles is two more than the number of stator poles it is respectively 26, 38, 50, 56 and 74. If the number of armature poles is two less than the number of stator poles it is respectively 22, 34, 46, 52 and 70.

The number of armature poles must be chosen rather high for the VIRYA-5 to realise an acceptable low design wind speed. It is chosen to take 36 stator poles and 34 armature poles. The rotational speed for a 2-pole generator is 3000 rpm for a frequency of 50 Hz. So for a 34-pole generator, the frequency is 50 Hz for a rotational speed of 3000 * 2 / 34 = 176.47 rpm or for 2.94 revolutions per second. In figure 5 it can be seen that n = 176.47 rpm results in a design wind speed of about 6.6 m/s which is a reasonable choice for a moderate wind regime.

The coil configuration of the VIRYA-5 generator is chosen the same as for the VIRYA-3.3S generator. So the coil configuration over 360° will be: 3 coils U, 3 coils W, 3 coils V, 3 coils U, 3 coils W and 3 coils V. A coil is wound around one stator spoke so every coil makes use of two adjacent stator grooves. All 18 coils are identical and are lying in one cylinder shaped plane, so there are no crossing coil heads

The stator pole angle for 36 stator poles is 360° / 36 = 10°. The angle in between the coils is the double value so 360 / 18 = 20°. The armature pole angle for 34 armature poles is 360° / 34 = 10.5882°.

The angle between two north poles is the double value so 360 / 17 = 21.1765°. The difference in between the stator pole angle and the armature pole angle is 10.5882° - 10° = 0.5882°. Assume a preference position is created if an armature pole is just opposite a stator pole. This means that the number of preference positions per revolution is 360° / 0.5882 = 612. This is a very large number so it can be expected that the fluctuation of the clogging torque can be neglected. The number of preference positions can also be found by multiplying the number of armature poles and stator poles and divide it by two as 34 * 36 / 2 = 612.

Provisionally it is chosen to make use of a motor housing which makes use of a stator stamping of manufacture Kienle and Spiess. The manufacturer which uses stampings of Kienle and Spiess for their motors has not yet been chosen. Information about the geometry of these stampings is given on the website: www.kienle-spiess.de. The largest stator stamping with 36 stator grooves is used for a 6-pole motor frame size 132 M. The longest stator stamping is used for a 5.5 kW motor. This stator stamping has an inside diameter of 135 mm, an outside diameter of 200 mm and a length of 180 mm. The armature stamping has an inside diameter of 50 mm but the armature stamping is not used.

The armature diameter is chosen 134.2 mm, so the air gap in between armature and stator is 0.4 mm. The armature length is chosen the same as the stator length, so 180 mm.

Some research has been done to neodymium magnets which are standard supplied by Internet companies and which can be used for this new generator type. The company www.enesmagnets.pl supplies magnets size 30 * 10 * 8 mm with quality N40H. The current price (including VAT, excluding transport) is € 1.43 per magnet for 140 magnets.
The armature is made from a mild steel cylinder with a diameter of 134.2 mm and a length of 180 mm. In this cylinder 17, 10 mm wide and 8.3 mm deep grooves are made parallel to the axis. Six magnets are glued in each groove, so 102 magnets are needed for one armature. The magnet costs are about € 150 which seems acceptable. All magnets are glued with the north pole to the outside.

The south poles are formed by the remaining material in between the grooves. A 2.4 mm wide and 5.3 mm deep groove is made at each side of a magnet.

This groove makes that a south pole also has a width of about 10 mm and that there is no magnetic short-circuit in between the sides of the magnets. The 17 north poles are called N1 - N17. The 17 south poles are called S1 – S17. A picture of armature and stator is given in figure 9. The position of the armature in figure 9 is drawn such that north pole N1 is just opposite coil U2.

The shaft can be made using the original motor shaft. So the shaft will also get a fine teething. The armature is pressed over the teething. The sheets of the original armature stamping have a central hole with a diameter of 50 mm. As the lamination is rather soft, a little larger inside diameter has to be used for the mild steel bush of the armature, other wise the required pressing force will be too high. It is expected that this must be 50.2 mm.

For small series, the armature must be made from massive bar. However, for big series it might be possible to make the armature also from sheet lamination which is already provided with the pattern of the grooves for the magnets.

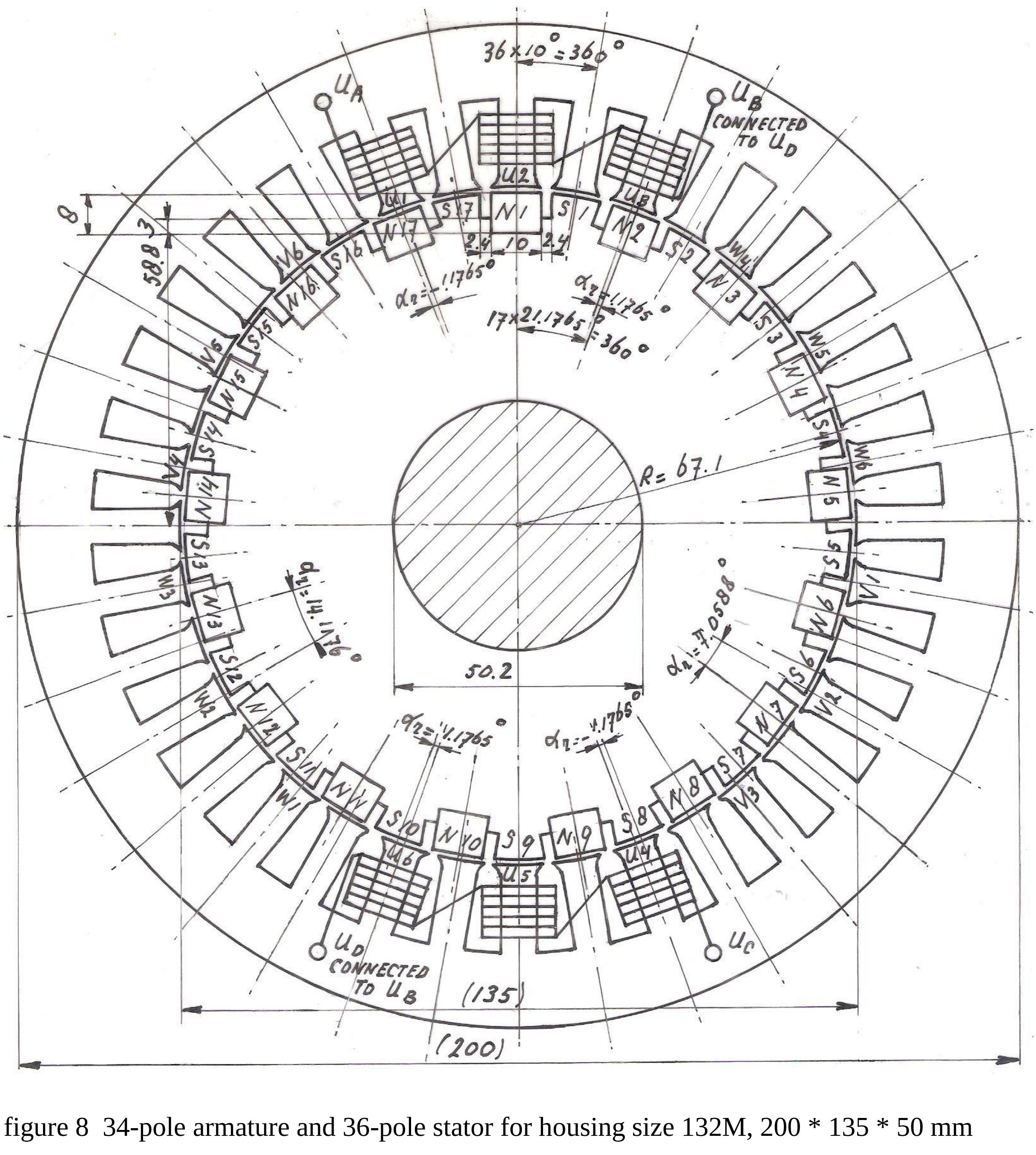

figure 8 34-pole armature and 36-pole stator for housing size 132M, 200 * 135 * 50 mm

8 Checking if a 3-phase current is generated

A 3-phase current has three phases called U, V and W. Normally the voltage U of each phase varies sinusoidal and the angle α in between the phases is 120°. The formulas for the voltage of each phase are:

$U_u = U_{max} * \sin\alpha$ (V) (28)

$U_v = U_{max} * \sin(\alpha - 120°)$ (V) (29)

$U_w = U_{max} * \sin(\alpha - 240°)$ (V) (30)

The three curves are shown in figure 9.

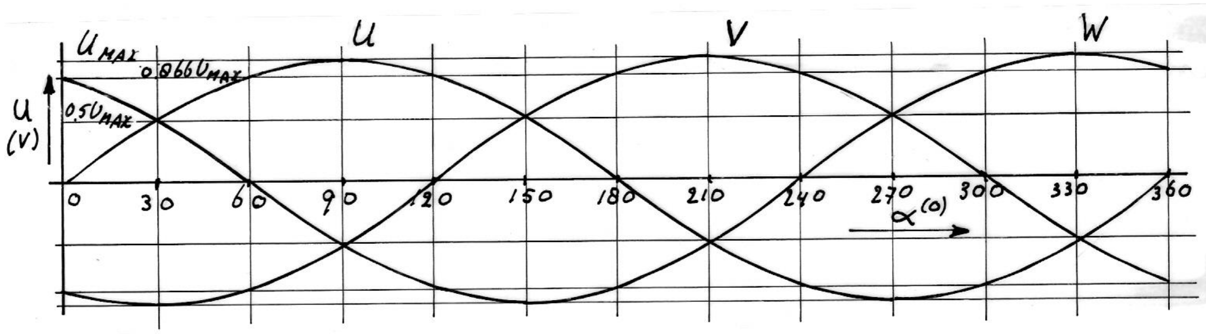

fig. 9 Three phases U, V and W

A pure sine wave is generated if a coil is rotating in a constant magnetic field because the magnetic field through the coil varies sinusoidal. If a permanent magnet is moving along a coil, the generated voltage may not be a pure sine wave, especially if the distance in between the magnets is large. But for the chosen generator configuration it is assumed that the generated voltage varies about sinusoidal.

If the rotor has two poles, the position of the rotor with respect to the stator will be the same if the rotor has rotated 360°. So the phase angle α is the same as the rotational angle α_r of the rotor. If the rotor has 34 poles this will be the case for 360 * 2 / 34 = 21.1765° rotation of the rotor. This results in the formula:

$\alpha = \alpha_r * p_r / 2$ (-) (31)

α is the phase angle, α_r is rotational angle of the rotor and p_r is the number of rotor poles.

In figure 8 it can be seen that $\alpha_r = 0°$ in between N1 and U2, that $\alpha_r = 7.0588°$ in between N7 and V2 and that $\alpha_r = 14.1176°$ in between N13 and W2. Substitution of $\alpha_r = 0°$ and $p_r = 34$ in formula 31 gives $\alpha = 0°$. Substitution of $\alpha_r = 7.0588°$ and $p_r = 34$ in formula 31 gives $\alpha = 120°$. Substitution of $\alpha_r = 14.1176°$ and $p_r = 34$ in formula 31 gives $\alpha = 240°$. The difference in between the phase angles is 120° and so a 3-phase voltage is created in between the coils U2, V2 and W2.

In figure 8 it can be seen that $\alpha_r = -1.1765°$ in between N17 and U1 and that $\alpha_r = 1.1765°$ in between N2 and U3. So this means that the voltages generated in U1 and U3 are not in phase with the voltage generated in U2.

In figure 8 it can be seen that the coils U4, U5 and U6 are not about opposite to north poles but that they are about opposite to the south poles S8, S9 and S10. This means that the generated voltage in this bundle of coils will be opposite to the voltage as generated in the bundle of coils U1, U2 and U3 if the coils have the same winding direction. It is decided to give all 18 coils the same winding direction and to connect all six coils of one phase in series. The coil ends of the bundle of the three coils U1, U2 and U3 are called U_A and U_B. The coil ends of the bundle of the three coils U4, U5 and U6 are called U_C and U_D. The first bundle of 3 coils of phase U has to be connected such to the second bundle of 3 coils, that the generated voltages in both bundles are strengthening each other. This is realised if coil end U_B is connected to U_D.

The generator winding is very simple if compared to the winding of a normal 6-pole asynchronous motor. This is because all coils have the same shape and because there are no crossing coil heads. The strength of the magnetic field flowing through a coil will be the same for each coil and the generated voltage in each coil will therefore be the same too. This is not the case for a normal 6-pole winding as some coils have a different pitch. The coil heads are very small if compared to the length of the part of the coil lying in the grooves. A minimum amount of copper will therefore be used and the winding will have a relatively low resistance resulting in a high generator efficiency.

The angles in between the coils U4 – U6 and the poles S8 – S10 are the same as the angles in between the coils U1 – U3 and the poles N17 – N2.

Coil U1 and U4. Substitution of $\alpha_r = -1.1765°$ and $p_r = 34$ in formula 31 gives $\alpha = -20°$.
Coil U3 and U6. Substitution of $\alpha_r = 1.1765°$ and $p_r = 34$ in formula 31 gives $\alpha = 20°$.

Addition of sinusoidal voltages which are out of phase but which have the same frequency results in a voltage which is also sinusoidal. The total voltage U_{tot} for the six coils U1 – U6 is given by:

$$U_{tot} = U_{max} * 2 * \{\sin(\alpha - 20°) + \sin \alpha + \sin(\alpha + 20°)\} \quad \text{(V)} \qquad (32)$$

It can be proven that this function has a maximum value for $\alpha = 90°$. Substitution of $\alpha = 90°$ in formula 32 gives:
$U_{tot\ max} = U_{max} * 2 * (\sin 70° + \sin 90° + \sin 110°) = 5.7588 * U_{max}$.

If the voltages U1 - U6 would be exactly in phase, the resulting maximum voltage would be $6 * U_{max}$. So the difference in phase angle gives a small reduction of the total voltage by a factor 5.7588 / 6 = 0.960 and therefore also a small reduction of the generated power. A factor 0.960 is certainly acceptable, so the given shift of the phase angles in between the three coils of a bundle U is allowed. The same counts for the coils V and W.

Probably a 3-phase relay in between the generator and the pump motor is needed to realise that the windmill starts unloaded. The relay is activated by the generator frequency.

In stead of use in combination with a pump motor it is possible to use the generator for high voltage battery charging. If the voltage for the standard winding is too high, the voltage is halved if the bundle of three coils of one phase is connected in parallel to the other bundle of three coils of the same phase. In this case coil end U_A has to be connected to coil end U_D and coil end U_B has to be connected to coil end U_C. For 24 V battery charging, one will need a special winding with a much lower number of turns per coil and a much larger wire thickness.

Rectification in star will give the lowest sticking torque because higher harmonic currents can’t circulate in the winding. If the generator is used as a brake, the star point should be short-circuited too because this gives a higher maximum braking torque. Because the frequency is high, it might be required to make short-circuit over a resistor to create a torque which is high enough at normal rotational speeds.

9 Calculation of the flux density in the air gap and the stator spokes

A PM-generator is normally designed such that the magnetic field in the stator is saturated or almost saturated. For this condition, the generator has its maximum torque level and this means that it can supply the maximum electrical power for a certain rotational speed. The stator can be saturated at the narrowest cross section of the spokes in between the stator slots but it can also be saturated at the bridge in between the bottom of the stator slots and the outside of the stator stamping. The stator stamping is originally designed for a 6-pole motor and for a 6-pole motor there is a large magnetic flux in the bridge. The magnetic flux in the bridge for a 34-pole PM-generator is very low because only half the flux coming out of one a stator pole is flowing through the bridge. So only the magnetic flux in the spokes is critical. The stator is about saturated if the calculated flux density in the air gap is 0.9 T or higher.

The remanence B_r (magnetic flux) in a neodymium magnet supplied by Enesmagnets with quality N40H is in between 1.26 T and 1.29 T, if the magnet is short-circuited with a mild steel arc which is not saturated. Assume it is 1.275 T. However, an air gap in the arc reduces the magnetic flux because it has a certain magnetic resistance. The resistance to a magnetic flux for the magnet itself is about the same as for air. The magnet thickness is called t_1. The magnetic resistance of the iron of the armature can probably be neglected. The magnetic resistance of the iron in the stator can’t be neglected if the stator is close to saturation. However, this is complicating the calculation a lot and so the magnetic resistance of the iron in the stator is also neglected. So the total magnetic resistance is only caused by the magnet itself and by the air gaps.

The air gap t_2 in between a south pole and the stator is 0.4 mm. The average air gap t_3 in between a north pole and the stator is somewhat larger because the magnet is flat and because the depth of a magnet groove is chosen 8.3 mm. It is assumed that $t_3 = 0.6$ mm. So the magnetic resistance is increased by a factor $(t_1 + t_2 + t_3) / t_1$ because of the two air gaps. This means that the remanence in the air gap is reduced by a factor $t_1 / (t_1 + t_2 + t_3)$. The effective remanence in the air gap $B_{r\,eff}$ is given by:

$$B_{r\,eff} = B_r * t_1 / (t_1 + t_2 + t_3) \qquad \text{(T)} \qquad (33)$$

Substitution of $B_r = 1.275$ T, $t_1 = 8$ mm, $t_2 = 0.4$ mm and $t_3 = 0.6$ mm in formula 33 results in $B_{r\,eff} = 1.133$. This is higher than 0.9 T so the stator will probably be saturated. The flux density in a spoke can be calculated if the spoke width is known. The spoke width is about 7 mm. As a magnet has a width of 10 mm, the magnetic flux is concentrated by a concentration factor k = 10 / 7 = 1.429. So the magnetic flux in a spoke can be calculated to be 1.429 * 1.133 = 1.62 T. This is higher than 1.6 T so the spokes are saturated and the maximum possible torque level will be realised.

I think that it is worth wile to make a prototype of a stator and an armature according to the geometry as given in figure 8 and chapter 7 and to test if the generator will have acceptable characteristics. The optimum number of windings per coil and the wire thickness are found by try and error. The open phase voltage must be a lot higher than 230 V for a frequency of 50 Hz as the loaded phase voltage must be about 230 V. A frequency of 50 Hz is realised for a rotational speed of 176.47 rpm.

First a half winding with three coils of one phase is laid with a thin wire with for instance 100 windings per coil. Assume one measures an open AC voltage of 100 V for f = 50 Hz. So the voltage for a whole winding with six coils would be 200 V. Assume an open voltage of 280 V at 50 Hz is needed. So the number of windings has to be increased by a factor 280 / 200 = 1.4 and becomes 140 windings per coil. Next one selects the maximum wire thickness for which 140 windings can be laid in a groove and one manufactures a complete 3-phase winding with this wire thickness. Next the generator is measured in combination with a loaded pump motor and it is investigated if the loaded phase voltage is about 230 V at 50 Hz.

At high wind speeds, the generator and the pump motor will run at higher frequencies than 50 Hz and it should be tested if this is allowed for the generator, for the pump motor and for the pump. If the maximum rotational speed is too high, a lighter vane blade has to be used to reduce the rated wind speed. It is assumed that a centrifugal pump with a 1.1 kW pump motor can be chosen so it has to be checked if this is possible for a prototype of the VIRYA-5 windmill.

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